Development of an Integral Twist-Actuated Rotor Blade for Individual Blade Control

by

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ABSTRACT

An integral twist-actuated rotor blade has been developed for helicopter individual blade control (IBC) applications. A 1/6th Mach scale CH-47D blade was designed, fabricated, and tested in hover at the MIT Hover Test Stand Facility. The design incorporates active fiber composite actuators (AFC) within the composite spar to induce a distributed twisting moment along the span. The anisotropic actuators are oriented at 45° to the blade axis to maximize the shear stresses generated. A baseline model blade was modified to obtain design targets of 4° of peak-to-peak tip twist, less than 20% added mass, matching stiffness properties with the exception of torsional stiffness, and ensuring adequate strength for forward flight.

As a part of the development, active fiber composite (AFC) were subjected to structural integrity tests in order to optimize the material system and qualify the actuator for the rotor application. A new manufacturing process was developed for producing the actuators required for the integral blade. The capabilities of the actuators were defined and shown to be suitable for the integral blade application.

Based on a passive model blade manufacturing procedure from Boeing, a manufacturing process was developed for the integral blade. The design and manufacture were validated and improved in three half-span blade section tests. With the final blade section, the twist actuation performance was demonstrated near predicted levels and damage tolerance was demonstrated in combined loading tests which simulated limit loads.

The twist actuation performance of the active blade was evaluated over a range of rotor speeds, actuation frequencies, and blade loading conditions in hover. Transfer function data were collected from input voltage to blade twist and induced vertical hub shear. Changing test conditions had little affect on the measured performance, though blade flapping mode dynamics had a significant effect. Actuator electrical system failures limited the quasi-steady twist actuation during tests to 0.8° peak-to-peak. Sectioning of the rotor blade revealed manufacturing defects as the probable cause. This project successfully demonstrated the effectiveness of integral twist actuation in Mach-scale hover testing, supporting the need for further investigation of the concept for IBC.

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NOMENCLATURE

a  2-D lift curve slope
b  span
c  blade chord length [m, in.]
c_i  Fourier cosine coefficient
c_{l_\alpha}  lift curve slope
d_{1,2}  laser displacement measurements [m]
d_{ij}  piezoelectric strain to electric field coupling coefficient [pm/V]
g  acceleration of gravity [m/sec^2]
k  accuracy relative to mean
m  mass per unit span [kg/m]
m_T  total mass [kg]
q  dynamic pressure
r  blade spanwise coordinate [m, in.]
r_{1,2}  distance between supports and center of gravity [m]
s  distance between laser sensors [m]
s_i  Fourier sine coefficient
s_{ij}  compliance [e^{-12} m^2/N]
t  thickness [mm]
z  cross-sectional geometry parameter

A  area, rotor disk area [m^2]
B  tip loss factor
C  confidence level
C_T  coefficient of thrust
E_L  longitudinal Young's modulus [GPa]
E_T  transverse Young's modulus [GPa]
E_A  axial stiffness [N]
E_{Ic}  chordwise bending stiffness [N-m^2]
E_{If}  flapwise bending stiffness [N-m^2]
F  axial load [N]
F_{X,y,z}  hub loads (lbs)
GJ  torsional stiffness [N-m^2]
I_b  blade flapping inertia about flapping hinge [kg-m^2]
I_\theta  section pitch inertia about center of gravity [kg-m^2]
L  length [m]
M  Mach number
M_{X,y,z}  hub moments (in-lbs)
M_t  induced torsional moment
N  number of blades
R  blade tip radius [m]
\( T \)  torque [N-m], thrust
\( V \)  volume

\( \alpha \)  angle of attack
\( \varepsilon \)  strain
\( \gamma \)  Lock number
\( \nu_{LT} \)  Poisson's ratio
\( \phi \)  twist angle [rad]
\( \phi' \)  twist rate [rad/m]
\( \lambda \)  induced velocity
\( \rho \)  fluid density [kg/m\(^3\)]
\( \sigma_{c,s} \)  standard deviation estimates for Fourier coefficient
\( \sigma_L \)  longitudinal tensile strength [MPa]
\( \sigma_T \)  transverse tensile strength [MPa]
\( \sigma \)  rotor solidity
\( \omega \)  natural frequency [rad/sec]

\( \Omega \)  rotor speed
Chapter 1

INTRODUCTION

1.1 Motivation

High vibration and noise levels both within the fuselage and in the surrounding environment are two notable characteristics of helicopter flight. One major source of the vibrations and noise is the unsteady aerodynamic disturbances which affect the rotating blades. The resulting blade vibrations are transferred through the hub to the helicopter cabin. The vibrations and noise result in structural fatigue, passenger fatigue, and the characteristic acoustic signature. To some extent, passive vibration absorbers and isolators can be applied to reduce vibrations; however, this methodology imparts undesirable weight penalties and insufficient vibration reduction.

Higher Harmonic Control (HHC) of rotor blade pitch has been investigated as a means of suppressing unsteady aerodynamic disturbances at their source. The pitch is controlled primarily at the rotor harmonics (N/rev, (N-1)/rev, (N+1)/rev for an N-bladed rotor). HHC has been applied using a swashplate to control the pitch of the blades [Shaw, 1985]. A more advantageous approach is Individual Blade Control (IBC), utilizing blade mounted actuators to control blade pitch. This is illustrated in Figure 1.1. IBC enables control of more degrees of freedom and at higher bandwidths. Active techniques have been found to successfully reduce vibration in comparison with passive techniques. In addition, HHC methods have been shown to effectively reduce Blade Vortex Interaction (BVI), a major source of exterior noise. Providing the blade designer with active twist distribution con-
control offers other possible advantages including increased induced power efficiency, payload, and cruise speed.

![Individual blade control of twist.](image)

**Figure 1.1** Individual blade control of twist.

The majority of current research in the field of HHC has focused on trailing edge flap actuation as the means of pitch control. The flap deflection creates an aerodynamic pitching moment which results in a twist along the blade. Although this concept appears simple, it requires complex stroke amplification mechanisms in order to develop adequate flap deflection and force. Designing for operation in the large centrifugal field of the rotor blade is the primary challenge for designers.

Another concept which offers a simpler actuation scheme is integral twist actuation. While flap concepts rely on aerodynamics to induce a twisting moment, direct twist actuation of the rotor blade uses the actuators distributed along the span of the blade to deform the relatively stiff blade structure. Active Fiber Composites (AFC) may be integrated within a composite rotor blade to induce a distributed twisting moment as depicted in Figure 1.2.
Figure 1.2 Integral twist actuation concept.

The AFC actuators are implemented in the form of active plies within the composite spar of the rotor blade. These anisotropic active plies can be oriented at a 45° angle to the blade span in order to induce shear stresses and a distributed twisting moment along the blade. The result is a twisting of the blade and a control input for IBC.

1.2 Objectives

The primary objective of this research is to develop a twist-actuated rotor blade for helicopter vibration control. By controlling the pitch of individual blades at harmonic frequencies, unsteady aerodynamic disturbances can be rejected. As a result, the vibrations transferred to the hub can be reduced. This work will demonstrate the integral twist actuation concept and its advantages over other actuation methods.

A secondary goal of this thesis is to demonstrate the effectiveness of active fiber composites as actuators in the integral twist application. This application takes advantage of the anisotropic actuation, conformability, and reliability of active fiber composites and is the first large scale application of AFC’s for structural actuation.
1.3 Background

Significant background studies have contributed to the motivation and direction of the current integral blade project. Early studies on HHC and IBC demonstrated the potential benefits for active blades. Several active blade designs have been developed for the purpose of vibration and noise control. In addition, research on active fiber composite actuators at MIT has provided a foundation for the current work.

1.3.1 Individual Blade Control

Early research into HHC investigated swashplate control of blade pitch to reduce vibrations. A demonstration of the benefits of HHC at Boeing [Shaw, 1985] has provided motivation for this work and that of many other researchers. Wind-tunnel testing on a 1/6th Mach-scale CH-47D demonstrated a 90% reduction in vibratory hub loads over a large range of flight conditions. Additional research has investigated IBC for gust alleviation, attitude stabilization, vibration alleviation, lag damping augmentation, stall flutter suppression, blade flapping stabilization, stall alleviation, and rotor performance enhancement [Ham, 1987]. Experimental work led to the conclusion that blade-mounted sensors and actuators offer the better performance than conventional swashplate-type actuators.

One example of a blade mounted actuator has shown vibration reductions of up to 80% using a trailing edge flap with open loop control inputs at rotor harmonics [Straub, 1994]. Other researchers have also investigated HHC using blade mounted actuators [Millott, 1992; Jacklin, 1994]. More recently, research has begun to focus on the development of improved blade-mounted actuators for IBC. The expansion of the field of smart structures has provided the foundation for much of this effort. A number of recently developed actuator will be described in the following section.

1.3.2 Individual Blade Control Actuators

IBC actuators are designed to create a twisting moment along a rotor blade and thus enable pitch control. The actuator must provide sufficient authority for the desired control
application while meeting the inertial and geometric constraints of the rotor blade. One way to twist a rotor blade is to use an aerodynamic control surface. Most current IBC research involves trailing edge flap actuation. Kaman first demonstrated the capability of a trailing edge flap for controlling the dynamic behavior of a rotor [Lemnios, 1972]. In operation, the trailing edge flap operates in reversal to apply an aerodynamic twisting moment to the blade. Smart materials offer a potentially mass and energy efficient source for servoflap actuation.

The first form of smart material-based actuators was the piezoelectric bimorph [Spangler, 1990]. Spangler and Hall designed a trailing edge flap actuator using a piezoelectric bimorph cantilevered from the blade spar. The deflection of the aft edge drives the flap using a set of hinges. This concept was improved upon and further investigated in a number of studies. Hall and Prechtl redesigned an optimized bimorph actuator with flexures in place of hinges to reduce friction and backlash problems [Hall, 1996]. Other researchers have implemented bimorph or "bender" concepts and demonstrated their capabilities in hover and wind-tunnel testing [Walz, 1993; Fulton, 1998].

In order to achieve greater mass efficiency and performance, several researchers have investigated stack-based flap actuators. The major focus of the design process is the development of an efficient actuation amplification mechanism to amplify the displacement output of the stack without accumulating excessive energy losses. Stack-based actuators have been designed using a simple lever arm for amplification [Spencer, 1996] or a hydraulic amplification mechanism [Giurgiutiu, 1995]. At MIT, a new high-efficiency discrete flap actuator has been developed which relies on piezoelectric stacks in an X-frame configuration [Prechtl, 1998]. A competing design at Boeing-Mesa also uses stacks in a mechanical amplification device [Straub, 1996].

Some other concepts attempt to take advantage of composite coupling mechanisms in order to actuate in torsion. In one design, an extension/twist-coupled composite tube transforms the extensional motion of a magnetostrictive stack into torsion to drive a flap
[Bothwell, 1996]. An alternative concept uses piezoelectric bimorphs in a bend/twist-coupled beam to drive a flap or a free-moving blade tip [Bernhard, 1996; Bernhard, 1997].

Twist-actuated rotor blades have also been investigated. Twist actuation concepts, such as the one described in this report, must deform the blade structure directly. In research at the University of Maryland, narrow strips of monolithic piezoelectric wafers, called Directionally Applied Piezoelectrics or DAP elements, are applied to the blade spar at ±45° [Barrett, 1990]. This concept is the most similar to the research in this project in which AFC actuators are embedded within a composite spar to induce blade twist. This concept was further investigated [Nitzsche, 1994] and later tested in a Froude-scale wind-tunnel model [Chen, 1997]. Another similar concept involves a bending/torsion coupled beam used to apply a twisting moment to the blade tip [Bernhard and Chopra, 1997].

1.3.3 Active Fiber Composites

The AFC, which will be described in further detail in Section 2.1, offers a number advantages over trailing edge flap and other active twist rotor concepts. The AFC is an anisotropic, conformable actuator which can be integrated with a passive structure. The actuators are distributed throughout the structure providing redundancy in operation. The active blade requires no articulating components, thus eliminating the need for an efficient actuation amplification device and complex flap driving mechanisms. The integral concept does not increase the profile drag of the blade unlike servoflap concepts. In addition, the integral blade can be designed to allow for both bend and twist control as well as additional spanwise degrees of freedom. A major challenge with the integral blade is to develop a design with sufficient authority to twist the relatively stiff blade structure without sacrificing the structural integrity of the system.

Several previous studies at MIT have formed the background for the current AFC research. Initial work on piezoelectric fiber composites for structural actuation developed modeling and manufacturing techniques as well as the preliminary characterization process [Hagood, 1993; Bent, 1995b]. The active fiber composite is composed of stiff elec-
trocerald fibers, a polymeric matrix, and interlaminar electrodes, as shown in Figure 1.3. Integration with composite structures and manufacturing issues were advanced in a subsequent study [Rodgers, 1995]. Performance of the AFC actuator system was then increased with the addition of an interdigitated electrode pattern [Bent, 1995]. Further development of the manufacturing and characterization techniques for the rotor application is included in the current study.

![Diagram of active fiber composite actuator with interdigitated electrodes showing longitudinal expansion with applied field in direction of polarization.](image)

**Figure 1.3** Active fiber composite actuator with interdigitated electrodes showing longitudinal expansion with applied field in direction of polarization.

### 1.4 Approach

The general approach for this project is to incorporate AFC actuators into a 1/6th Mach-scale CH-47D rotor blade. The actuators will be integrated into a modified composite spar, as illustrated in Figure 1.2, to create a distributed twisting moment. The Mach-scaled rotor system will provide the most realistic demonstration of the integral actuation at the model scale. Mach-scale testing will provide realistic torsional stiffness and material stress levels and equal tip speeds as the full scale blades.

In order to evaluate the feasibility of the integral twist actuation concept, a Rehfield single-cell composite beam model was developed [du Plessis, 1995]. AFC actuators were
selected and used in a 1/16 Froude scale model of a CH-47D. Integral twist actuation was demonstrated in a benchtop twist test. A rotor dynamic and systems level analysis of the integral actuation scheme was later performed [Derham, 1996]. This included a cost-benefit analysis and demonstrated the potential impact of the integral actuation concept.

A preliminary design has been developed using existing modeling tools at MIT and Boeing-Philadelphia. Benchtop tests of performance and reliability have been used to refine the design. Finally, hover testing in the MIT Hover Test Stand Facility has been used to demonstrate performance in the rotating environment.

The thesis roadmap in Figure 1.4 illustrates the three levels of the project: the material level, blade level, and system level. The design process involves all three levels in parallel. Requirements flow downward from the helicopter rotor system to the blade design and finally to the actuator design. The capabilities flow upward, from the actuators to the blade and back to the rotor system. Experimental studies are used to establish the capabilities of the actuators as well as the blade in characterization tests and in hover. The thesis roadmap shows each of the components as a numbered chapter heading. The following paragraphs will summarize the chapters sequentially.

Chapter 2 describes the development of the active fiber composite actuators for the integral blade application. Following an introduction to the actuator concept and component materials, the design of the actuators or blade packs and the process for their manufacture is described.

Chapter 3 details the characterization of the AFC material system for both performance and structural integrity. Preliminary characterization testing provides the basic mechanical, electrical, and electromechanically-coupled behavior and properties. Another group of tests is used to select an optimal material system for the blade application. Further structural integrity testing is used to develop a set of actuator capabilities with regard to damage tolerance and survivability. Finally, the proof testing of the blade packs for quality control is described.
Figure 1.4 Thesis roadmap including chapter numbers.

Moving up to the blade level, Chapter 4 details the design of the integral blade. The baseline model rotor system is described. A modified Rehfield model of a single-cell beam [du Plessis, 1995] is used to analyze prospective designs for comparison with target actuation, stiffness, and inertial properties. Several configurations are compared for performance including variations in actuator authority and actuator distribution. Manufacturing issues and the interface with the test stand are considered. The predicted performance and results of a stress analysis are provided.

Chapter 5 details the manufacture of the integral blade. Model blade fabrication techniques developed at Boeing-Philadelphia are modified for the incorporation of the active plies and power distribution system. The process is subdivided into the spar construction,
power leads attachment, and finally a fairing cure. Preparations for hover testing are also described.

Chapter 6 describes the testing of representative blade sections. In order to evaluate the selected design and validate model predictions and manufacturing techniques, short spar sections are manufactured and tested. Actuation tests will measure actuator performance and reliability. Stiffness tests will verify model predictions and confirm manufacturing practices. Combined tension/torsion load testing will also be used to evaluate the damage tolerance of the integral blade.

Chapter 7 presents the results of the major objective of this study: the hover testing of an active blade. Following a benchtop evaluation of the actuation system, the blade will be tested up to full CH-47D tip speeds (1336 RPM). Blade-mounted strain gages and a 6-axis load cell on the hub will be used to measure the performance of the active blade over a range of actuation frequencies, RPM, and blade loading conditions. The data are analyzed and presented as transfer functions (frequency response) from actuator input voltage to blade twist and induced vertical hub shear.

Chapter 8 analyzes actuator electrical failures which occurred in the integral blade during testing. The analysis includes the sectioning of the blade and visual inspection of the cross sections. Blade manufacturing difficulties are addressed, including the correlation between actuator failures and blade defects.

Chapter 9 concludes the thesis with a summary of accomplishments. Conclusions are drawn and recommendations are made for future work. Appendices are also included with design drawings and additional details for the manufacture of the integral blade, design drawings of the hover test stand, and the input files and code for the integral actuation model.
Chapter 2

ACTIVE FIBER COMPOSITE DEVELOPMENT

Active fiber composites have been developed for structural actuation. In particular, the AFC material system has been optimized for the integral blade application. The following subsections will provide an introduction to the material system, the manufacturing process, and the configurations used for the characterization testing and the blade.

2.1 Introduction to Active Fiber Composites

Active fiber composites have been developed for structural actuation and sensing applications. The composite geometry of the AFC enables greater performance and wider applicability than other conventional actuators. The AFC actuators are fundamental to the integral blade concept in which the actuators are completely integrated within the composite blade and can induce the stresses required for twist actuation.

One of the primary advantage of the AFC is that the actuation is anisotropic. Anisotropic actuation is necessary for inducing the shear stresses required to directly twist a structure. In a monolithic ceramic, the actuation is transversely isotropic, i.e. the actuation is equal in the plane of the structure. Figure 2.1 compares the modes of operation for three types of piezoceramic actuators. The monolithic wafer employs the transverse piezoelectric effect (in-plane contraction for voltage applied in polarization direction), while the layered stack and AFC take advantage of the primary piezoelectric effect (longitudinal expansion). Monolithic ceramics can be made to behave anisotropically using either interdigital elec-
trodes [Hagood, 1993b] or directional applications [Barrett, 1990]. In the former, the interdigital pattern results in an in-plane electrical field and therefore takes advantage of the primary piezoelectric effect. However, stress concentrations at the edges of the electrodes have limited the application of this concept. The directional application concept takes advantage of shear lag effects in bonding narrow strips of piezoceramic material onto a structure. The result is a preferential lengthwise expansion.

![Diagram](image)

**Figure 2.1** Modes of operation for monolithic wafer, stack, and AFC showing exaggerated deformation (dashed lines) with applied electric field in polarization direction.

AFC's are inherently anisotropic because of their unidirectional fiber structure. With conventional electrodes orienting the electric field through the thickness, the piezoceramic material contracts (transverse actuation with electric field aligned with polarization). Since the active material is only continuous along one dimension (a 1-3 composite structure), the net effect is much greater along the fiber axis of the composite. With the interdigital electrode pattern, the electric field is oriented along the fiber axis and thus further enhances both the anisotropy and actuation performance as shown in Figure 2.2 [Bent, 1995]. Moving lengthwise along a fiber, both the polarization and the electric field alternate orientation in between successive electrode lines or fingers of opposing polarity. The
net effect is a positive strain in each segment with an applied electric field in the polarization direction. This is taking advantage of the primary piezoelectric effect. The free strain performance of the AFC with interdigitated electrodes is about the same magnitude as the transverse performance of a conventionally electroded monolithic piezoceramic.

![Diagram](image)

**Figure 2.2** FEM of one quarter fiber in AFC showing E-field distribution and equipotential lines (one electrode line shown on top left).

A second major advantage of the AFC is the conformability. Conventional electroceramics are brittle and must be specifically designed and fabricated to match the required curvature for an application. The AFC can be bonded to or embedded within a non-planar structure as in the rotor blade.

The structural integrity of the AFC is also significantly greater than that of monolithic ceramic actuators. The composite construction allows for load sharing between fibers through the matrix. When embedded in a structure, the structure itself provides a load transfer path around any cracks in the ceramic fibers thus enhancing the integrity of the active structure. The risk of damage during handling and manufacturing is also greatly reduced with AFC’s. The structural integrity of the AFC’s has been extensively investigated through a number of characterization tests which will be described in the next chap-
ter. It will be shown that the AFC's enable the structural integrity of the active blade to be maintained at the level required for flight.

Two disadvantages of AFC's in their current implementation are the cost and high driving voltages required for operation. The cost is expected to decrease as fiber production volumes increase and new large scale manufacturing techniques are developed. The higher voltages increase the required dielectric breakdown strength of the component materials and increase the required insulation around the actuators and the power distribution system. The required voltage (about 3 times that of conventional piezoceramics) is also expected to be reduced as advances are made in the AFC technology. Possible improvements under development are matrix additives and scaling down the dimensions of the AFC system.

Implementation of the AFC concept has required the development of a manufacturing process as well as characterization techniques. The following sections will describe the specialization and optimization of manufacturing process for the integral blade application. The integral blade concept incorporates active plies within the composite laminate of the rotor blade. These active plies are composed of AFC actuator material divided into "packs" for manufacturing and redundancy purposes. The characterization and selection of an optimal material system for the blade packs will be described in Section 3.3.

2.2 Critical Manufacturing Issues

A manufacturing process has been developed to produce the AFC actuators or "packs" for the integral blade project. This process builds on previous techniques developed at MIT [Rodgers, 1995, Bent, 1995]. The scale of the manufacturing process was modified to accommodate increases in both pack size and pack production rate as well as to improve the quality of the product. The basic concept of pack production is to assemble the component fibers, matrix, and electrodes to form the AFC.
Several requirements constrain the manufacturing process for the AFC’s. The major constraints are:

- aligned fibers
- compaction
- void content
- electrode pattern registration

The manufacturing process must address each of these constraints. The following paragraphs explain each in detail.

**Aligned fibers.** The active fibers must be aligned in a single layer such that the longitudinal axis of all fibers are roughly parallel and aligned with respect to the electrode pattern. The single layer is necessary to ensure close proximity of the electrodes to all of the fibers, which leads to the next constraint.

**Compaction.** The resulting composite must be well compacted such that the electrode layers are located in close proximity to the electroceramic fibers. This will minimize electric potential losses in the typically low dielectric matrix material. Any gap between the conductors and the fibers necessitate the application of higher voltages in order to achieve the required electric field levels in the fiber for actuation. Otherwise, the performance of the AFC actuator will be significantly degraded. Higher field levels also increase the risk of failure due to dielectric breakdown in the matrix. Acceptable compaction will result in a gap of less than 5 microns between the fibers and electrode layers.

**Void content.** Another issue in the dielectric breakdown strength of the matrix is the void content. The void content of the polymeric matrix must be minimized in order to reduce the risk of dielectric breakdown in the composite during actuation. Since the relative dielectric of the void is much less than that of the matrix, the voids create electric field concentrations with the application of a driving voltage. This can result in dielectric breakdown failure of the composite.
Electrode pattern registration. The interdigitated electrode consists of a matched pair of thin polymer sheets with a conductive pattern on one side. The electrode patterns must be registered with respect to each other and the alignment axis of the fibers. Any misalignment results in performance losses and increases the risk of dielectric breakdown failure in the AFC actuator.

The scaling up of the AFC pack size increases the difficulty in meeting each of the constraints on the process. The larger size (roughly 10 square inches compared to 1 square inch) is necessary for the rotor blade application. A larger area requires longer fibers to be aligned without overlap. In addition, the larger area increases the difficulty in getting uniform compaction and a void-free matrix. Another difficulty is the stretching and distortion of the electrode pattern across the packs.

2.3 Component Materials

The constraints on the manufacturing process leads to significant quality control requirements for the component materials. The following subsections describe the fibers, matrix, and electrodes used in the AFC packs and introduce some of the quality issues associated with each.

2.3.1 Fibers

The active fibers of the AFC provide the actuation performance and the fibrous structure of the composite. The AFC has been developed around piezoceramic fibers with a diameter of 130 µm and a maximum length of 13 cm. The capabilities of fiber manufacturers limit the range of possible fiber dimensions. The majority of fibers used in AFC’s have been extruded with a circular cross section. The composition of the fibers manufactured have been PZT’s (Lead Zirconate Titanates) such as PZT 5A, PZT 5H, and PZT 4S. These compositions were selected to optimize the performance of the AFC for specific applica-

1. Cera Nova Corp., Franklin, MA
tions. A photomicrograph of a typical AFC cross section is shown in Figure 2.3. The circular fibers are in close proximity to the electrode layer above and below. The matrix material, which was doped with PZT powder in this case, surrounds the fibers. Properties for fiber compositions that were considered for the integral blade are listed in Table 2.1. The entire material selection process for the integral blade project will be discussed further in Chapter 3.

**TABLE 2.1** Properties of Several Piezoceramics [Berlincourt, 1971].

<table>
<thead>
<tr>
<th>Property</th>
<th>PZT-5H (x10^{-12} m^2/N)</th>
<th>PZT-5A (x10^{-12} m^2/N)</th>
<th>PZT-4 (x10^{-12} m^2/N)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$s_{11}$</td>
<td>16.5</td>
<td>16.4</td>
<td>12.3</td>
</tr>
<tr>
<td>$s_{12}$</td>
<td>-4.78</td>
<td>-5.74</td>
<td>-4.05</td>
</tr>
<tr>
<td>$s_{13}$</td>
<td>-8.45</td>
<td>-7.22</td>
<td>-5.31</td>
</tr>
<tr>
<td>$s_{33}$</td>
<td>20.7</td>
<td>18.8</td>
<td>15.5</td>
</tr>
<tr>
<td>$s_{44}$</td>
<td>43.5</td>
<td>47.5</td>
<td>39.0</td>
</tr>
<tr>
<td>$s_{66}$</td>
<td>42.6</td>
<td>44.3</td>
<td>32.7</td>
</tr>
<tr>
<td>$\varepsilon^{r}<em>{33}/\varepsilon</em>{0}$</td>
<td>3400</td>
<td>1700</td>
<td>1300</td>
</tr>
<tr>
<td>$d_{31}$ (pm/V)</td>
<td>-274</td>
<td>-171</td>
<td>-123</td>
</tr>
<tr>
<td>$d_{33}$ (pm/V)</td>
<td>593</td>
<td>374</td>
<td>289</td>
</tr>
</tbody>
</table>

In order to optimize the quality of the active fibers and ultimately the performance of the AFC's, the active fibers have been characterized extensively. The actuation performance of the fibers has been measured not only in AFC testing, but also through testing of fiber bundles and single fibers. This investigation enabled the correlation of AFC performance with fiber performance. Fiber data can also be compared with bulk ceramic data. The tests on the fibers include: maximum free strain measured in AMSL, fiber dielectric and polarization measured at the Penn State Materials Research Lab (MRL), and fiber surface SEM's and X-ray Diffraction compositional analysis at MRL and the MIT Ceramics Processing Research Lab (CPRL).
A typical fiber surface SEM\(^1\) is shown in Figure 2.4. One major quality concern for the piezo fibers is porosity. Porosity may reduce fiber strain performance and strength. Another factor evaluated was the ceramic grain structure. The grain structure can be used to link steps in the fiber manufacturing process with factors known to correlate with performance. Fiber dielectric measurements can be used as a check of composition and porosity.

\begin{figure}[h]
\centering
\includegraphics[width=\textwidth]{figure2_3}
\caption{AFC cross section photomicrograph.} \label{fig:2.3}
\end{figure}

\begin{figure}[h]
\centering
\includegraphics[width=\textwidth]{figure2_4}
\caption{SEM of typical fiber surface.} \label{fig:2.4}
\end{figure}

In comparison with the microscopic fiber studies, macroscopic fiber behavior studies provide a more direct measure of performance. Polarization tests measure the piezoelectric properties of the fibers. Free strain tests directly evaluate the actuation capability of the fibers. Some typical data for the fibers and comparisons with other types of fibers can be found in a previous publication [Bent, 1997]. In general, the extruded circular fibers were found to have somewhat reduced strain performance in comparison with bulk ceramic test samples. The grain structure in these fibers was also less dense and the ceramic had greater porosity than the bulk material.

In an attempt to implement a fairly simple quality control standard for the active fibers to be used in the integral blade actuators, random samples of fibers were studied from each of

---

1. Courtesy of Dr. Fan Chu, Penn State University, Materials Research Lab
several fiber batches produced. Approximately 6 fibers were tested from each batch of 1000 fibers produced. The fiber dielectric was measured for each sample. In addition, the polarization was measured for some samples and an SEM analysis was performed. It was later determined through correlation with AFC data that the correlation between fiber dielectric and actuator performance was low. It is not known whether the poor correlation resulted from inaccurate fiber dielectric measurements or a true lack of correlation between the two metrics.

Additional quality checks on the fibers include measurements of the variation in fiber diameter and visual inspection for discoloration and curvature. Variations in fiber diameter reduce the uniformity of compaction in the composite. The effective electric field reaching the smaller fibers is significantly reduced. Discoloration can be an indication of compositional inaccuracy or contamination. Curvature in the fibers creates manufacturing difficulties such as overlapping fibers and a reduction in the attainable volume fraction.

2.3.2 Matrix

The current baseline matrix for the AFC's consists of Epon 9405 resin and Epi-Cure 9470 curing agent¹. This particular resin was selected because it is a structural adhesive with low viscosity, long pot life, a cure temperature compatible with typical structural composites, and a B-staging capability. This is a composite filament winding resin composed of a modified bisphenol-A epoxy resin with an amine curative. The low viscosity and long pot life allow for the addition of matrix additives for improving dielectric properties and potentially improved electric field distribution to the active fibers. The B-staging capability allows for the possibility of an AFC prepreg manufacturing process. The term prepreg refers to a composite in which typically is produced in a tape form with an uncured resin applied to the fibers. Table 2.2 provides basic properties of the resin with a mix ratio of 100:37 by weight. Additional details on the resin selection can be found in a previous publication [Rodgers, 1995b].

¹ Shell Chemical Co., Short Hills, NJ
TABLE 2.2  Resin Properties\textsuperscript{a} [Shell, 1994]

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>mix ratio (by wt.)</td>
<td>100:37</td>
</tr>
<tr>
<td>viscosity (cps)</td>
<td>850</td>
</tr>
<tr>
<td>pot life (hrs.)</td>
<td>41</td>
</tr>
<tr>
<td>gel time (10x $\mu$)</td>
<td>1 hr @ 250°F</td>
</tr>
<tr>
<td>$T_g$ (^\circ\text{C})\textsuperscript{b}</td>
<td>147</td>
</tr>
<tr>
<td>density (kg/m(^3))</td>
<td>1158</td>
</tr>
<tr>
<td>relative dielectric\textsuperscript{c}</td>
<td>5</td>
</tr>
<tr>
<td>E (GPa)</td>
<td>2.83</td>
</tr>
<tr>
<td>$\sigma_{\text{ult}}$ (MPa)</td>
<td>77.2</td>
</tr>
<tr>
<td>elongation</td>
<td>7.5%</td>
</tr>
</tbody>
</table>

\textsuperscript{a} 250°F cure for 4 hrs unless otherwise specified.
\textsuperscript{b} Cure: 1 hr @ 176°F, 1 hr @ 250°F, 1 hr @ 300°F, 4 hrs @ 350°F.
\textsuperscript{c} MIT Data

Because of the low relative dielectric of the epoxy and high relative dielectric of the fibers, a significant portion of the electric field applied at the electrode conductors is lost in the low dielectric matrix. As a result, the electric field applied to the piezoelectric fibers is reduced along with performance. High dielectric and conductive fillers have been investigated as a means of increasing the effective dielectric of a hybrid matrix [Bent, 1997].

A preliminary resin system was selected for initial characterization testing which consisted of 60 percent by weight PZT5H powder\textsuperscript{1} and 5 weight percent dispersing agent\textsuperscript{2}. In addition, an air release agent\textsuperscript{3} is added (2 weight percent). Variations to this baseline matrix for the blade actuators will be considered in the material selection section of Chapter 3. Other variations to the matrix including thermoplastics and magnetic particle doping are under investigation for future improvements.

---

1. Morgan Matroc, Inc., Bedford, OH  
2. ICI Surfactants, KD-2, Wilmington, DE  
3. BYK-Chemie A530, Wallingford, CT
2.3.3 Electrodes

The interlaminar electrodes provide two primary functions for the AFC: application of the electric field to the active composite and insulation from the surrounding structure. Kapton polyimid film\(^1\) was selected as a base for the electrodes because of its strength, resistance to high temperature and chemicals, and high dielectric strength [Adrova, 1970; DuPont, 1997]. Some typical properties of the kapton are presented in Table 2.3. The baseline electrodes consist of 12.5 μm kapton with 2000-3000 Å of copper on one side\(^2\). The copper thickness later increased to 3 μm for the blade actuators\(^3\). A photolithography process is used to obtain the interdigital electrode pattern. This processing was accomplished at MIT for smaller test articles. Outside vendors have been used for the blade actuators\(^4\).\(^5\) The processing must be controlled to ensure that the electrode pattern has the proper line widths, smooth edges, continuity, and is free of distortion which could result in poor registration between the upper and lower electrodes in the AFC. A finite element analysis was used to determine the optimal line widths (0.175 mm) and spacing (1.125 mm) for the electrode pattern [Bent, 1995].

<table>
<thead>
<tr>
<th>TABLE 2.3  Kapton HN Film Properties [DuPont, 1997]</th>
</tr>
</thead>
<tbody>
<tr>
<td>density (kg/m(^3))</td>
</tr>
<tr>
<td>Tg (°C)</td>
</tr>
<tr>
<td>E (GPa)</td>
</tr>
<tr>
<td>(\sigma_{\text{ult}}) (MPa)</td>
</tr>
<tr>
<td>elongation</td>
</tr>
<tr>
<td>relative dielectric</td>
</tr>
<tr>
<td>dielec. strength(^a) (kV/mm)</td>
</tr>
<tr>
<td>volume res. (Ω-cm)</td>
</tr>
</tbody>
</table>

\(\text{a. 25 μm film thickness}\)

---

1. DuPont Kapton Polyimid Film, Wilmington, DE
2. Southwall Technologies, copper/Kapton, Palo Alto, CA
3. Gould Electronics, Eastlake, OH
4. Elume, Inc., Simi Valley, CA
5. Advanced Circuit Technology, Nashua, NH
A second processing technique has also been investigated for producing interdigitated electrodes on a kapton substrate. The technique is a screen printing process with a conductive ink. The screen printing technique is currently under investigation as an alternative to the photolithography with lower cost and larger area capabilities. Screen printed samples have been produced at MIT and have been obtained from an outside vendor\textsuperscript{1} using a special conductive ink\textsuperscript{2}. Data from composites with both electrode types will be compared in the material selection discussion (Section 3.3).

2.4 Blade Pack Geometry

The geometry of the blade packs is presented here to better quantify the scale and exact dimensions of the manufacturing apparatus and procedure described in Section 2.5. The design of the blade packs will be described as part of the blade design chapter.

The AFC characterization test articles that have been manufactured in support of initial characterization studies were 25.4 mm by 25.4 mm in size with interdigital electrodes. Later characterization test articles were designed for tensile testing. A maximum available fiber length of 3.5 inches limited the size of the test articles to 12.8 mm by 89.0 mm, with the outer 12.8 mm on each end reserved for loading tabs. The blade packs are significantly larger with dimensions of 48.8 mm by 145.0 mm. In addition, the fibers are oriented at 45° to the longitudinal axis of the pack, parallel to the span of the blade. The pack dimensions and electrode pattern are shown in Figure 2.5.

The fibers have a length of 65 mm and are oriented perpendicular to the lines of the interdigital electrode pattern. Additional details of the packs will be provided in the manufacturing procedure description.

---

1. Eastprint, Inc., N. Andover, MA
2. CB115, DuPont Electronic Materials, Research Triangle Park, NC
Figure 2.5 Pack dimensions and electrode pattern.
2.5 Manufacture

The manufacturing techniques described in this section have been developed as an improvement to earlier techniques as well as a specialization for the integral blade application. The goals for the process improvement were increased performance, reliability, and manufacturing time/cost.

The first manufacturing procedure developed for AFC's [Hagood, 1993] provided a starting point for developing the first characterization test articles. The test articles also provided data for correlation with microelectromechanical models of the fiber/matrix system. Later, the process was refined and scaled up to demonstrate anisotropic actuation in a composite plate [Rodgers, 1995b]. Around the same time, the process was also being refined to incorporate the interdigital electrodes [Bent, 1995]. These previous studies provided the basis for the manufacturing process developed for the integral blade.

2.5.1 Concept and Apparatus

The basic procedure used in the manufacture of AFC's consists of a few basic steps. First a mold is constructed on the lower electrode layer which is attached to a cure plate. Next the fibers are positioned within this mold. Epoxy is then added to fill between the fibers. The upper electrode layer is then positioned. Finally, pressure is applied through a top plate and the temperature is elevated for the cure. These steps form the foundation for the manufacturing process developed for the blade packs.

The manufacturing process developed for the blade packs achieves the requirements described in Section 2.2 using a hot press and vacuum combination. The hot press provides the heat required to cure the epoxy matrix. The hot press also provides the compressive forces required for adequate compaction of the composite. The vacuum is the essential component necessary to prevent the trapping of air pockets or bubbles within the matrix. Air bubbles cannot escape during the cure because of the surrounding electrode
**Figure 2.6** Hot press with vacuum for blade pack manufacture.

layers, which are currently impermeable. The vacuum must be applied prior to and during the positioning of the top electrode onto the fiber/matrix layer.

A laboratory scale manufacturing rig has been fabricated for the simultaneous production of 4 blade packs. A diagram of one quadrant of the assembly is shown in Figure 2.6. The
base of the assembly is a 0.5" aluminum lower plate which spans all 4 quadrants (4 packs). Four separate 0.5" aluminum upper plates mate with the lower plate using alignment pins. The upper and lower plates have alignment marks around the pack perimeter for aligning the electrodes. The upper plate also has a machined 0.020" relief in the shape of the pack perimeter (parallelogram) for applying cure pressure only within the pack dimensions. This allows for extra tape, etc. to be placed around the perimeter without affecting compaction. Four centrally located bolts join each upper plate to Unistrut\textsuperscript{1} crossbeams (Section A-A). During the vacuum phase of the process, these crossbeams support the upper plates above the composite which is assembled on the lower plate. The lower support nuts on the threaded rods are used to support the crossbeams (Section B-B). The vacuum bag will be sealed around the perimeter of the upper plate and will extend to outward on all sides to seal to the lower plate. The threaded holes in the upper plate do not go through to the mating surface so that the vacuum can be applied to the volume between the upper and lower plate. During the cure, downward pressure is applied using the upper clamping nuts to apply a force to the crossbeams. The pressure of the crossbeams is distributed to the upper plates through an aluminum and rubber load spreading layer. A vacuum port is located on the lower plate as well as strip heaters mounted on the underside (not pictured). A feedback control system\textsuperscript{2} is used to adjust the plate temperature using a thermocouple mounted on the surface of the lower plate. More precise drawings of the manufacturing apparatus are provided in an appendix. The details of the procedure will be discussed in the following sections.

\subsection{2.5.2 Batch Processing}

In order to improve the efficiency of the blade pack manufacturing process, batch processing steps were incorporated where possible. A large number of packs were required for the integral blade project. Approximately 40 packs were made during the process development stage of the project. Next, an additional 80 packs were manufactured for the

\begin{itemize}
\item[1.] Unistrut P6000 steel beam, Wayne, MI
\item[2.] Chromalox Wiegand Industrial Division, Emerson Electric, Pittsburgh, PA
\end{itemize}
blade. Those with the highest quality were reserved for the final integral blade while the remainder were used in blade section test articles and structural integrity test coupons.

The batch processing of various steps in the production process allows for faster manufacturing and efficient division of labor. Certain steps in the process were performed in advance for several batches of packs (4 packs per batch). These steps include the preparation of the fibers (Section 2.5.3), preparation of the electrodes (Section 2.5.4), and preparation of the cure materials such as GNPT\(^1\) (teflon) and vacuum bag. Having these items prepared in advance reduces the total cycle time for the assembly and cure steps for each batch (Section 2.5.5, Section 2.5.6, and Section 2.5.7). In addition, a second person can accomplish the preliminary batch processing steps in parallel with the main manufacturing cycle.

Another batch processing step incorporated into the process is the pack preparation (Section 2.5.8). This process can be performed in parallel with the main manufacturing cycle. In this step, several batches of packs are trimmed, poled, and characterized in preparation for operation.

For each pack that is manufactured, a summary data sheet is prepared. This sheet includes a checklist that ensures that all manufacturing steps have been completed for the pack. Entries are made for the results of various characterization tests as well. The summary data sheets for all the packs can then be used as a complete record for each pack during the selection process.

### 2.5.3 Fiber Preparation

The fibers are received from the manufacturer in batches of approximately 1000 each. The length of the fibers is 13 cm. The manufacturer provides a mean and standard deviation of a random sampling of fibers. The fibers are cut to a length of 65 mm. Fibers having any visible discoloration or curvature are discarded. Next, the fibers are divided into groups

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1. Guaranteed non-porous teflon, American Durafilm, Holliston, MA
by weight for each pack. With nominal 128 \( \mu \text{m} \) diameter fibers, 3.88 g of fibers are required to achieve the desired volume fraction of 0.68 (line fraction of 0.76). This is the empirically determined maximum volume fraction that can be manufactured without excessive difficulty with overlapping fibers. A higher volume fraction enables higher induced stress capability per uniform cross-section area. Model predictions and previous characterization studies have also supported this decision [Bent, 1997].

Each group of fibers is then rinsed in acetone to remove any surface contaminants. The fibers are dried at 100°C for 20 minutes to ensure the evaporation of all solvents. Finally, each group is stored in a separate box marked with its original batch number. This enables correlation between pack performance and fiber batch.

2.5.4 Electrode Preparation

Each of the copper/kapton electrode patterns received from the manufacturer is visually inspected at 10x magnification using a stereo microscope. Typical flaws include overetching which results in gaps in the copper lines, underetching which results in excess copper on the edges of lines or between lines, a halo effect in which excess copper surrounds the lines of the pattern, and structural flaws such as tears or holes in the kapton. Open gaps in the pattern can result in arcing across the gap upon application of the poling voltage to the packs. Sharp edges on the pattern concentrate the electric field, increasing the risk of dielectric breakdown in the surround matrix. Any excess copper between lines of the pattern can result in a low resistance path between lines of opposite polarity which can result in dielectric breakdown. Acceptable electrodes are prepared for the manufacturing process while flawed patterns are discarded.

In order to make a more robust contact point to connect each of the packs to the power leads within the blade, the electrode flaps or tabs are reinforced with a 3 mm wide, 25 mm long, and 25 \( \mu \text{m} \) thick copper strip. A standard solder\(^1\) is used to join the copper strip to

\(^1\) Tin/Lead Rosin Core Solder, 361°F liquidus, Micromeasurements, Raleigh, NC
the copper of the electrode such that one end of the strip is flush with the inner edge of the aft electrode rail (refer to Figure 2.5). The maximum thickness of the joint is limited to 75 μm to avoid any detrimental effect on the compaction of the packs during the cure. Finally, the electrodes are cleaned with acetone to remove any contaminants.

2.5.5 Assembly

The assembly of the packs is described in conjunction with a series of photographs showing the stages of the process. In addition, an assembly drawing is provided in Figure 2.7. The assembly process is performed in a clean area protected from general lab traffic and with a filtered air intake. The upper and lower plates as well as the surrounding lab bench area are cleaned with acetone before each production cycle. The assembly table, lower plate, four upper plates, vacuum pump, and temperature controller are pictured in Figure 2.8. A layer of Guaranteed Non-Porous Teflon (GNPT) is positioned on each of the mating surfaces of the upper and lower plates as shown in Figure 2.9. The GNPT sheets are prepared in advance using a template to size and cut holes for viewing the electrode alignment marks on the plates. The sheets are sized to match the dimensions of the upper plates. The sheets are taped in place around the perimeter using flash tape\(^1\) to eliminate any wrinkles. On the upper plates, the tape is positioned around the edges of the aluminum so that no tape contacts the mating surface. This is essential for releasing the upper plate from the pack after the cure.

Next, the upper and lower electrodes are taped to the upper and lower plates, respectively. Several narrow strips of flash tape are used to pin the edges of the electrode so that the alignment marks etched onto the electrode match with the corresponding marks on the plates. Any stretching of the electrode can result in improper registration in the cured composite. Once the electrode is position, additional flash tape is used around the perimeter of the kapton. Figure 2.10 shows the electrodes taped in position.

\(^1\) Mylar flash tape, Richmond Aircraft Products, Norwalk, CA
In order to reduce the overall mass of the packs and to maintain structural integrity, the non-active areas of the packs (where no electric field is applied) are filled with E-glass fabric prepreg\(^1\). The fiber length of 65 mm brings the ends of the fibers to the inner edge of the copper electrode rails. A strip of 0.114 mm thick and 5 mm wide ±45° E-glass is placed along the forward and aft edges of the lower electrode flush with the inner edge of the electrode rails. Cutouts are made around the two copper strips soldered to the ele-

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1. Hexcel E120-F155 fabric, Pleasanton, CA
trode flaps. Two additional strips are placed along the 45° ends of the electrode flush with the ends of the electrode lines or fingers in the pattern. These strips are also parallel to the fibers which will be added later. Once the E-glass strips are in position, a small piece of GNPT is positioned over the two electrode flaps on the lower pattern to aid in the pack preparation step. Figure 2.11 shows the E-glass in position on the lower electrode with the GNPT covering the copper strip. Then strips of 12.7 mm wide kapton tape\textsuperscript{1} are used around the perimeter of the E-glass to hold it in place. The kapton tape creates a mold for containing the matrix. Therefore, the kapton tape should provide a good seal to the electrode layer outside of the E-glass perimeter. The inner edge of the kapton tape should be outside the perimeter of the electrode rails so that none of the tape is contained in the final trimmed pack. The last step before the fibers are added is to cut a small slit in the upper and lower electrode outside the mold perimeter to ensure that any trapped air can escape when the vacuum is applied.

\textsuperscript{1} K-250, Furon, CHR, New Haven, CT
The next step is to add the fibers. A temporary strip of GNPT is placed over the exposed E-glass prepreg so that the fibers to not stick to the tacky prepreg while the fibers are being positioned. The fibers are placed into the mold in groups of roughly 100 as shown in Figure 2.12. In this stage, the fibers are roughly distributed and aligned by hand. The fibers will be positioned more precisely after the matrix is added. Figure 2.13 illustrates the kapton tape and E-glass mold surrounding the fibers. Figure 2.14 shows all of the fibers in place. Once all of the fibers (total by weight from Section 2.5.3) are in place, the lower plate temperature is elevated to 45°C.

The upper plates (4) are then prepared for the vacuum application and cure steps. As with the lower electrode, a small piece of GNPT is positioned over the copper strip and electrode flap on the upper electrodes. This is held in place with flash tape. A strip of vacuum tape is placed around the perimeter of the upper surface of each of the upper plates (matting surface facing down). A sheet of vacuum bag\(^1\) prepared in advance with cutouts for the central upper plate bolts is then placed onto the upper plate as shown in Figure 2.15. Next, the rubber\(^2\) and aluminum spreader layers are placed on top of the upper plates. The load spreader layers are shown in position in Figure 2.16. A pair of Unistrut crossbeams is placed across two of the upper plates. Hand-tightened bolts are used to join the beams to the plates. The upper plate assembly is shown in Figure 2.17 and Figure 2.18. Then the assembly consisting of the upper plates with attached electrodes, the crossbeams and spreader layers, and the vacuum bag is placed into an oven at 60°C. This will help in maintaining equal thermal expansion between the upper and lower plates.

### 2.5.6 Matrix Preparation and Application

The epoxy matrix for the composites is prepared using a mortar and pestle to mix the components. Part A is the Epon 9405 resin. Part B is the Epi-cure 9470 curing agent added to obtain a 100:37 mix ratio by weight. Approximately 6 g of epoxy is mixed for each batch

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1. Vacuum bag, Northern Fiberglass Co., Hampton, NH
2. 0.25" silicone rubber, medium hardness, Green Rubber, Woburn, MA
of 4 packs. After mixing for 5 minutes, the mortar is placed on the cure plate which has been warmed to 45°C. One drop of air release agent is then added to the mixture.

Next the matrix material is added to the fibers on the lower plate. A line of epoxy is first dripped along the longitudinal centerline of the fibers to hold them in position. Additional epoxy is added around the perimeter. Some of this epoxy is absorbed into the E-glass. The epoxy is allowed to spread throughout the mold. A gloved fingertip is used to aid the spreading process and to ensure that all of the fibers are adequately wetted. Potential manufacturing difficulties bound the net quantity of resin added to the fibers. Too much resin will result in the displacement of the fibers during the cure as epoxy flows outward. Too little resin may result in regions of poor resin content and possibly voids in the cured composite. Once the fibers are coated, the fibers are positioned to be centered widthwise to avoid overlapping the E-glass. The placement or distribution of fibers is also adjusted to obtain a single layer of fibers with no overlapping fibers. Sufficient resin should be present to fill all gaps between fibers without completely covering them. The last step is to remove any foreign particles which may have fallen into the mold during the assembly.

2.5.7 Vacuum Application and Cure

With the fibers and matrix in place as shown in Figure 2.19, the lower plate is prepared for positioning the upper plates and crossbeams. First, a strip of vacuum tape is placed around the perimeter of each pack and corresponding vacuum port. Next, threaded rods are locked into position for supporting the crossbeams. The lower support nuts are positioned such that the lower surface of the crossbeams will be 30.5 mm from the surface of the lower plate. This will ensure that an adequate gap is present between the upper electrode and the fibers and matrix on the lower plate. The upper electrode cannot come into contact with the fibers and matrix until the vacuum is pulled. The crossbeam and upper plate assembly is then positioned on the threaded rods and lower support nuts as shown in Figure 2.20. The support nuts are slowly lowered to 27.9 mm so that the gap between the upper electrode and the fibers and matrix is approximately 2 mm. The position of the
upper plates can be adjusted to ensure that the alignment pins mate properly with the holes on the lower plate. The bolts holding the upper plates to the crossbeams are again hand-tightened to ensure a solid and level connection.

Next, 7 mm by 7 mm strips of silicone rubber are placed around the perimeter of the upper plate to prevent the vacuum bag from being pulled underneath when the vacuum is pulled. Several layers of coarse fiberglass fabric (dry) are placed over the vacuum port and a small brass tube is used to ensure adequate flow between the edge of the upper plate and the vacuum port. The rubber strips and brass tube are visible in Figure 2.21. The vacuum bag is then sealed to the lower plate. The volume inside the bag is then evacuated to approximately 0.2 mm Hg using a vacuum pump.

The vacuum is held for 10 minutes to allow for degassing of the epoxy. Then the lower support nuts are gradually lowered one quarter turn at a time (approx. 0.4 mm) sequentially to maintain a uniform gap. After several iterations, the loads on the support nuts will decrease as the upper electrode comes into contact with the fibers. It is important that the plates be as close to parallel as possible at this point to avoid disturbing the positioning of the fibers. The support nuts are then lowered, as shown in Figure 2.22, so that the fibers are carrying all the pressure from the vacuum. The upper clamping nuts are then positioned to apply downward cure pressure to the packs. The nuts are torqued to 5 N-m. The completed cure assembly is shown in Figure 2.23.

With the cure pressure applied, the vacuum is then released to collapse any voids trapped in the matrix. Then the temperature is elevated to 120°C for a 3 hour cure. After slowly cooling to room temperature, the cured packs can be removed from the hot press assembly.

First the clamping nuts are released and the crossbeams are unbolted from the upper plates. The load spreading layers and vacuum bag are removed. Next, the tape used to hold the GNPT and electrode to the upper plates is peeled from the sides of the upper plates. This is illustrated in Figure 2.24. While gently lifting the edge of the upper plate, the tape attached to the GNPT can be pushed down toward the lower plate to release it
from the upper plate. The upper plate can then be removed as shown in Figure 2.25. Finally, the tape holding the GNPT onto the lower plate is removed which released the pack from it. Scissors are used to roughly trim around the perimeter of the packs. This removes all excess tape and GNPT.

2.5.8 Pack Preparation

Preparing the packs for poling and characterization requires several operations to be performed. First, the edges of the pack are trimmed using scissors as shown in Figure 2.26 and a sharp razor such that a 1-2 mm border remains outside the edge of the copper electrode rails. This border provides insulation around the pack edges. The copper strips attached to the positive and negative terminals of the upper and lower electrodes extend beyond the trimmed edge of the pack. Next, the copper strips from the upper and lower electrodes are peeled apart to remove the GNPT and tape layers between them.

At this point, several initial measurements are made of the pack. The pack is weighed and the thickness is measured in 15 points across the area. Any macroscopic flaws that may be present such as an overlapping fiber are noted on the summary data sheet for the pack. Next, a test lead is soldered to both the upper and lower copper strips of both the pack terminals. The solder is applied near the end of the copper strip away from the pack. The copper strips will be trimmed after the characterization testing, removing the test leads and solder in the process. Each test lead is solder to both the upper and lower copper strips, which effectively joins the upper and lower electrodes. At this point, the capacitance of the pack is measured\(^1\) and recorded. The pack is then stored in a desicador box until the batch poling and characterization steps are performed (Section 3.5). A photo of a complete pack is provided in Figure 2.27.

\(^{1}\) Omega Multimeter HHM57A, Omega Engineering, Stamford, CT
Figure 2.9  GNPT application to cure plates.

Figure 2.10  Positioning of electrodes.

Figure 2.11  Lower electrode with E-glass fabric border and GNPT covering copper strip.

Figure 2.12  Adding fibers to mold and aligning.

Figure 2.13  Kapton tape over E-glass forms mold.

Figure 2.14  Fibers in position on lower plate.
Figure 2.15 Vacuum bag applied to upper plates with cutouts for cross-beam bolts.

Figure 2.16 Load spreading aluminum and rubber layers.

Figure 2.17 Cross beams attached to upper plates to form upper plate assembly.

Figure 2.18 Bottom view of upper plate assembly.

Figure 2.19 Fibers and matrix in mold.

Figure 2.20 Positioning of upper plate assemblies over composites.
Figure 2.21  Vacuum port and rubber strips surrounding upper plate.

Figure 2.22  Lowering upper plates with vacuum applied.

Figure 2.23  Pressure application and cure.

Figure 2.24  Release of GNPT sheet from upper plate.

Figure 2.25  Release of upper plate from pack (covered in GNPT and still taped to lower plate).

Figure 2.26  Trimming pack edges.
Figure 2.27  Complete AFC pack for integral blade (to scale) with test leads attached
2.6 Summary

This concludes the pack manufacturing discussion for the AFC's. Additional information will be provided in the next chapter regarding the poling, characterization, and proof testing of the packs. Other variations on the standard AFC manufacturing procedure, geometry, and materials will also be discussed.
Chapter 3

ACTIVE FIBER COMPOSITE CHARACTERIZATION

In order to design an active structure incorporating AFC’s, a full knowledge of the capabilities of the actuator must be available. This includes the general properties of the material system, the performance, and the structural integrity. This chapter will describe the characterization test methods developed for AFC’s and the results from a number of studies. The first focuses on the characterization of the basic behavior and electromechanical properties of the material. Selection of an optimal material system for the integral blade application serves as motivation for the second test series. In the next series, the focus shifts to fully characterizing the structural integrity of the actuators for the rotor blade. The objective of the final series is the proof testing of the blade packs for the integral blade.

3.1 Introduction

The AFC is a relatively new form of material which requires significant characterization testing in order to fully understand its behavior and to build a base of knowledge of its properties. The AFC is designed to be integrated directly into composite structures. Thus, the AFC material will be subjected to the same design requirements as any other structural material. Since the AFC is an electromechanically coupled material, it has been necessary to develop a testing methodology to address the electrical, mechanical, and coupling behavior and properties of the material. The test methodology is intended to be general, but also can be tailored to the specific application, such as the rotor blade in this case.
3.1.1 Objectives and Strategy

The primary objective of the characterization testing is to measure all the properties of the AFC material system that are relevant to structural actuation. For the mechanical properties, this includes stiffness and strength, both static and in fatigue. Electrical properties of interest include the capacitance, the dielectric breakdown strength, and electrical fatigue life. For the electromechanical coupling, it is necessary to quantify the performance of the actuator. First, the coercive field level, or applied voltage required to pole the piezoceramic at a given temperature, must be determined. Then, the induced strain capability of the material for a given applied electric field can be measured. This information could be considered sufficient for characterizing an active material for some application. For this application, the induced strain capability is not an adequate measure of actuator performance.

Another major group of tests must be performed to adequately characterize the performance of the actuators under simulated operational conditions. Since the AFC material system is designed for high driving field levels to achieve maximum actuation authority, the nonlinear induced strain capability must be measured. Since the actuator material will directly deform the structure of the rotor blade, the AFC will not be in a free strain condition. The passive blade structure will provide a partially clamped boundary condition which requires knowledge of the induced stress capability of the actuators. In addition, the blade structure and integrated actuators will be subjected to steady and vibratory loads during operation. The performance and survivability of the actuators must be evaluated under similar mechanical strain conditions. These tests focus on time-varying environmental loads which may lead to cycle-dependent degradation of the internal integrity of the material system [Reifsnider, 1991]. The performance of the actuators will be evaluated to determine the expected life and operational limits, such as maximum stress or strain levels, or maximum applied voltage. This evaluation also requires a definition for failure, which can be a phenomenological performance metric, such as a reduction in actuation capability.
The presentation of the characterization tests and data begins with the preliminary characterization of the material system and moves toward the specific testing required for the rotor blade application. The preliminary characterization tests evaluate the basic properties and behavior of the AFC and examine the feasibility of the using the AFC material system in the integral blade. The results from these tests were used in conjunction with the operational requirements to develop a series of material selection tests. The goal of these tests was to optimize the material system to be used in the integral blade application. The next group of tests focuses on the structural integrity of the material system in order to qualify the AFC for the integral blade application. The final tests are designed to evaluate the specific actuators that were manufactured for the 1/6th Mach-scale CH-47D blade. These serve as a proof test and quality control step in the manufacturing process.

Following the presentation of the operational requirements for the integral blade application, each of the characterization tests will be presented. Within each section, the test article configuration used in the tests will be described along with the testing procedures. Then the results of the tests will be presented. A summary of the actuator capabilities will be provided at the end of the chapter.

3.1.2 Operational Requirements

A set of predicted operational requirements has been developed for the AFC material for the integral blade. The strain levels in the material were calculated for a nominal three-active ply integral blade design subjected to limit loads. The limit loading condition is based on scaled 3g maneuver loads for the CH-47D and the predicted centrifugal loads for the mass distribution of the integral blade. Table 3.1 presents the predicted worst case strain levels in the AFC’s for three cases [Weems, 1996]. The material strain levels were calculated in several cross-section locations and at a number of blade stations spanwise. The first case represents the strain levels at the location of maximum active fiber strain loading, or maximum tension. The second case represents the strain levels at the location of minimum active fiber strain loading, or maximum compression. The third case repre-
sents the worst case fatigue loading. The fiber strain levels for these three cases are highlighted in the table. Further details of the blade design and the loads analysis will be presented in Chapter 4.

<table>
<thead>
<tr>
<th>Strain Condition</th>
<th>Laminate Strains</th>
<th>+45° Ply Strains</th>
<th>-45° Ply Strains</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Axial</td>
<td>Trans</td>
<td>Shear</td>
</tr>
<tr>
<td>Max. Fiber</td>
<td>6186</td>
<td>-2854</td>
<td>3882</td>
</tr>
<tr>
<td>Min. Fiber</td>
<td>-2702</td>
<td>1246</td>
<td>3681</td>
</tr>
<tr>
<td>Steady</td>
<td>1478</td>
<td>-681</td>
<td>622</td>
</tr>
<tr>
<td>Alternating</td>
<td>1212</td>
<td>559</td>
<td>735</td>
</tr>
</tbody>
</table>

The calculated worst case strain levels in the AFC material have been used as a guide in developing the characterization tests for the mechanical properties as well as for the performance of the actuator under realistic loading conditions. The predictions will also be used in the testing of representative blade laminates and blade sections. This blade level testing will be described in Chapter 6.

3.2 Preliminary Characterization Studies

The preliminary characterization studies were intended to evaluate the material properties of the AFC actuators to be used in the design process for an active structural system. Another major objective of the tests was to examine the feasibility of the integral blade concept or other applications involving severe mechanical loading. To address this concern, AFC's with passive glass reinforcement are investigated along with the baseline material system.

The preliminary characterization tests are divided into four major groups. The first is the passive mechanical properties testing. These tests focus on the stiffness, stress-strain behavior, and static tensile strength of the composites. The next section of tests evaluates the electromechanical coupling properties of the AFC including the low-field d-constants.
(refer to linear piezoelectric material model [IEEE, 1978]), and the induced strain and induced stress at higher drive levels. This will lead into the testing of the actuator performance under tensile loading and tensile fatigue loading in the next sections. The final section will address the electrical properties of the AFC including dielectric, dielectric strength, and electrical fatigue.

### 3.2.1 Test Articles

The test articles for the preliminary characterization tests were designed to take advantage of the maximum fiber length available and to have an appropriate aspect ratio for tensile testing. The baseline configuration for the actuators has dimensions of 88.9 mm by 14.0 mm. Each actuator consists of a monolayer of continuous PZT-5H-type piezoceramic fibers\(^1\) embedded within an epoxy-based matrix. The low viscosity resin is modified with high dielectric, PZT-5H particles\(^2\) (60% by mass) and a dispersing agent\(^3\) (5% by mass of PZT) in order to increase the effective matrix dielectric. The etched interdigitated electrode pattern on the copper/Kapton film\(^4\) covered the center 63.5 mm of the length, which corresponds to the gage length for the tensile tests. The remaining area at each end was left inactive for application of the loading tabs, also for the tensile tests. Strain gages are also applied to the tensile test articles. A drawing of the test article is provided in Figure 3.1.

In addition to the baseline configuration, two other actuator configurations were also included in the characterization tests. A potential weakness of the AFC is the brittle ceramic fibers. When a crack develops in a fiber, the load it carried must be transferred to the other fibers or to the surrounding structure. The alternate configurations will investigate load a load transfer path outside the AFC through the structure in which it is embedded as well as inside through passive reinforcing material.

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1. Cera Nova Corp., Franklin, MA
2. Morgan Matroc Inc., Bedford, OH
3. Hypermer KD-2, ICI Americas Inc., Wilmington, DE
4. Southwall Technologies, Palo Alto, CA
Figure 3.1  Standard test article with loading tabs and surface strain gages (to scale).

The first configuration is the laminate form, in which the AFC is laminated with E-glass fabric plies to create a symmetric, three-ply composite. The two outer plies were a 0°/90° E-glass fabric\(^1\) used to provide partial clamping to the embedded 0° AFC actuator. The relative level of clamping in the actuator can be described by the cross-sectional area weighted elastic moduli of the plies. Using this rule of mixtures approximation, the stiffness ratio of the passive E-glass plies to the active ply is 1.0. The laminate configuration simulates the approximate level of structural clamping expected in the integral blade application. Data collected from actuation of the laminated actuators will provide a measure of the induced stress capability of the AFC which is essential for a full understanding of the actuator authority.

The second alternative configuration for the actuators incorporates high modulus S-glass filaments\(^2\) within the AFC. The filaments are embedded in the matrix material surrounding the ceramic fibers to provide a load path around the crack in close proximity to the ceramic fibers. The glass filaments are incorporated at a 10% of the total fiber/matrix volume. The evaluation of this actuator configuration will test the hypothesis that the

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1. Hexcel, Style 120-F155, Pleasanton, CA
2. Owens Corning, 449 S-2 Glass Roving, Anderson, SC
improved path for load transfer will improve the toughness and damage tolerance in the composite.

The added stiffness of the glass filaments will increase the stiffness of the actuator and will thus impart a partial clamping on the active material, as with the laminate configuration. The relative stiffness ratio of the reinforcing glass to the baseline actuator material is 0.24. This clamping effect will reduce the strain capabilities of the actuator; however, the inclusion of the glass improves the structural load-bearing capability of the actuator, which enables it to replace structural plies in the composite host structure. Thus any glass reinforcement included within the actuator is equivalent to added structural stiffness in the surrounding composite structure, so there should be no net loss in actuation capability associated with the increased clamping.

The test articles differ from the blade packs described in Chapter 2 in several respects. The fibers are oriented at 0° rather than at the 45° required for the blade. The fibers are spaced such that the average space between fibers is 25% of the fiber diameter. This results in an approximate fiber volume fraction of 57%. Kapton tape is used to form a mold around the active area of the electrode which is about 10.5 mm wide. The manufacturing process for the test articles was similar to that of the blade packs with the exception that the assembly is completed without a vacuum. This process preceded the current blade pack manufacturing process and was designed for small scale composites. The relatively small size of the test articles allows for most trapped air pockets to be pushed out with excess matrix prior to the cure step. Additional details of this manufacturing process can be found in previous publications [Rodgers, 1996; Bent, 1997].

For the laminated test articles, two E-glass prepreg plies were cut and a $[0^\circ/0^\circ/0^\circ]$ laminate stack was prepared. The center ply was the AFC actuator which was cleaned with MEK solvent in preparation for the lamination. The laminate was then cured at 125°C for 90 minutes with pressure applied.
For the glass reinforced test articles, the glass filaments were incorporated along with the active fibers during the AFC manufacture. The filaments were obtained in a continuous strand which contained approximately 200 filaments, each having a diameter of 9 mm. An appropriate number of strands were selected to obtain the 10% glass volume fraction. In the assembly of the composite, half of the glass was placed above the piezoceramic fibers, and half below in order to improve the uniformity of the distribution. The glass strands were spread out to create a more uniform layer while maintaining their orientation parallel to the piezoceramic fibers.

The completed actuators were trimmed and then prepared for poling. First, the upper and lower electrodes were soldered to a common wire for application of the driving voltage. Each actuator was poled at twice the coercive field level, at 60°C, and in silicon oil, for 20 minutes. The glass-reinforced actuators were poled in air, at room temperature. For the laminate configuration, the AFC was poled prior to lamination. Each test article was aged for at least 24 hours prior to testing.

Loading tabs were mounted on actuators intended for tensile testing. The loading tabs were cut from a 7 ply E-glass laminate stock, having dimensions of 12.7 mm square and a total thickness of 1.75 mm. The tabs were bonded to the ends of the actuator using 5-minute epoxy\(^1\). Strain gages were applied to all tensile test articles.

The modulus and thickness of each of the component materials used in the test articles is provided in Table 3.2. The modulus of the hybrid matrix and E-glass were determined experimentally. The cross section of the glass reinforced and laminate configurations are shown in Figure 3.2 and Figure 3.3. In the former, the S-2 glass filaments are dispersed around the larger ceramic fibers in the matrix, while in the latter, the E-glass filaments are outside the AFC with the thin electrode layer separating them from the ceramic fibers.

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1. Devcon 5-minute epoxy, Danvers, MA
TABLE 3.2 Component Material Properties.

<table>
<thead>
<tr>
<th>Material</th>
<th>Modulus (GPa)</th>
<th>Thickness (μm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Kapton\textsuperscript{a}</td>
<td>2.9</td>
<td>12.5</td>
</tr>
<tr>
<td>PZT-5H\textsuperscript{b}</td>
<td>60.6</td>
<td>130</td>
</tr>
<tr>
<td>Hybrid Matrix</td>
<td>5.0</td>
<td>-</td>
</tr>
<tr>
<td>S-2 Glass\textsuperscript{c}</td>
<td>86.0</td>
<td>9</td>
</tr>
<tr>
<td>120E Glass Fabric</td>
<td>21.5</td>
<td>83</td>
</tr>
</tbody>
</table>

\textsuperscript{a} duPont  
\textsuperscript{b} Morgan Matroc  
\textsuperscript{c} Owens-Corning

![Figure 3.2](image1.png) Photomicrograph of glass-reinforced actuator cross-section (400x).

![Figure 3.3](image2.png) Photomicrograph of laminate actuator configuration (160x).

3.2.2 Passive Mechanical Properties

The passive property tests include standard mechanical stiffness and strength characterizations. Standard tensile testing procedures were followed in the tests [ASTM D3039]. An Instron 8500 series tensile testing machine\textsuperscript{1} was used to obtain stress versus strain data for test articles. A photo of the test set-up including the testing machine, data acquisition system, and amplifier is shown in Figure 3.4. Strain gage and load data were collected. In all tests, short circuit electrical boundary conditions were applied. Due to limits on the avail-

\textsuperscript{1} Instron Corp., Canton, MA
ability of the testing machine, a few of the stiffness and strength measurements were acquired in a custom test apparatus. Each test article was mounted in a pair of stainless steel grips, designed to apply a pure tensile load. A consistent clamping pressure was applied using a torque wrench on two machine screws on each grip. A gravitational load was used for the tests. This was achieved by fixing one grip to a rigid support and allowing the test article to hang vertically. A mass load was attached to the lower grip. The attachments at both grips allowed all three rotational degrees of freedom to prevent the application of moments. The mass consisted of small diameter (0.04 g) lead shot, which was gradually poured into an open container hanging from the lower grip. A force transducer\(^1\) was used for stiffness measurements, while a spring-scale was used for ultimate tensile strength measurements. Strain gages\(^2\) were used to obtain the strain data. A strain rate of approximately 0.5% per minute was maintained during the stiffness tests. For the strength tests, mass was added until catastrophic failure of the test article occurred.

The capacitance of each test article was measured after poling and aging. The capacitance is also used as a metric for the integrity of the piezoceramic fibers during several of the performance-based tests. Measurements were made with a constant stress mechanical boundary condition.

**Stiffness**

Stiffness data were collected for each of the actuator configurations and are presented in terms of the average longitudinal modulus. This includes the effects of any added materials such as Kapton, glass filaments, or glass laminae. In addition, the component material properties were used to predict the stiffness using a *Mechanics of Materials* approach [Jones, 1975]. Table 3.3 summarizes the stiffness data including the mean value, standard deviation, number of samples, and model prediction for each of the configurations. In addition, the relative difference between the model and the data is provided for each case.

---

1. PCB, Depew, NY
2. Strain Gages and Model 2120A Conditioner, MicroMeasurements, Raleigh, NC
Figure 3.4  Test set-up for tensile testing of AFC test articles.

<table>
<thead>
<tr>
<th>Configuration</th>
<th>Num. Samples</th>
<th>Mean</th>
<th>Standard Dev.</th>
<th>Model</th>
<th>Diff. (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline</td>
<td>7</td>
<td>24.6</td>
<td>3.9</td>
<td>25.2</td>
<td>+2.5</td>
</tr>
<tr>
<td>Glass-reinforced</td>
<td>8</td>
<td>27.4</td>
<td>3.5</td>
<td>27.5</td>
<td>+0.4</td>
</tr>
<tr>
<td>Laminate</td>
<td>8</td>
<td>25.9</td>
<td>4.7</td>
<td>23.4</td>
<td>-9.7</td>
</tr>
</tbody>
</table>

The simple model provides a guide for the expected stiffness of the various actuator configurations tested in the study. For each configuration, the spread in the data was fairly small (15% standard deviation). The elastic modulus of the baseline actuators was close to the expected value, which was based on referenced material properties (see Table 3.2). For the glass-reinforced actuators, the total cross-sectional area of the actuator is greater as a result of the glass filaments overlapping the piezoceramic fibers. Thus, there is a greater percentage of hybrid matrix material, and a slightly reduced total volume fraction of S-glass filaments (9.4%). For the laminates, the difference between the predicted and actual stiffness may be a result of variability in the resin content of the cured E-glass laminae.
Static Tensile Strength

For the strength testing of the actuators, the test articles were loaded to catastrophic failure in tension. The strain level at failure was recorded, and the average failure stress was calculated. Table 3.4 presents the failure data for each actuator configuration.

<table>
<thead>
<tr>
<th>Configuration</th>
<th>Num. Samples</th>
<th>Mean Stress (MPa)</th>
<th>Stan. Dev.</th>
<th>Mean Strain (%)</th>
<th>Stan. Dev.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline</td>
<td>4</td>
<td>28.2</td>
<td>2.7</td>
<td>0.125</td>
<td>0.013</td>
</tr>
<tr>
<td>Glass-reinforced</td>
<td>5</td>
<td>135</td>
<td>4.5</td>
<td>1.51</td>
<td>0.063</td>
</tr>
<tr>
<td>Laminate</td>
<td>2</td>
<td>204</td>
<td>6.7</td>
<td>1.85</td>
<td>0.050</td>
</tr>
</tbody>
</table>

Several important features are evident in the data. In the baseline configuration, the mean failure strain corresponds closely with the static strength of PZT-5H, which is 0.125% [Berlincourt, 1971]. In contrast, both the glass-reinforced and the laminate configurations failed at much greater strain levels. In the S2 glass-reinforced actuators, the strain level at failure was 12 times greater, while the stiffness ratio was only 0.24 (ratio of glass stiffness to actuator stiffness). Some typical failed glass-reinforced test articles are shown in Figure 3.5. For the E glass laminates, the failure strain level is 15 times greater.

Stress-Strain Behavior

Although the presence of the S-glass filaments and E-glass laminae greatly enhance the failure stress and strain levels of the actuators, the yield points may not be improved to the same extent. To analyze the yielding behavior of the composites, several stress-strain curves are presented. Figure 3.6 shows data for a typical baseline actuator configuration. In general, the loading curve is fairly linear. The residual strain is approximately 200 microstrain after unloading. Due to the brittle nature of the ceramic fibers in the composite actuator, the onset of yielding corresponds with that of catastrophic failure. The non-linearity in the signal is mostly the result of signal noise.
Figure 3.5  Tensile failure of glass-reinforced test articles.

Figure 3.6  Stress-strain curve for baseline actuator configuration.

In contrast, the glass-reinforced test articles exhibit a noticeable decrease in stiffness during the loading cycle. The first is shown in Figure 3.7, occurring around 6500 microstrain
(0.65%). In repeated cycles past this strain level, the "knee" in the stress-strain locus remains observable. The slope of the stress-strain curve near the 1% strain level corresponds to an average elastic modulus of 5.4 GPa. Using a Mechanics of Materials prediction for the modulus and assuming that the effective modulus of the piezoceramic fibers has been reduced to zero, the predicted stiffness is 7.2 GPa. This calculation does not account for any decrease in the effective properties of the other component materials. Similar behavior was observed in each of the glass-reinforced test articles.

In the laminate configuration of the test articles, less significant changes were observed at high strain levels. This is shown in Figure 3.8. In this case, the measured modulus at 1% strain is approximately 11.1 GPa. The model prediction is 9.8 GPa, assuming a reduced E-glass lamina stiffness of 17 GPa (based on experimental data at 1% strain) and zero piezoceramic stiffness. The greater accuracy of this prediction may be a result of the much greater proportion of passive, structural glass in this configuration. The E-glass plies maintain structural integrity to significantly higher strain levels.

![Figure 3.7](image1.png)  **Figure 3.7** Stress-strain behavior for glass-reinforced test article.

![Figure 3.8](image2.png)  **Figure 3.8** Stress-strain behavior for laminate test article.
3.2.3 Electro-Mechanical Properties

The active property tests are designed to evaluate the standard electromechanical coupling properties used to describe most active material systems. The most commonly referenced property for structural actuators is the linear strain-field coupling parameter, or d-constant [IEEE, 1978]. For the case of the AFC actuators, the majority of the applied electric field and polarization in the active material is aligned with the fibers in the composite (3-direction). Thus the extension of the fibers due to the primary piezoelectric effect is described by the d_{33} parameter. In addition, the voltage is normalized by the distance between the electrode fingers (45 mil). Since this is a linear theory, the d-constants are valid only for low applied electric field levels.

The d_{33} parameter is calculated from measurements of the peak-to-peak strain resulting from an applied voltage cycle. Voltage was applied using a high voltage amplifier\(^1\), and strain levels were calculated using displacement measurements from a laser interferometer system\(^2\). One end of the AFC is clamped while the other is free to move as shown in Figure 3.9. A retro-reflector is attached to the free end. Data were collected at 100 V, 200 V, and 400 V peak-to-peak sinusoidal voltages at 1 Hz for each sample tested. Linear regression was applied to calculate the low-field strain-to-electric field ratio.

In order to establish and compare the actuation capabilities of the actuators, the representative cycle strain levels were measured. A 1 Hz, sinusoidal voltage signal was applied, which was 3000 volts peak-to-peak with a 600 volt DC offset toward the direction of polarization. The measured peak-to-peak strain represents the work cycle for the actuator, which is the most accurate measure of the true actuation capability.

\(^{1}\) Model 664, Trek Inc., Medina, NY
\(^{2}\) Zygo Corp. Axiom System, Middlefield, CT
Representative Cycle

The primary performance metric used in the characterization tests was the representative cycle actuation capability. This standard operational voltage cycle for the actuators was defined to maximize the induced strain while staying within certain limits. On the lower end of the cycle, the coercive field of the piezoceramic fibers limits the applied electric field. On the upper end of the cycle, the field is applied in the poling direction of the ceramic. The upper limit is saturation, the point at which an increase in the field level no longer increases the strain level. The risk of dielectric breakdown and the capabilities of driving amplifiers are more realistic limits.

The representative cycle for this study was defined relative to the coercive field level in the AFC actuators. The limits were 90% of the coercive field against the poling direction, and 200% of the coercive field with the poling direction. The mean coercive field of the baseline actuators was 980V/mm or 24 V/mil (1100 V applied). Experimental studies confirmed that the large applied field against the poling direction did not have a measurable
effect on the polarization of the ceramic. This operating cycle produces approximately 900 microstrain, or 0.09\% actuation strain in the free AFC actuator, peak-to-peak. A typical representative work cycle actuation locus is shown in Figure 3.10. The plot demonstrates the strain capability as well as the characteristic hysteresis in the material system. The tests in this study are performed at 1 Hz; however, the capability of the actuators extends to much higher frequencies. This representative work cycle was used in the preliminary characterization tests and the material selection tests in the next section. Note that this representative cycle differs from that of the blade packs which will be described in the proof testing discussion.

![Figure 3.10](image.png)

**Figure 3.10** Representative work cycle (600 VDC + 3kVpp) for baseline AFC at 1 Hz.

**Actuation Capability**

The objective of the first active property test was to characterize the low-field d-constant used in the linear model of piezoelectricity. For the laminate configuration, the effective d-constant is used (indicated by the * in Table 3.5) to represent the low-field strain capability. The previously described manufacturing difficulties with the glass-reinforced actuators precluded measurements of the electromechanical coupling properties in most cases.
Data were also collected for the second test, the representative work cycle strain capability for each of the test articles used in the study. Table 3.5 summarizes the data.

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline</td>
<td>39</td>
<td>116</td>
<td>22</td>
<td>907</td>
<td>133</td>
</tr>
<tr>
<td>Glass-reinforced</td>
<td>3</td>
<td>-</td>
<td>-</td>
<td>247</td>
<td>41</td>
</tr>
<tr>
<td>Laminate</td>
<td>11</td>
<td>59</td>
<td>13</td>
<td>268</td>
<td>40</td>
</tr>
</tbody>
</table>

The baseline AFC actuators produce about 900 microstrain of peak-to-peak strain when excited by the representative voltage cycle. The low-field d-constant has a mean of 116 pm/V. The glass-reinforced actuators have a significantly reduced representative work cycle strain capability. This is partially the result of the clamping effects of the embedded S2-glass filaments, but it is more substantially an effect of the reduced electric field distribution to the piezoceramic fibers. In the laminate configuration, both the effective d-constant and the representative cycle strain level are reduced, to 51% and 30% of their unrestrained values, respectively, primarily as a result of the clamping effects of the E-glass laminae. The degree of reduction in the strain capability is significantly greater at high fields. Similar trends have been observed in tests of clamped PZT wafers [Chan, 1994], suggesting a strain-dependent nonlinearity in the piezoceramic.

### 3.2.4 Performance Under Static Tensile Loads

In the actuation under load study, the actuation capability of the actuators was measured under a constant tensile load. A gravitational load was used for the tests, as was described for the passive property tests. The load was monitored using a spring scale in the load path. The actual independent variable measured for the tests was the applied mechanical or static strain value, which was accurately determined using strain gages.
A maximum static strain level was set for each sample tested. The load was started at zero and stepped upward. At each loadstep, the capacitance of the actuator was measured as well as the representative cyclic strain. This process was repeated until the maximum desired strain level was reached. Once data were collected at the maximum load level, the load was removed. For each test article, the complete procedure was repeated three times in order to evaluate any damage incurred in the previous load cycles. The actuation strain levels were found to settle by the third cycle in all cases. The effects of high-cycle loading were investigated separately in the passive performance test.

**Typical Behavior**

The second set of active performance-driven tests were the actuation under load experiments. The representative work cycle strain was measured as a function of the static tensile strain level applied to the test article. No data is presented for the baseline actuator due to the low failure strain level and the sensitivity to load misalignment in that configuration. Several laminate test articles were evaluated as well as a pair of glass-reinforced actuators. Three data sets were collected for each test article, each ending at the same static strain level. Results for the laminate configuration are plotted in Figure 3.11. Each plot shows data for a single virgin test article. Each successive test article (Figure 3.11a-d) was tested to a higher maximum static strain level.

Several important features are illustrated in the data plots. In all four of these samples, the performance actually increases by about 20% with the addition of a small tensile load which correlates to a static strain level of 500 to 1000 microstrain. This may be attributed to a relaxation of internal crystalline stresses which resulted from polarization. Further increases in the static strain result in a gradual decline in performance. However, this reduction is not permanent in samples tested up to 3000 microstrain static strain, as shown in Figure 3.11a and b. In these cases, the performance was not degraded in repeated cycles. On the other hand, the samples tested above 3000 microstrain, shown in Figure 3.11c and d, had degraded performance in the second and third testing cycles. It appears that the majority of the damage to the composites occurs in the first cycle. Very
little progressive damage accumulates in the second and the third cycle. No additional deterioration in performance was found in samples tested up to 5 cycles.

One additional laminate sample was tested to 6000 microstrain static strain level as shown in Figure 3.12. For this sample, the performance was measured for a slightly reduced electrical cycle consisting of 500 V DC and 2600 V peak-to-peak. The trends in the data
are similar to those in the other samples tested. The rate of decreasing performance with increased static loading begins to level out after 4000 microstrain, though it does continue to decline.

In addition to the laminate test articles, a pair of glass-reinforced actuators was tested. Rather than testing to a set static strain level, these samples were tested to failure. Thus only one load cycle was recorded. Figure 3.13 displays the performance of the glass-reinforced actuators as a function of the static strain level. The trends are similar to those evident in the laminate configuration. The performance of the glass-reinforced samples was measured to much higher static strain levels, where the trend of diminishing actuation was found to continue. Actuation capability remained all the way to failure, although at a greatly reduced level. Although the fibers may become discontinuous, the applied electric field induces strain in the fiber segments which contributes to the overall actuation.

![Figure 3.12 Actuation under load data for a laminate with maximum static strain level of 6000 microstrain.](image1)

![Figure 3.13 Actuation under load data for glass-reinforced samples tested to failure.](image2)

Measurements of the capacitance of the test articles during the actuation under load tests also support the trends noted for damage in the laminate actuators. The capacitance of each test article is plotted as a function of the static strain level in Figure 3.14. These data
correlate with the data plotted in Figure 3.11. Two important phenomena are evident. The first is that in all tests, the capacitance decreases in a nearly linear fashion as the applied load is increased. The second is that in repeated cycles above 3000 microstrain static strain, the capacitance is significantly degraded.

![Figure 3.14](image.png)

**Figure 3.14** Capacitance data for laminate test articles as a function of applied tensile load.
Table 3.6 quantifies the trends shown in the actuation under load data for the laminate configuration. The first column refers to the maximum static strain level of the particular sample tested. The second column provides the maximum degradation in the actuation strain between the first and the third cycles at any particular load level. The last column provides a measure of the absolute reduction in actuation from the maximum recorded actuation level to the minimum recorded at the maximum applied static load level.

<table>
<thead>
<tr>
<th>Max Static Strain (microstrain)</th>
<th>Max Degradation (Cycle 1 vs. 3)</th>
<th>Max Reduction (OverallMaxsMin)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2000</td>
<td>2%</td>
<td>0%</td>
</tr>
<tr>
<td>3000</td>
<td>3%</td>
<td>16%</td>
</tr>
<tr>
<td>3500</td>
<td>23%</td>
<td>40%</td>
</tr>
<tr>
<td>4300</td>
<td>42%</td>
<td>56%</td>
</tr>
<tr>
<td>6000</td>
<td>43%</td>
<td>69%</td>
</tr>
</tbody>
</table>

From the data table it is evident that damage does not begin to accumulate appreciably in the composite actuator for static strain levels up to 3000 microstrain. Damage is significant for the laminates tested to 3500 microstrain and above. The maximum reduction in strain for each of the test articles also becomes significant at 3500 microstrain and above. This metric is less meaningful in terms of damage to the composite because it includes reversible decreases in performance as well. Data from the glass-reinforced samples show that some actuation capability may be retained at static strain levels as high as 14000 microstrain.

The trends in the capacitance data correlate well with the actuation strain performance metric measurements. The permanent degradation in the performance of the actuators and the corresponding decrease in the capacitance in test articles cycled above 3000 microstrain suggest that damage is occurring in the actuator. This is most likely in the form of cracks in the piezoceramic fibers. The presence of cracks may affect the distribution of the
electric field in the surrounding ceramic. This may decrease the capacitance and reduce the actuation capability. The trend of decreasing capacitance with increased static strain or stress is a reversible process and is therefore more likely the result of a piezoelectric nonlinearity.

3.2.5 Mechanical Fatigue Tests

The primary long-term, passive performance test for materials is the mechanical fatigue loading. The tests were performed in an Instron 8500 series testing machine and controller\(^1\) following standard procedures [ASTM D3479]. Since the tests were performed at simulated operational stress levels, complete mechanical failure was not expected, and was not used as the primary metric for degradation. The representative work cycle strain produced by the test articles in stress-free tests was the degradation metric, similar to more standard stiffness measurements used commonly in fatigue testing. This value was measured at logarithmic cycle intervals. The eventual goal for the testing is to achieve 200 million cycles for various steady and alternating tensile loadings.

The goal of the tests was to simulate the estimated maximum operational fatigue level corresponding to a 750 microstrain steady and 650 microstrain alternating mechanical strain levels. Tests were run for different test articles at several cycles above and below this level. Because of the small size of the test articles and the resolution of the load cell, the tests were run in position control, using strain gage data to set the correct position range. A haversine waveform was used for the alternating stroke. The tests were performed at a cycle rate of 50 Hz, which was near the maximum capability of the testing machine for adequately maintaining the waveform. The position set-point was moved to account for the steady component or offset of the waveform, and to ensure a tension-tension cycle. The ratio of the minimum to the maximum load for the tests (R value) was approximately 0.1. The test article was removed from the tensile testing machine for the representative work cycle strain measurements at logarithmic intervals. Additional tests will measure the

\(^1\) Tests performed at Instron Training Center, Canton, MA
degradation at other tensile load levels. A full range of tests is required to fully characterize the operational life of the material system.

The mechanical fatigue test evaluates the actuation capability of the actuator as a function of the number of mechanical loading cycles endured. Degradation in performance is measured using the representative work cycle strain capability with no load applied. The first mechanical cycle to be tested consisted of a 750 microstrain steady strain level, with a \( \pm 500 \) microstrain alternating component. Thus the maximum strain level in a mechanical cycle was approximately 1250 microstrain. For this cycle, the minimum and maximum average stress levels were 6 MPa and 35 MPa. The effects of a second mechanical cycle with strain levels of 950 \( \pm 700 \) microstrain was also measured. For this cycle, the stress levels ranged from approximately 6 MPa to 44 MPa. A third test was conducted at a strain level of 1250 \( \pm 900 \) microstrain. The corresponding stress levels ranged from 9 to 57 MPa. Since the maximum strain level in all of the mechanical cycles exceeds the failure strain level in the baseline actuators, the laminate configuration was selected for testing. Three laminate configuration test articles were characterized as shown in Figure 3.15. Each successive test article was cycled at a higher level, and data are normalized by the initial actuation capability.

The results show that there is minimal damage evident in the actuator up to 10.5 million cycles for the lower two of the mechanical cycles. The total reduction in performance was 5%. The performance level remained constant between the 1 million and 10.5 million cycle evaluations for the 750\( \pm 500 \) microstrain cycle (Cycle 1). For the highest load cycle, Cycle 3, the data were normalized by the 10000 cycle actuation value, rather than the initial, unstressed actuation capability (0 cycles). It is most likely that experimental error for that data point is responsible for the apparent increase in performance up to 5 million cycles. Thus far, it is evident further testing is needed to complete the study to 200 million cycles to complete the qualification for the current rotor blade application. Additional studies at higher load levels are also required to completely describe the mechanical fatigue characteristics of the actuators.
3.2.6 Electrical Properties

The electrical property tests include measures of the actuator capacitance, dielectric breakdown strength, and electrical fatigue behavior. The capacitance is a simple test to estimate the relative dielectric of the sample. The breakdown strength indicates the maximum voltage which can be applied to the AFC without dielectric breakdown failure occurring. Finally, the electrical fatigue test simulates actuator operation and evaluates any changes in performance. Additional data on the remnant polarization and coercive field of the AFC’s can be found in another study [Bent, 1997].

Dielectric

The final passive property test was the capacitance measurement for each of the test articles. The capacitance, or effective dielectric, is a function of the dielectric properties of the component materials and of the composite geometry. In general, a higher dielectric value indicates a stronger effect from the high dielectric piezoceramic fibers. This may indicate a close proximity between the fibers and the electrode conductors. This, in turn,
enables a larger electric field to be applied to the active fibers with less of a voltage drop in the low dielectric matrix in between.

The data are summarized in Table 3.7. Variations in the alignment of the upper and lower electrode pairs may contribute to the spread in the data. The significantly reduced capacitance for the glass-reinforced configuration is a result of a manufacturing difficulty with the S-2 glass filaments. Some filaments are trapped between the interlaminar electrode layers and the piezoceramic fibers. This affects the electrical properties of the active composite. The presence of the low dielectric glass reduces the effective capacitance of the hybrid matrix. Thus, the electric field concentration in the active fibers is reduced. The affect of this will be evident in the active property tests.

**TABLE 3.7 Capacitance Measurements (pF)**

<table>
<thead>
<tr>
<th>Configuration</th>
<th>Number</th>
<th>Mean</th>
<th>Stan. Dev.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline</td>
<td>36</td>
<td>508</td>
<td>89</td>
</tr>
<tr>
<td>Glass-reinforced</td>
<td>3</td>
<td>268</td>
<td>20</td>
</tr>
<tr>
<td>Laminate</td>
<td>15</td>
<td>453</td>
<td>56</td>
</tr>
</tbody>
</table>

**Dielectric Strength**

Two major factors affect the dielectric strength of the AFC's: the dielectric strength of the component materials, and any manufacturing flaws. In practice, the latter effect is much more significant. The manufacturing flaws are typically air bubbles or voids in the matrix which cause a local concentration in electric field. This results in arcing or dielectric breakdown failure in the composite.

In a perfect composite, dielectric breakdown strength of the matrix governs the dielectric strength of the composite. Since the relative dielectric of the matrix is roughly 1/200th that of the fibers, a significant voltage drop and hence a high field is present in the small gap between the fibers and electrodes (refer to for a discussion of the electric field distribution). Model predictions for the electric field distribution in the composite show that
approximately 30% of the applied electric field actually reaches the piezoelectric fibers. [Bent, 1997] The breakdown strength in the matrix is then more significant than that in the fiber.

Data for the dielectric strength of the epoxy matrix has also been measured [Bent, 1997]. The breakdown strength of the pure epoxy was found to be greater than 640 kV/cm, the limit of the amplifier used in the tests. A test on the hybrid matrix, doped with 60 weight percent PZT-5H powder, resulted in a breakdown strength of 610 kV/cm.

AFC samples without significant manufacturing flaws are commonly tested up to field levels that achieve saturation strain levels in the active fibers. This may be approximately 6000 Vpp. A composite with significant matrix voids may fail at 2400 Vpp. This indicates the need for quality control in the form of a proof test to ensure that the blade packs can operate at the desired voltage levels. This will be further discussed in Section 3.5.

**Electrical Fatigue**

The electrical fatigue test simulates the repeated electrical cycling of the actuators during HHC operation. Each sample tested was actuated at the representative work cycle. The cycling frequency was 200 Hz. The long duration, high voltage cycle was applied to the samples using a high voltage transformer\(^1\). A high voltage amplifier\(^2\) was used to apply the proper DC offset voltage. At logarithmic cycle intervals, a single representative cycle at 1 Hz was run to determine the actuation strain level, measured on the laser interferometer system. The actuation strain produced in the representative cycle serves as the performance metric for this study.

Two baseline actuators and two laminated actuators were driven at the representative cycle for 230 million cycles with no external loading applied. At logarithmic cycle intervals, the representative work cycle strain levels were measured. The data are plotted in

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1. France Co. Franceformer, Fairview, TN
2. Kepco Model BOP 1000M, Flushing, NY
Figure 3.16a for baseline test articles and Figure 3.16b for laminate test articles. The strain levels are plotted relative to the representative cycle strain capability before cycling.

The trend in each of the data sets shows no degradation in the performance with increasing numbers of cycles. No long term change is evident in the baseline configuration. The increase in actuation capability (roughly 30%) in the laminate configuration may be attributed to changes in the state of the polarization in the piezoceramic. It is possible that the polarization may have been affected during the lamimation cure (125°C), although this was well below the Curie point (193°C) of the material. Overall, there is no evidence of cumulative damage in the actuators under high cycle electrical loading.

### 3.3 Material Selection Testing

In order to optimize the material system to be used for the blade packs, an additional series of characterization tests were performed. The objective of the tests is to select AFC component materials which will provide the best performance in the blade application. Thus, the operating environment of the rotor blade provides the performance metrics for the study. A subset of the preliminary characterization tests is used to evaluate several actua-
tor configurations. The configurations include variations in fiber composition and geometry, matrix dopants, and electrode type. The primary metrics include the induced stress performance or laminate actuation, and the damage tolerance or actuation under tensile loads.

3.3.1 Fiber Selection

The objective of the fiber selection testing is to select the fiber composition and geometry which maximizes performance and damage tolerance in the AFC packs. The measured strain performance of actuators in the laminate configuration is used as the performance metric. Residual actuation capability after the application of large tensile loads is the primary damage tolerance metric along with the predicted compressive stress depolarization limits for the fibers.

Configurations

The test articles used in this study have the same geometry as those used in the preliminary characterization testing. Two fiber geometries were investigated: the baseline circular cross section and a square cross section. The circular fibers are the original extruded piezoceramic fibers\(^1\) used in the preliminary characterization test articles. The square fibers\(^2\) are produced by dicing a solid block of piezoceramic into small strips. The square fibers offer two potential advantages over the baseline fibers. The first is that the extrusion process and sintering of the individual fibers introduces porosity and grain structure difficulties which may reduce the actuation capability and strength of the fibers. The second advantage is that the square geometry may enable a greater portion of the applied voltage to reach the active fibers in the composite. A comparison of the cross sections of the two geometries is provided in Figure 3.17 and Figure 3.18.

\(^1\) Cera Nova Corp., Franklin, MA
\(^2\) Staveley Sensors, Hartford, CT
Three compositions of piezoceramic were investigated as well. The baseline PZT-5H was originally selected for its high low field coupling properties. For the rotor blade application, this is not necessarily the optimal parameter to maximize. Other important variables are the relative dielectric of the ceramic, which affects the electric field distribution, and the stiffness of the ceramic, which affects the induced stress capability. The high field or representative voltage cycle performance, which the low field d-constants do not adequately characterize, is also a more pertinent measure of performance.

PZT-5A offers a lower dielectric and slightly higher stiffness, with somewhat reduced d-constants. PZT-4 is a much harder and stiffer PZT which also has a lower dielectric and lower d-constants. PZT-4S is a slightly enhanced composition which offers the potential for better performance. However, this composition was not available in fiber form at the time of this study.

Four actuator configurations were considered in the study based on the availability of fibers. The baseline configuration is the PZT-5H circular fibers (PZT-5H circ). Both square and circular PZT-5A fibers were tested (PZT-5A circ and PZT-5A sq). Square PZT-4 fibers were also tested (PZT-4 sq). The matrix for all the actuators was the hybrid 60% PZT powder in epoxy system used for the preliminary characterization test articles. All test articles were laminated in E-glass.
**Induced Stress**

The induced stress tests consist of measurements of the induced strain in the laminate configuration. The applied voltage was the representative work cycle, 600 VDC and 3000 Vpp. Data were collected for a 1 Hz sinusoidal input and strain was measured using the interferometer system described in Section 3.2.3.

Figure 3.19 presents a summary of the actuation tests performed on each active fiber composite sample, before and after lamination. The ratio of the laminate to free actuation, shown in Figure 3.20, provides a relative measure of the induced stress capability for each active composite type. The representative work cycle simulates a possible operating cycle for actuators in the integral blade.

![Figure 3.19](image1.png)  
*Figure 3.19* Free and laminate strain performance at representative voltage cycle.  

![Figure 3.20](image2.png)  
*Figure 3.20* Relative performance of laminate to free actuators at representative voltage cycle.

From the data in Figure 3.19, the 5A square doped configuration stands out as having the highest laminate representative work cycle strain of the doped samples at 369 microstrain. This is not a significant increase over the baseline configuration. The PZT-4 square fiber
systems showed a higher Rep Cycle ratio (Lam/Free) than the other fiber systems. The PZT-4 square system produces much lower strain values even though it showed the highest ratio, because of low saturation strain levels. The square fiber samples used in the tests had approximately 10% more active material as a result of fiber cross section dimensions. This may account for some of the advantages shown in the laminate actuation levels. The standard deviations for the free and laminated sample data are given in Table 3.8. The relative performance of the laminate and free configurations at low field levels has also been evaluated in another study [Bent, 1997].

<table>
<thead>
<tr>
<th>Composition</th>
<th>Number of Samples</th>
<th>Free Strain</th>
<th>Laminate Strain</th>
</tr>
</thead>
<tbody>
<tr>
<td>5H circ</td>
<td>4</td>
<td>63</td>
<td>18</td>
</tr>
<tr>
<td>5A circ</td>
<td>4</td>
<td>75</td>
<td>19</td>
</tr>
<tr>
<td>5A sq</td>
<td>3</td>
<td>92</td>
<td>28</td>
</tr>
<tr>
<td>4 sq</td>
<td>3</td>
<td>51</td>
<td>22</td>
</tr>
</tbody>
</table>

a. Strain units: microstrain

**Damage Tolerance**

Actuation under load tests were used to characterize the damage tolerance of the test articles. For each sample type, the representative work cycle strain was measured for different loading cases. Each sample was loaded to a static tensile strain level of either 4000 or 6000 microstrain. These strain levels are representative of the limit loads for the AFC plies in the rotor application. After a load cycle to the maximum level, the performance was measured at a static strain level representative of the centrifugal (CF) strain in the blade. This measurement was compared with the performance measured prior to the maximum loading. The results are plotted as a ratio of the final strain at the CF level to the initial strain at the CF level. In Figure 3.21, the CF level selected was 1000 microstrain, a number which is representative of the 1/6th Mach-scale CH-47D blade design. The results were also compared for a higher strain level, 3000 microstrain, in Figure 3.22.
From these data, the 5A square system stands out as having the highest ratios after loading to 4000 or 6000 microstrain. For example, 99% of the representative work cycle actuation was retained after loading to 4000 microstrain static tensile load, measured at the expected blade CF level. Even at the higher static level (3000 microstrain), 93% of the initial capability was retained. After experiencing a 6000 microstrain loading, the numbers fall to 98% and 68% in the two simulated CF cases. The 5H circular undoped also performed well in the tests up to 4000 microstrain, while the 4 square system did not stand out. Each data point was based upon either one or two samples of each type so no standard deviation data is available.

Another major factor in the selection of the fiber composition is the compressive stress depolarization limit. Table 3.9 lists the approximate maximum stress levels achievable before significant depolarization of the piezoceramic results. The values are taken from experimental studies at Penn State [Zhang, 1996] on bulk ceramic. The significance of the compressive strain limits depends on the expected maximum strain levels in the hover...
tests of the integral blade. The depolarization phenomenon is in reality dependent on both
the stress and the electric field applied to the ceramic. Thus, the combination of hover
loads, applied field, and actuator induced stress and strain complicate the prediction of
depolarization conditions for the active plies.

<table>
<thead>
<tr>
<th>Composition</th>
<th>Compressive Stress Limit (MPa)</th>
<th>Compressive Strain Limit (µε)</th>
</tr>
</thead>
<tbody>
<tr>
<td>PZT-5H</td>
<td>30</td>
<td>270</td>
</tr>
<tr>
<td>PZT-5A</td>
<td>50</td>
<td>450</td>
</tr>
<tr>
<td>PZT-4</td>
<td>150</td>
<td>1100</td>
</tr>
</tbody>
</table>

Based on the characterization test results and predicted loads for the active plies in the
hover testing, the PZT-5A composition was selected. While the PZT-4 compositions
offers much better compressive stress tolerance, the loss in actuation performance is sig-
nificant. While the square fibers performed the best, especially in damage tolerance, both
square and round 5A fibers have been used in the preliminary pack manufacture for the
blade section tests. Performance of the blade-size packs has shown that the circular fibers
are currently the best choice for the material system. Manufacturing difficulties and dam-
age to the copper electrode pattern have precluded the use of square fibers in the blade
packs.

3.3.2 Matrix Selection

The matrix material surrounding the piezoceramic fibers consists of an epoxy resin with
high dielectric filler in the baseline configuration. Two configurations were considered in
this study: the baseline consisting of 60 weight percent PZT-5H powder, and a pure epoxy
matrix without dopants. Both configurations include an air release additive. Additional
data on matrix additives including varying amounts of PZT-5H powder as well as conduc-
tive particles have been evaluated in another study [Bent, 1997]. The matrix variation was
tested in conjunction with PZT-5H circular fibers in the standard test article geometry.
The measured actuation capabilities of the tested samples are presented in Table 3.10. The results indicate that the samples with the pure epoxy or undoped matrix actually outperform those with the high dielectric filler. Although the filler raises the relative dielectric of the matrix, in theory improving the electric field distribution to the fibers, the added matrix viscosity and/or filler particle size may be increasing the gap between the fibers and electrodes. The result is a decrease in actuation. The undoped matrix requires less effort to mix and allows for easier assembly of the composite during the manufacturing process. In addition, the added filler material substantially increases the density of the matrix. Therefore, the undoped matrix was selected for the blade packs.

**TABLE 3.10**  Comparison of Matrix Configuration Strain Data

<table>
<thead>
<tr>
<th>Configuration</th>
<th>Samples</th>
<th>d</th>
<th>3(μm/V)</th>
<th>S.D.</th>
<th>Strain (με)</th>
<th>S.D.</th>
</tr>
</thead>
<tbody>
<tr>
<td>doped</td>
<td>5</td>
<td>105</td>
<td>34</td>
<td></td>
<td>911</td>
<td>63</td>
</tr>
<tr>
<td>undoped</td>
<td>4</td>
<td>119</td>
<td>28</td>
<td></td>
<td>1048</td>
<td>120</td>
</tr>
</tbody>
</table>

**3.3.3 Electrode Selection**

Two types of interdigital electrodes have been considered for the blade packs. The first is an etched copper pattern. The second is a screen printed conductive ink. In both cases, the substrate is 12.5 μm kapton film. In the case of the copper, the copper is first deposited onto the kapton, and is then etched using a photolithography process. In the screen printed case, the ink is printed directly onto the kapton. The configurations tested used PZT-5H circular fibers and a doped matrix with the standard characterization test article geometry.

Preliminary actuation data for the screen printed samples showed improved performance and the representative voltage cycle. Screen printed samples demonstrated 1070 microstrain as compared with 911 for the baseline case. The higher performance may be attributed to the thickness of the screen printed lines of the interdigital pattern. The thick lines make better contact with the active fibers than the copper lines. In addition, no problems were found with electrical conductivity under tensile strain. Therefore, additional testing was pursued.
A second round of tests were performed on blade size packs using both screen printed and copper electrodes. For both types, the electrodes were fabricated by an outside vendor, rather than in-house at MIT. Seven packs were manufactured with the screen printed electrodes. In 5 of the 7 samples, the attempted poling process resulted in multiple electrical failures attributable to problems with the conductive ink. The ink was found to have many cracks which under an applied voltage result in arcing. The arcing carburizes material and eventually causes a dielectric breakdown failure in the form of a low resistance path between electrode fingers of opposing polarity as shown in Figure 3.23. This problem was not commonly found with copper electrodes. The problem may be resolved in the future with an improved conductive ink. Thicker copper lines are also under investigation. The copper/kapton electrodes have been selected for the blade packs.

![Figure 3.23](image) Damage to screen printed electrode sample during poling.

### 3.4 Structural Integrity Testing

An additional series of structural integrity tests was performed on the AFC material system in order to address specific concerns for the rotor blade application which the preliminary characterization testing did not directly address. It is essential that the integration of the AFC material into the composite blade does not introduce any structural failure modes which would significantly reduce the integrity of the blade. A more detailed knowledge of the actuator capabilities allows for a blade design with greater confidence of structural
integrity and lifetime. The conditions addressed are transverse tensile loading and interlaminar shear stresses. An additional set of compressive stress depolarization tests was also included to reduce the potential performance risks. With this set of tests, the previously described material selection and characterization test, and the blade section tests to be described in Chapter 6, the confidence in the integral blade design will be greatly increased.

3.4.1 Transverse Tensile Tests

The objective of the transverse tensile tests is to evaluate the integrity and performance of the AFC under limit loads applied transversely or normal to the active fibers. The test articles consisted of an AFC actuator\(^1\) embedded in an E-glass fabric laminate. The AFC had dimensions of 1 inch along the fibers and 2 inches transversely, which is the longitudinal axis of the test article, as shown in Figure 3.24. An additional 2 inches of E-glass extends beyond the AFC on each end for a total test article length of 6 inches. Of this length, 1 inch on each end is used for the gripping surface. The layup in the center portion or active section was [0\(^\circ\) E-glass/90\(^\circ\) AFC/0\(^\circ\) E-glass], while in the passive outer sections, the layup was [0\(^\circ\) E-glass\(_3\)].

Four test articles were evaluated during the tests. Each test article was loaded in tension up to a predetermined strain level corresponding to a limit load condition. At several steps during the loading process, the actuation performance was measured for the representative voltage cycle while the load was applied. A T-gage was used to measure the strain at 0\(^\circ\) and 90\(^\circ\) to the test article axis. The four maximum load levels corresponded to 50\%, 100\%, 125\%, and 150\% of the limit load level of 3600 microstrain. The latter case corresponds to the ultimate load condition. The four test articles are pictured in Figure 3.25. The testing set-up is shown in Figure 3.26.

---

1. AFC actuators provided by Mide Technologies, Cambridge, MA
The results of the transverse tensile test revealed no evidence of performance degradation or loss of structural integrity in the test articles. The induced strain levels along the transverse or fiber axis remained constant at each load step in each of the test articles as shown in Figure 3.27. The induced strain levels measured along the laminate axis, transverse to the fibers, showed no trends indicative of damage or degradation, as plotted in Figure 3.28. The small magnitude of the strains measured transverse to the fiber axis, as well as noise from the Instron load controller may account for the large scatter in the laminate axis data.

In general, the results do not provide a measure of the true transverse tensile strain limit of the AFC material system. One possible failure mode is the debonding of the active fibers
from the matrix. The correlation between this failure mode and possible changes in the induced strain performance is not certain. Thus the transverse tensile tests performed are in general inconclusive.

3.4.2 Interlaminar Shear Tests

The objective of the interlaminar shear test is to evaluate the potential for a delamination failure in the integral blade. The potential failure may occur between the kapton electrode and the AFC matrix or the external composite. Another potential failure mode is a shearing of the active fiber layer. A short-beam shear test method [ASTM D2344] was selected for the study. The test consists of a 3-point bending load applied to an S-glass\textsuperscript{1} laminate with either a 0° or 90° AFC middle ply. The total laminate was [0° S-glass\textsubscript{6}/AFC/0° S-glass\textsubscript{6}]. A control group with no AFC layer was also tested. The AFC plies were sectioned from a standard blade pack. The test articles had a width of 0.25 inches and length

\textsuperscript{1} S-glass, S2/SP381, 3M Aerospace, Springfield, MO
of 1.25 inches. The loading span was 0.55 inches. Five test articles of each of three configurations were fabricated and tested to failure at Boeing Helicopters (Philadelphia) [Downey, 1998]. The test articles are pictured in Figure 3.29.

A custom test fixture was mounted in a standard Instron load frame for the tests. The test fixture, shown in Figure 3.30, includes a span adjustment control and an automatic centering feature. The upper platen of the load frame applies the load through the plunger at the top of the fixture.

The results are summarized in Table 3.11. The mean and standard deviation for the shear stress at failure are provided. All failures were found to be in acceptable locations and modes for the test method. The shear strength of the $0^\circ$ and $90^\circ$ AFC test articles were found to be somewhat reduced from the control case. For the control group, the failure
occurred at the midplane in all cases. For the 0° AFC group, two failures occurred at 30% and 70% of the thickness and one occurred between the active fibers and the kapton electrodes. The ratio of the maximum shear stress for this group to the control group was 0.91. Although finding a crack to photograph was difficult, one potential crack is evident between the active fiber and kapton electrodes in the sample shown in Figure 3.31. For the
90° AFC group, three failures occurred at 30% and 70% of the thickness and two occurred between the active fibers and kapton electrodes. In this group, the ratio of shear stress at failure was 0.80. In one of the test articles, the failure is clearly visible as a crack between the active fibers and matrix layer in the AFC as shown in Figure 3.32. The two internal AFC failure conditions resulted in the lowest failure stress levels (0.72-0.74 of control).

<table>
<thead>
<tr>
<th>Configuration</th>
<th>Mean (MPa)</th>
<th>S.D.</th>
<th>Ratio</th>
</tr>
</thead>
<tbody>
<tr>
<td>Control</td>
<td>80.0</td>
<td>0.4</td>
<td>1.00</td>
</tr>
<tr>
<td>0° AFC</td>
<td>72.4</td>
<td>0.2</td>
<td>0.91</td>
</tr>
<tr>
<td>90° AFC</td>
<td>64.1</td>
<td>0.8</td>
<td>0.80</td>
</tr>
</tbody>
</table>

In conclusion, it appear that the presence of the AFC layer in the glass laminate reduced the effective shear strength by 10 to 20% in the short beam tests. The failure mode of greatest concern is the separation at the fiber/matrix interface in the AFC which accounted for the lowest shear stress levels at failure in the 90° orientation. Further study of the fiber/
matrix interface is required to better understand and possibly to improve the structural bond to the fibers.

3.4.3 Compressive Stress Depolarization Tests

In order to quantify the potential for compressive stress depolarization of the AFC plies during operation in the rotor blade, 4-point bend tests were conducted on several beam configurations. Preliminary decisions on the selection of the active fiber composition for the application were based on data from bulk piezoceramics (Section 3.3.1). This study examines the exact material used in the blade packs and in the same configuration, a laminated AFC. The actuation performance of the AFC will be monitored after successively increasing load steps in order to monitor the polarization of the active fibers. Data for the harder PZT-4 composition will be compared with the blade baseline PZT-5A fibers.

The tests were performed at Boeing (Seattle)\(^1\) using a Satec testing machine. The test setup is pictured in Figure 3.33 including the Trek amplifier, testing machine with 4-point bend hardware, and data acquisition computer. Each beam had a length of 36 inches. Two facesheets bonded\(^2\) to a 0.5 inch foam\(^3\) core formed the beam. In the first beam type, of

---

1. D. Morris and C. Davis, The Boeing Company, Seattle, WA
2. Micromeasurements AE-10 epoxy, cured 1hr. at 150°F
which only one was tested, each facesheet contained a 2 inch wide by 3 inch long AFC\textsuperscript{1} laminated between layers of 0°/90° E-glass fabric and with an E-glass filler ply on either end of the active region. The completed beam is shown in Figure 3.34. One AFC was embedded in the upper and lower facesheets, in the center of the beam span, as shown in Figure 3.36. The fibers of this first beam were PZT-5A circular fibers. The AFC actuators were poled prior to the fabrication of the sandwich beam at 2kV and 60°C\textsuperscript{2}.

![Figure 3.33](image)

**Figure 3.33** Set-up for 4-point bend testing for compressive depolarization (Boeing-Seattle).

A second beam type was fabricated to evaluate an AFC with PZT-4 square fibers. The actuators used for this beam were initially fabricated and tested in a laminate configuration for the material selection testing study (Section 3.3.1). Some of the actuators used in this

3. Rohacell HF71 foam, Rohm GmbH, Richmond Aircraft Products, Norwalk, CA
1. Supplied by Mide Technologies, Cambridge, MA
2. Poling may not have been complete at 2kV and 60°C. Further testing of AFC's with PZT-5A have shown that the response may be reduced by 50%, suggesting a partial polarization. Full polarization can be achieved at 3kV and 80°C.
study had previously experienced tensile loads. Since the actuators were already laminated between single plies of 0°/90° E-glass fabric, a beam was first fabricated using single E-glass ply facesheets. Then the laminated actuator was bonded to the surface of the beam as shown in Figure 3.35. The active section of one of the beams is shown in Figure 3.37. Two beams in this configuration were tested.

![Figure 3.34](image1)

**Figure 3.34**  Beam type 1 with PZT-5A embedded AFC in upper and lower facesheet.

![Figure 3.35](image2)

**Figure 3.35**  Beam type 2 with PZT-4 (and PZT-5A) AFC laminate surface bonded to upper facesheet only.

![Figure 3.36](image3)

**Figure 3.36**  Close-up view of beam type 1 actuator.

![Figure 3.37](image4)

**Figure 3.37**  Close-up view of beam type 2 actuator.
The test apparatus provided fixed supports near the ends of the beam, 34 inches apart. A symmetric load was applied at two points 17 inches apart. A load cell mounted between the load frame and upper load fixture was used to monitor the loads during the test. The surface mounted strain gages were used to estimate the compressive strain levels in the active material and also to measure the actuation performance. The axial strain gage is visible in the photographs in Figure 3.36 and Figure 3.37.

For each beam tested, the following sequence was followed. First the capacitance of each actuator is measured\(^1\) and recorded. Next, the actuator on the lower surface of the beam (for the beams of type 1) is shorted, and the upper actuator is excited with a 1200 Vpp signal at 1 Hz. The induced strain in the upper facesheet is measured. This step can be reversed for evaluating the lower actuator. The capacitance is also measured. Next, all actuators are shorted and the beam is loaded to the desired surface strain level at 0.4 inches/minute. The load is held for 30 seconds, and then the beam is unloaded at 1.6 inches/minute. Finally, the actuator performance measurement is repeated. The beam is then loaded to a higher strain level. Performance is measured in the unloaded condition after each load step. The load is increased until failure of the beam occurs.

The first beam type, PZT-5A, 2 inch wide, was loaded to 4200 microstrain in compression before it failed. Performance data were collected up to a 3000 microstrain compressive load level. The testing of the beam is shown in Figure 3.38. Figure 3.39 shows the upper facesheet buckled and delaminated from the core.

Results for the first beam type are plotted in Figure 3.40 and Figure 3.41. The induced strain performance of the upper actuator was measured after exposure to a specified static compressive strain level. The capacitance was also measured. The data indicate a 10% reduction in the induced strain performance of the PZT-5A actuator occurring gradually between 1200 με and 3000 με. Note that this level of compression is significantly greater than that found to depole bulk PZT-5A. Studies on bulk compressive stress depolarization

\(^1\) Fluke 87 multimeter, Everett, WA
have measured partial depoling around 450 με [Zhang, 1996]. In the current study, the capacitance, another indicator of polarization, did not decay. Since the piezoceramic material in the composites tested may not have been fully poled, the results may not be fully representative of the behavior of PZT-5A composites.

Figure 3.38  Testing of beam type 1.

Figure 3.39  Facesheet buckling failure of beam type 1.

Figure 3.40  Induced strain performance of beam type 1 (PZT-5A) as a function of compressive strain exposure.

Figure 3.41  Capacitance of beam type 1 actuators as a function of compressive strain exposure.
For the second beam type, with PZT-4 fibers in the 0.5" wide configuration, two identical beams were tested. The first was tested up to 600 microstrain compression before failing at 780 microstrain. The lower face sheet (in tension) and core failed near the end of the actuator as shown in Figure 3.42.

![Failure of first narrow beam tested.](image)

**Figure 3.42** Failure of first narrow beam tested.

![Testing of second narrow beam. Failure of second narrow beam at load application point.](image)

**Figure 3.43** Testing of second narrow beam. **Figure 3.44** Failure of second narrow beam at load application point.

In order to improve the strength of the beam, a strip of welding tape was applied to the lower side of the second beam tested. The testing of this beam is shown in Figure 3.43. Performance data were collected to 2500 microstrain and failure occurred at 2510 microstrain. The failure, visible in Figure 3.44, occurred at the point of load application as the upper facesheet buckled.
The measured performance and capacitance for the second beam type are plotted in Figure 3.45 and Figure 3.46. The actuators in this beam show no signs of depolarization up to 2500 microstrain in compression. Note that the strain data point corresponding to the 2500 microstrain exposure was slightly greater than 20% more than the initial value and thus does not appear in the plot. No trend in the strain or capacitance data is evident. For reference, the bulk depolarization strain level for PZT-4 has been measured as 1100 microstrain.

![Figure 3.45](image1.png) **Figure 3.45** Induced strain performance of beam type 2 (PZT-4) as a function of compressive strain exposure.

![Figure 3.46](image2.png) **Figure 3.46** Capacitance of beam type 2 actuators as a function of compressive strain exposure.

The lack of correlation between the bulk depolarization levels and AFC depolarization levels may be attributed to differences in the actual piezoceramic material in the fibers (composition, grain structure, porosity, etc.) or possibly some effect of the composite geometry. The softer piezoceramic tested, PZT-5A, does show signs of depolarization. However, the reduction in properties may be only 10% at the predicted worst case limit loading condition for the blade application. The harder material tested in this study, PZT-4, showed no
signs of reduced polarization. The harder PZT's remain a viable option for increased resistance to compressive stress depolarization in demanding applications.

3.5 Blade Pack Proof Testing

Each of the blade packs manufactured is subjected to a series of tests in order to evaluate the performance or quality of the actuator. This includes a poling operation in which the steady-state leakage current is monitored and a measure of the free strain capability at the representative voltage cycle planned for operation in the blade. Data from the poling and testing steps are then used to select packs for the integral blade and for the blade sections used in other testing.

3.5.1 Poling

Following the final pack preparations described in Section 2.5.8, each of the packs is poled. The poling is performed in air at 80°C with a voltage of 4000 V. The pack is allowed to warm up to the poling temperature. Then the voltage is applied and held for 20 minutes. Once the current flowing through the pack has reached steady-state, the leakage current is recorded. A high leakage current is an indicator of a potential flaw in the pack. With the voltage applied, the pack is next removed from the oven and cooled before the field is removed. Finally, the poled pack is aged for at least 24 hours prior to characterization testing.

Many packs experience localized dielectric breakdown failures during the poling operation. The failures may be the result of manufacturing flaws such as: arcing between exposed copper near overlapped fibers, arcing through internal matrix voids, arcing due to flaws in the etched electrode pattern, or other sometimes unknown factors. The failed pack is visually inspected to determine the location and cause of the failure. Then the electrode finger or fingers which deliver the power to the locally damaged region are cut in order to deactivate the area. 5-minute epoxy is next used to fill any voids and seal any exposed copper. A photo of an fiber overlap and the resulting damage to the kapton elec-
trodes is shown in Figure 3.47. A photo of damage and the repair of a dielectric breakdown is shown in Figure 3.48. In this case, the cause was a void near the edge of the active area of the pack. The poling process is then repeated. The number of repairs for each successfully poled pack is recorded and considered in the pack selection process.

![Figure 3.47](image1)  ![Figure 3.48](image2)

**Figure 3.47** Fiber overlap in AFC and resulting damage to kapton electrode layer.  **Figure 3.48** Repair of dielectric breakdown due to void near edge of AFC.

### 3.5.2 Characterization

Following the poling operation, the induced free strain performance of each pack is measured. The test serves not only as a basis for comparison between packs, but also as another proof test. The first proof test was survival for 20 minutes at 4 kV during poling along with a measure of leakage current. In this second stage, each pack is subjected to characterization at the intended blade operating cycle of 800 VDC and 4000 Vpp. This operating cycle was found to deliver the maximum strain performance without significant repolarization (-1200 V) or dielectric breakdown (2800 V). For comparison purposes, each pack is also tested at the original representative cycle of 600 VDC and 3000 Vpp. In both cases, strain data is collected at 1, 10, and 100 Hz.

Strain data are collected using the laser interferometer system previously described. The test set-up is illustrated in Figure 3.49. Because of the large area of the pack, the strain performance can only be measured in a fairly small strip parallel to the fibers. The test
location is randomly selected for each pack. One edge is clamped while a retro-reflector is clamped to the other which is free to move. Thus the test measures the average strain along a group of fibers. This has been found to be a fairly accurate measure of the overall pack performance provided that the thickness of the pack and the distribution of any flaws is widespread.

![Image](image.png)

**Figure 3.49** Pack characterization testing using interferometer system.

Typical behavior measured in the testing of a blade pack is shown in Figure 3.50 and Figure 3.51 for the original representative cycle and blade operating cycle, respectively. The metric used for comparison with the previous characterization test articles is the peak-to-peak strain at the original representative cycle at 1 Hz. The average value for the blade packs is 900 microstrain which is similar to that measured in the preliminary characterization testing of AFC’s. The metric chosen for the blade pack selection is the blade operating cycle strain at 10 Hz. The average value for this metric is approximately 1100 microstrain. The data collected for the blade packs will be summarized in more detail in the next section.
3.5.3 Blade Pack Data and Selection

A summary of the data collected for the blade packs is presented in Table 3.12. The data are divided into packs selected for three different half-span blade sections and the complete integral blade. The testing of the three blade sections will be described in Chapter 6. An additional 40 packs were not included in the data summary. These packs were used for the preliminary manufacturing process development, for the initial testing of packs with screen printed electrodes and/or square fibers, or had reduced performance. Some of these packs were eventually used in other structural integrity tests of the AFC material system. The packs used in the summary data have the same composition and manufacturing process. The variations in the properties can be attributed to unintentional variations in the piezoceramic fibers or in the manufacturing process. The number of packs used in the test article is provided along with preliminary characterization measurements of the packs. The induced free strain performance at the original representative voltage cycle and the blade operating voltage cycle are then listed, as well as the number of repairs that were required during the poling process.
<table>
<thead>
<tr>
<th>Property</th>
<th>Blade Mean</th>
<th>Blade S.D.</th>
<th>Section 1 Mean</th>
<th>Section 1 S.D.</th>
<th>Section 2 Mean</th>
<th>Section 2 S.D.</th>
<th>Section 3 Mean</th>
<th>Section 3 S.D.</th>
</tr>
</thead>
<tbody>
<tr>
<td>number</td>
<td>42</td>
<td>-</td>
<td>4</td>
<td>-</td>
<td>12</td>
<td>-</td>
<td>12</td>
<td>-</td>
</tr>
<tr>
<td>thickness (μm)</td>
<td>170</td>
<td>6.0</td>
<td>162</td>
<td>5.2</td>
<td>165</td>
<td>2.9</td>
<td>166</td>
<td>4.6</td>
</tr>
<tr>
<td>mass (g)</td>
<td>5.14</td>
<td>0.095</td>
<td>4.72</td>
<td>0.015</td>
<td>4.80</td>
<td>0.096</td>
<td>5.08</td>
<td>0.056</td>
</tr>
<tr>
<td>elec. offset (μm)</td>
<td>64</td>
<td>62</td>
<td>194</td>
<td>63</td>
<td>109</td>
<td>46</td>
<td>107</td>
<td>64</td>
</tr>
<tr>
<td>C&lt;sub&gt;prepole&lt;/sub&gt; (nF)</td>
<td>2.58</td>
<td>0.47</td>
<td>1.84</td>
<td>1.07</td>
<td>1.72</td>
<td>0.31</td>
<td>2.35</td>
<td>0.60</td>
</tr>
<tr>
<td>C&lt;sub&gt;poled&lt;/sub&gt; (nF)</td>
<td>2.87</td>
<td>0.41</td>
<td>2.15</td>
<td>1.03</td>
<td>1.78</td>
<td>0.41</td>
<td>2.45</td>
<td>0.30</td>
</tr>
<tr>
<td>3kV strain (με)</td>
<td>911</td>
<td>100</td>
<td>660</td>
<td>116</td>
<td>790</td>
<td>189</td>
<td>840</td>
<td>95</td>
</tr>
<tr>
<td>4kV strain (με)</td>
<td>1168</td>
<td>111</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>1015</td>
<td>88</td>
</tr>
<tr>
<td>no. repairs</td>
<td>1.2</td>
<td>1.6</td>
<td>0.5</td>
<td>0.7</td>
<td>1.2</td>
<td>1.7</td>
<td>3.1</td>
<td>3.4</td>
</tr>
</tbody>
</table>

Several factors contributed to the selection of packs for each of the test articles. These include the free strain performance, the leakage current during poling, the number of repairs, and the order of manufacture. The packs used for the first blade section were selected from a small group of packs available in the early stages of the pack production. Packs with higher strain performance at the original representative voltage cycle were saved and used in the second blade section.

The packs used for the third blade section and the final blade were selected from a much greater pool. The best packs were used in the final blade. The criteria for selection were: 950 microstrain minimum for blade operating cycle, no more than 4 localized repairs, and less than 1 μA leakage current during poling. Three additional packs were selected to reach the 42 required for the blade. These packs had either lower strain performance, or more repairs. Twelve other packs, also having slightly lesser performance or more repairs, were used in the third blade section. The strain performance of the packs used in the third blade section and the integral blade are compared in Figure 3.52. The number of packs falling into each strain range which were used in either of the blades is plotted.
3.6 Summary of Actuator Capabilities

The effective properties of the final active ply configuration are presented in Table 3.13. These stiffnesses were determined from a Uniform Fields microelectromechanical model [Bent, 1995] using experimental stiffness data from the composite to adjust the active fiber compliance (1.22 times bulk PZT-5A compliance). The longitudinal or 1-axis is aligned with the fiber direction. The density was determined experimentally. The piezoelectric d-constants represent high field effective properties and are based on experimental measurements (1100 microstrain for 4000 Vpp).

<table>
<thead>
<tr>
<th>TABLE 3.13 Summary of Active Ply Properties</th>
</tr>
</thead>
<tbody>
<tr>
<td>thickness (mm)</td>
</tr>
<tr>
<td>density (kg/m^3)</td>
</tr>
<tr>
<td>d_{31} (pm/V)</td>
</tr>
<tr>
<td>d_{32} (pm/V)</td>
</tr>
</tbody>
</table>
A summary of the various structural integrity and actuation performance tests is provided in Table 3.14. The details of each of the tests performed can be found in previous sections of this chapter. The results are presented in terms of the test level and a maximum performance reduction. For the tensile strength and interlaminar shear strength, the samples were tested to failure. In each of the other tests, the actuation performance of the actuator was measured prior to and following the testing to estimate the maximum reduction.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Test Performed</th>
<th>Test Level</th>
<th>Max Reduction</th>
</tr>
</thead>
<tbody>
<tr>
<td>longitudinal tensile strength</td>
<td>0° laminate tensile test</td>
<td>18500 με</td>
<td>-</td>
</tr>
<tr>
<td>longitudinal tension</td>
<td>0° laminate actuation in tension</td>
<td>4000 με</td>
<td>6%</td>
</tr>
<tr>
<td>longitudinal tensile fatigue</td>
<td>0° laminate actuation after 1x10⁷ cycles</td>
<td>1250 ±900 με</td>
<td>5%</td>
</tr>
<tr>
<td>transverse tension</td>
<td>90° laminate actuation in tension</td>
<td>5500 με</td>
<td>0%</td>
</tr>
<tr>
<td>longitudinal compression</td>
<td>0° laminate facesheet on 4-pt. bending beam</td>
<td>3000 με</td>
<td>10%</td>
</tr>
<tr>
<td>interlaminar shear strength</td>
<td>90° AFC in glass laminate in 3-pt. bending</td>
<td>64 MPa</td>
<td>-</td>
</tr>
<tr>
<td>electrical fatigue</td>
<td>laminate actuation after 2.3x10⁸ rep. voltage cycles</td>
<td>3 kVpp</td>
<td>0%</td>
</tr>
</tbody>
</table>

In comparison with the requirements presented in Section 3.1.2 for the rotorblade application, the data presented show that the current AFC has sufficient capability. The maximum performance reduction in any test was 10% at the levels listed, which meet or exceed the worst case predictions for the blade. The exceptions are the electrical fatigue, where the test article was cycled at the original 3kV representative cycle rather than the 4kV blade operating cycle. Additional data collection is needed to add more data points to several of the tests and to test other properties such as in-plane shear and combined electrical/mechanical fatigue.
Chapter 4

INTEGRAL BLADE DESIGN

This chapter will present the design of the integral blade including considerations for the passive structure, the active plies, and the power distribution system. The baseline blade system and design requirements will first be discussed. Several active ply configurations and variations in the distribution of the actuators will be considered. Passive structural requirements are addressed. Next, the design of a flexible circuit for distributing power to the active plies will be presented. Finally, the blade design is summarized along with the predicted properties. The model code and input files used to predict the integral blade properties and performance are provided in Appendix C.

4.1 General Design Philosophy

The general design philosophy is to incorporate AFC actuators as active plies within a composite rotor blade to directly twist the blade. The active plies are an integral part of the blade structure. While the active plies could in theory be distributed anywhere in the blade cross section, the specific requirements for this design limited the application to the blade spar in order to maintain the section center of gravity without excessive weight penalties for balance weights. Variations in the distribution of the active plies within the spar are considered to optimize performance while meeting each of the design constraints. The passive structural plies are also varied in the design process.
4.1.1 Baseline Blade

A 1/6th Mach-scale Chinook CH-47D blade system was selected as a baseline for the integral blade model. The size of the system, roughly 10 feet in diameter, was suitable for testing at MIT on the Hover Test Stand Facility, which was constructed for this project. The size was also reasonable for the first demonstration of the integral actuation concept. In addition, significant data and manufacturing experience were available through Boeing-Philadelphia for this model system.

The model CH-47D blade, shown in Figure 4.1, has a span of 60.619 inches\(^1\) and a chord of 5.388 inches. It is designed to be used on a fully articulated hub with a single pin located at 0.15R (15% radius). The blade has built-in 12° linear twist and tapers from a VR7 airfoil at 0.85R to a VR8 airfoil at the tip\(^2\).

![Diagram of blade geometry](image_url)

**Figure 4.1** Model blade geometry showing possible active ply locations (shaded).

---

1. Blade station measured in inches from the center of rotation, e.g. BS30.3, or as a ratio of the total span, R, e.g. 0.50R.

2. VR7 and VR8 are airfoil designations of The Boeing Company.
The model blade is similar in construction to the composite full-scale CH-47D blades [Sandford, 1980]. The primary structural member of the model blade is a co-cured "D" spar, while the aft fairing is added in a secondary cure. For the active blade design, several of the materials used were updated to reflect current best practices. E-glass fabric, S-glass unidirectional, and IM7 unidirectional tapes are used with a Rohacell foam core. A film adhesive is also used around the core and for secondary bonds. Additional details on the blade materials will be provided in Chapter 5.

4.1.2 Design Objectives and Constraints

In general, the integral blade design was intended to have nominally the same elastic properties as the baseline blade. In updating the materials and lay-ups, the target stiffness and inertial properties of the blade were maintained with the exception of the torsional stiffness. A reduction in torsional stiffness allows for greater blade twist for a given level of actuator authority. Thus the target torsional stiffness was set at 50% of the nominal baseline value. Results from a rotor dynamic analysis of the modified blade suggest that the changes would have no detrimental effects [Derham, 1996]. This analysis supported the selected target twist level requirement for HHC.

The design process was directed at achieving a number of goals for the model blade. While the model blade will only be used for hover testing at the MIT Hover Test Stand Facility, it was designed to meet strength requirements representative of a full-scale service environment in order to demonstrate the viability of this approach. The following is a summary of the design requirements:

- ±2° tip twist
- <20% mass increase over baseline model blade with updated materials
- no less than 50% of nominal torsional stiffness of baseline model blade
- Equivalent axial, bending, and shear stiffness of nominal baseline blade
- Section center of gravity at quarter-chord
- Ply strain levels do not exceed allowables
• Provide passive load transfer path in plies surrounding active plies
• Counter crack propagation and delamination failure modes
• Manufacturability
• Adequate electrical insulation

The variables in the design include the amount and placement of the active material and the passive ply lay-up of the spar. Other details of the design that are considered include manufacturability, actuator pack design, power distribution, and interconnections with the hub.

Candidate configurations for the active blade were developed by adding various numbers of active composite plies to the baseline spar laminate. For each configuration, the passive lay-up for the blade was then modified to match the target stiffness values at a representative section. Leading edge mass was then added to properly locate the center of gravity of the section at the quarter-chord.

The static loads used in this analysis were estimated from the design loads for full-scale blades, including the assumption of 20% rotational overspeed in addition to a 1.5 ultimate load factor for the CF and using scaled 3g maneuver loads for moments and shear. The fatigue loads were based on analytical predictions from Boeing's TECH-01 analysis [Shultz, 1994] for high-speed forward flight. The centrifugal loading includes the contribution of the added blade mass from the distributed actuators.

4.1.3 Model Blade Scaling

The baseline model blade is Mach scaled from the Boeing CH-47D with a geometric scaling of 1:5.939 (approximately 1/6th scale). Mach scaling was selected to provide actuator performance data which would be the most applicable to the development of actuators for the full scale blade. Mach scaling has also been suggested as the most appropriate for testing vibration reduction [Friedmann, 1998]. The tip speed and internal stresses are on the same level as full-scale values. Also in Mach scaling, the torsional stiffness of the blade is more realistic in comparison with a Froude-scaled system. The model blade is aerody-
namically similar to the full scale blade. The mass distribution and torsional stiffness properties were allowed to vary in order to achieve the design goals for twist, as described in the design requirements section below. The predicted first torsional mode of the active blade remains greater than 3.5/rev. The first torsional mode of the baseline blade was near 5/rev. The actual measured properties of the fabricated integral blade used in the hover tests are described in Chapter 7.

Another important rotary-wing aeroelastic scaling parameter is the Lock number [Johnson, 1980]. This is the ratio of aerodynamic to inertial forces in the blade. It is defined:

\[ \gamma = \frac{(\rho acR^4)}{I_b} \]  

(4.1)

where \( \rho \) is the fluid density, \( a \) is the blade section two-dimensional lift curve slope, \( c \) is the blade chord, \( R \) is the blade radius, and \( I_b \) is the blade flapping inertia. For the full-scale CH-47D, the Lock number is 9.35. The 1/6th Mach-scale baseline blade has a Lock number of 8.32. The predicted Lock number for the integral blade is 9.23.

4.1.4 Active Blade

The basic design concept of the active blade is to replace some portion of the passive composite materials in the blade spar with active plies. The shaded regions in Figure 4.1 illustrate the regions of the spar laminate considered. The active plies may be embedded in the upper and lower spar laminates and possibly in the web. Regardless of the position around the spar contour, the active plies will be oriented to induce shear strains which will in turn twist the blade.

The distribution of active plies in the fairing portion of the blade was also considered. The low stiffness of the fairing skin could enable more effective twist actuation performance. However, the addition of actuator mass in the aft section of the blade would require a significant addition of nose weights to maintain the section center of gravity at the quarter chord. While the baseline blade does require nose weights to properly position the section
center of gravity, the addition of the active plies does not require any additional nose weights. The total mass constraint for the active blade would severely limit the addition of active plies to the fairing. This penalty precludes the integration of active plies within the fairing for this design.

Actuators can be manufactured with the piezoelectric fibers aligned at any angle. For this application, the fibers are aligned at a 45° angle to the longitudinal axis of the actuator pack, while the longitudinal axis of the actuator pack is aligned with the longitudinal axis of the blade. In the blade spar, the active plies alternate from +45° to -45°. By actuating the +45° and -45° plies opposite to each other, i.e., extending the fibers in the +45° plies while contracting them in the -45° plies, the actuators will produce a shear deformation of the spar laminate. By then coordinating all the actuators in the spar, the result is a twisting of the entire blade.

4.2 Blade Design Procedure

The blade design procedure relies upon a number of modeling tools to predict the properties of prospective designs. Variations in the amount and distribution of active plies are evaluated. The design of the passive blade structure is also included. The final detailed summary of the blade design will be provided in Section 4.4.1.

4.2.1 Modeling Tools

Several modeling tools were used in the design of the integral blade. The primary model used was a relatively simple mathematical actuation model developed to analyze the integral twist concept [du Plessis, 1995 and du Plessis, 1996]. The model employed is a modified Rehfield thin-walled, single-celled beam model and is used to predict the static twist capability of prospective blade designs [Rehfield, 1985]. Cross-sectional properties of the beam are calculated and used in the formulation of a beam finite element model. The complete model code used in this research is provided in Appendix C.
The Rehfield beam model was augmented with anisotropic active plies for predicting structural actuation. The following assumptions apply to the single-celled beam model:

- Cross-sectional shape is maintained during deformation
- Wall thickness is small in comparison to other dimensions
- Transverse, in-plane, normal stresses are negligible (no internal pressure)
- The twist rate varies spanwise and acts as a measure of the cross-sectional warping

There is no restriction on the cross-sectional shape of the beam. The properties of the cross-section may vary around the contour. The actuation stress resultants are developed from the piezoelectric constitutive relations [Bent, 1995b] and Classical Laminated Plate Theory [Jones, 1975] applied around the contour.

For a given cross-section, the modified Rehfield model is used to predict the effective stiffness, mass, center of gravity, and actuator-induced forces and moments of the single-celled beam. In order to account for the effects of the other components of the model blade, lumped stiffness and mass terms are added to the section stiffness matrix. This includes the contributions of the leading edge weights, foam core material, and fairing. Estimated stiffness and mass terms are added to the diagonal elements of the stiffness produced as an output of the sectional analysis\(^1\). This modified stiffness matrix is then used along with the unmodified forcing vector in the beam finite element formulation of the blade.

A comparison of the predicted mass and stiffness properties for the spar of the blade with predictions for the complete blade from another analysis [Weems, 1996] were used to estimate the mass and stiffness of the unmodeled components. A multi-cell beam analysis which applied a simple St. Venant-type warping function but modeled all cross-sectional elements was used in the comparison. This model is a part of Boeing’s Blade Design Environment (BDE) analysis package. A second model was used at Boeing to more accu-

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1. Note that this method neglects the effect of the unmodeled stiffness on the predicted actuator-induced forces and moments.
rately predict the torsional stiffness. It is based on an MSC/Nastran\textsuperscript{1} laminated plate finite element model (FEM) of the blade which includes solid elements for the nose block and core. This technique of using the more complex models to correct the modified Rehfield formulation enabled a rapid analysis capability for prospective blade designs at MIT.

A beam finite element model was implemented using the cross-sectional properties from the modified Rehfield model to develop the section stiffness matrix and forcing vector. This also allows for piecewise variations in the cross-section of the beam. In addition, distributed loads may be applied. A static solution for the deformation of the beam yields the effective twist distribution used as a primary metric in the design process.

Additional modeling tools at Boeing Helicopters (Philadelphia) were used to check predicted section properties, estimate design loads, and perform a stress analysis [Weems, 1996]. The models include the BDE model, described above, and the TECH-01 rotor dynamic model [Shultz, 1994]. The BDE model was used to predict the stiffness of the blade and to estimate the material strain levels under a prescribed loading. TECH-01 was used to estimate blade loads and rotor system dynamics, and to predict the rotor system response to the twist actuation.

### 4.2.2 Blade Composite Materials

The passive composite materials used in the design were selected from current materials used in other Boeing rotor programs. The materials include an E-glass fabric, an S-glass unidirectional tape, and an IM7 graphite unidirectional tape. Some typical values used in the design process are presented in Table 4.1. Boeing proprietary data on the fatigue strength of the composites was used in addition to the static tensile strength values presented. Several other materials used in the integral blade including adhesives, foam, and leading edge weights will be further described in the manufacturing discussion of Chapter 5.

\textsuperscript{1} MacNeal-Schwendler Corporation, Los Angeles, CA
TABLE 4.1  Summary of Composite Material Properties\textsuperscript{a}

<table>
<thead>
<tr>
<th>Fiber</th>
<th>E-glass</th>
<th>S2-glass</th>
<th>IM7</th>
</tr>
</thead>
<tbody>
<tr>
<td>resin</td>
<td>F-155</td>
<td>SP381</td>
<td>SP381</td>
</tr>
<tr>
<td>weave</td>
<td>style 120 fabric</td>
<td>uni tape</td>
<td>uni tape</td>
</tr>
<tr>
<td>manufacturer</td>
<td>Hexcel</td>
<td>3M</td>
<td>3M</td>
</tr>
<tr>
<td>thickness (mm)</td>
<td>0.120</td>
<td>0.225</td>
<td>0.138</td>
</tr>
<tr>
<td>density (kg/m³)</td>
<td>1700</td>
<td>1850</td>
<td>1550</td>
</tr>
<tr>
<td>$E_L$ (GPa)</td>
<td>20.7</td>
<td>46.7</td>
<td>165</td>
</tr>
<tr>
<td>$E_T$ (GPa)</td>
<td>20.7</td>
<td>12.1</td>
<td>8.83</td>
</tr>
<tr>
<td>$\nu_{LT}$</td>
<td>0.13</td>
<td>0.28</td>
<td>0.34</td>
</tr>
<tr>
<td>$\sigma_L$ (MPa)</td>
<td>393</td>
<td>1675</td>
<td>2468</td>
</tr>
<tr>
<td>$\sigma_T$ (MPa)</td>
<td>393</td>
<td>57</td>
<td>38</td>
</tr>
<tr>
<td>distinguishing quality</td>
<td>durable fabric</td>
<td>high strength</td>
<td>high modulus</td>
</tr>
</tbody>
</table>

\textsuperscript{a}  manufacturer's data

4.2.3 Spar Lay-up Design

The analysis of preliminary integral blade configurations established a general set of specific lay-up requirements for the design of the blade. These provide direction for the design studies in which various parameters are investigated for possible improvements offered to a given design configuration. The requirements, formed from the previously described design constraints, limit the design space and thus simplify the design process.

The starting point for the model blade design was to add active composite plies to the baseline spar laminate in order to maximize the twist. The passive lay-up for the blade was then modified to achieve reasonable stiffness values. Leading edge mass is added to properly locate the center of gravity of the section at the quarter-chord. A strength analysis of the blade is performed to determine maximum strain levels in each of the ply materials [Weems, 1996].

The strength analysis checks the strain levels at critical locations around the spar contour as shown in Figure 4.2. These include locations where chordwise and flapwise bending
stresses and torsional shear stresses are high, as well as transition points in the spar laminate. The analysis is repeated at a total of 19 critical spanwise stations along the blade. The spanwise checkpoints are well distributed and include transition points for the lay-up of the blade. At each checkpoint, the longitudinal (along the fibers) ply stresses and strains are calculated for comparison with established design allowables for each material.

![Figure 4.2 Stress checkpoints in blade cross section.](image)

The worst case combination of loads is applied simulating 160 kt forward flight and 20% rotational overspeed. The maximum and minimum static design loads were determined from scaled data from CH-47D rotor blades. Fatigue design loads are based on predictions from Boeing’s TECH-01 analysis. The centrifugal loading includes the contribution of the added blade mass from the distributed actuators.

Factors of safety are also included in the analysis. An ultimate load factor of 1.5 is applied in addition to the 20% overspeed assumption to yield a total load factor of 2.16. Utilizing the forward flight loads adds additional conservatism to the design of the current blade to be tested in hover. The strength analysis were used to develop the actuator material requirements presented in Chapter 3. The worst-case predicted strain levels will be summarized in Section 4.4.2.

The results of preliminary analyses showed that two to four active plies could be integrated into the spar/web laminate. The required tip twist set the minimum actuator authority while the mass constraint and constraints on the total spar laminate thickness limited the maximum. Active plies were excluded from the nose of the spar to avoid the high curvature and high strain levels resulting from chordwise bending.
In order to ensure the structural integrity, maintain the performance, and provide additional insulation for the active plies, passive plies are required to surround the active plies. The characterization studies of the active fiber composites have shown that passive plies laminated to the active fiber composites have been provide a load transfer path around cracks in the ceramic fibers. The passive plies may also inhibit the propagation of cracks in the active plies. Passive plies also can be used to minimize strain levels in the active plies resulting from loading.

For the passive plies, the approximate required number of each type of ply could be established from stiffness and strength requirements. Unidirectional plies with fibers running spanwise are required to carry the centrifugal loads and to provide flapwise and chordwise stiffness to the spar. These plies run continuously from the blade tip, around the lead-lag pin, and back to the tip. Two plies of IM7 graphite or roughly 4 plies of S-glass are sufficient to carry the CF loads. While the graphite option offers the lesser mass, the S-glass offers higher strain allowables. A compromise of one IM7 graphite ply and one to two S-glass plies was a reasonable solution. In addition, the graphite plies are conductive and must be insulated from the active plies. This constraint is more easily met with fewer graphite plies and more insulating S-glass plies. Placing the unidirectional plies adjacent to active plies in the lay-up is also useful for transferring the significant centrifugal loads from the dense active material.

In order to achieve the torsional stiffness and strength constraints, ±45° plies are necessary. With the constraint that the torsional stiffness of the blade should be half that of the baseline model blade, the original +45° and -45° S-glass plies in the baseline blade were replaced with ±45° E-glass fabric to reduce the stiffness. The E-glass plies have a lower modulus and half the thickness of the stiffer S-glass. The ±45° active plies, which will be distributed along the spar, also provide significant torsional stiffness. In addition, the S-glass would have to be distributed in +45°/-45° pairs in order to create a more balanced spar laminate.
In the passive sections of the blade inboard and outboard of the active plies, replacement plies are used to maintain torsional stiffness and strength. A $+45^\circ$ and a $-45^\circ$ graphite ply were selected to increase the torsional stiffness with minimal weight. These replacement plies were later changed to glass plies for the final blade after preliminary tests on a half-span blade section. The change reduced the risk of interaction between the active plies and the conductive graphite.

One E-glass ply is constrained to the outer skin of the spar, as well as the fairing as a standard manufacturing practice. Three E-glass plies at $\pm 45^\circ$ are required for the web. Similarly, at least 3 E-glass plies are required in the spar laminate, including the outer skin. Strength concerns are the primary drive for this requirement. The E-glass along with the S-glass can be placed adjacent to the active plies to improve overall reliability.

All spar plies with the exception of the active fiber composites are continuous around the nose of the blade. Additional filler plies can be added to reduce the drop-off at the edges of the active plies. Otherwise, the nose lay-up is generally similar to that of the spar.

### 4.2.4 Actuator Placement

In order to maximize performance of the integral blade, the distribution and placement of the active plies was considered. The following subsections will discuss the chordwise and spanwise distribution of the active material.

**Chordwise**

Another design parameter is the location of the active plies around the spar contour. Near the leading edge (refer to Figure 4.1), the increasing curvature of the spar surface restricts the active fiber composites. Excessive curvature can create significant bending stresses in the active fibers. The relatively large diameter of the fibers is the true source of the problem. Another concern near the leading edge is increased loading from chordwise bending stresses. There is no rigid limitation on placement toward the aft of the spar. However, the section center of gravity must be considered when placing active material aft of the quar-
ter-chord. Given these constraints, the optimal active ply location is between 0.024c (0.13 inches from the nose) and 0.380c (or the spar heel).

Another consideration in the chordwise placement is the total width of the activeply. Each active ply is composed of segmented active fiber composites. These active plies have a certain dead area associated with the interdigitated electrodes. The percentage of dead area in the active ply is reduced for greater widths. Thus for active plies placed in the web, the relative dead area is significantly greater (roughly 3 times) than for the upper and lower spar. Actuator placement in the web also complicates manufacture in terms of wiring since the passive web plies wrap around the upper and lower spar surfaces. Web actuation will be considered for comparison with upper and lower spar laminate actuation.

**Spanwise Distribution**

Several factors constrain the spanwise distribution of active plies in the model blade. For performance, the spanwise distribution of induced aerodynamic forces must be considered rather than just the tip twist. Adding active material inboard changes the angle of attack over a greater length than adding the same material outboard. In addition, the increase in centrifugal loads are less significant inboard. On the other hand, strains in the active material will be generally higher inboard as a result of centrifugal loads. The spanwise placement of the added actuator mass also effects the dynamic modes of the blade. A uniform distribution of mass has a lesser effect than concentrating the mass in any one region.

From 0.27R inboard to the blade root, the section stiffness of the blade significantly increases due to added spar plies to react the high centrifugal loads. As a result, active plies are only considered outboard of 0.27R. For the outboard extent, the important considerations are the induced aerodynamic forces and the increased centrifugal loads near the root. For a marginal increase in outboard extent, the blade twist is increased from the location of the added active ply to the tip, but the centrifugal load is also increased at the root. The further outboard the marginal increase is considered, the greater increment in the root loads and the lesser the increment in the total length of the blade with increased
angle of attack. An increased lift coefficient counteracts this latter effect until approximately 0.98R where tip losses begin to take effect. Increasing the spanwise extent from 0.85R to 0.95R will increase the root loads by 10% while increasing the aerodynamic forces by 20%. The linear trends are plotted in Figure 4.3. For the current design, the maximum outboard extent of 0.95R was selected. Adding active material outboard of 0.95R becomes increasingly difficult because of the taper in the airfoil section.

![Figure 4.3](image)

**Figure 4.3** Comparison of root centrifugal load and relative total lift for varying spanwise extent of active plies relative to a 0.85R extent.

Another trade study compared two designs with the same amount of active material but uniformly distributed (0.27R-0.95R) in one case, and concentrated over half the span (0.27R-0.61R) in the second case. Nominally, the tip twist should be equal for the two cases. A doubling of the actuator authority per unit span might be expected to double the induced twist rate. If this were the case, the twist distribution would differ, with the second design achieving the twist inboard of the first design, thus providing better performance (30% steady lift increase). However, the doubling of the active material in a given spar section does not double the induced torsional moment. This is a result of the increase in stiffness due to the active plies. As the relative contribution of the active plies to the
total blade stiffness increases, the effectiveness of the active plies decreases as induced stress actuators. The active plies move closer to the limitation of induced strain actuation. In addition, an extra passive ply would have to be added for strength, which would further reduce the performance.

The two designs are compared in Figure 4.4 and Figure 4.5. The induced twist and section lift are shown as a function of span. Clearly, the initial uniform distribution achieves greater performance with greater twist over the entire span, and therefore greater section lift. The total lift for the double actuation case is 92% of the uniform case. Although other distributions of active material could result in better total performance, constraints on the mass distribution of the blade and on the complexity of the manufacture support a uniform distribution for this specific blade design.

![Figure 4.4](image1.png) ![Figure 4.5](image2.png)

**Figure 4.4** Comparison of relative induced twist for two spanwise distributions of active plies. **Figure 4.5** Comparison of relative section lift for two spanwise distributions of active plies.

### 4.2.5 Design Comparisons

Four designs were developed with differing levels of actuator authority in the blade. In each, the amount of actuator material around the spar is different. Each design attempted
to meet each of the design constraints, while maximizing the induced twist. The four types are: 2 active plies in upper and lower spar walls, 2 active plies in upper and lower spar walls and the web, 3 active plies in the upper and lower spar walls, and finally 4 active plies in the upper and lower spar walls. Figure 4.6 depicts the spar lay-up for each case. The model-predicted induced moment and twist are compared for each case in Figure 4.7 and Figure 4.8 using a nominal 1100 microstrain for the active plies. Figure 4.8 also includes dashed lines representing the initial design constraints. Note that the mass is stated relative to the baseline model blade with updated materials and that the material properties used in this comparison differ slightly from those in the final design predictions.

![Diagram of ply lay-ups](image)

**Figure 4.6** Two, three, and four active ply spar lay-ups compared.

The induced twist moves toward an asymptote with increased active material. This occurs as the ratio of active mass to passive structural mass increases. The actuation of the blade spar approaches the induced strain limit of the active material.

In general, the 2-ply design did not have sufficient authority to meet the desired twist. The addition of active material to the web lay-up was found to be very effective but was limited by manufacturability, increased torsional stiffness, and center-of-gravity related weight penalties. The 4-ply design was most effective, but involved excessive spar wall thickness and possibly excessive mass. The 3-ply design nearly meets all of the requirements. One drawback of this design is the unbalanced spar laminate which results in an extension/twist-coupled blade. This was not expected to have any significant effect on the predicted blade performance for two reasons: the extensional (centrifugal) loads are steady, and any
dynamic extension of the blade resulting from the coupling should not affect aerodynamic performance.

4.3 Actuation System Design

With the distribution of the active plies selected, the details of the component AFC packs and associated electrical connections can be considered. In addition, the distribution of strain gage sensors for both safety and performance monitoring will be discussed.

4.3.1 Pack Design

The active plies which are distributed in the upper and lower spar laminates are divided into actuator packs in order to simplify manufacturing and increase reliability. In theory, segmenting each active ply into multiple sections does not reduce the overall performance of the blade. However in practice, dead area around the perimeter of each pack slightly reduces the effectiveness. From a manufacturing standpoint, making a pack with a length on the order of 0.15 m is much easier than one which is closer to 1 m. The smaller packs allow for a more reliable manufacturing process. The process is repeated a larger number
of times which improves the quality of the packs as skills improve. Extra packs can be manufactured at little additional cost so that only those with the highest performance are integrated into the final blade. Reliability is also improved during blade operation since packs can be wired independently. This reduces the effect of a short-circuit failure on the overall performance of the blade.

The fact that the packs will be independently wired creates an additional design constraint. The smaller the actuator segments, the more packs there will be, and the more wires will be required. The total length of each active ply is 1.047 m of which 6 are required. Segmenting each layer into 7 pieces results in a repeated length of about 15 cm and a total of 42 packs. This design maintains a pack size which is manufacturable while increasing the number of leads and internal connections to a high but reasonable level. The electrical component of the design will be discussed in the next subsection.

Blade strength and endurance concerns also drive the actuator pack design. One issue is the transition between the active plies and the adjoining passive plies on the inboard and outboard ends. Rather than dropping all 3 active plies at the same blade station, each ply is shifted by 12.7 mm to stagger the transitions. Another concern is a delamination failure within an active ply which could propagate through the brittle layer of ceramic fibers. Allowing for a gap (5 mm) between adjacent packs will enable glass plies from above and below the active ply to fill the space in between, thus creating a discontinuity in the active material. Figure 4.9 illustrates the segmented active ply design. Although the gaps increase the effective dead area in the active ply, the reliability of the blade is increased.

4.3.2 Power Distribution

The electrical connections from the packs to the leads supplying the power are placed along the web of the blade. This arrangement allows direct access to the aft edges of all of the packs. The leads are not embedded within the spar core, which simplifies the assembly but places mass aft of the quarter-chord. Placing the leads along the web also allows for connections to be made after the spar is cured.
A simple method to distribute power to the packs would rely on a positive and negative bus along the web. However, this arrangement does not allow access to the individual packs after the blade is cured. Independently connecting one flap from each pack would enable disconnection of a single pack in the event of an internal short circuit. However, common lead will deliver a constant DC voltage. In the event of a short between the DC voltage and some external conductive material at the edge of a pack, there would be no means of disconnecting the pack.

Individual connections to each electrode flap require 84 leads along the web of the blade. Another option considered involved surface mounted jumper connections for each pack to a main power bus. This concept was rejected because of the high voltages involved (4000 V) and the associated safety concerns. Individual connections allow each pack to be tested in situ, poled in situ if necessary, and actuated independently. Although it is not required for the initial hover testing, independent control of the packs allows for more degrees of freedom in the control of blade vibrations.

The rails are in turn connected to the external leads through the electrode flaps. One flap is connected to each of the two rails. Both flaps can be placed along a single edge in order to constrain all connections to the edge of the pack along the web of the spar. While the flap connecting the forward rail (toward leading edge) is constrained to the end of the pack where the rail terminates, the flap on the other rail may be placed at any point along the length. This degree of freedom allows for placement to avoid interference with the electrode flaps from other packs in other active plies. Figure 4.10 depicts the relative position
of the electrode flaps from the packs in each of the three active plies in the upper and lower spar laminates.

\[\text{Figure 4.10} \quad \text{Unwrapped view of spar laminate showing positioning of electrode flaps for connection to flexible circuit.}\]

**Flex Circuit Design**

A lightweight, low volume solution for distributing the power to the packs is a flexible circuit. The flex circuit consists of multiple parallel lines of copper arranged in layers with kapton insulating layers in between. Each line of the flex circuit terminates with a 0.1" by 0.1" solder pad to be connected to a particular electrode flap. Figure 4.11 presents a diagram of the flex circuit interfacing with electrode flaps on the spar web. The area of the copper cross section was designed to carry several times the current required to drive a single pack (100 mA), and also to carry the total amplifier current in the event of a short circuit (1 A). The space between lines of copper within a layer was designed to meet the 4000 Vpp voltage isolation requirement. An epoxy-based adhesive separates adjacent copper lines, while layers of kapton separate adjacent layers.

Given these constraints, the 1 oz./ft.\(^2\) copper with a 10 mil width was selected for the conductor, with two layers of 1 mil kapton separating layers. This allows for 14 lines across the width of the web, with an allowance for solder pads along the upper and lower edges to interface with the electrode flaps as shown in Figure 4.12. A total of 6 flex circuit layers are required for the blade. Each layer drops off after reaching 14 connection locations such that only one layer reaches the most outboard packs.
Figure 4.11  Interface between flex circuit and active ply contacts on web of spar.

Figure 4.12  Drawing of flex circuit and interface with pack electrode flaps on web of spar.
Hub Interface

At the inboard end of the web, the flex circuit exits the blade and terminates below the hub. Here each flex circuit layer is connected to a common printed circuit board (PCB). The PCB serves as a matrix connector, connecting individual lines from the flex circuit to a total of five source wires. The five high voltage lines are connected to amplifiers in the stationary frame through a slip ring. Of the five lines, one is a common DC signal, one is for the $+45^\circ$ active plies in the upper spar, one is for the $-45^\circ$ active plies in the upper spar, and the remaining two are for the active plies in the lower spar. This enables signals of opposite phase to be used to drive the $+45^\circ$ plies and $-45^\circ$. In addition, actuation of the upper and lower spar laminates is independently controlled.

4.3.3 Sensor Design

The primary sensor to be used as a performance monitor for the integral blade is the strain gage. Strain gages embedded in the blade will be also used as safety-of-flight monitors of the root strain levels during hover testing. Other strain gages will provide data on the deformation of the blade.

The integral blade will incorporate a total of 12 strain gage bridges. The maximum limitations are the number of required leads and the number of available slip ring channels on the test stand. Three types of strain bridges will be used: torsional strain, flap bending strain, and chordwise bending strain. Three of the strain gages (one of each type) will be dedicated to safety-of-flight monitors at the root of the blade. Two other flap bending gages will be used to monitor the flap bending deformation, especially the excitation of resonant modes. The remaining 7 gages will be torsional strain gages primarily measuring induced twist performance of the blade.

Several factors affect the placement of the sensors. In general, the strain gages are located along the pitch axis on the inner surface of the spar laminate (upper and lower). The dynamic modes of the blade and the location of the active plies determine the spanwise distribution. Interference between the gages and required leads is also avoided. The flap
bending gages were located in order to observe the first three elastic bending modes. The torsion gages were distributed such that one bridge aligns with the center of each group of packs. The gage locations are summarized in Table 4.2.

<table>
<thead>
<tr>
<th>Number</th>
<th>Type</th>
<th>Location (r/R)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Chord Bend</td>
<td>0.196</td>
</tr>
<tr>
<td>2</td>
<td>Flap Bend</td>
<td>0.185</td>
</tr>
<tr>
<td>3</td>
<td>Torsion</td>
<td>0.205</td>
</tr>
<tr>
<td>4</td>
<td>Flap Bend</td>
<td>0.390</td>
</tr>
<tr>
<td>5</td>
<td>Flap Bend</td>
<td>0.750</td>
</tr>
<tr>
<td>6</td>
<td>Torsion</td>
<td>0.319</td>
</tr>
<tr>
<td>7</td>
<td>Torsion</td>
<td>0.416</td>
</tr>
<tr>
<td>8</td>
<td>Torsion</td>
<td>0.513</td>
</tr>
<tr>
<td>9</td>
<td>Torsion</td>
<td>0.610</td>
</tr>
<tr>
<td>10</td>
<td>Torsion</td>
<td>0.707</td>
</tr>
<tr>
<td>11</td>
<td>Torsion</td>
<td>0.804</td>
</tr>
<tr>
<td>12</td>
<td>Torsion</td>
<td>0.901</td>
</tr>
</tbody>
</table>

4.4 Design Summary

The final design of the integral blade will be presented in this section. In addition, the predicted performance, elastic and inertial properties, and limit strain levels in the active plies are provided. Further details of the design can be found in Appendix A.

4.4.1 Final Configuration

The final active blade design features three active plies in the upper and lower spar laminates between 0.27R and 0.95R. In the chordwise direction, the active plies extend from 0.024c to 0.380c at the heel of the spar. Each active ply is divided into 7 packs for a total of 42. Two electrical connections with a flexible circuit along the web of the spar provide
power to each pack individually. The locations of the active plies and flex circuit within the blade are shown Figure 4.13.

![Diagram of integral blade showing location of active plies and flex circuit in planform and cross-section views.](image)

**Figure 4.13** Drawing of integral blade showing location of active plies and flex circuit in planform and cross-section views.

The lay-ups for the typical section of the blade are presented in Figure 4.14. The sketches depict the transitions between the nose and spar laminates, at the forward edge of the active plies, and also the transition near the heel of the spar. Here, the web plies and fairing skin overlap the spar laminate (over approximately 0.25”). The passive plies in the spar of the integral blade (0.337R-0.65R) include an inner graphite unidirectional ply, three ±45° E-glass fabric plies, and one S-glass unidirectional ply. The web consists of three ±45° E-glass fabric plies. The graphite ply is placed under the web plies in order to provide additional insulation between it and the electrode contacts for the packs.

Each of the passive spar plies continues around the nose. In addition, two S-glass filler plies are used in the nose to maintain the laminate thickness forward of the active plies. An S-glass nose block encapsulates the tungsten leading edge weights to maintain the section center of gravity at the quarter chord. The complete details of the blade lay-up including spanwise variations and root-end reinforcement are provided in Appendix A.
4.4.2 Predicted Performance

A summary of the predicted properties for the design relative to target values is given in Table 4.3. The predicted twist rate was 14% below the target required for 4° of peak-to-peak tip twist. This was the result of the 3-ply configuration selected and updated active ply material properties obtained during the characterization testing. The predicted torsional stiffness exceeds the target by 7% due to the stiffness of the active plies and strength-driven passive ply requirements.

<table>
<thead>
<tr>
<th>Property</th>
<th>Prediction</th>
<th>% Target</th>
</tr>
</thead>
<tbody>
<tr>
<td>mass (kg)</td>
<td>0.697</td>
<td>98</td>
</tr>
<tr>
<td>twist rate (deg/m)</td>
<td>3.26</td>
<td>86</td>
</tr>
<tr>
<td>EA (MN)</td>
<td>6.09</td>
<td>107</td>
</tr>
<tr>
<td>GJ (Nm²)</td>
<td>111</td>
<td>107</td>
</tr>
<tr>
<td>EI_{flap} (Nm²)</td>
<td>186</td>
<td>91</td>
</tr>
<tr>
<td>EI_{lag} (Nm²)</td>
<td>4280</td>
<td>99</td>
</tr>
</tbody>
</table>
The ultimate design strains predicted for the active plies in a blade subjected to maneuver loads are 5100 microstrain tension and 3600 microstrain compression, both of which occur at blade station 0.337R [Weems, 1996]. While such strain levels could permanently degrade the performance of the active plies, they would not produce structural failure. Considering the 1.5 ultimate load factor and the limited area over which these high strains occur, this is certainly acceptable for hover testing and potentially for a forward flight design as well. The most severe fatigue strains predicted for the active plies are 680±660 microstrain, which is below the level at which minimal degradation in 10 million cycles was demonstrated during the characterization tests (Section 3.2.5).

The peak actuator strains predicted for steady hover testing are 819 microstrain tension and 173 microstrain compression in the extremes [Weems, 1996]. If a load factor of 2 is applied to all loads except CF to account for unsteady effects, the peak actuator strains are 1277 microstrain tension and 685 microstrain compression. Under these conditions, minimal damage accumulation is expected for the active composites. Based on the characterization data for the AFC’s (Section 3.4.3), compressive stress depolarization is not expected to be a problem. Note that the strain predictions do not include the effects of actuation, which will increase the maximum stress levels in the active plies. A more complex analysis including thermal prestress, applied electric field, and temperature effects would provide a more accurate gauge of the state of polarization in the piezoceramic.

The rotor system dynamics will also affect the twist performance of the integral blade. Aerodynamic loads are not expected to reduce twist, but may add damping to the system [Derham, 1996b].
Chapter 5

BLADE MANUFACTURE

This chapter describes the fabrication of the integral blade used for hover testing. The manufacture of three half-span blade sections will also be discussed. Boeing Helicopter (Philadelphia) provided the basis for the model blade manufacturing procedures. These procedures were modified in order to integrate the active plies and power distribution system. Each step of the process will be presented along with supporting drawings and photographs. The photographs are grouped at the end of the chapter. Additional drawings and details can be found in Appendix A. Details of the AFC pack manufacture have been described in Chapter 2.

5.1 Introduction

The 1/6th Mach-scale CH-47D model rotor blade consists of a composite D-spar and an aerodynamic fairing as shown in Figure 5.1. An overview of the manufacture of the full-scale CH-47D composite blades, which are similar in construction to the model blades, has been published [Sandford, 1980]. The composite plies are wrapped around a foam mandrel and are then cured in a mold. The main spar plies wrap around the nose and end at the web and the web plies wrap around the heel of the spar. The plies overlap on the upper and lower surface to form a lap joint. The spar is fabricated first and is cured with a filler block in the aft portion of the mold. Then the fairing is attached in a secondary cure
with the filler block removed. A flex circuit may also be included along the web for power connections.

![Diagram of blade components](image)

**Figure 5.1** Drawing of blade components for assembly.

Additional reinforcing plies are used near the root of the blade. The rotor is articulated and the composite blade root connects to the pitch shaft and hub through a vertical pin. The axial unidirectional plies in the blade wrap around the root pin hole to transfer the centrifugal loads. Several added layers of S-glass are required to carry the loads near the root. Additional reinforcing plies are used to ensure adequate load transfer between the fairing and the root of the blade.

The cure is performed in an aluminum mold obtained from Boeing Helicopters (Philadelphia). Heater cartridges inserted throughout the aluminum mold are used in conjunction with thermocouples in a dual-loop feedback control system. The inner and outer halves of the blade have separate thermal control loops. Large clamps on the outside of the mold and back pressure from the foam mandrel, which is slightly oversized, provide the cure pressure for the composite. No vacuum bag is used.

The general procedures used in the blade fabrication process were obtained from Boeing Helicopters (Philadelphia). Boeing has significant experience in making model scale rotors for wind-tunnel testing using the methods described. The following sections will provide a detailed description of the procedure.

---

1. Richard Bussom, Boeing Helicopters, Rapid Prototyping, Philadelphia, PA
5.2 Component Materials

The blade component materials were selected to conform with Boeing standard blade manufacturing practices. The materials include three main structural composites: E-glass fabric, S-glass tape, and graphite tape. Some typical properties for these composites were provided in Section 4.2.2. The composites are prepgregs which have compatible cure schedules. The composites are reviewed in Table 5.1.

<table>
<thead>
<tr>
<th>Material</th>
<th>Man’t</th>
<th>Significant Property</th>
<th>Cure</th>
<th>Application</th>
</tr>
</thead>
<tbody>
<tr>
<td>E-glass fabric</td>
<td>Hexel(^a)</td>
<td>durable fabric</td>
<td>90 min @ 250°F</td>
<td>±45° plies, web, skin, shear ties</td>
</tr>
<tr>
<td>S-glass tape</td>
<td>3M(^b)</td>
<td>high strength</td>
<td>90 min @ 250°F</td>
<td>0° uni’s, root reinforcement</td>
</tr>
<tr>
<td>IM7 tape</td>
<td>3M</td>
<td>high modulus</td>
<td>90 min @ 250°F</td>
<td>0° uni’s, trailing edge reinforcement</td>
</tr>
</tbody>
</table>

\(^a\) Hexcel, Pleasanton, CA  
\(^b\) 3M, St. Paul, MN

Several types of adhesives are also used in the blade construction. Table 5.2 summarizes the adhesives and their uses. The most significant adhesive is the film adhesive, which is used to adhere the spar laminate to the foam core. It is also used to bond the flex circuit and metallic components of the blade. Other adhesives are used for structural bonding and filling applications.

The remaining materials include the foam core, the leading edge weights, the wire harness shielding, and a wooden dowel as described in Table 5.3. Three varieties of foam are used in various sections of the core. Higher density foam is used where strength is critical while lower density foam is used in low stress areas where mass penalties are more significant. Tunsten rods provide a low volume leading edge weight for the nose block. The dowel is used as a temporary filler for the lead-lag pin. The wood is drilled out after the
TABLE 5.2  Summary of Blade Adhesives

<table>
<thead>
<tr>
<th>Material</th>
<th>Man’f</th>
<th>Significant Property</th>
<th>Cure</th>
<th>Application</th>
</tr>
</thead>
<tbody>
<tr>
<td>AF-163-2U03</td>
<td>3M</td>
<td>film adhesive</td>
<td>90 min @ 250°F</td>
<td>spar-foam interface, flex circuit bond, leading edge weight wrap, section tip fitting bond</td>
</tr>
<tr>
<td>FM 490</td>
<td>Cytec&lt;sup&gt;a&lt;/sup&gt;</td>
<td>foaming adhesive</td>
<td>90 min @ 250°F</td>
<td>fill around flex circuit, gaps in foam</td>
</tr>
<tr>
<td>Epon 828/3140</td>
<td>Shell&lt;sup&gt;b&lt;/sup&gt;</td>
<td>durable, viscous</td>
<td>~12 hrs @ 120°F</td>
<td>wire harness root end plug</td>
</tr>
<tr>
<td>Epon 828/3223</td>
<td>Shell</td>
<td>low viscosity</td>
<td>~12 hrs @ 120°F</td>
<td>potting connectors</td>
</tr>
<tr>
<td>EA9309</td>
<td>Hysol</td>
<td>durable, thick</td>
<td>1 hr @ 150°F</td>
<td>wire trough fill</td>
</tr>
<tr>
<td>M-Bond 200</td>
<td>MM</td>
<td>fast cure</td>
<td>2 min @ RT</td>
<td>tacking strain gages</td>
</tr>
<tr>
<td>5-minute epoxy</td>
<td>Devcon&lt;sup&gt;c&lt;/sup&gt;</td>
<td>fast cure</td>
<td>30 min @ RT</td>
<td>joining foam, tacking wires</td>
</tr>
<tr>
<td>Epo-Tek 410E</td>
<td>Epoxy Tech.&lt;sup&gt;d&lt;/sup&gt;</td>
<td>conductive</td>
<td>1 hr @ 150°F</td>
<td>bond between copper strip and aluminum shielding</td>
</tr>
<tr>
<td>aluminum putty</td>
<td>Devcon</td>
<td>stiffness, strength</td>
<td>12 hrs @ RT</td>
<td>mold repair and filler</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

<sup>a</sup> Cytec, Havre de Grace, MD  
<sup>b</sup> Shell Chemicals, Houston, TX  
<sup>c</sup> Devcon, Danvers, MA  
<sup>d</sup> Epoxy Technologies, Billerica, MA

cures. A grounded aluminum layer is used to shield the strain gage wire harness from the high voltage lines.

5.3 Mandrel Fabrication

The blade mandrel consists of a spar and fairing core along with any internal instrumentation. The core is shaped to provide cure pressure for the surrounding composite material.
TABLE 5.3 Summary of Other Passive Component Materials

<table>
<thead>
<tr>
<th>Material</th>
<th>Man’f</th>
<th>Significant Property</th>
<th>Application</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rohacell 31 IG foam</td>
<td>Rohacell(\text{a})</td>
<td>low density, high strength</td>
<td>fairing core</td>
</tr>
<tr>
<td>Rohacell 71 IG foam</td>
<td>Rohacell</td>
<td>low density, high strength</td>
<td>spar core, fairing root</td>
</tr>
<tr>
<td>Rohacell 300 WF foam</td>
<td>Rohacell</td>
<td>high strength</td>
<td>spar root core</td>
</tr>
<tr>
<td>tungsten rod</td>
<td>Rembar(\text{b})</td>
<td>high density</td>
<td>leading edge weights</td>
</tr>
<tr>
<td>wood dowel</td>
<td>-</td>
<td>easily removed</td>
<td>temporary root pin filler</td>
</tr>
<tr>
<td>aluminum foil</td>
<td>Reynolds</td>
<td>thin, conductive</td>
<td>wire harness shielding</td>
</tr>
</tbody>
</table>

\(\text{a}\). Rohacell, Richmond Aircraft, Norwalk, CA
\(\text{b}\). The Rembar Co., Dobbs Ferry, NY

Strain gages are positioned on the foam such that they will bond to the inner surface of the composite laminate. Instrumentation leads are also routed through the core.

5.3.1 Foam Core

The blade mandrel consists of two foam cores, one for the spar, and one for the fairing. Rohacell 71 is used for the spar core between BS10.608 and the tip. A higher strength and higher density foam, Rohacell 300, is used between the root pin hole and BS10.608. The fairing core outboard of BS16.406 is composed of a lighter foam, Rohacell 31, since the loads are less severe and the weight penalty is more significant. Inboard of BS16.406, the Rohacell 71 is used for the fairing core for increased strength. A drawing of the foam core is presented in Figure 5.2. The density and elastic properties for each type of foam are presented in Table 5.4.

The mandrel is fabricated in a number of spanwise segments. For each segment, a pair of steel templates were manufactured. Separate templates are used for the spar and fairing. The blade outer mold line (OML) is used as the starting point for the template design. The ply thicknesses are then subtracted from the OML. Then an extra 0.010" is added in the
thickness and 0.015" chordwise to provide backpressure on the composite laminate during the cure. The template pairs are joined with a back plate along the web edge as shown in Figure 5.16. A belt sander is then used to shape the foam blocks. Figure 5.17 shows a block of foam positioned in a spar template prepared for belt sanding. Since the belt sanding operation can be somewhat inaccurate, the foam dimensions are checked using calipers at a number of locations.

Some additional hand sanding is used to meet dimensional tolerances and to create specific features. Refer to the design drawings in Section A.1.2 for specific details. These include the space for the nose block, which has a spanwise variation in size, and ply overlaps, where foam must be removed to allow for the added laminate thickness. This is most significant along the aft edge of the upper and lower spar surface. Foam is removed to account for the web plies, fairing skin overlap and adhesive film for a total of 23 mil. Along the trailing edge of the fairing core, the foam is sanded for the trailing edge tab and stiffener plies. Near the inboard end of the fairing core, the aft, inboard edge of the foam
(Rohacell 71) must be shaped to match the contour of the blade mold. In addition, foam is removed for the chord and root doublers.

For the root core, made with the high density foam, a block of foam is first faced off with an end mill. This face is used as a reference for drilling the root pin hole with a 37/64" bit. The block is split through the thickness and then a pair of templates are used to provide the outer contour. Figure 5.18 shows the root block before splitting adjacent to the template. Because of the large number of reinforcing plies near the root, determining the core geometry was somewhat complicated. Several practice root sections were manufactured with variations in the core template geometry in order to obtain an adequate design. An oversized core results in the pinching of composite material between the two mold halves, while an undersized core can result in voids. In either case, the integrity of the root, where stresses are severe, can be jeopardized.

Once all of the foam segments have been shaped and their dimensions are within tolerances (±0.005"), the segments are joined with 5-minute epoxy. The mass of the foam core is recorded for reference. Additional foam is removed along the web for the wire harness, which will be described in the next subsection.

### 5.3.2 Instrumentation

Strain gages will be used as the primary safety and performance monitors within the blade. As described in Section 4.3.3, the blade incorporates twelve full strain gage bridges. The wiring and mounting procedures were developed for model blades at Boeing Helicopters (Philadelphia).

The strain gages are implemented in a full bridge configuration, with four active gages, as shown in Figure 5.3. For torsion bridges, a pair of gages\(^1\) oriented at \(+45^\circ\) and \(-45^\circ\) are placed along the pitch axis on the upper and lower spar laminate. The gages are wired such that the \(-45^\circ\) strains are subtracted, while \(\pm 45^\circ\) are added. Thus axial and bending

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1. Micromeasurements, EA-06-125TK-350, Raleigh, NC
strains are cancelled. For the flapwise bending bridges, a pair of gages\(^1\) oriented at 0° are placed along the pitch axis on the upper and lower spar laminate. For the chordwise bridge, the 0° gage pairs\(^2\) are placed on the foreward and aft surfaces of the spar. For all bending bridges, the gage pair on one side is subtracted while the pair on the other side is added. The result is that axial strains are cancelled.

![Diagram](image)

**Figure 5.3** Wheatstone bridge for strain gages. **Figure 5.4** Strain gage pair configurations (to scale).

The gage pairs, displayed in Figure 5.4, are first bonded to a single ply of precured E-glass fabric using M-Bond adhesive\(^3\). Terminal strips\(^4\) are also mounted on the E-glass. A specialized strain gage wire\(^5\), which is resistance to centrifugal loads causing the conductor to pull out of the insulation, is used in the blade. Approximately 6 inch lengths are soldered to the gages using a standard Pb/Sn solder. Four wire colors are used to ensure proper configuration of the full bridge circuit. With the torsion gages, which come in a ±45° pair, care must be taken to correctly assign the colors so that the gages are in the correct bridge

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1. Micromeristments, EA-06-125AC-350, Raleigh, NC
2. Micromeristments, EA-06-125BZ-350, Raleigh, NC
3. Micromeristments, AE-10, Raleigh, NC
4. CEG-50C terminals, Micromeristments, Raleigh, NC
5. Gore, GWN1709REVA, AWG 34 stranded silver-plated copper conductor, 0.004" MIL-ENE C insulation, Newark, DE
positions. If the gage pair is inadvertently rotated by 180°, the torsional strain measurements on the upper and lower surfaces will cancel out.

A sample of the orientation and relative position of the gages is shown in Figure 5.5. The complete wiring diagram for the integral blade will be provided in Appendix A. The wiring pattern is designed to avoid wire overlaps which could lead to a short under the high pressures of the cure. Leads from the gages are routed to a terminal strip on the lower surface of the spar. Leads from the gages on the upper surface pass through holes drilled in the foam to the lower surface. The gage locations are marked on the spar core in pencil. The solder pads are positioned first using M-Bond 200 adhesive.

A wire trough is cut on the aft edge of the spar core to accommodate the wire harness as illustrated in Figure 5.6. The wire harness is then formed with leads extending from the solder pads along the web to the root of blade. Extra length is left at the root end to reach the hub connector on the hover test stand.

Once all of the wires are routed along the web, the wire trough is filled in with EA9309 epoxy to encapsulate the wires. The epoxy is added to create a level surface along the web. The wires are kept close to the surface to enable good load transfer to the web plies. The wire bundle exits the blade on the aft face at BS10.608, at the inboard edge of the fairing. To ensure that no resin flows into the wire bundle, which could make it stiff and brittle, a 1/4" length of shrink tube is positioned around the bundle. The tube is positioned such that it will extend outside the spar composite material after the cure. 5-min epoxy is placed in the tube to act as a block against any flow or wicking of the low viscosity prepreg resins during the spar cure. The EA9309 epoxy filling the wire trough is cured with the blade oriented nose down and taped in the upper mold. The mold ensures the proper position and orientation of the strain gage wire bundle to fit properly in the channel cut in the mold. In addition, the mold heaters were used to accelerate the cure at 150°F.

1. M-Bond 200 Adhesive, Micromeasurements, Raleigh, NC
Figure 5.5  Sample of strain gage positioning and wiring for flap bending and torsion bridge.

Figure 5.6  Diagram of spar cross section with wire trough at heel of spar core.

Each gage pair mounted on the fiberglass tab and with wires attached is tacked face-down on the foam using M-Bond 200 adhesive. The leads are routed to the solder terminal as shown in the diagram. The wires are pressed into shallow razor blade cuts in the foam to
hold them flush with the surface. Small amounts of 5-minute epoxy are used to tack the wires in place. Figure 5.19 shows the instrumented root of the spar core including the shrink tube and wire harness.

Once all of the wires are in position, the resistances can be measured at the root end leads. The leads should also be connected to strain conditioners in order to verify the proper operation of each of the bridges. The polarity of the signals can also be checked while deforming the spar core. The mass of the spar core with instrumentation is recorded for reference.

5.4 Spar Fabrication

The first major assembly step in the fabrication of the blade is the spar cure. Several preparation steps are completed prior to the final assembly. These include the preparation of the composite plies, preparation of the AFC packs, and preparation of the aluminum mold. The spar assembly, especially the integration of the active plies, is the most critical step in the integral blade manufacturing process.

5.4.1 Preparation of Spar Plies and Components

Several blade components are prepared in advance for the spar assembly. A complete listing of all of the blade components and their dimensions is provided in Appendix A (Section A.1.1). The passive composite plies are cut to the proper dimensions. A temporary root dowel pin is fabricated. Finally, the leading edge weights, prepared from tungsten rod, are cut and measured as required.

Preparation of Composite Plies

Each of the composite plies, including the film adhesive, is cut to the proper size and with proper fiber orientation. During the handling of the composite materials, care is taken to avoid contamination such as foreign particles or chemicals which could damage or degrade the prepregs. After allowing the composites to warm to near room temperature
while in a sealed bag, the materials are removed and unrolled on a clean glass surface. Each composite part is cut from the roll using a sharp utility knife and teflon coated straight edge. The backing paper of each part is labelled. Then the parts are organized on a sheet of GNPT and sealed in a vacuum bag. Finally, the bag of parts is returned to cold storage until the assembly process.

The following is a general list of the composite parts used in the spar cure:

- main spar laminate plies
- inboard/outboard active ply replacements
- web plies
- nose filler plies
- spar doubler plies (root)
- web doubler plies (root)
- nose block
- root pin wrap
- interpack filler strips
- spar film adhesive
- peel ply

Plies that terminate at the tip of the blade are cut oversized so that excess material extends beyond BS60.619. The tip of the blade is trimmed back after the fairing cure. The E-glass spar laminate plies are cut oversize in width and are then trimmed more precisely during the actual assembly process. The replacement plies are used to fill the inboard and outboard sections of the active plies, providing torsional stiffness where the packs are absent. Interpack filler strips are used in the spanwise gaps between packs. The doubler plies act as reinforcement around the root of the blade. The nose block is formed from a 0° S-glass strip which encapsulates the leading edge weights. The peel ply is used on the outside of the spar to provide rough secondary bond surfaces for the fairing cure. The various components and their relative positions will be further described in the assembly discussion (Section 5.4.4).
Preparation of Other Spar Components

A wooden dowel pin is fabricated to act as a root pin filler during the blade manufacturing process. The pin is wrapped in S-glass tape during the spar assembly. The wood will be later removed leaving the glass as the load bearing surface. The wood also engages the upper and lower mold halves to ensure proper alignment of the root pin. A drawing of the wooden dowel is shown in Figure 5.7.

![Figure 5.7 Drawing of wooden dowel for root pin hole.](image)

The leading edge weights are prepared from tungsten rod. The density of the tungsten is 19700 kg/m$^3$. The stiffness of the tungsten is 400 GPa and the shear modulus is 150 GPa. The high density minimizes the cross-sectional area of tungsten required in the blade section. The mass per unit length required to balance the section center of gravity at the quarter-chord varies along the span of the blade. The high stiffness of the tungsten contributes significantly to the overall stiffness properties of the blade. In order to meet the design constraints, some of the tungsten rods are segmented to reduce their effective stiffness contribution. The 0.5" tungsten segments are used in place of continuous lengths. Two rod diameters were used in the blade: 0.020" and 0.0395". Table 5.5 summarizes the distribution of tungsten in the integral blade.

The ends of the tungsten segments are filed flat. Then, the tungsten segments are weighed for comparison with the expected total mass per length.
TABLE 5.5  Spanwise Distribution of Tungsten Leading Edge Weights

<table>
<thead>
<tr>
<th>Weight No.</th>
<th>Diameter</th>
<th>Segmented</th>
<th>Inner</th>
<th>Outer</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.0395</td>
<td>no</td>
<td>12.966</td>
<td>60.619</td>
</tr>
<tr>
<td>2</td>
<td>0.020</td>
<td>no</td>
<td>31.098</td>
<td>60.619</td>
</tr>
<tr>
<td>3</td>
<td>0.0395</td>
<td>yes</td>
<td>12.966</td>
<td>60.619</td>
</tr>
<tr>
<td>4</td>
<td>0.0395</td>
<td>no</td>
<td>51.526</td>
<td>60.619</td>
</tr>
<tr>
<td>5</td>
<td>0.0395</td>
<td>no</td>
<td>51.526</td>
<td>60.619</td>
</tr>
<tr>
<td>6</td>
<td>0.020</td>
<td>no</td>
<td>12.966</td>
<td>19.337</td>
</tr>
<tr>
<td>7</td>
<td>0.0395</td>
<td>yes</td>
<td>12.966</td>
<td>31.098</td>
</tr>
<tr>
<td>8</td>
<td>0.020</td>
<td>no</td>
<td>31.098</td>
<td>48.738</td>
</tr>
<tr>
<td>9</td>
<td>0.020</td>
<td>no</td>
<td>31.098</td>
<td>45.464</td>
</tr>
</tbody>
</table>

5.4.2 Preparation of Packs

Following the proof testing process described in Section 3.5, 42 packs were selected for the integral blade. The selected packs were next assigned locations within the blade based on their rankings in strain performance and also repair history. Packs with the highest induced strain performance are placed inboard so that the total twist times length or generated aerodynamic forces will be maximized. Packs with a significant number of repairs are considered higher risk and are placed in the outermost spar ply. This could potentially enable an in situ repair of the pack if the location of the damage is visible through the outer E-glass skin. The pack locations are designated as follows: ply number (2,4,6), upper/lower spar laminate, and spanwise position (1-7).

Each pack is then prepared for integration during the spar lay-up. The copper strips attached to the electrode tabs are trimmed to 0.25" length. The upper and lower strips are carefully soldered together. Then the kapton outer surface of the pack is cleaned with acetone. Figure 5.20 shows the 42 packs prepared for integration.

5.4.3 Mold Preparation

The blade mold consists of an upper and lower half split along the chordline of the blade. The root of the blade has two machined channels extending from the aft edge of the spar at
BS10.608" to the edge of the mold. The first enables the strain gage leads to extend out of the mold during the cures while the second is used for the flex circuit during the fairing cure. During the spar cure, the flex circuit channel is blocked with a strip of 0.060" teflon.

The first step in the preparation of the blade mold for the spar cure is to clean all surfaces. Excess resin from previous cures must be removed. The surfaces are wiped with acetone. Any cracks or other imperfections in the surface are filled with aluminum putty. Once cured, the aluminum putty can be sanded to obtain a smooth finish. With all surfaces adequately clean, the mold is treated with three coats of mold release¹.

A trailing edge filler block is also required for the spar cure. To form the block, the mold was filled with 0° S-glass tape. A strip of aluminum tape along the web line of both mold halves is first positioned to create a guide line for cutting the cured block. Once the block is cured, two mounting holes are drilled in the fairing portion of the block to lock it to the upper mold. Then, the spar portion is removed using a diamond abrasive bandsaw. The filler block is cleaned and coated with mold release in preparation for the spar cure.

The next step in the mold preparation is to apply relief tape in both mold halves. The tape effectively modifies the OML surface for the spar cure to leave space for plies that will overlap the spar during the second, or fairing cure. The thickness of tape applied to the mold is equal to the thickness of the overlapping plies. Relief tape is necessary for the fairing skin overlap, root doublers, chord doublers, and shear ties. The mold taping details are provided in Figure 5.8. The shading patterns correlate with the thickness of tape required. Two forms of tape were used: 5 mil aluminum tape, and 3 mil GNPT tape. Once all of the relief tape is in position, the mold is again treated with mold release. Figure 5.21 illustrates the relief tape in the lower mold.

Some additional relief tape is required along the forward edge of the filler block where the flex circuit channel enters the blade. The relief tape will ensure that nothing interferes

¹. Frekote 700-NC Releasing Agent, Dexter Corp., Seabrook, NH
with the proper location of the flex circuit on the spar so that the flex circuit will be accurately positioned in the channel to exit the blade. Eight layers of aluminum tape are used at BS10.608 tapering to one layer at BS12.6. The aluminum tape at BS10.608 merges with the teflon strip placed in the flex circuit channel in both mold halves. The aluminum tape and teflon are shown in Figure 5.22 for the upper mold.

5.4.4 Lay-up and Cure

The lay-up of the integral blade spar is performed on a clean table covered with GNPT. The composite parts for the spar are defrosted. The assembly process is completed in two parts. All steps are completed up to the incorporation of the active plies in the first stage. The second stage then includes the assembly of the main spar plies, including the packs, and finishes with the spar cure.

Assembly of Core Components

The first step in the assembly process is to wrap the intrumented spar mandrel in a single layer of film adhesive. The film adhesive covers all foam surfaces on the core.

Next, the aluminum shielding for the strain gage wire harness is prepared. The 0.4" wide and 1.4 mil thick strip will extend from the root at BS10.608 to the outermost strain gage leads along the web. At the root end of the strip, a 0.25" wide, 1" long, and 1 mil thick strip of copper is bonded to the aluminum using a conductive epoxy. The copper strip pro-
vides a large surface area for bonding and a good surface for soldering. The aluminum strip is then cleaned with a degreaser. Next, a strain gage lead wire is soldered to the copper strip and routed out of the blade along with the main strain gage wire bundle. The wire will be used to ground the shielding during operation of the high voltage actuators. Figure 5.23 shows the completed shielding. The aluminum strip is placed over the film adhesive already on the web and is then covered with another layer of film adhesive as shown in Figure 5.24.

Next, the wooden dowel pin is wrapped in the S-glass pin wrap. The dowel pin diameter is undersized so that the S-glass wrap will surround the lead-lag pin hole. The outer diameter of the wrapped pin will mate with the root block of the spar mandrel. While wrapping, the width of the S-glass should be approximately 0.88". The S-glass tends to spread out as it is applied. However, the roll of S-glass on the pin can be reshaped to obtain the proper dimensions. The wrapped pin is shown in Figure 5.25. When complete, the wrapped pin is pressed into position at the root of the spar mandrel.

The next step is the assembly of the nose block. The nose block consists of 0° S-glass surrounding tungsten leading edge weights. First, the tungsten rods or rod segments are wrapped in a layer of film adhesive. Care must be taken to tightly pack the rod segments end to end to ensure a proper mass per length distribution. To start the nose block, the strip of S-glass is rolled at one edge in a widthwise direction. Once a small roll is formed, the first tungsten rod is positioned. Then the rolling is continued to surround the rod in S-glass. The next rod is then positioned and rolled into the S-glass. This process is continued until all of the tungsten has been incorporated into the S-glass. Figure 5.26 shows a tungsten rod segment being positioned on the film adhesive to be rolled into the nose block. The remaining S-glass can be folded back and forth to form the flat aft edge of the nose block which will mate with the forward face of the spar core. The nose block is shaped by hand to pack the S-glass tightly and to achieve the desired curvature along the leading edge. The nose block can be pressed against a flat surface to shape the aft face.
When completed, the nose block is positioned on the spar core and is covered with a nose wrap ply of E-glass as shown in Figure 5.27.

**Spar Doublers**

Several layers of spar doubler plies are used to reinforce the root of the blade. The spar doubler plies, designated SDP 1-10, vary in length so that the total thickness of S-glass tapers. All spar doubler plies wrap around the root of the blade. A single spar doubler ply consists of four strips of 0° S-glass 0.52" in width. Two of the strips wrap around the upper half of the root pin, while the other pair wrap around the lower half. The upper and lower strips overlap slightly along the midline of the root. Of the two strips on each half, one starts on the forward edge of the surface, flush with the leading edge, wraps around the root, and then extends along the aft edge of the spar surface, flush with the web. The second strips starts flush with the first on the forward half of the spar surface. Then it is wrapped around the pin and extends along the aft half of the spar, flush with the aft section of the first strip. The result is that there are four strip halves spanning the spar chordwise to completely cover the spar surface. This concept is illustrated in Figure 5.9.

![Figure 5.9](image)

Illustration of unidirectional ply placement on spar.

The procedure for positioning the strips of S-glass is to start at the midpoint of the strip, placing it on the root. The composite should be tacky enough to stick to the film adhesive and each other. Then one end of the strip is placed down on the spar, moving outboard. The strip can be sheared to keep it flat while moving around the root contour onto the spar surface. As illustrated in Figure 5.9, the inner (chordwise) strip edges should meet
roughly 4 inches outboard of the root pin. The same technique is used for all unidirectional plies in the blade.

The stacking sequence for the spar plies is illustrated in Figure 5.10. The shading of the plies indicates the material and fiber orientation. The relative thickness of the plies is scaled according the actual ply thickness. The complete details of the spar plies including dimensions and spanwise positioning is provided in Appendix A. The plies on the bottom of the stacking sequence, corresponding to the inner plies, are positioned first. Thus the shortest of the spar doublers, SDP10, is the first ply. Two extra sets of spar doubler plies, SDP10b and SDP9b, were included in order to ensure adequate volume of composite to fill the mold around the root. Figure 5.28 shows the first few spar doubler plies in position on root. Several practice root lay-ups, root core redesigns, and volume calculations were used to determine the required material.

Figure 5.10  Illustration of spar lay-up showing ply sequence around root and extending outboard.
As the spar doublers are positioned, the primary objectives are to correctly position the plies and to minimize wrinkles or air gaps between the plies. In addition, the net shape of the root can be adjusted to maintain an approximate thickness of 0.88". The completed spar will need to fit into the cavity in the mold. This drives the need for tightly packing the plies. The spar is shown with all spar doublers in position in Figure 5.29. Note that the spar doublers on the aft surface of the spar root must diverge slightly around the strain gage wire bundle as shown in Figure 5.30.

**Web Plies and Graphite**

In the typical blade manufacturing procedure, the web plies would be the next composites to be positioned on the spar. For the integral blade, the ply order was slightly modified so that the graphite ply (SP8), would be positioned inside the web plies. The purpose is to have more layers of insulative glass plies between the edges of the packs and the copper connects which bend over the corner of the spar from the upper and lower surfaces to the web, and the conductive graphite ply. Thus, the graphite unidirectional ply is positioned on the spar first.

Then the web plies are attached. The three main E-glass fabric plies are positioned first. Then, the web doubler plies are added. Figure 5.31 shows the web plies over the graphite on the spar. Web plies 1 and 2 (WP1 and WP2) extend inboard of BS10.608 where the strain gage leads exit the blade. Therefore, a slit must be cut in the plies so that the wires can pass through the E-glass as shown in Figure 5.32.

This completes the first stage of the spar manufacture. At this point, the spar is wrapped tightly in GNPT and is then wrapped in shrink tape. A heat gun is used to shrink the tape around the spar. This compresses the plies and helps to remove any air pockets. The shrink wrapped spar is shown in Figure 5.33. Next, the spar is sealed in a vacuum bag,

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1. HI-SHRINK tape, 0.002" x 2", Dunstone Co., Inc., Charlotte, NC
shown in Figure 5.34, and placed in a freezer for storage until the second stage of the manufacture commences.

**Main Spar Pies**

To begin the second stage of the spar fabrication, the partially assembled spar is defrosted along with the remaining main spar plies. The packs should already be prepared for integration. To keep the packs in the proper order, the packs are positioned on a glass table top which has the spar locations marked. Not all of the plies are continuous from the blade root to the tip. For example, the active plies require inboard and outboard replacement plies. The spanwise variations in the lay-up, outboard of those shown around the blade root (Figure 5.10), are shown in Figure 5.11. This figure serves as a reference for the ply positioning discussions to follow.

![Diagram of spar lay-up showing plies and variations](image)

**Figure 5.11** Spanwise variation in spar lay-up showing changes at BS39.402 and BS57.588.

The first E-glass ply (SP7) is wrapped around the spar starting from the nose and working aft. The fabric is applied with the backing material attached. Working from the nose aft makes it easier to avoid wrinkles. The ±45° E-glass fabric should extend from the root pin to the blade tip. Because the E-glass prepreg roll width is 38" and the fiber orientation is ±45°, the ply is not quite long enough to cover the spar all the way to the tip on one side. Approximately 2" of material in a 45° right triangle is missing. A separate scrap of E-
glass is used to fill the gap, with a 0.25" overlap over the main ply. Near the root, the E-glass must be trimmed to cover the upper and lower surface around the pin as shown in Figure 5.12. In addition, the center portion of the ply is trimmed to wrap around the leading edge face of the root. Any excess E-glass along the web of the spar is trimmed as well.

![Diagram of spar with glass trimming](image)

**Figure 5.12** Pattern for trimming E-glass plies near the root of the spar, symmetric about ply centerline along leading edge (approximately to scale).

Next, the web of the blade is marked lightly with a pencil to show the desired locations for the pack electrode connections. This will serve as a guide for positioning the packs spanwise. The innermost electrode strip is located at BS15.216 and the last is at BS58.810. Proper spanwise positioning is essential for the copper strips to match up with the solder pads on the flex circuit.

Finally, the first packs are positioned on the spar as shown in Figure 5.35. This is the +45° ply, SP6. Each pack is positioned so that the aft edge of the pack is flush with the edge of the web and the copper strips align with the marks on the web as shown in Figure 5.36. Each pack is pressed against the spar surface so that the heat from the hand (gloved) will aid in adhering the pack to the prepreg layer below. Near the nose of the spar, where the curvature is greatest, the packs tend to straighten out and cannot be held against the spar
contour. In between the packs, small strips of S-glass are cut to fill the gaps. Since the S-glass is thicker than the packs, the width of the strips should be undersized to allow for spreading during the cure.

Once the packs are positioned on both sides of the spar, the nose filler ply and inboard replacement plies are attached. The nose ply fits in between the foreward edges of the packs on the upper and lower surfaces. The inboard replacement plies, designated SP6A1 and SP6A2, cover the upper and lower spar surfaces, from the outer edge of the root pin to the inner extent of the packs. These plies wrap around the leading edge and root of the blade and are trimmed to be flush with the inner edge of the packs as illustrated in Figure 5.37. There are no outboard replacement plies in this layer.

The next ply is an E-glass ply (SP5) which is applied in the same manner as the previous E-glass ply. The E-glass is positioned along the nose and is pressed into place working aft. During this process, the foreward edges of the packs are pressed down. The E-glass ply will help in holding the packs in place. The E-glass ply can be easily wrinkled because it is difficult to maintain adhesion to the packs. The E-glass will adhere to the S-glass filler strips between the packs. The root of the ply is trimmed as before, and a replacement patch is used to complete the outboard end of the ply.

The -45° active ply (SP4) can then be added. The packs are positioned flush with the web and aligned with the marks as before. The relative positions of the electrode tabs on the web can be seen in Figure 5.38. The S-glass filler strips are positioned. There is not a nose filler ply in this layer. Both inboard and outboard replacement plies are then attached to the spar. The inboard plies (SP4AU and SP4AL) are trimmed tangent to the outer edge of the root pin and flush with the inner edge of the packs as shown in Figure 5.39. The outboard replacement plies (SP4XU and SP4XL) are positioned flush with the outer edge of the packs and extend to the blade tip.

The next ply is the unidirectional S-glass ply (SP3), which consists of 4 strips. The strips wrap around the root and extend to BS39.402. As shown in Figure 5.11, two E-glass plies
replace the S-glass ply. The plies overlap in a 1" lap joint. The E-glass plies extend to the
tip.

The final layer of packs (SP2) is next positioned on the spar as with the previous layers. Figure 5.40 shows the packs being positioned over the S-glass and E-glass ply below. A nose filler ply is applied. Then the inboard and outboard replacement plies are added. The inboard plies (SP2AU and SP2AL) span between the outer edge of the pin and the inner edge of the packs. The outboard replacement plies (SP2XU and SP2XL) extend from the outboard edge of the packs to the tip of the blade.

The final composite ply is the E-glass skin (SP1). This ply is wrapped around the nose of the blade as with the other E-glass plies. The ply also wraps around the root and requires a small patch to extend completely to the tip of the blade. Figure 5.41 shows the completed web of the spar.

Because of the relatively stiff packs in the upper and lower spar, the laminate tends to straighten out chordwise which results in a separation between the aft edge of the packs and the web. In order to prevent any undesirable motion of the packs once the spar is in the mold, the spar laminates must be pulled back against the core. To accomplish this, small strips of 0° E-glass fabric, approximately 1" long and 0.2" wide, are used to effectively "tape" the upper spar laminate to the lower spar laminate along the web. The strips are shown in position in Figure 5.42 before the backing material was removed. The strips are placed in between the electrode copper strips. The ends of the strips extend about 0.2" onto the upper and lower surface of the spar. Although the strips create a slightly uneven surface, the effect is minimal and the benefit is potentially significant. Once the strips are in position and all of the electrode copper strips are pressed flat against the web, a strip of GNPT tape¹ is used to cover the entire web surface as shown in Figure 5.43. This tape will protect the copper strips during the stresses of the cure process.

¹ GNPT tape, tool coat, Richmond Aircraft Products, Norwalk, CA
Next, the leading edge of the spar between the root pin and the inboard edge of the nose block (BS12.966) should be checked for proper shape. At some points, additional material may be required to obtain a smooth, linear leading edge. Small lengths of S-glass can be applied to fill the space. Lastly, a narrow strip of S-glass, referred to as the sacrificial unidirectional ply, is positioned along the entire leading edge of the blade. This serves as excess composite material which can be pinched between the mold halves and trimmed after the cure. Additional GNPT tape is used to wrap the strain gage leads to protect them during the cure.

With all of the composites in position on the spar, the peel ply can be added. The peel ply\(^1\) covers all secondary bond surfaces on the spar. The placement of the peel ply is shown in Figure 5.13. One long strip is cut to wrap around the web to cover the fairing overlap surface. Another piece is cut to cover the location of the leading and trailing edge shear ties and root doubler.

![Figure 5.13](image-url) Location of peel ply for spar cure (hatched area).

**Cure**

The next step in the spar manufacturing process is to position the spar in the upper mold for the cure as shown in Figure 5.44. The spar will be oversized, especially near the root. To get the spar into the mold, the root will have to be shaped by hand to fit in the mold. After some initial shaping, the spar is pressed down into the mold and also pushed inboard to align the wooden dowel with the hole in the mold. Once this alignment is achieved, a block of wood coated in GNPT is used to apply downward pressure against the blade root.

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1. 52006 GFR yellow, Burlington Impression Fabrics, New Rochelle, NY
It may be necessary to warm up the mold to approximately 90°F to help soften the composites. This requires the two thermocouples to be positioned in the strain gage lead channel near the blade root and also in the blade tip. The entire spar is also pressed down and toward the trailing edge filler block along the entire length of the mold. In addition, care must be taken to avoid stressing the strain gage lead wires which must align properly with the groove cut in the mold. A teflon strip is placed in the flex circuit channel in each half of the mold. This will ensure that no material will interfere with the proper positioning of the flex circuit. The root end of the spar is shown in Figure 5.45. Note the peel ply visible as well as the flex circuit and strain gage wires positioned in the mold channels.

Another major concern is composite material getting pinched between the halves of the mold when closed. To help avoid this, especially near the root, the composite material can be squeezed inward on the forward and aft faces. With the blade in position, the other mold half (lower) is brought into position. The mold is slowly and carefully lowered. A narrow strip of metal, such as a ruler, can be used to push any pinched composite inward to help the material fit into the lower mold. It may be necessary to push the leading edge of the spar inward toward the trailing edge along the entire span to minimize pinching. The lower mold is repeatedly lowered slightly and then the pinched material is again repositioned.

Finally, the eight large C-clamps are positioned along the mold. The mold is warmed to 90°F as the clamps are slowly tightened. The clamps are tightened evenly, increasing the pressure and closing the gap between the mold halves. The mold with C-clamps and thermal control system are shown in Figure 5.46. The inevitable pinching of some material near the root will prevent the complete closure of this gap. The gap may be approximately 10-20 mil at some points. For the integral blade, the gap was 25 mil near the root and about 10 mil along the entire leading edge of the blade. As a result, the OML of the blade will be slightly oversized.
Once the clamps are tightened completely, the mold is heated to 250° F at approximately 3°F/min. The temperature is held for 90 minutes. Then the heaters are turned off and the mold is allowed to slowly cool.

The clamps are removed and the mold is slowly opened. The root pin can be tapped to help release the root from the mold. Once again, the strain gage lead wires should be handled with care to avoid damage. Then, the peel ply is removed. Next, any flash or pinched material around the mold line is trimmed with a knife. The blade can then be prepared for attaching the flex circuit.

5.5 Flex Circuit Attachment

With the integral blade spar completed, the next step in the manufacturing process is the attachment of the flexible circuit. The flex circuit will connect the packs embedded in the spar to the hub of the hover test stand and the high voltage amplifiers. The spar and flex circuit are shown in Figure 5.47. The following sections will describe the preparation of the spar and flex circuit and the procedure for establishing the electrical contacts and making a structural bond between the parts.

5.5.1 Flex Circuit Preparation

Several steps must be performed to prepare the flex circuit for the blade. An inboard section of a flex circuit it shown in Figure 5.48. The 6-layered circuit has 14 lines across the width with solder pads at the outer extent of each line. The solder pads align with the electrode tabs of the packs in the spar. The flex circuit tapers from 0.6" to 0.4" inboard of the solder pads. Three identical flex circuits were obtained for the project. One was tested in a half-span blade section, one was used in the integral blade, and one served as a reserve. As an initial quality control check, a 100 Hz, 4kVpp voltage was applied to adjacent copper lines on each flex circuit. A couple of lines were found to have a low resistance

1. All Flex Inc., Northfield, MN
between them which resulted in arcing on two of the flex circuits. The best flex circuit with no flaws was used for the integral blade.

Next, connectors were attached to the ends of the flex circuit for mating with the single-row, 14-pin header on the printed circuit board. The connectors\(^1\) are designed to clamp onto the flex circuit layer on one side. The other side has 0.1" centerline spaced sockets. Since the connectors are rated for 200 V between lines, potting was required to enable 4kV operation. The potting also strengthens the connection. Shell Epon 828/3223 epoxy was used to pot the connectors. The mix ratio was 100:12 by weight.

First, the six flex circuit layers were trimmed to have a 0.25" stagger as shown in Figure 5.49. This is necessary because of the 90° turn the flex circuit layers must make to mate with the PCB on the hub. Then, the clincher end of the connectors was pressed onto each flex circuit layer. The clincher has prongs which puncture the kapton to contact the copper. To ensure a solid connection during the high centrifugal and vibratory loads in hover, solder was also applied to each joint. In order to prevent the resin from filling in the sockets on the connector, the back end of the sockets were blocked with wax. A layer of vacuum tape and then GNPT tape was used to form a mold. Then the epoxy was added and cured at approximately 120°F using a heat lamp.

Following the cure, most of the wax was removed with tweezers. Then a hot-melt thermoplastic glue gun was used to fill in the unpotted void where the wax had been located. This technique was found to provide adequate insulation between the adjacent terminals of the connector. The flex circuit was then tested to 800 VDC and 4kVpp with all lines activated as intended for twist operation of the blade. The completed flex circuit is shown in Figure 5.50 next to the mating headers on the PCB.

The flex circuit is then cleaned and prepared for mating with the spar. One of the solder pads is shown in Figure 5.51. This solder pad is the last one of the particular layer of the

\(^1\) Clincher connector Series 65801, BERG Electronics, St. Louis, MO
flex circuit. The copper solder pads are tinned with a thin layer of tin/lead solder. This will be further explained in the solder selection discussion of the next subsection.

5.5.2 Web Preparation

The web surface of the spar must be prepared for mating with the flex circuit. The copper strips which connect to the pack electrodes are first inspected. Any epoxy or other residue on the copper surface is scraped off to expose the bare copper. The outer layer of kapton is also removed. Figure 5.52 shows the layer of kapton covering the copper strips on the surface of the web. The exposed copper is shown in Figure 5.53. The capacitance of each of the packs is measured and recorded. The E-glass fabric surface of the web must be cleared of any excess or foreign material so that the surface will be smooth and uniform. Cotton-tipped applicators moistened with methanol are used to wipe particles from the web surface. The fit of the spar and flex circuit should be checked in the blade mold to ensure that the flex circuit will align properly with the channel cut for it in the aluminum. The groove cut in the aluminum was 0.0625" wide and has a minimum radius of curvature of 0.8".

Next, an RTV mold of the web was fabricated. The mold will be used to apply pressure to the flex circuit while structurally bonding it to the web. A strip of GNPT was used to cover the web surface. The rest of the spar was covered with flash tape for protection. A 1.5" C-channel of aluminum was used as an outer mold. The sides were bent outward slightly to ease the release of the RTV from the aluminum. Rubber spacer blocks were taped to the bottom of the channel in several locations to ensure a depth of RTV of at least 0.5". Then the channel was filled with a 2-part RTV1. The spar was then lightly pressed into the RTV so that the RTV extended approximately 0.75" up the spar surface. The spar was centered and the RTV was allowed to cure overnight at room temperature.

The next concern is the solder connection between the copper strips and the solder pads on the flex circuit. Since the solder connections are "blind" or not accessible once the parts

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1. GE RTV 630B, 2-part silicone, Newark Electronics, Woburn, MA
are mated, an additional strip of copper is required. This strip, referred to as the auxiliary copper strip, will serve as the interface between the copper strips on the web and the solder pads and will extend outward from the web. The strip can then be used as a heat conductor to melt the solder that will join the electrode copper strip to the solder pads through this extra strip. Then the excess copper can be cut along the edge of the web.

Since a regular lead/tin solder was used to join the original copper strips to the electrodes, a lower temperature solder was selected for the additional solder joints. The melting point of the first solder was 361°F. The low temperature solder required a melting temperature less than 361°F but greater than the blade cure temperature of 250°F. Indium 2901 solder was selected for its melting point of 290°F. In addition, manufacturer’s data for the solder suggests that the solder joint will have good strength and ductility, though the strength is somewhat less than that of the standard lead/tin solder. However, the presence of indium in the solder presents a potential interaction problem with the copper. At elevated temperatures, the bond between the indium and copper could degrade. A solution is to tin the copper surfaces in advance with standard lead/tin solder.

Next, the alignment of the copper strips is checked against the flex circuit to ensure that the strips will match up with the solder pads. The extra copper strips are soldered to the existing strips along the web of the blade using a 350°F soldering iron as shown in Figure 5.54. If necessary, a small strip of extra copper can be used to move the copper contact on the web to improve the alignment. The extra copper strips extend outward from the web on the same side as the original copper strip.

The last step is to apply film adhesive to the surface of the web in between the copper strips. The film adhesive is a solid at room temperature and has a working life of many hours at room temperature. The concept is to position the film adhesive on the web and then bring the flex circuit into position. Then the solder connections can be made. Finally,

1. Indium Corporation of America, Utica, NY
the spar and flex circuit are clamped in the RTV mold and an elevated temperature cure will be used to cure the film adhesive.

Three layers of the 5 mil film adhesive were placed on the web in between the copper contacts. An extra narrow strip was placed along the centerline where the web bows inward slightly\(^1\). No film adhesive was used between BS10.608 and BS14. This will allow for adjustment in the position of the flex circuit during the faring cure. The prepared spar and flex circuit are shown in Figure 5.55.

### 5.5.3 Electrical Connections

With both the flex circuit and web fully prepared, the flex circuit is positioned on the web so that the solder pads match up with the auxilliary copper strips. The outboard end of the flex circuit and web are shown in Figure 5.56. Flash tape is used to hold the position of the flex circuit. Then the electrical contacts are soldered one-by-one using a 450°F soldering iron as shown in Figure 5.57. This temperature was required to ensure that the Indium 290 would reach the required 290°F. With the heat source in position, the flex circuit is pressed against the spar to ensure good contact between the parts.

Then, all of the pack capacitances are checked through the flex circuit contacts. This test indicates any poor connections which can then be corrected. The twist actuation performance of the spar was then measured prior to the cure of the flex circuit bond. This data will be presented in Chapter 7. Some of the connections required resoldering after the testing. The relatively stiff flex circuit caused some solder joints to separate prior to the cure.

### 5.5.4 Structural Bond

Once all of the connections passed the electrical continuity test, the film adhesive could be cured. First, the spar surface was covered in flash tape to protect it from excess resin. The

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\(^1\) The slight concavity of the web resulted from a slightly convex surface on the trailing edge filler block.
auxilliary copper strips extending from the web were taped against the spar laminate. Then, the flex circuit and aft end of the spar were covered with GNPT tape. GNPT tape was also used between the flex circuit and web from BS14 inboard to prevent bonding as shown in Figure 5.58. This will allow for some flexibility in positioning the flex circuit during the fairing cure.

The web end was placed in the RTV mold. A strip of rubber and another aluminum channel were placed along the leading edge. Then several C-clamps were used to apply light chordwise pressure to the spar. The entire assembly was then heated in an oven to 250°F for 90 minutes.

During the cure, some of the clamps slipped. This resulted in somewhat uneven pressure along the web. Some segments of the flex circuit were not compressed well against the web, but the film adhesive bond was solid along the entire span. The bond appeared to be uniform with no voids.

Next, the contacts are checked again. At this point, it was found that a number of connections had been broken during the flex circuit bond cure. Since the film adhesive had flowed into any gaps that may have formed, another technique had to be used to reestablish the connections. Using a low power stereo microscope, a razor was used to cut away the kapton on the edge of the flex circuit adjacent to a particular bad connection. The kapton was removed to expose the edge of the solder pad. The solder pad of interest was the only conductor in the vicinity of the removed material. The film adhesive was also scratched away to expose the copper strips on the web. With both contacts exposed, a small strip of copper was soldered across the contacts using the Indium 290 solder. This technique was necessary for repairing 21 of the 84 connections. The cut-outs in the flex circuit and the exposed solder were covered with pieces of film adhesive prior to the fairing cure. The capacitance of each of the packs was again measured and recorded.
5.6 Fairing Cure

The fairing lay-up and cure is the last major step in the fabrication of the integral blade. In this step, the fairing portion of the blade, between the web and the trailing edge, is attached to the spar and cured. The following sections will describe the preparations and the procedure for the fairing lay-up and cure.

5.6.1 Preparation of Fairing Plies

Several composite prepreg components must be cut to the proper dimensions for the fairing cure. The other major component, the foam core, is also prepared in advance as described in Section 5.3.1. The composite components are cut from rolls of material in the same fashion as those of the spar, described in Section 5.4.1. The following is a list of the fairing components:

- upper and lower fairing skin
- trailing edge stiffener
- trailing edge tab
- trailing edge filler
- root doublers
- chord doublers
- leading edge shear tie
- trailing edge shear tie

The fairing consists of an E-glass skin covering a foam core and overlapping the spar. Several other components reinforce the trailing edge and the root section. The inboard section of the fairing is diagrammed in Figure 5.14. The fairing lay-up is constant to the blade tip. The TE stiffener is a 0° graphite strip which aids in carrying the centrifugal loads of the fairing. The TE tab is a 90° graphite ply which provides chordwise stiffness to the trailing edge. The S-glass root doublers reinforce the root of the fairing and join the trailing edge and the root of the spar. The chord doublers provide additional strength to the inboard section of the fairing. The E-glass fabric shear ties reinforce the entire root of
the fairing and spar in shear loading. Complete details of the fairing components are provided in the parts list in Appendix A (Section A.1.1.). The fairing components are stored in a vacuum bag at 0°F until the fairing lay-up process.

![Diagram of fairing and spar components](image)

**Figure 5.14** Drawing of fairing components and interface with spar.

### 5.6.2 Mold Preparation

The mold is cleaned and cleared of all excess resin and leftover relief tape from the spar cure. The trailing edge filler block is removed. Next, the two mounting holes for the filler block must be filled. The threads are treated with mold release. Then, holes are stuffed with gauze material almost to the surface. Aluminum putty is used to fill the top of the holes flush with the surface of the mold. The putty is covered with GNPT and a rubber
pad, and light pressure is applied to ensure a smooth surface. Once cured, the patch can be sanded lightly. Then the mold is sprayed with another coat of mold release.

5.6.3 Lay-up and Cure

In preparation for the fairing lay-up and cure procedure, the spar is first prepared. Any tape covering the spar is removed. The auxiliary copper strips extending from the web are cut flush with the spar surface. The part of the flex circuit which will be external to the blade is covered in GNPT tape to aid in the release from the mold. The loose portion of the flex circuit along the inboard end of the web will allow some freedom of movement for the flex circuit when the blade is positioned in the mold. A layer of film adhesive is placed between the flex circuit and the web. Foaming adhesive is used to fill the voids along the web where the flex circuit is tapered to 0.4" (inboard of BS15). This is illustrated in Figure 5.59. Then the entire web and fairing skin overlap region of the spar are covered in a layer of film adhesive. The spar is shown in Figure 5.60 along with the fairing core.

The first stage of the lay-up process is performed in the lower blade mold. The lower fairing skin is positioned in the mold with the forward edge aligned with a line 1.79" from the leading edge. The lower skin is shown in the lower mold in Figure 5.61. The backing of the E-glass is removed before it is finally positioned. Contact with the E-glass should be avoided to avoid stretching or wrinkling the fabric. The excess E-glass near the aft, inboard edge of the fairing is trimmed to match the mold planform with approximately 0.5" excess to wrap around the foam core. Then the spar is lowered into the mold and pressed down against the lower fairing skin. The alignment of the fairing skin and the spar must be fairly accurate to ensure that the lap joint between the fairing and spar is adequate.

Next, the fairing core is positioned against the web and lower fairing skin. Any gaps in the foam along the trailing edge and the root around the flex circuit are filled with foaming adhesive. The trailing edge filler is positioned along the trailing edge of the foam. An enlarged view of the trailing edge cross section is provided in Figure 5.15. The relative ply thicknesses are to scale, but are exaggerated in the drawing for clarity. The next layer
in the stacking sequence is the trailing edge stiffener. The aft edge of the ply is aligned with the aft edge of the foam core, which is 0.28" forward of the actual trailing edge. Then the TE tab is positioned. This 90° graphite ply begins at the trailing edge and extends forward 0.75". Both the stiffener and tab extend inboard to BS16.406. Figure 5.62 illustrates the TE tab in position.

![Diagram](image)

**Figure 5.15** Chordwise cross-section of trailing edge (ply thickness exaggerated).

With the trailing edge components in place, the next step is to position the upper fairing skin. The backing of the prepreg is removed prior to placing the ply onto the blade. The E-glass prepreg will not adhere well to the foam core but will adhere to the film adhesive along the web and to the other composites along the trailing edge. The inboard end of the ply is trimmed as with the lower skin.

Next, the root reinforcement plies will be added. First is the upper chord doubler which is an S-glass strip running from the spar to the trailing edge at BS16.406 with fibers oriented chordwise. Then, the upper root doubler is positioned as shown in Figure 5.63. This S-glass ply also has lengthwise fibers which will be oriented parallel to the line tangent to the root pin and passing through the trailing edge at BS16.406. Extra length is left at both ends to be trimmed later.

With the upper fairing components in place, the blade is carefully removed from the lower mold and temporarily supported nose-down in the RTV mold originally used for the flex circuit bond cure. This is shown in Figure 5.64. The lower chord doubler and root doubler are added to the blade. The root doublers are trimmed to be flush with the trailing
edge. Near the leading edge, the root doublers are trimmed to allow a 0.2" overlap between the upper and lower S-glass strips.

The last components are the TE and LE shear ties. Refer to the design drawings in Section A.1.2 for the exact details of the part locations. These E-glass fabric plies wrap around the trailing edge and leading edge, respectively. First the TE shear tie is added. The upper and lower fairing skin plies are folded over on the aft, inboard edge of the fairing core. Then an oversized E-glass fabric strip is positioned on the center of the edge. A sheet of GNPT with a straight edge is placed under the fabric on the upper surface of the fairing. The GNPT is aligned 0.6" inboard from the outboard edge of the root doubler ply. The E-glass is then trimmed along that line. The process is repeated on the lower side. Then the fabric is pressed into position on the fairing. The ply is also trimmed around the root pin and along the trailing edge.

For the LE shear tie, an oversized E-glass strip is used and trimmed in a similar manner to the TE shear tie. The LE shear tie extends 0.65" from the leading edge. Near the root, the ply extends to the pitch axis line, where it meets the TE shear tie. A cutout is also made around the root pin. The root of the blade with all reinforcements attached is shown in Figure 5.65.

Finally, the blade is placed in the upper mold as shown in Figure 5.66 and Figure 5.67. The strain gage wires and flex circuit are positioned in the mold channels. Thermocouples are put in position near the root and at the tip. The actual sensor is embedded in the excess composite near the root and tip. Next, the other half of the mold is slowly lowered in to position. The flex circuit must fit into the machined channel on the lower mold which has a beveled edge to allow for some initial misalignment. The clamps are then positioned and pressure is slowly and evenly applied. A 10 mil gap between the mold halves remained near the root between two clamps. The gap was less than 5 mil along the rest of the mold. The blade was cured at 250°F for 90 minutes.
5.7 Final Preparations

The fully cured blade can next be prepared for hover testing. The preparations include cleaning up the edges and surface and drilling the lead-lag pin hole. A teflon-based fabric is also bonded to the root as an interface between the blade and the pitch shaft which connects the blade to the hub.

5.7.1 Trimming and Splining

The first step in the trimming process is to cut the excess material from the tip of the blade at BS60.619. A diamond abrasive bandsaw was used to make the cut. Then the edge was hand-sanded flat and smooth. A thin coat of EA9309 epoxy was applied to seal the end surface of the blade.

Next, the leading and trailing edges of the blade were lightly sanded to remove any flash from the cure and to obtain straight, smooth edges. The mold line around the root was also lightly sanded. The chord length was checked along the span.

Several surface imperfections on the blade were removed or reduced using EA9309 epoxy and a set of VR7 female templates in a splining process. One particular flaw is the offset at the leading edge between the upper and lower surfaces of the spar. The chordwise offset resulted from an offset in the mating of the two mold halves. The result is a step along the nose of the blade. The epoxy was applied to the convexity on the spar surface near the nose. Then the template was pulled across the blade spanwise to spline the surface as shown in Figure 5.68. The remaining epoxy filled the gap and since the viscosity of the EA9309 is high, it retained the contour of the template. The same technique was used to fill other surface defects on the blade.

5.7.2 Lead-Lag Pin Hole

The next step is to drill the lead-lag pin hole in the root of the blade. The upper mold is used to fixture the blade on a milling machine. No additional clamps are required to hol...
the blade in the mold. First, a dial caliper is used to locate the center of the hole in the upper mold which is clamped to the base of the mill. Then the blade is positioned in the mold and the blade, strain gage wires, and flex circuit are covered such that only the root is exposed.

Next, a 7/16" carbide-tipped bit is used to drill through the root of the blade. This operation removes the wooden dowel and some of the S-glass pin wrap. Then a boring bar is used to slowly increase the hole diameter in several steps. The tool and set-up are shown in Figure 5.69. The boring bar cutting tool has an adjustable radius. The tool is run through the hole. Then the diameter of the hole is checked with calipers and the tool radius is increased. The process is repeated several times until the hole is approximately 0.498" in diameter. Then the actual 0.500" lead-lag pin to be used in the hover tests is checked against the hole. The hole is enlarge in 0.5 mil diameter and then 0.1 mil diameter increments until the pin just fits. The fit should be snug.

Once a satisfactory fit is obtained, the operation is complete. Any excess material around the edge of the hole can be removed with a razor or sandpaper.

### 5.7.3 Pitch Shaft Interface

To provide a tough and low friction interface between the root of the blade and the pitch shaft, a layer of Ruslon\(^1\) fabric is used. The Ruslon is bonded to the upper and lower surface of the blade root around the lead-lag pin hole. A clamping fixture, shown in Figure 5.70, was fabricated to ensure that the total thickness of the blade and two Ruslon layers would be 0.910" to form a sliding fit with the pitch shaft. The fixture consists of a pair of parallel steel plates bolted together through 0.910" aluminum spacer blocks. The fabric is coated with EA9309 epoxy and is placed on both side of the blade. Then the blade root is inserted into the fixture. The bolts are tightened and the epoxy is allowed to cure.

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1. Ruslon or Fenlon, Fenner Manheim Div., Fenner Inc, Manheim, PA
Following the cure, a hand reamer is used to remove any excess epoxy from the lead-lag pin hole. Then the fit of the blade into the pitch shaft is checked. This completes the manufacture of the integral blade.

Figure 5.16  Spar and fairing foam templates.
Figure 5.17  Foam block in spar template prepared for shaping.

Figure 5.18  Root foam block with lead-lag pin hole prior to shaping in template.
Figure 5.19  Spar core with shrink tube around strain gage wire harness and showing root strain gage bridges.

Figure 5.20  AFC packs prepared for integration in the blade spar.
Figure 5.21 Aluminum relief tape in lower mold and teflon strip in flex circuit channel.

Figure 5.22 Relief tape, trailing edge filler block, and teflon filler strip in upper blade mold.
Figure 5.23  Aluminum web shielding showing bonded copper strip and ground lead.

Figure 5.24  Aluminum shielding on web wrapped in film adhesive.

Figure 5.25  Wooden dowel pin wrapped in S-glass.
Figure 5.26  Formation of S-glass nose block showing incorporation of segmented tungsten rod in film adhesive.

Figure 5.27  Wrapped pin, nose block, and nose wrap on spar wrapped in film adhesive.
Figure 5.28  Root of spar showing first spar doublers wrapping around root.

Figure 5.29  Spar with all spar doublers in position.
Figure 5.30  Close-up of strain gage wires passing between spar doublers on root.

Figure 5.31  Web plies over graphite unidirectional ply (SP8).
Figure 5.32  Web plies split around strain gage wire bundle.

Figure 5.33  Spar wrapped in shrink tape prior to addition of main spar plies.
Figure 5.34  Vacuum bagged spar.  Figure 5.35  First active ply.

Figure 5.36  Alignment of first pack with marks on web (Pack 6U1).
Figure 5.37  Inner replacement plies (SP6A) for inner active ply, partially trimmed.

Figure 5.38  Alignment of second active ply (SP4) with marks on web.

Figure 5.39  Inner S-glass replacement ply (SP4AU) for middle active ply.
Figure 5.40  Transition in SP3 from S-glass unidirectional to E-glass fabric (SP3X, SP3Y) and partial completion of outer active ply (SP2).

Figure 5.41  View of entire web with all packs in place.
Figure 5.42  Small E-glass strips used to pull upper and lower laminates together at web.

Figure 5.43  GNPT tape covering electrode tabs during spar cure.
Figure 5.44  Spar in upper mold with flex circuit and strain gage wires secured in mold channels.

Figure 5.45  Close-up view of spar in mold showing peel ply and oversized root.
Figure 5.46  Blade molds with clamps and heater control system (rear).

Figure 5.47  Completed spar and flex circuit prior to joining.

Figure 5.48  Inboard end of flex circuit showing taper from 0.6" to 0.4".
Figure 5.49  Bare end of flex circuit layers cut and prepared for attaching hub connectors.

Figure 5.50  Flex circuit connectors attached and potted shown next to printed circuit board.
Figure 5.51  Photomicrograph (10x) of the last flex circuit solder pad on a particular layer.

Figure 5.52  Photomicrograph (10x) of electrode tab on web after spar cure.
Figure 5.53  Photomicrograph (10x) showing exposed copper on electrode tab.

Figure 5.54  Photomicrograph (10x) showing auxiliary copper strip soldered in place.

Figure 5.55  Spar and flex circuit prepared for soldering and structural bonding.
Figure 5.56  Outer extend of flex circuit in position on spar.

Figure 5.57  Photomicrograph (10x) of soldering iron in contact with auxiliary copper strip and with pressure applied through silicon rubber block.
Figure 5.58  GNPT tape applied to spar near root to prevent bonding of flex circuit.

Figure 5.59  Film adhesive (pink) and foaming adhesive (white) applied around inboard section of flex circuit.
Figure 5.60  Film adhesive applied to web of spar and fairing core prepared for assembly.

Figure 5.61  Lower fairing skin trimmed for fit in lower mold.

Figure 5.62  Graphite trailing edge tab in position along fairing core.

Figure 5.63  Upper skin, chord doubler, and root doubler in position.
Figure 5.64  Blade removed from mold to apply lower reinforcements and shear ties.

Figure 5.65  View of shear ties and upper root doubler and chord doubler.
Figure 5.66  Close-up view of blade in upper mold ready for cure showing all root reinforcements in place.
Figure 5.67  Blade in upper mold prepared for fairing cure.

Figure 5.68  Splining of upper surface using VR7 airfoil template.
Figure 5.69  Boring bar operation to increase root pin hole diameter.

Figure 5.70  Jig for bonding ruslon fabric to upper and lower surface of blade root.
Chapter 6

BLADE LEVEL BENCH TESTING

This chapter describes the testing of a number of test articles leading up to the fabrication and testing of the integral blade. The test articles include a sandwich beam with representative laminate facesheets, three iterations of half-span blade sections, and finally, the full integral blade. The design of each blade section incorporated improvements based on testing of the prior test articles. The tests described include actuation performance, damage tolerance, and general structural properties.

6.1 Representative Laminate Tests

The objective of the representative laminate tests is to evaluate the actuation performance and damage tolerance of an active laminate similar to that of the integral blade. In particular, the sandwich beam construction of the test article allows for testing of the facesheets under large tensile and compressive loads. The tests performed are identical to those described in Section 3.4.3 for small AFC test articles.

6.1.1 Test Article and Test Procedure

The sandwich beam test article consists of a 36 inch long beam with a width of 2 inches. The beam had a 1 inch thick Rohacell 71 foam core and identical facesheets with a lay-up representative of the spar laminate of the integral blade. Figure 6.1 diagrams the facesheet lay-up. The laminate has an active section consisting of a $+45^\circ$ and a $-45^\circ$ AFC pack in...
two separate plies centered in the span of the beam. In the outer extents of the beam, beyond the packs, a ±45° E-glass fabric ply replaces the active ply. ±45° E-glass fabric plies also are used for the inner and outer plies of the laminate. A 0° S-glass unidirectional ply separates the two active plies. This lay-up is equivalent to the outer 5 plies of the spar laminate used in the integral blade.

![Diagram of facesheet lay-up for representative laminate including outer passive sections and inner active section.](image)

**Figure 6.1** Diagram of facesheet lay-up for representative laminate including outer passive sections and inner active section.

The beam test article is shown in Figure 6.2. A strain gage rosette centered on the active section is used to monitor the static strain level of the laminate during loading, and the actuation strain of the laminate during performance testing. A closer view of the active section of the beam is provided in Figure 6.3.

![Representative laminate test article for 4-point bend testing.](image)

**Figure 6.2** Representative laminate test article for 4-point bend testing.

During the tests, two fixed supports 34 inches apart support the ends of the beam while a load is applied at 2 points 17 inches apart. This results in an approximately constant moment distribution on the active section of the beam. The upper laminate is placed in
compression while the lower laminate is in tension. The test procedure consists of loading the beam to a prescribed static strain level, unloading, and then measuring the actuator performance. This cycle is repeated for increasing static strain levels. The static strain level is measured in terms of 0° laminate strains along the span of the beam. For the performance tests, a 1 Hz, 1200 Vpp, sinusoidal voltage is applied separately to the upper and lower laminates. In both cases, the -45° and +45° packs are driven out-of-phase so that the induced shear strain combine. The induced 45° laminate strain is measured as an indicator of the actuator performance. Measuring the upper and lower laminate performance separately allows for independent data correlating with compressive and tensile loads.

6.1.2 Results

The beam was tested to structural failure in the 4-point bend test. The maximum 0° laminate strain level for which data were collected was 4500 microstrain. The corresponding maximum 45° laminate strain, or AFC fiber strain level, was approximately 1150 microstrain. Figure 6.4 shows the deformation in the beam during testing. Performance data were collected at 500 microstrain increments up to the 4500 microstrain maximum. After each successive loading, the performance of the upper and lower (compressive and tensile) packs was recorded. The data for the relative strain performance of the packs are presented in Figure 6.5 for compression, and in Figure 6.6 for tension.
Figure 6.5  Induced strain performance of ±45° AFC packs as a function of 0° compressive strain history.

Figure 6.6  Induced strain performance of ±45° AFC packs as a function of 0° tensile strain history.

The data for the compressive loaded laminate show no evidence of performance degradation in either the +45° or -45° induced laminate strains. The maximum fiber strain level tested was approximately 10% greater than the compressive stress depolarization limit measured for bulk PZT-5A material [Zhang, 1996]. The referenced data would suggest that at a compressive strain level of 1150 microstrain, the polarization of the piezoceramic fibers would be degraded. Reduced polarization should then decrease the strain performance as well as the measured capacitance of the packs. For the beam test article, the packs subjected to the compressive stress showed no degradation in strain performance or in capacitance.

Data for the packs subjected to tensile strains also showed no degradation in performance under the applied loading. These data are consistent with data collected for 0° AFC characterization test articles subjected to tensile loads (Section 3.3.1). Compressive stress depolarization data collected for 0° characterization test articles described in Section 3.4.3 found that induced strain performance in PZT-5A test articles was reduced by 90% following exposure to 3000 microstrain compressive loading. No measurable degradation
was found at the fiber strain levels reached in the representative laminate tests, as was the case in the current data set.

6.2 Blade Sections

The development of the integral blade concept included the manufacture and testing of three half-span blade sections. The blade sections represented risk reduction steps which provided the opportunity to: 1) evaluate the prospective design with less cost than a full blade, 2) develop a suitable manufacturing process, 3) eliminate deficiencies in the design, 4) compare performance with model predictions, and 5) perform structural integrity testing under simulated forward flight maneuver loads. The following sections will describe the design and the results of various tests performed on the blade sections.

6.2.1 Design

Three blade sections were fabricated and tested as part of the integral blade development. The design of each reflects the progression in manufacturing techniques as well as the planned testing. The three planforms are diagrammed in Figure 6.7. Additional details of the specific lay-ups used for each of the blade sections is provided in Appendix A (Section A.1.3)

Blade Section 1

The first blade section, shown in Figure 6.8, consists of two spanwise pack groups and only a single active ply. Neither the root of the blade or the fairing were included. This blade section served as the first trial for the manufacturing process. The lay-up included E-glass plies in place of the inner active plies and graphite replacement plies at the ends. The inner graphite unidirectional apply was located outside the web plies with the other spar plies. A total of 4 AFC packs were incorporated into the spar. The packs used did not have the 1 mil copper reinforcing strips on the electrodes. The lay-up was for the typical section of the spar and was constant in span. Standard hook-up wires were used to test the actuators in the spar and the twist performance.
Figure 6.7  Drawings of three blade sections manufactured.

Figure 6.8  Blade section 1 prepared for testing.
Blade Section 2

The second blade section was intended to improve upon the first section and to include the complete root, complete spar lay-up, and a tip fixture. The primary objective for this test article was the demonstration of the twist actuation performance for comparison with model predictions. The tip fixture enables tensile loads to be applied to the spar. The fairing was not included. A total of 12 packs were included in the spar. Hook-up wire was again used to provide power to the packs along the web in place of the flex circuit. This section also incorporated 6 internal strain gage bridges. The completed blade section is shown in Figure 6.9. Figure 6.10 provides a closer view of the electrode tabs along the web and their relation to the solder pads on the flex circuit.

![Figure 6.9](image)

**Figure 6.9** Blade section 2 with flex circuit (not attached).

![Figure 6.10](image)

**Figure 6.10** Close-up view of section 2 web showing electrode flaps and alignment with flex circuit.

![Figure 6.11](image)

**Figure 6.11** Aluminum tip fitting mounted on spar core (strain gage visible on core).

The tip fixture was machined from a solid aluminum block. The inboard end has the same cross section as the foam core and has a length of 1 inch. The outboard end has a rectangular cross section of 1.5 inches by 1 inch for gripping in a tensile testing machine. The
design is described in detail in Section A.4. A photo of the tip fitting is provided in Figure 6.11.

The lay-up of the second blade section included 3 active plies with graphite replacement plies inboard and outboard of the packs. The aft edge of the replacement plies were trimmed back 0.25" from the web to ensure adequate separation of the conductive graphite from the edges of the active plies. In addition, the inner spar ply, which is also graphite, was positioned inside the web plies to further insulate the edges of the packs from the conductive graphite. The second blade section was tested to evaluate twist performance, stiffness, and damage tolerance in tension.

**Blade Section 3**

The third blade section incorporated several design improvements as well as the flex circuit and fairing of the integral blade. The improvements include changing from graphite to S-glass replacement plies, adding an insulative boundary around the edges of the packs, and improving the copper strips and electrode contacts on the web. The modifications were intended to increase the reliability of the packs and the power distribution system during high voltage operation. The composition of this blade section was nearly the same as the final complete integral blade. Internal strain gages were not included. The aluminum tip fitting was included again.

Following the manufacture of the spar, the flex circuit was attached. A different attachment technique was implemented for this blade section. A low viscosity epoxy was injected into the gap between the flex circuit and web after the solder connections were completed. Then the fairing was attached in the fairing cure. The completed blade section is shown in Figure 6.12.

The first objective for testing of the third blade section was to demonstrate the twist actuation capability and electrical system integrity up to full operating voltage (4kV). The secondary objective was to demonstrate damage tolerance in combined tension-torsion
loading. This blade section also served as the final practice for the manufacture of the integral blade.

6.2.2 Twist Performance

The twist actuation performance of each of the blade sections was measured for comparison with predicted levels and to characterize the integrity and limitations of the embedded packs and the power distribution system. The details of the packs used in each of the blade sections are provided in Section 3.5.3 Each blade section tested incorporated improvements based on experiences in testing the previous test articles, leading up to the final blade.

For the tests, the inboard or root end of the blade was clamped while the outboard end was free. The tip twist was then measured using a pair of laser displacement sensors. The laser displacement sensors sense the location of a narrow aluminum strip attached to the surface of the spar as shown in Figure 6.13. The midpoint between the lasers is aligned with the quarter-chord of the blade section. Figure 6.14 shows the third blade section with clamped root during testing.
The fixed distance \((s)\) between the parallel laser measurements can be used along with the difference of the two signals to estimate the angle of twist.

\[
\phi = \tan \left( \frac{(d_1 - d_2)}{s} \right)
\]  

\[ (6.1) \]

A pair of high voltage amplifiers are used to drive the blade section actuators. A minimum of two channels are required to drive the +45° and -45° actuators out of phase in order to induce twist in the blade section. In addition, a DC offset is required for testing at the design voltage of 800 V and 4000 Vpp. For some of the testing, standard commercial laboratory amplifiers were used\(^1\). When the required power exceeds the capabilities of the commercial amplifiers (piezos require higher current at higher drive frequencies), a custom amplifier system developed for the integral blade project is used. This amplifier offers 4-channels, greater than 4kVpp, up to 1kV DC offset, and 1 A of current. This satisfies the

---

1. Trek 664, 609C, Medina, NY
2. Kepco, Flushing, NY
requirements for the full integral blade operating at 150 Hz. The design of this amplifier is
detailed in Appendix B (Section B.2.3).

Blade Section 1

The twist data collected on the first blade section consisted of a few preliminary measure-
ments. The primary objective of the tests was to develop testing techniques and more
importantly, to evaluate the electrical integrity of the electrical contacts and the overall
manufacturing process. Twist actuation data were collected at increasing voltage levels.

Inadequate electrical insulation limited the performance of the blade section. Arcing
occurred between one pack and the graphite spar ply along the edge of the web at 300
VDC and 590 Vpp. Testing continued with 3 of 4 packs operating. An average twist rate
of 0.2 deg/m peak-to-peak was recorded at 15 Hz and 600 VDC and 2500 Vpp. Another
breakdown occurred between a pack and the graphite at 600 VDC and 3000 Vpp. With 2
packs operational, a twist rate of 0.3 deg/m at 45 Hz and 600 VDC and 3250 Vpp was
recorded. Arcing occurred between the remaining two packs and the graphite at slightly
higher voltages.

The testing of the first blade section revealed a significant electrical insulation problem
between the active plies and the graphite plies. The problem was concentrated along the
aft edge of the spar laminate where the edge of the packs and the graphite ply (innermost
ply) are in close proximity. The edges of the packs used in this blade section were
trimmed to the edge of the copper electrode rails. Thus the copper edges were exposed to
the graphite with only an air gap in between. The solution was to isolate the graphite ply
with the three layers of E-glass web plies. Then the pack edges were only exposed to glass
plies. The graphite replacement plies also required separation from the web edge to avoid
contact with the pack edges. Within the active ply, a 0.25" separation was maintained
between the packs and the graphite.
Blade Section 2

The tip twist of the second blade section was measured for increasing voltages at an actuation frequency of 20 Hz. The voltage was ramped to 2kVpp with no DC offset. Then a DC offset was added to reach a maximum voltage of 600 VDC and 2600 Vpp. The induced twist rate is plotted as a function of the voltage applied in Figure 6.15. The trend of the induced twist rate data is plotted in Figure 6.16. The maximum twist rate measured was approximately 1.3 deg/m peak-to-peak. The locus of the twist rate versus voltage data is distorted because of a bandwidth limitation of the laser displacement sensors being used\(^1\). The mode of the sensors was inadvertently set to a low bandwidth resulting in attenuation and phase loss in the collected data. To compensate for this, the attenuation and phase loss were estimated from manufacturer’s data on the sensors. These factors were then used to adjust the magnitude and phase of the data as shown.

Figure 6.15 Twist rate versus voltage data for blade section 2 at 20 Hz (Note: adjusted to compensate for laser sensor roll-off).

Figure 6.16 Peak-to-peak twist rate data for blade section 2 as a function of applied voltage.

---

1. Keyence LB11/70 sensors at 20 Hz; 20 msec mode has roll-off beginning around 4 Hz.
The plotted data reflects the maximum voltage level attained prior to an electrical failure in the blade section. At a voltage slightly above 600 VDC and 2600 Vpp, a dielectric breakdown occurred in the spar. The high power amplifier, which had no circuit breaker or current limiting circuit, delivered approximately 1-2 Amps of current through the arcing. This resulted in significant damage on the lower spar surface. The cause was most likely dielectric breakdown between two packs in separate active plies. The single ply of glass between packs in different plies was found to be insufficient insulation. The lack of insulation on the edges of the packs, which were trimmed to be flush with the edge of the electrode rails during the manufacturing process, contributed to the problem. The high power dissipated in the arcing led to a low resistance path between several of the packs and a local delamination of the spar laminate. The surface damage is shown in Figure 6.17.

![Dielectric Breakdown and Delamination](image)

**Figure 6.17** Dielectric breakdown damage in lower spar laminate of blade section 2.

Three corrective measures were taken to address the causes of the damage. First, an insulative border was maintained on all packs manufactured to eliminate the exposed copper along the edges. Secondly, the graphite replacement plies were changed to glass plies as a precaution. Finally, the amplifier was improved with the addition of fuses and a current limiting circuit.
Blade Section 3

The twist performance of the third blade section was measured in two stages of the manufacturing process. Data were collected on the spar alone, prior to the fairing cure, and then again after the fairing was attached. Initial data was collected at a maximum voltage of 400 VDC and 2800 Vpp with a 1 Hz sinusoidal input. The measured twist rate was 2.00 deg/m peak-to-peak. In order to evaluate each of the packs independently for potential dielectric breakdown problems, the packs were tested up to the design voltage of 800 VDC and 4kVpp. Two of the 12 packs failed during the testing at 2.8kV and 4kV. The testing of the spar continued with 10 of 12 packs operational. Twist rate data for the spar only at full design voltage is presented in Figure 6.18. The maximum twist rate was 2.46 deg/m peak-to-peak. Following the fairing cure, the twist was again measured. A maximum twist rate of 2.04 deg/m was recorded as shown in Figure 6.19.

![Figure 6.18](image1.png)  ![Figure 6.19](image2.png)

**Figure 6.18** Twist rate data for blade section 3 at 1 Hz, prior to fairing cure, and with 10/12 packs operational.

**Figure 6.19** Twist rate data for blade section 3 including fairing at 1 Hz with 10/12 packs operational.

In Figure 6.20, the induced twist rate is compared for three configurations of the third blade section: spar with 12/12 packs operational, spar with 10/12 packs, and spar with
fairing and 10/12 packs. The trends in the data clearly show a reduction in slope for the loss of actuation authority and then for the increase in torsional stiffness. Extrapolating the data for the complete blade section with 100% actuation, the predicted twist rate is 2.45 deg/m or approximately 2.57 deg of peak-to-peak tip twist in the full span blade. The data will be compared with model predictions in Section 6.4.1.

Figure 6.20 Comparison of induced twist rate for blade section 3 with spar only (all packs), spar only (10/12 packs), and spar with fairing (10/12 packs).

6.2.3 Stiffness Tests

Two types of stiffness measurements were performed on the second and third blade sections: axial (EA) and torsional (GJ). Additional stiffness data including flapwise and chordwise bending stiffness were collected for the full length integral blade. This will be described in Section 6.3.4.
Axial

The axial stiffness tests were performed at MIT on an Instron 8500 load frame\(^1\). One hydraulic grip was used to grab the tip fitting on the outer end of the blade sections. The interface on the root end used a 0.5" dowel pin through the lead-lag pin hole. A fork or Y-shaped steel fixture\(^2\) which consisted of a single flat section which split into two parts on either side of the blade root. A hole in each side engaged the dowel pin. The second hydraulic grip was used to grab the other end of the fixture. Blade section 3 is shown mounted in Figure 6.21. The same mounting technique was also used in testing the second blade section.

For the second blade section, an axial strain gage\(^3\) was located on the lower spar surface at 0.32R, centered on the inboard group of packs. The spar was loaded to 3000 lbs. of tension in 200 lb. steps. A linear fit to the load versus strain data was used to estimate the slope of the data, which represents the ratio \(F/\varepsilon\). The axial stiffness was then estimated using the expression:

\[
EA = \frac{F}{\varepsilon}
\]  

(6.2)

This results in an axial stiffness of 6.5 MN for the spar only. This measured value is approximately 3% lower than the predicted value of 6.7 MN.

For the third blade section, the stiffness data were collected with a 200 lb. preload. A 100 lb. ramp up at 10 lbs./sec. was used to obtain load and strain data. A symmetric pair of axial strain gages\(^4\) was used to obtain the average strain at a given blade section, averaging the upper and lower surface strains measured at the quarter-chord. Strain gages were placed in the uniform active section of the blade at 0.315R and also at a transition between packs at 0.364R. The respective axial stiffness measurements were 6.8 MN and 6.4 MN.

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1. Instron Corp., Canton, MA
2. Designed by Eric Prechtl, MIT
3. EA-06-125AC-350, Mircromeasurements, Raleigh, NC
4. EA-06-125AC-350, Mircromeasurements, Raleigh, NC
for the full blade section including the fairing. The higher stiffness inboard reflects the additional spar doubler ply of unidirectional S-glass composite present. The measured stiffness was 5% less than the predicted value at 0.315R and 5% greater than the prediction at 0.364R (typical section).

**Torsional**

The torsional stiffness data were collected using a custom load frame. A cable and pulley system is used to apply a force couple at the tip of the blade section. Loads are reacted against a rigid steel frame. Laser displacement sensors are used to measure the twist at various blade stations in order to estimate the twist rate resulting from an applied tip torque. Figure 6.22 shows the full length blade mounted in the test frame. The root of the
blade is clamped. The laser sensors are mounted on an independent beam. A mass is suspended on the cable system such that forces equal to half of the total weight are applied to the tip of the blade or blade section through an aluminum clamp as shown in Figure 6.23. The forces are applied 2 inches forward and 2 inches aft of the quarter-chord. Each of the blade sections was mounted in a similar fashion. Tests performed on two isotropic, uniform rods resulted in torsional stiffness values within 6% of predictions.

![Figure 6.22](image1) ![Figure 6.23](image2)

**Figure 6.22** Torsional stiffness test rig showing load applied (rear) to create couple at tip.  
**Figure 6.23** Application of tip couple through aluminum clamp on blade tip.

The test procedure for measuring the torsional stiffness of the blades consisted of applying a known tip moment to the blade section and measuring the change in twist angle at a given blade station. This test was repeated to obtain 10 data points at each blade station of
interest. The mean twist angle at any two blade stations can then be used in the calculation of the average twist rate. The torsional stiffness, GJ, can then be estimated:

\[ GJ = \frac{T}{(\phi')} \]  

(6.3)

The torsional stiffness of the first blade section was measured across the uniform active section. The estimated stiffness was 110 Nm² for the spar only. The lay-up of the first spar consisted of one active ply with substitute E-glass plies in the other two active plies. The testing of the second blade section resulted in a stiffness estimate of 115 Nm², also for the spar only. The predicted stiffness for the spar was 91 Nm² for the BDE section model and 97.7 Nm² for the FEM. Thus, the measured stiffness was between 18% and 26% greater than the predictions. The measured stiffness of the third blade section, which included the fairing, was 135 Nm². The model predictions for this case were 111 Nm² and 120 Nm² for the two model types. The measured stiffness was between 13% and 22% greater than predictions.

Several factors may have contributed to the discrepancy between the data and model predictions. First, modeling of the torsional properties of the blades is significantly more difficult and less accurate than the modeling of axial and bending stiffnesses, or inertial properties. Secondly, the models of the cross-sectional geometry did not include the flex circuit in the third blade section. Finally, some experimental error must be considered in the stiffness measurements. The standard deviation on the measurements was approximately 5%. While the torsion tester was calibrated on an aluminum rod, the possibility remains that slight flexibility in the system could result in overestimates of the torsional stiffness.

### 6.2.4 Tensile Testing

The second and third blade sections, which were designed with a tip fitting for mounting in an Instron machine, were loaded in tension. The objective of the tensile testing was to evaluate the performance of the actuators and the integrity of the electrical connections under tensile loads. The blade sections were mounted in the tensile testing machine as in
the axial stiffness tests (Section 6.2.3). Blade section 3 is shown mounted in the testing machine in Figure 6.21.

Surface-mounted axial strain gages\textsuperscript{1} were used to monitor the tensile strain level in the active section of the blade during the tests. For the damage tolerance tests, the blade section was loaded to a set load or strain level. Actuation data could not be readily collected while the blade section was mounted in the load frame. The load frame restrained torsion at the root and the tip. Therefore, twist actuation measurement were taken in an unloaded condition, before and after a given load was applied. The capacitance of the packs was monitored during the tests to evaluate any possible charges or lost connections (section 3 only).

In order to evaluate the performance of the packs with a tensile load applied, the blade was loaded in tension and operated in extension mode, i.e. with all packs driven in-phase. Then the axial strain can be used as an actuation performance metric with the testing machine holding a constant load. Conversely, the induced axial force capability can be measured with the testing machine in position control mode.

**Blade Section 2 Results**

The second blade section (spar only) was loaded to 3000 lbs. static load in three repeated cycles. This load corresponds approximately to the maximum centrifugal load at the root of the blade in hover. Therefore this load represents the worst case pure centrifugal loading in the blade. Note that this does not include bending or torsional loads which could increase the local strain levels.

Due to electrical damage that occurred during the twist performance testing, only a few of the packs were tested before and after the loading. The result was that no change in performance was evident as a result of the loading. In addition, no change in capacitance was measured.

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\textsuperscript{1} EA-06-125AC-350 strain gage, Micromeasurements, Raleigh, NC
Blade Section 3 Results

In order to examine the induced strain performance of the packs while under a tensile load, the blade section was operated in extension mode. All active plies were driven in-phase, rather than out-of-phase when operating in twist mode. The testing machine was operated in load control to allow for extensional freedom. In addition, the load control gain was tuned and the actuation cycle was reduced to 0.1 Hz in order to improve the accuracy of the load during the induced extension of the blade.

Data were collected with 9 of 12 packs operational at a voltage cycle of 2000 Vpp. The blade section was loaded in steps up to 3000 lbs. and then returned to zero load. The maximum load corresponds to an axial strain level of 2100 microstrain. The measured induced axial strain is plotted as a function of load in Figure 6.24. The strain decreases in a fairly linear fashion with the applied load, with a maximum reduction of about 18%. During the unloading, the strain levels track along nearly the same line, returning to a slightly higher induced strain level at zero load. The first and last measured strain cycles are plotted along with the cycle measured at 3000 lbs. in Figure 6.25 for comparison. The data were filtered with a 1 Hz low pass filter in MATLAB\(^1\).

Another means of evaluating the performance of the packs while under a tensile load is to measure the induced axial force with clamped boundary conditions. This eliminates the need for accurate position control in the testing machine. Position control is used to obtain the desired load level. The actual boundary condition on the actuators is not totally clamped since the blade is relatively compliant. While the active portion of the blade section expands and contracts in the extension mode of operation, the inboard and outboard sections will elastically deform. Thus, the boundary condition does not allow for a true block force measurement.

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1. MATLAB, The Mathworks, Natick, MA
The data were collected in the same manner as for the strain data previously described except that the actuation frequency was 1 Hz. The data represent the time average of 10 cycles recorded. The peak-to-peak induced axial or extensional load is plotted as a function of the applied static load in Figure 6.26. The maximum decrease is approximately 15%. Once again, the induced force returns to nearly the same initial value as the load is reduced. The reduction is on the same order as that found in the strain data obtained with free boundary conditions. The initial and final no-load cycles are plotted along with the 3000 lb. cycle in Figure 6.27. The data were filtered using a 10 Hz low pass filter.

6.2.5 Combined Loading Tests

Combined tension/torsion loading tests were performed on the third blade section. The objective of the tests was to evaluate the damage tolerance of the active blade under simulated limit loads. The limit loads correspond to a 3g maneuver for the CH-47D. The highest predicted material strain levels in the integral blade occur at 0.337R on the upper and lower spar surface (end of spar doubler ply 1) at 0.332C chordwise (forward of web and fairing skin) [Weems, 1996]. The strain levels, presented in Table 3.1, include a maximum
fiber tension in the active plies of 3600 microstrain and a maximum compression of 2568 microstrain.

The testing was performed in a blade testing rig at Boeing Helicopters (Philadelphia). The blade section was mounted such that a tensile load and a torsional moment could be applied. The root end was mounted through the lead-lag pin. Typically, the outboard end of the blade section would be built up with glass composite to mount in the test rig. Due to time constraints, the existing tensile testing tip fitting in the spar was used for mounting. A pair of steel plates were bonded and bolted to the aluminum flat extending from the spar. The blade section is shown mounted in the test rig in Figure 6.28.

The blade section was instrumented with external strain gages to monitor the strain levels in the spar laminate. Strain gage rosettes were located on upper and lower spar laminate, on the quarter-chord line, and at 0.41R, the center of the outboard packs. One additional rosette was centered over the inboard packs at 0.32R. Figure 6.29 shows the instrumented lower surface of the blade section.
Figure 6.28  Blade section 3 mounted in combined loading test rig at Boeing Helicopters.

Figure 6.29  Lower surface of blade section 3 showing strain gage rosettes centered on inboard and outboard pack groups.
Several combinations of tensile and torsional loading were selected to achieve specific strain conditions in the blade section. Unlike the true loading conditions in the blade, the loads in this test will be constant across the width and span of the blade section. The strain levels in the active plies will also be constant and uniform from 0.337R to 0.47R. Three types of loading tests were performed: initial load-strain data collection at low levels, blade limit loads for CF and torsion, and fiber limit loads. The load conditions and the predicted strain levels in the laminate coordinates as well as the active fiber coordinates are provided in Table 6.1 [Weems, 1996]. Note that the active fiber coordinates are referenced to a +45° ply. For the -45° ply, the axial and transverse strains are reversed and the sign of the shear term is flipped. The limit conditions correspond to the actual limit load configurations. The fiber loading conditions are intended to evaluate the limit strain levels in the AFC packs including the strain contributions from bending loads in the blade. The potential failure modes for the AFC are fiber fragmentation in tension and compressive stress depolarization in compression. The first of the fiber loading conditions extends the +45° fibers to 3600 microstrain. The second conditions compresses the -45° fibers to 2500 microstrain. The final condition achieves the maximum fiber tension and compression simultaneously.

### Table 6.1 Loading Conditions and Predicted Strain Levels for Blade Section 3.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Loads</th>
<th>Laminate Strains</th>
<th>+45° Fiber Strains</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
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<td>Tors (in-lb)</td>
<td>Axial (με)</td>
</tr>
<tr>
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<td>0</td>
<td>639</td>
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<td>fiber-max</td>
<td>8400</td>
<td>430</td>
<td>6131</td>
</tr>
<tr>
<td>fiber-min</td>
<td>0</td>
<td>550</td>
<td>0</td>
</tr>
<tr>
<td>fiber-combined</td>
<td>3500</td>
<td>700</td>
<td>2555</td>
</tr>
</tbody>
</table>
The twist actuation performance of the blade section served as the performance metric during the testing. A pair of amplifiers was used to drive the actuators with a ±1kV, 1 Hz sinusoid. The induced torsional strain was monitored with strain gages. Mean strain levels were measured with the loads applied while peak-to-peak actuation strains were measured in an loaded condition with the root pin removed to enable a free boundary condition. The induced moment was monitored in both the loaded and unloaded conditions.

The loading conditions described in Table 6.1 were tested sequentially on the blade section. Often, the loads were applied in a few steps. At each step, the mean strains were recorded and the induced torque performance was measured. After major steps, the load was returned to zero and the induced torque was again measured. Then the root pin was removed and the free strain performance was recorded. During the fiber limit strain steps, the performance of the +45° and -45° packs were tested separately and together. The tests were performed with 8 of 12 packs operational.

Results

For each of the loading conditions, the actual material strain values were recorded. Table 6.2 presents the various load levels tested and the corresponding strain data in the blade axes as well as in the +45° fiber axes. The transverse strains in these coordinates represent axial strain on the -45° packs. The small steps within each test group are enumerated for reference. The blade limit load tests were performed with no problems. Failure of the tip fitting attachment on the outboard end of the blade section limited the fiber limit tests. The maximum axial load achieved was 4900 lbs which corresponds to an axial strain of 4500 microstrain. The fibers in the +45° plies were exposed to a maximum of 3600 microstrain in tension, while the fibers in the -45° plies were exposed to 2500 microstrain in compression.

The failure of the tip fitting at 4900 lbs tension and -430 in-lbs. of torque (load case fiber-max 2) corresponded with a failure which developed between the fairing skin at the aft
### TABLE 6.2  Actual Loading Conditions and Measured Average Strain Levels for Blade Section 3.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Loads</th>
<th>Laminate Strains</th>
<th>+45° Fiber Strains</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Axial (lbs)</td>
<td>Trans (με)</td>
<td>Shear (με)</td>
</tr>
<tr>
<td>limit-min CF 1</td>
<td>2400</td>
<td>2097</td>
<td>-930</td>
</tr>
<tr>
<td>limit-min CF 2</td>
<td>2400</td>
<td>2147</td>
<td>-891</td>
</tr>
<tr>
<td>limit-max CF 1</td>
<td>3500</td>
<td>3106</td>
<td>-1355</td>
</tr>
<tr>
<td>limit-max CF 2</td>
<td>3500</td>
<td>3220</td>
<td>-1287</td>
</tr>
<tr>
<td>fiber-max 1</td>
<td>4000 -430</td>
<td>3696</td>
<td>-1389</td>
</tr>
<tr>
<td>fiber-max 2</td>
<td>4900 -430</td>
<td>4474</td>
<td>-1739</td>
</tr>
<tr>
<td>fiber-max 3</td>
<td>3860 -370</td>
<td>3885</td>
<td>-1395</td>
</tr>
<tr>
<td>fiber-min</td>
<td>0 -430</td>
<td>-41</td>
<td>124</td>
</tr>
</tbody>
</table>

Edge of the spar. Immediately after the failure, the performance of the blade section was measured in a partially unloaded condition (fiber-max3). The deformation around the bolt hole in the tip fitting is shown in Figure 6.30. The crack along the web line in the fairing skin on the upper surface of the blade section is shown in Figure 6.31. Figure 6.32 shows the lower surface of the blade section including the crack at the forward edge of the fairing skin and the buckled E-glass aft of the web. The tip fitting of the blade section was able to support torque loads after the failure. This enabled testing for the fiber-min case.

![Figure 6.30](image1)  Plastic deformation around bolt hole in tip fixture.  
![Figure 6.31](image2)  Crack in fairing skin at web on upper surface of blade section.
Two performance metrics are used to evaluate the damage tolerance of the integral blade section: induced torsional strain and induced torque. Data for the induced strain collected after each load cycle with a free boundary condition are presented in Table 6.3. The data were collected at zero load first with all packs operating, and then with the +45° and -45° independently. This enables for the correlation of reduced strain with the actual AFC fiber strains. The data, which are an average of the shear strains measured at three strain gage locations, are presented in terms of relative performance change. The normalized (Norm.) performance relates the strain performance before and after the specified loading condition. The cumulative (Cum.) performance relates the initial performance from the first actuation test to the performance after the specified loading condition. For the independent testing of the +45° and -45° plies, the initial data set was collected after the limit-min CF loading condition. The Norm. columns represent the change in performance during one particular loading cycle, while the Cum. columns indicate the cumulative degradation of the performance.
TABLE 6.3 Average Induced Torsional Strain Performance of Blade Section 3.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Both</th>
<th>+45° Only</th>
<th>-45° Only</th>
</tr>
</thead>
<tbody>
<tr>
<td>limit-Min CF 2</td>
<td>-1.9%</td>
<td>-1.9%</td>
<td>-</td>
</tr>
<tr>
<td>limit-Max CF 2</td>
<td>-2.2%</td>
<td>-4.1%</td>
<td>0.0%</td>
</tr>
<tr>
<td>fiber-max 2</td>
<td>-2.8%</td>
<td>-6.8%</td>
<td>-1.3%</td>
</tr>
<tr>
<td>fiber min</td>
<td>-1.7%</td>
<td>-8.3%</td>
<td>-0.9%</td>
</tr>
</tbody>
</table>

The data show that performance changes were minimal in all tests with the exception of the fiber-max case. For this loading cycle, the -45° active plies suffered an 11.8% reduction in performance. The fibers reached compressive strain levels of 1100 microstrain combined with 3600 microstrain transverse loading in tension. It is also possible that the blade section encountered higher loads during the failure event which could have contributed to the damage in the -45° plies. The cumulative performance reduction under the blade limit load conditions was 4.1%. The worst case compressive load resulted in a fiber strain of 2500 microstrain in the -45° active plies and had no measurable effect on the induced strain performance.

The data for the induced torque performance are presented in Table 6.4. Data were collected with the load applied and after the load was removed. The relative performance is presented as with the strain data: normalized to the previous data collected at zero load, and relative to the initial data. Data was not collected with the load applied during the fiber-max 2 case, which was the load at which failure occurred in the tip fitting.

The induced torque data represents an average performance metric for all the packs in the blade section. The performance measured with the load applied may provide a more accurate measure of the damage to the active fibers as was the case with the tensile load tests on AFC coupons (Section 3.2.4). However, data collected under load typically show some recoverable reduction in performance as the load is increased. The data show a gradual decline in induced torque after each of the test conditions. Following the max CF limit loads, the cumulative reduction in the loaded performance was 14.5%, while the unloaded
TABLE 6.4  Induced Torque Performance of Blade Section 3.

<table>
<thead>
<tr>
<th>Condition</th>
<th>Loaded</th>
<th></th>
<th></th>
<th>Unloaded</th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Norm</td>
<td>Cum</td>
<td>Norm</td>
<td>Cum</td>
<td>Norm</td>
<td>Cum</td>
</tr>
<tr>
<td>limit-min CF 1</td>
<td>-2.4%</td>
<td>-2.4%</td>
<td>-2.4%</td>
<td>-2.4%</td>
<td></td>
<td></td>
</tr>
<tr>
<td>limit-min CF 2</td>
<td>-1.2%</td>
<td>-3.6%</td>
<td>-4.9%</td>
<td>-7.2%</td>
<td></td>
<td></td>
</tr>
<tr>
<td>limit-max CF 1</td>
<td>-2.6%</td>
<td>-9.6%</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>limit-max CF 2</td>
<td>-7.8%</td>
<td>-14.5%</td>
<td>+1.3%</td>
<td>-6.0%</td>
<td></td>
<td></td>
</tr>
<tr>
<td>fiber-max 1</td>
<td>-7.7%</td>
<td>-13.3%</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>fiber-max 2</td>
<td>-</td>
<td></td>
<td>-5.1%</td>
<td>-10.8%</td>
<td></td>
<td></td>
</tr>
<tr>
<td>fiber-max 3</td>
<td>-23.1%</td>
<td>-27.7%</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>fiber-min</td>
<td>-14.9%</td>
<td>-24.1%</td>
<td>-6.8%</td>
<td>-16.9%</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

performance remained only 6% lower. More severe degradation was measured following the fiber-max loading conditions and the failure of the tip fitting. The cumulative reduction in performance measured at load was 27.7%. The performance after the load was removed was reduced only 5.1% during the fiber-max loading conditions. The unloaded performance degraded further after the fiber-min loading to a cumulative reduction of 16.9%. It is possible that the damage to the fairing of the blade section which occurred during the fiber-max 2 cycle may have contributed to the reduced actuation.

6.2.6 Summary

Performance testing of the three blade sections has led to a number of design improvements for the integral blade, as well as improvements in the manufacturing process. The design improvements generally relate to the need for better insulation around the AFC packs within the composite spar. The design also must isolate any necessary graphite from the high voltage. Graphite replacement plies have been replaced with glass plies with the added cost of slightly reduced stiffness and higher mass inboard and outboard of the active section.

Blade section testing has also demonstrated twist performance of roughly 2 deg/m peak-to-peak in bench testing with 84% of the actuators operational. This performance will be
compared with model predictions in Section 6.4. The measured torsional stiffness of the blade sections suggests that a higher than predicted stiffness has contributed to lower than expected actuation.

When tested under combined tension/torsion limit loads, the total performance reduction (induced torque) was 15% when measured with the load applied. When measured at zero load, the reduction in induced torque was 6% while the free strain output reduction was 4%. In tests designed to evaluate the limit strain levels in the AFC plies, an 12% reduction in strain performance was measured under a ply loading of 1100 microstrain in compression and 3600 microstrain in transverse tension. However, a failure of the tip fitting and fairing damage may have adversely affected the performance.

6.3 Integral Blade Bench Testing

Several bench tests were performed on the integral blade in order to evaluate the performance and structural properties. Twist performance tests are used to correlate with model predictions and the hover data in the next chapter. The structural tests, which include stiffness, inertia, and dynamic behavior, are also used for comparison with models and in the analysis of hover data. This data also provides important information on the accuracy of the blade manufacturing techniques. A photo of the completed integral blade is shown in Figure 6.33.

![Photo of completed integral blade.](image)

During the performance testing of the integral blade, several electrical failures resulted in a reduced number of operational packs. The failures include open connections between
the flex circuit and the packs and low resistance or short-circuit failures within or between packs. Capacitance measurements are used to check each of the packs. Individual leads can be disconnected at the hub to eliminate shorted or arcing connections. The failures will be discussed in detail and potential causes will be investigated in Chapter 8. For each data set presented for the integral blade, the number of active packs will be stated.

6.3.1 Twist Performance

The benchtop twist actuation of the integral blade was measured for just the spar, prior to the fairing cure, and then for the complete blade. The spar test also was used to check connections to the flex circuit. The tests were performed with the root clamped and with a pair of laser displacement sensors at the tip as with the blade section tests. For the initial spar data, the actuators were driven in twist mode with a pair of Trek high voltage amplifiers. Data were collected at 1 Hz with no DC offset voltage. The maximum voltage applied in these preliminary tests was 1400 Vpp.

The maximum twist rate measured was 0.76 deg/m at 1400 Vpp. This twist rate represents the average twist rate along the active area from 0.27R to 0.95R. The data are plotted in Figure 6.34 along with data from the testing of the full blade. The full blade was tested using the MIT-built high voltage amplifiers at four drive frequencies: 10, 22.27, 44.53, and 66.80 Hz. The latter three correspond to 1, 2, and 3/rev frequencies. This initial data was collected prior to any hover testing of the blade. During these tests, 11 of the 42 packs had open or intermittent connections to the flex circuit. One other pack was disconnected due to a possible short. The maximum applied voltage was limited to 50% of the design voltage to reduce the risk of damage prior to the hover testing.

Several important trends can be seen in the data. First, the data collected for the spar only shows a higher twist. The reduced stiffness without the fairing and the greater number of active packs are the causes for the higher performance. It is also evident that the twist performance increases at higher driving frequencies. The increase can be attributed to the proximity to the first torsional resonance of the system. This will be further investigated in
the dynamic testing of the blade. The data will be compared with model predictions for the induced twist in Section 6.4.2. In addition, the frequency response or transfer function data presented in Section 6.3.2 will provide twist performance data over a range of frequencies from 10 Hz to 150 Hz.

Data are also presented for torsional strain and tip twist as a function of the driving voltage in Figure 6.35. The tip twist response of the blade was measured using the laser displacement sensors. During the tests, the blade was mounted on the hover test stand and was oriented horizontally as shown in Figure 6.36. The data were collected at 10 Hz and were averaged over 10 cycles. The data show the maximum quasi-steady twist response of the blade, 0.78° peak-to-peak measured at 1920 Vpp. The corresponding strain levels mea-
sured at 0.8R and 0.9R are 77.8 με and 77.2 με, respectively. This correlation will be used to estimate the tip twist from the measured torsional strain during hover testing.

### 6.3.2 Transfer Function Data

Frequency response data has been collected for the integral blade using a white noise input and also using a sine sweep method. The white noise tests were performed on the blade prior to hover testing in order to characterize the blade structural dynamics. A bandlimited white noise signal was input to the high voltage amplifiers. The tip twist response of the blade was measured using the laser displacement sensors. During the tests, the blade was mounted horizontally as shown in Figure 6.36. The voltage applied was approximately 350 Vrms. The blade was tested in both twist mode and bending mode. In bending mode, the +45° and -45° plies are driven in-phase on each side of the spar, but the upper and lower spar plies are driven out-of-phase. The twist mode of operation was used to excite the torsional resonances while the bending mode was used to excite the bending resonances.

The torsional transfer function is plotted in Figure 6.37. The magnitude is plotted in degrees of tip twist per kV of input voltage. The response clearly shows the first torsional mode of the blade. The first torsional mode is 109 Hz in this configuration. The bending mode data is plotted in Figure 6.38. The magnitude for this data is tip deflection per kV of input voltage. The bending mode peaks are located at 5.3 Hz, 27.3 Hz, and 111 Hz. The peak near 60 Hz is due to signal noise.

Another set of twist and bending mode transfer function data was collected after the completion of the hover tests (described in Chapter 7) with the blade root clamped. The blade was mounted in the base used for the benchtop twist data in a vertical orientation. The blade was operated in twist and bending mode with a 2kVpp signal. Transfer function data were collected using a swept sine technique. Single-frequency is collected and estimates of Fourier coefficients are calculated along with a confidence level. Data at a given frequency are collected until a desired confidence is reached. Then the frequency is
switched to the next frequency in the sweep and the process is repeated. The method will be described in detail in Section 7.2.3. The transfer function data is presented in Figure 6.39 and Figure 6.40 from voltage to torsional strain at 0.8R and from voltage to flap bending strain at 0.39R, respectively. The first torsional mode was located at 113 Hz, while bending modes were located at 7 Hz, 38.5 Hz, 100 Hz, and 192 Hz.

It is evident that the location of the modes measured in the two data sets is somewhat different. In particular, the natural frequencies of the first two bending modes apparently shifted from 5.3 Hz and 27.3 Hz to 7 Hz and 38.5 Hz. This may be attributed to a change in the root end boundary condition on the blade in the two tests. In the first dataset, the blade was mounted in the pitch shaft assembly and was oriented horizontally. In the second dataset, the blade was clamped at the root and was oriented vertically. It is unlikely that the hover testing led to a stiffening of the blade (non-rotating).

6.3.3 Electrical Behavior

Characterizing the electrical behavior of the integral blade will be important for future development of an IBC system. In particular, the effect of the driving voltage on the twist performance and the electrical admittance have been investigated. Transfer function data from voltage to torsional strain at 0.8R are presented in Figure 6.41, and from voltage to current are presented in Figure 6.42, for +45° actuators only. The -45° actuators were not included because the amplifier was configured with a single return path. The current of the -45° actuators would be out-of-phase with that of the +45° actuators and would reduce the measured current. The data were collected using a sine sweep with 13 packs operating in the +45° plies at three operating voltages: 500 Vpp, 1kVpp, and 2kVpp.

In the torsional strain transfer functions, the increasing voltage has the effect of increasing the quasi-steady strain response. However, the amplitude of the first torsional resonance decreases with increasing voltage and also shifts slightly lower in frequency. This may be the result of increased piezoelectric nonlinearities at higher voltages.
Figure 6.35  Comparison of hysteresis loops for tip twist and torsional strain at 10 Hz.
If the piezoelectric actuators were modeled as a simple capacitor, the current would be proportional to the voltage input, the capacitance, and the actuation frequency. Since the transfer function represents the impedance, the linear model would consist of a single line for all operating voltages with a slope equal to the capacitance. In actuality, polarization effects in the piezoceramic material increase the current or effective capacitance in the linear model as the voltage is increased. This is clearly evident in the significant increases in slope in the data for increasing voltages. The data also show a distortion near the first torsional resonance of the system which is indicative of the electromechanical coupling in the system. In addition, the phase of the transfer function, which is nominally 90°, increases slightly with frequency. At higher voltages where polarization effects are significant, the measured current signal is no longer a pure sinusoid. As a result, the single frequency Fourier coefficient is a less accurate measure of the true current at higher driving voltages.

6.3.4 Structural Properties

The passive structural properties of the integral blade have been measured for comparison with model predictions. Stiffness properties of the blade were collected using similar techniques to those used for the testing of the blade sections. Next, the total mass and flap inertia of the blade was measured. Once all data collection was completed for the whole blade, the blade was sectioned in order to evaluate the spanwise distribution of inertial
Figure 6.37  Twist mode transfer function from voltage to tip twist using 350 Vrms bandlimited white noise.

Figure 6.38  Bend mode transfer function from voltage to tip deflection using 350 Vrms bandlimited white noise.

properties and to examine the cross sections for an analysis manufacturing process. The following subsections will present the stiffness and inertial data for the blade. The data are compared with model predictions for the spanwise distribution of properties [Weems, 1996]. The cross-section analysis will be presented in Chapter 8.
Figure 6.39  Twist mode transfer function from voltage to torsional strain at 0.8R using sine sweep.

Figure 6.40  Bending mode transfer function from voltage to flap bending strain at 0.39R using sine sweep.

Stiffness

Three types of stiffness tests were performed on the integral blade: torsional, flapwise bending, and chordwise bending. Axial stiffness tests were not performed but estimates from the blade section testing will be included for completeness. All the stiffness data will be summarized in a figure at the end of the subsection.
The torsional stiffness measurements were performed using the same technique as for the blade section tests, as described in Section 6.2.3. A tip moment couple is applied to the blade with the root clamped. Then a pair of laser displacement sensors is used to measure the twist resulting from a static moment. The measurement can be repeated at a number of blade stations in order to get a distribution of properties.
In order to measure the chordwise and flapwise bending stiffness, a tip force was applied to the tip of the blade with the root clamped. The tip force was aligned with the pitch axis (0.25c) either in the flapwise or chordwise direction. Since the blade has a pretwist, a force applied normal or parallel to the blade tip chordline will not be aligned with all spanwise sections of the blade. This will introduce some error into the measurements. The pair of laser displacement sensors was used to obtain an average deflection at each spanwise station for the applied static tip load. The application of the tip load, in the chordwise bending configuration, is shown in Figure 6.43.

![Figure 6.43](image.png) Chordwise tip load applied to blade during chordwise bending stiffness measurement.

![Figure 6.44](image.png) Dual pinned boundary conditions for measuring chordwise bending stiffness.

For the chordwise bending configuration, the root clamp, which includes a lead-lag pin and clamps the blade in the thickness direction, was insufficient for restraining chordwise motion. The clamp essentially provided a simply supported boundary condition at the root pin. An additional simply supported contact point was added at BS14 in order to effectively clamp the blade root. The two pin joints are shown in Figure 6.44. The chordwise
bending deflection measurements are particularly sensitive to misalignment of the tip load due to the pretwist. This is because the blade is an order of magnitude stiffer chordwise than flapwise. Chordwise bending data were only collected at the blade tip to estimate an average value for the blade. Flapwise measurements, which are less sensitive to alignment errors, were collected at several blade stations. The blade stiffness data are summarized in Figure 6.45.

Axial stiffness data were collected from the third blade section rather than the full span integral blade. The data were collected using strain gage measurements at 0.31R and 0.42R. This local stiffness was then used to represent the stiffness over the surrounding uniform span of the blade, i.e. no changes in lay-up. Properties of the actual integral blade may vary slightly from this data as a result of added strain gage wiring and balancing nose weights. In general, the predictions are within 3% of the data.

For the torsional stiffness data, the measured twist at a number of blade stations was used to estimate the local twist rate. The torsional stiffness was then estimated using Equation 6.3. The measured stiffness of the blade was significantly higher than predicted. Between 0.337R and 0.65R, the stiffness was 30% greater than predicted. In comparison with a FEM prediction, generally regarded as a more accurate prediction, the blade data were 20% greater than predictions.

In addition to the twist rate measurements collected for an applied tip moment, torsional shear strain data were also collected. The data were obtained simultaneously during the torsional stiffness tests to provide a good correlation. This may be useful in analyzing the torsional shear strain data collected during hover testing. First, three model predictions are compared for the torsional stiffness and deformation properties of the typical blade section. The first two models are the Boeing Helicopters (Philadelphia) BDE and FEM model types. The FEM has the most accurate cross-sectional geometry. A third model, a 2-cell beam model which includes warping, was also used in the comparison although it neglects the nose block and core of the blade and thus has a lower predicted stiffness
Figure 6.45  Summary of blade stiffness data including axial (estimate from blade section 3), torsional, flapwise bending, and chordwise bending along with model predictions.
[Cesnik, 1998]. Table 6.5 presents the predicted stiffness values along with the expected shear strain for a 1 N-m tip torque and the correlation between shear strain and twist rate.

**TABLE 6.5** Comparison of Model Predictions for Torsional Stiffness and Deformation (0.337R-0.65R).

<table>
<thead>
<tr>
<th>Model</th>
<th>GJ (Nm²)</th>
<th>Shear/Torque (µε/Nm)</th>
<th>Shear/Twist Rate (µε/°/m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>BDE (Boeing)</td>
<td>123</td>
<td>79.2</td>
<td>170</td>
</tr>
<tr>
<td>FEM (Boeing)</td>
<td>133</td>
<td>87.3</td>
<td>203</td>
</tr>
<tr>
<td>2-cell (Cesnik)</td>
<td>99</td>
<td>85.0</td>
<td>147</td>
</tr>
</tbody>
</table>

Table 6.6 presents the actual data collected using comparable parameters. The torsional stiffness estimated from the measured twist rate is provided for three spanwise regions where strain gages were located. The next column presents the measured torsional shear strain (twice the actual 45° strains) for an applied 1 N-m tip torque. The last column lists the torsional shear strain equivalent to a 1°/m twist rate. In general, the measured torsional stiffness was significantly greater than all of the model predictions. Similarly, the shear strain for a given applied torque was much less than predicted. The correlation between shear strain and twist rate at 0.413R was fairly close to the FEM predicted value. The relatively high shear strain for a given torque at 0.413R may be the result of the externally applied strain gage bridge at that location.

**TABLE 6.6** Correlation Between Strain Gage Data and Measured Twist Rate Data.

<table>
<thead>
<tr>
<th>Strain Gage Location</th>
<th>GJ (Nm²)</th>
<th>Shear/Torque (µε/Nm)</th>
<th>Shear/Twist Rate (µε/°/m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.413R</td>
<td>160</td>
<td>67.8</td>
<td>189.3</td>
</tr>
<tr>
<td>0.8R</td>
<td>148</td>
<td>51.0</td>
<td>131.7</td>
</tr>
<tr>
<td>0.9R</td>
<td>116</td>
<td>53.3</td>
<td>107.9</td>
</tr>
</tbody>
</table>

a. external strain gage
The flapwise bending deflection data were compared with a simple cantilevered beam model in order to estimate the bending stiffness. First, the deflection data were fit to a fourth order polynomial. Then, a piecewise linear fit between the deflection curve and the beam model was used to estimate the stiffness. The resulting blade stiffness in the mid-span of the blade was 4% stiffer than model predictions. This trend was fairly consistent across the entire blade span.

Flapwise bending strains were also measured during the tests using an internal strain bridge at 0.39R. The BDE model prediction for the flap bending strain is 68.2 με for a 1 N tip load. The measured strain level for an equivalent load was 64.8 με, only 5% less than predicted.

For the chordwise bending data, the deflection measurements were again fit to a polynomial. A simple beam model including the two simply supported joints was developed. A linear fit between the model and deflection curve was used to determine the chordwise bending stiffness. In order to improve the fit, rigid body translation and rotation were included. This accounted for any imperfections in the root boundary conditions which were more significant because of the high chordwise bending stiffness. The estimate blade average was 6% stiffer than the BDE model prediction for the midspan stiffness. The stiffness estimates and model predictions for the typical section of the blade are summarized in Table 6.7.

<table>
<thead>
<tr>
<th>Property</th>
<th>Data</th>
<th>Model</th>
<th>% Diff.</th>
</tr>
</thead>
<tbody>
<tr>
<td>EA (MN)</td>
<td>6.4</td>
<td>6.2</td>
<td>+3.2</td>
</tr>
<tr>
<td>GJ (Nm²)</td>
<td>160</td>
<td>133</td>
<td>+20</td>
</tr>
<tr>
<td>EI_f (Nm²)</td>
<td>195</td>
<td>188</td>
<td>+3.7</td>
</tr>
<tr>
<td>EI_c (Nm²)</td>
<td>4590</td>
<td>4340</td>
<td>+5.8</td>
</tr>
</tbody>
</table>
Inertial

The first inertial measurements of the integral blade were collected following the completion of hover testing. The external portion of the flex circuit and strain gage leads were removed to enable a more accurate measurement. The total blade mass was 801.7g. The spanwise center of gravity was located at BS33.22 or 0.548R. The flapwise inertia of the blade was measured using the method of bifilar suspension\(^1\). In this technique, the blade is suspended horizontally from a pair of vertical wires. Then the natural frequency of the blade in a rotational mode can be measured. Given the length of the wires (s), the distance from each wire to the blade cg (\(r_1, r_2\)), the mass of the blade (mg), and the rotational frequency (\(\omega\)), the inertia about the cg can be calculated from:

\[
I_\theta = (mgr_1r_2)/s\omega^2
\]  

(6.4)

The flapping inertia of the blade about the flap pin located at 0.028R can be calculated using the parallel axis theorem. The resulting flap inertia was 0.638 kg·m\(^2\).

Next, the integral blade was sectioned in order to measure the spanwise distribution of inertial properties. The mass per unit span, chordwise center of gravity, and pitch inertia per unit span were measured. The blade was cut into 2 inch intervals between BS17 and BS59. Inboard of BS17, the sections were cut in 1 inch intervals. A diamond abrasive circular saw with water cooling was used to cut the sections. The faces of the sections were lightly sanded and were dried at 100°C for 2 hours. Each piece was placed on a razor-edge balance in order to measure the chordwise center of gravity of each section. Then the previously described method of bifilar suspension was used to estimate the pitch inertia. The data are summarized in Figure 6.46 along with model predictions.

Geometry

In order to investigate the discrepancy between the measured and predicted torsional stiffness of the blade, the cross-sectional geometry of the blade sections were checked. The

---

Figure 6.46  Comparison of measured and predicted values for mass per unit span, chordwise center of gravity, and pitch inertia per unit span for the integral blade.

thickness of each section was measured with calipers at the web and at the location of maximum thickness. The results show a consistently oversized cross-section along the span, with an average thickness 1% greater than expected. The cause was most likely the failure of the mold to fully close during the cure. Accounting for this error in the blade structural model resulted in a 2% increase in the predicted torsional stiffness.
6.4 Comparison of Bench Performance Data with Model Predictions

In order to evaluate the performance of the various integral blade sections and the full integral blade, the twist actuation performance was compared with model predictions. The modified Rehfield beam model was used to predict the induced twist rate. For each case, the following considerations were taken into account: the relative number of active packs, the average proof test strain performance of the packs, and the voltage applied. In addition, the measured torsional stiffness is included as a correction on the baseline model prediction.

6.4.1 Blade Section 3

The comparison for the third blade section is presented in Table 6.8 and in Figure 6.47. The blade section was tested with only the spar, and then with spar and fairing together. Initial data for the spar with all packs operational were collected up to 2800 Vpp. During the testing of the blade section at the full design voltage, 10 of 12 original packs were operational. The baseline model predictions were significantly higher than the measured twist rate. Taking into account the measured torsional stiffness, which was 22% greater than model predictions, the resulting model correlation is much improved. For the full section, the prediction is 1.5% greater than the measured twist.

<table>
<thead>
<tr>
<th></th>
<th>Spar Only</th>
<th>Full Section</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>TwistRate</td>
<td>% diff.</td>
</tr>
<tr>
<td></td>
<td>(°/m)</td>
<td></td>
</tr>
<tr>
<td>Data</td>
<td>2.46</td>
<td>-</td>
</tr>
<tr>
<td>Baseline Model</td>
<td>2.81</td>
<td>+14</td>
</tr>
<tr>
<td>122% GJ Model</td>
<td>2.30</td>
<td>-7.5</td>
</tr>
</tbody>
</table>

The comparison of the trends of the data and model versus voltage also illustrates the linear piezoelectric theory incorporated in the model. The piezoelectric induced strain prop-
erties were based on unconstrained AFC pack data collected at high fields. The model assumes 1030 microstrain of free strain actuation at 4 kVpp and 10 Hz. In actuality, the piezoelectric behavior is somewhat nonlinear, as the data illustrate. Thus the model correlation should be more accurate at higher field levels.

### 6.4.2 Integral Blade

Data for the full integral blade are presented in Figure 6.48 and Figure 6.49. The data are peak-to-peak tip twist measurements collected at 10 Hz. The data are compared with the predicted twist from the modified Rehfield model. The model accounts for the packs which were operational during the tests and the experimentally determined torsional stiffness. Data from the proof testing of the AFC packs at 10 Hz were used to estimate the effective linear piezoelectric properties used in the model (1168 microstrain at 4 kV).
In Figure 6.48, the nonlinearity of the actuation is evident as function of the applied voltage. This nonlinear behavior is typical of piezoelectrics in which the effective coupling is increased at higher field levels. Since the estimated properties were calculated using high field properties, the linear estimate more accurately models the response at the higher voltages. The model predicts $0.74^\circ$ at 1920 Vpp which is 5% below the measured $0.78^\circ$. Figure 6.49 illustrates the spanwise variation in the beam model, accounting for variations in lay-up and in the number of active packs. The data are plotted as a line since the twist was only measured at the tip. The model discretizes the blade according to spanwise pack groups and experimental torsional stiffness estimates, as summarized in Table 6.9. The input files used for this calculation are included in Appendix C.

In general, the predictive capability for the induced twist rate appears to be fairly accurate if the experimentally determined torsional stiffness and pack properties are considered. Torsional stiffness predictions can be improved with more accurate modeling of the cross-sectional geometry of the blade, including the core, nose block, and flexible circuit. The
TABLE 6.9 Model Input Data.

<table>
<thead>
<tr>
<th>Spanwise Location/R</th>
<th>Packs Operating</th>
<th>GJ (Nm²)</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.000-0.273</td>
<td>N/A</td>
<td>250</td>
<td>root section</td>
</tr>
<tr>
<td>0.273-0.338</td>
<td>5</td>
<td>177</td>
<td>pack group 1</td>
</tr>
<tr>
<td>0.338-0.442</td>
<td>4</td>
<td>160</td>
<td>pack group 2, ply drop</td>
</tr>
<tr>
<td>0.442-0.546</td>
<td>3</td>
<td>160</td>
<td>pack group 3</td>
</tr>
<tr>
<td>0.546-0.610</td>
<td>3</td>
<td>160</td>
<td>pack group 4</td>
</tr>
<tr>
<td>0.610-0.714</td>
<td>4</td>
<td>148</td>
<td>pack group 5, ply change</td>
</tr>
<tr>
<td>0.714-0.818</td>
<td>6</td>
<td>148</td>
<td>pack group 6</td>
</tr>
<tr>
<td>0.818-0.961</td>
<td>5</td>
<td>116</td>
<td>pack group 7, taper</td>
</tr>
<tr>
<td>0.961-1.000</td>
<td>N/A</td>
<td>100</td>
<td>taper</td>
</tr>
</tbody>
</table>

The current method of using an estimate of the average pack free strain properties has been successful for predicting the high field level performance of the blade.
Chapter 7

HOVER TESTING

In this chapter, data from the hover testing of the integral blade will be presented. First, the properties of the integral blade rotor system will be reviewed. Next, the MIT Hover Test Stand Facility will be described including the data acquisition system and testing environment. Then hover data will be presented primarily in the form of transfer functions from input voltage to the active plies to various performance metrics.

7.1 Integral Blade Properties

The objective of this section is to summarize the properties of the integral blade rotor system. The 1/6th Mach scale CH-47D blade system was spin tested in air at MIT. The two-bladed system consisted of the integral blade and a passive balance blade. The integral blade was manufactured at MIT as described in Chapter 5. A complete set of data for the elastic and inertial properties of the integral blade were provided in Section 6.3.4. The general properties of the rotor system are provided in Table 7.1. The properties reflect the actual measurements collected on the integral blade. Data collected in hover testing will provide additional details about the blade properties including the rotating natural frequencies of the rotor system.

The complete integral blade is shown in Figure 7.1. The blade contains 42 AFC packs distributed in three active plies of the spar laminate. The seven spanwise groups of packs are visible in the photo. Near the root end of the blade, a flexible circuit for high voltage and
TABLE 7.1 Model Scale Integral Blade Properties

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>geometric scaling</td>
<td>1:5.939</td>
</tr>
<tr>
<td>radius</td>
<td>60.619 in.</td>
</tr>
<tr>
<td>chord</td>
<td>5.388 in.</td>
</tr>
<tr>
<td>mass/span (typ.)</td>
<td>0.030 lb./in.</td>
</tr>
<tr>
<td>no. blades</td>
<td>2</td>
</tr>
<tr>
<td>rotor type</td>
<td>articulated</td>
</tr>
<tr>
<td>flap hinge loc.</td>
<td>0.028R</td>
</tr>
<tr>
<td>lag hinge loc.</td>
<td>0.15R</td>
</tr>
<tr>
<td>pretwist</td>
<td>12°</td>
</tr>
<tr>
<td>Lock no.</td>
<td>9.32</td>
</tr>
<tr>
<td>rotor speed</td>
<td>1336 RPM</td>
</tr>
</tbody>
</table>

a strain gage wire bundle are visible. The interface of the blade with the hover test stand will be described in the next section. The blade will be actuated in twist during the hover testing. Based on bench testing measurements (Section 6.3.1), the expected tip twist of the blade will be approximately 1 degree peak-to-peak. The hover testing will provide further data on the performance of the blade. The blade was also tested in the bending mode of operation.

Figure 7.1 Photo of integral blade prepared for hover testing.
7.2 Hover Test Stand

The MIT Hover Test Stand Facility was developed for testing Mach-scaled rotor blades. The system was designed for testing a 10 foot diameter rotor with 2 or 3 blades. This section will review the capabilities of the test facility and will provide other information on the test environment, the data collection techniques, and the rotor system tested. Additional details may be found in Appendix B.

7.2.1 Overview

The MIT Hover Test Stand Facility was designed for the testing of active blades at Mach scaled rotor speeds. The test stand consists of a steel frame in a pyramid configuration which houses an electric motor with a direct-coupled shaft which passes through a slip ring assembly and terminates at the hub. The test stand is shown in Figure 7.2. The stand structure was designed using a finite element analysis to ensure that the structural resonances of the stand would not interfere with the intended operating frequencies of rotation or actuation (10-150 Hz). The base of the stand is isolated on air cushions to attenuate the transmission of floor vibrations, which are significant in the testing room, to the stand. Dampers were added to reduce the risk of ground resonance.

Capabilities

The drive motor for the test stand was sized to provide sufficient speed and power for testing a 3-bladed rotor system, such as the 1/6th Mach scale CH-47D, at 1336 RPM with a blade loading of 0.10. The design thrust level was approximately 1200 lbs. A 150 HP AC vector drive motor with 0.1% speed accuracy was selected. The motor has a maximum speed capability of 1500 RPM.

The drive shaft connects the motor to the hub and passes through a slip ring assembly. The slip rings pass sensor signals and power leads between the rotating frame (hub) and the stationary frame. The system has 138 sensor channels and 24 high voltage channels. The
high voltage channels are capable of 8 kV between lines for driving the AFC or other actuators in an active blade.

At the top of the shaft, the hub is attached for mounting the blades. In addition, a number of rotating frame sensors (described in the next subsection) are mounted. Connectors are mounted in a cylindrical leads shell for interfacing with the instrumented blades.
Collective Pitch

The blades are mounted to the hub through a pitch shaft assembly. The pitch shaft assembly has a horizontal pin interface at the hub and a vertical pin interface with the blade root. The system does not have a swashplate pitch control system. Instead, the pitch shaft assemblies contain a pitch adjustment mechanism to set a fixed pitch for a given hover test. The pitch of each blade can be manually adjusted for blade tracking or changing of the collective pitch angle.

Sensor Suite

The hover test stand and the rotor blades are instrumented with a number of sensors for safety and performance monitoring. In the stationary frame, the test stand frame has accelerometers for monitoring vibration levels during testing. A magnetic proximity sensor is used to detect a shaft keyway to provide a 1/rev striker signal. This provides a monitor for rotor speed and azimuth. A video camera and strobe are mounted approximately 2 feet outboard of the blade tips in the tip path plane for safety monitoring and blade tracking. A sample image from the fixed frame camera (no strobe) is shown in Figure 7.3.

In the rotating frame, a 6-axis load cell is used to measure all forces and moments transferred between the main shaft and the hub. The load cell was not dynamically calibrated. Each pitch shaft is mounted with torsional shear, flapwise bending, and chordwise bending strain bridges for safety monitoring. Hall effect transducers are mounted on the horizontal and vertical pins to measure the relative angular motion. The blades are instrumented with strain gages which interface through a multipin connector in the leads shell. A rotating frame camera mounted on top of the hub can be used to monitor the active blade during testing. The camera uses an RF link to transmit the video signal. A sample image is provided in Figure 7.4.
7.2.2 Testing Environment

The MIT Hover Test Stand Facility is located within an enclosed space in the Active Materials and Structures Lab at MIT. The testing room has dimensions of approximately 66 feet by 28 feet with a ceiling height of 14 feet. The floorplan, shown in Figure 7.5, includes the testing room and the adjacent control room and laboratory space. The testing room also contains a number of columns, beams, and pipes which may affect the airflow. The column positions are all outside of the 14 foot diameter containment ring. One main beam across the ceiling traverses the center of the test stand between columns. The bottom of the beam is 12 feet 8 inches from the floor. The hub is approximately 7 feet 6 inches from the floor, so the tests are run in ground effect. The containment ring is 4 feet high and is roughly centered at the height of the blade tips assuming a 5° precone angle.

The obstacles and boundaries of the room have a strong effect on the flow through the rotor disk in hover. Tests have shown that a combination of effects results in nonuniform inflow in azimuth. This results in added aerodynamic disturbances which contribute to sensor noise. Data acquisition techniques were implemented to reduce the effects of the noise. Additional details on the background disturbance levels and the error bounds on the collected data will be provided in Section 7.3.6.
7.2.3 Data Acquisition

Data are collected using a 48 channel National Instruments data acquisition system based on a 200 MHz Pentium computer. A number of analog and strain gage input modules transfer multiplexed data into a data acquisition board in the computer. The board samples at up to 100 kHz. The interchannel delay used during the tests was 2.5 μs. Two Labview virtual instrument systems were developed to control the data acquisition and to display and save the collected data. Both enable real time monitoring of 12 to 15 data channels during testing. The first is designed to stream all 48 data channels directly to disk at a maximum scan rate of 2 kHz. The second is specialized for obtaining transfer function data using a swept sine technique.

Using the first method, the time trace data can be post-processed for analysis. Initial transfer function data were collected using a band-limited white noise or single frequency sinusoidal input to the actuators. Then a Fourier analysis was performed to obtain frequency response characteristics. For the single frequency data, the magnitude of the peak in the transfer function at the driving frequency could be obtained and compared with the magnitude of the transfer function with no actuation. The time trace used in the analysis was parsed to an integer number of actuation cycles.
The swept sine method was developed in order to minimize the signal noise affecting the transfer function data collection. The blade is actuated at a single frequency. For each actuation cycle, the sine and cosine components of the Fourier coefficient are calculated at the actuation frequency for each data channel, including the voltage, strain, hub loads, etc. For each subsequent actuation cycle, the process is repeated and a estimate of the Fourier coefficient is calculated in real time. Using the mean, standard deviation, and number of cycles, and desired level of accuracy, a confidence level can be calculated for each data channel. For example, data can be collected to obtain 90% confidence in a 5% level of accuracy for each channel. The data acquisition can be automated to continuously collect more data at a particular actuation frequency until a desired confidence level is attained, or a maximum number of cycles is reached.

The Fourier coefficient integrals for each cycle \((i)\) were estimated using 20 points \((n)\) per cycle,

\[
c_i = \frac{2}{n} \sum_{j=1}^{n} x(t_j) \cos \omega t_j \tag{7.1}
\]

and,

\[
s_i = \frac{2}{n} \sum_{j=1}^{n} x(t_j) \sin (\omega t_j) \tag{7.2}
\]

Then the mean value of the cosine and sine coefficients were estimated after each cycle, where \(N\) is the current total number of cycles:

\[
\hat{c} = \frac{1}{N} \sum c_i \tag{7.3}
\]

\[
\hat{s} = \frac{1}{N} \sum s_i \tag{7.4}
\]

Similarly, the standard deviations for the coefficients were calculated:
\[ \sigma_c = \sqrt{\left( \sum c_i^2 - N \bar{c}^2 \right)/(N - 1)} \quad (7.5) \]

\[ \sigma_s = \sqrt{\left( \sum s_i^2 - N \bar{s}^2 \right)/(N - 1)} \quad (7.6) \]

Finally, the confidence level for each coefficient can be calculated given a desired accuracy, \( k \), which can be defined as a fraction of the mean. The confidence is found using a student's T-distribution function, \( F \):

\[ C = 2F((k \sqrt{N})/\sigma) \quad (7.7) \]

The calculations are updated after each cycle of actuation until the desired confidence and accuracy or reached, or a maximum of 5000 averages have been collected. Then the next frequency in the sine sweep is tested.

A sufficient number of averages is collected to average out the effect of rotor azimuth on the collected data. This also has the effect of attenuating uncorrelated noise on the signal. In addition to the averaging process, low pass filters were wired into each of the data channels. The filters reduce the high frequency electromagnetic noise originating from the high frequency switching of the AC motor controller.

### 7.2.4 Hub Interface

The structural interface between the blade and the hub is through a vertical pin at BS9.093 which engages the outboard portion of the pitch shaft. The interface between the blade and the hub is shown in Figure 7.6. The flex circuit and strain gage leads originate at the blade root on the right side of the photo. The leads travel inboard along the pitch shaft and then turn downward into the leads shell. The flex circuit interfaces with the high voltage supply lines through a printed circuit board connector. The strain gage wires are connected through a 44-pin connector. The leads are restrained against centrifugal loads and relative motion where possible to avoid wearing against metal components.
Figure 7.7 shows the interface between the flex circuit and the 5-pin high voltage connection in the leads shell. The 6 flex circuit layers separate and terminate in Clincher connectors which plug into headers mounted on a printed circuit board (PCB). The PCB connects the 84 lines to the proper terminals of the hub connector.

7.2.5 Rotor System

The rotor system consists of the integral blade and a passive balance blade. The passive blade has the same geometry as the active blade with the exception that the blade tapers in planform toward the tip. In addition, the mass and center of gravity of the passive blade differ significantly from the integral blade. In order to balance the centrifugal loads at the hub, extra mass was added to the pitch shaft assembly on the passive blade side. An aluminum clamp used to restrain a pair of keys that transfer axial loads from the inner to outer portions of the pitch shaft assembly was replaced by a larger steel clamp to add mass. Additional mass was added in the form of a steel ring bolted to the inner face of the key clamp. The blades were balanced prior to mounting on the test stand using a knife
edge balance. Each blade is attached to an aluminum balance fixture which rests on a razor edge as shown in Figure 7.8. The standard aluminum key clamp is placed on the fixture at the proper location from the center on the active blade side. On the passive side, the steel clamp and additional ring are located. The flex circuit and strain gage leads are taped to the fixture to avoid contact with the table top.

Once the blade set has been balanced on the table top, the blades can be mounted on the test stand. The blade tracking procedure is then used to adjust the pitch of the blades so that the blades track at the same height. This means that both blade tips travel in the same arc. The track is checked using a fixed frame video camera pointed at the tip path plane and a strobe which is synchronized to the 1/rev striker pulse. The phase of the strobe signal is adjusted to effectively stop the blade tip image in front of the camera. The tip location is then marked on a video monitor for comparison with the second blade. Figure 7.9 shows the image of the integral blade at a low speed during the initial tracking. Figure 7.10 shows the passive blade at full speed with the precone angle visible.
The typical procedure is to set the pitch of the active blade to the desired collective angle. A level is used in conjunction with an airfoil template to measure the angle of the chordline with respect to horizontal at 0.75R. The pitch can be set at the root to obtain a particular angle. Then the blade tracking procedure is used to adjust the pitch of the passive
blade to match. Adjustments are first made at 800 RPM. Then the speed is increased and further fine tuning can be performed until proper track is obtained at 1336 RPM.

7.3 Hover Data

The integral blade has been tested in hover in order to evaluate the twist actuation performance and integrity of the design. The primary performance metrics include the torsional strain measurements which correlate with twist rate, and the induced vertical hub shear. The hub shear metric provides data on the effectiveness of the individual blade control on hub loads.

The following sections will present the collected data primarily in frequency response or transfer function form. Magnitude and phase information is provided along with confidence levels. RPM and collective sweep data are also presented, as well as a fan diagram of the natural modes of the system as a function of rotor speed. The chapter concludes with an evaluation of the error bounds on the data and finally an analysis of the performance.

7.3.1 Test Plan and Procedure

The hover testing of the integral blade presented the opportunity to collect a variety of interesting data on actuator performance as well as the dynamics of the rotor system. An effort was made to collect and present data in a form that would be comparable to previously published data on active rotor testing [Fulton, 1998; Shaw, 1985; Chen, 1997; Bernhard, 1997]. Rotor testing experts at MIT\(^1\), Boeing Helicopter (Philadelphia)\(^2\), the University of Maryland\(^3\), the US Army Aviation RD&E Center at NASA-Ames\(^4\), and the US Army Research Lab at NASA-Langley\(^5\) were consulted directly. The test matrix

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1. Prof Steve Hall, Dept. of Aeronautics and Astronautics, MIT, Cambridge, MA
2. Robert Derham, Douglas Weems, and Richard Bussom, Boeing Helicopter, Philadelphia, PA
3. Andy Bernhard, U. Maryland Alfred Gessow Rotorcraft Center, College Park, MD
4. Mark Fulton, US Army Aviation RD&E Center, Moffet Field, CA
developed for the integral blade attempted to collect data which would address as many performance issues as possible within the constraints of time and testing capabilities.

The objectives of the hover testing include an evaluation of the following:

- twist actuation performance under various conditions (speed, blade loading)
- induced vertical shear under various conditions
- static twist performance
- system identification
- bending actuation mode performance
- actuator power and hysteresis

The primary focus is on evaluating the twist actuation performance of the integral blade in hover. The performance will be compared at different rotor speeds and blade loadings. Data will be collected primarily in the form of transfer functions between voltage input to the AFC actuators to several sensors monitoring performance. This will enable a secondary objective, the identification the rotor system dynamic modes. This data can then be used in the design of control systems for IBC which will be tested in the future.

An overview of the test matrix for the integral blade is presented in Table 7.2 in chronological order. Initial testing was performed using single frequency actuation. Time traces of the data channels were recorded for post-processing. Transfer function data was initially collected using a band-limited white noise input. In order to improve the statistical significance of the data, a swept sine technique which evaluates single frequency Fourier coefficients in real time was used in place of time traces. This technique was described in Section 7.2.3. Background noise baselines were collected as well.

Data were collected at three collective pitch levels: 0°, 4°, and 8°. Data for the 8° collective were collected first. This corresponds to a blade loading of 0.07 (C_T/\sigma). The nominal loads will be described later in this subsection. Testing at 0° followed in order to compare

5. Keats Wilkie, US Army Research Lab, Hampton, VA
TABLE 7.2  Chronological Test Matrix for Hover Testing of Integral Blade.

<table>
<thead>
<tr>
<th>Description</th>
<th>Method</th>
<th>Mode</th>
<th>RPM</th>
<th>Collective</th>
</tr>
</thead>
<tbody>
<tr>
<td>bench testing</td>
<td>single freq.</td>
<td>twist</td>
<td>0</td>
<td>N/A</td>
</tr>
<tr>
<td>noise baselines</td>
<td>time trace</td>
<td>N/A</td>
<td>200-1336</td>
<td>8°</td>
</tr>
<tr>
<td>initial performance</td>
<td>single freq.</td>
<td>twist</td>
<td>800-1336</td>
<td>8°</td>
</tr>
<tr>
<td>bench testing</td>
<td>white noise</td>
<td>twist, bend</td>
<td>0</td>
<td>N/A</td>
</tr>
<tr>
<td>high voltage testing</td>
<td>single freq.</td>
<td>twist</td>
<td>0</td>
<td>N/A</td>
</tr>
<tr>
<td>DC Twist</td>
<td>time trace</td>
<td>twist</td>
<td>400-1336</td>
<td>8°</td>
</tr>
<tr>
<td>transfer functions</td>
<td>Fourier coeff.</td>
<td>twist</td>
<td>200-1336</td>
<td>8°</td>
</tr>
<tr>
<td>noise baselines</td>
<td>time trace</td>
<td>N/A</td>
<td>200-1336</td>
<td>0°</td>
</tr>
<tr>
<td>transfer functions</td>
<td>Fourier coeff.</td>
<td>twist</td>
<td>200-1336</td>
<td>0°</td>
</tr>
<tr>
<td>transfer functions-bending</td>
<td>Fourier coeff.</td>
<td>bend</td>
<td>1336</td>
<td>0°</td>
</tr>
<tr>
<td>current data</td>
<td>Fourier coeff.</td>
<td>+45° twist</td>
<td>0</td>
<td>N/A</td>
</tr>
<tr>
<td>noise baselines</td>
<td>time trace</td>
<td>N/A</td>
<td>200-1336</td>
<td>4°</td>
</tr>
<tr>
<td>transfer functions</td>
<td>Fourier coeff.</td>
<td>twist</td>
<td>200-1336</td>
<td>4°</td>
</tr>
<tr>
<td>bench testing</td>
<td>Fourier coeff.</td>
<td>twist, bend</td>
<td>0</td>
<td>N/A</td>
</tr>
</tbody>
</table>

performance at a different blade loading as well as under lower signal noise conditions. The nonuniform inflow of the hover test stand results in significant aerodynamic noise especially at higher collective pitch settings. The 4° pitch provides data at an intermediate point. Data were collected over a range of rotor speeds for the 0° and 8° collectives, but were collected only at 1336 RPM for the 4° setting.

Procedure

The integral blade is shown in position on the hover test stand in Figure 7.11. The first step in the general procedure for the hover testing of the integral blade is to select the desired collective pitch setting. The pitch is adjusted on both blades. Then the blade track is checked at 800 RPM. The passive blade pitch is adjusted to match the track of the integral blade. Then the track is again tested at higher speeds. The process repeats until the blades track within 1 blade thickness at the tip at 1336 RPM.
Data were collected for a number of rotor speed and collective pitch settings. For each condition, a baseline noise time trace was collected for reference. For each test, the rotor speed was ramped to the desired level and maintained with a 0.1% accuracy. The actuation testing was then completed.

During some of the transfer function data runs, which typically last approximately 90 minutes, one or more of the AFC packs may have failed. Testing is immediately stopped and the pack is disconnected at the hub. Then the testing is resumed. The data collection is repeated at the actuation frequency where the failure occurred. The frequency sweep is then completed. Thus a slight discontinuity may be present in the transfer function as a result of the pack failure. Pack failures which may have occurred between different data sets also will affect direct performance comparisons to some extent. Any changes in actuation authority will be noted in the presentation of the data. Section 8.1 will provide further details about the failures of the packs and the correlation with the various data sets.
Typical Loads

The first data presented are the baseline mean hub loads for each test condition. For each condition, the average value of the time trace data collected is referenced to the value at 0 RPM to determine the change in loads. The data are summarized in Figure 7.12. The primary loads are the vertical shear or thrust ($F_z$), and the $z$-axis moment ($M_z$) which corresponds to drag. The in-plane forces indicate imbalances in the rotor system. The $y$-axis is aligned with the blade span. The in-plane moments may indicate mass imbalances or uneven aerodynamic lift and moment forces between the two blades in hover and may be affected by the blade tracking adjustments at each collective setting.

The vertical hub shear, which is one of the primary performance metrics for the active blade, will be referenced in terms of the nondimensional coefficient of thrust, $C_T$,

$$C_T = F_z / (\rho A (\Omega R)^2) \quad (7.8)$$

or the blade loading, $C_T/\sigma$, where $\sigma$ represents the rotor solidity,

$$\sigma = (Nc)/(\pi R) \quad (7.9)$$

Table 7.3 provides the nominal values of the thrust for each collective pitch level tested. Note that the blade airfoil (VR7) in nonsymmetric.

7.3.2 Initial Single Frequency Testing

Following the bench testing of the integral blade, the integral blade was prepared for testing at 8° collective pitch on the hover test stand. Data were collected in the form of time traces from each of the sensors while a single frequency excitation voltage of 1920 Vpp was provided to the AFC packs. A Fourier analysis was then used to evaluate the response at the driving frequency referenced to the actuators-off condition. Four frequencies were tested: 10 Hz which represents a quasi-steady, low frequency, and then 1/rev, 2/rev, and 3/rev frequencies referenced to 1336 RPM (22.27 Hz). Data were collected at several rotor
Figure 7.12  Baseline loads as a function of rotor speed for three collective settings.
TABLE 7.3 Correlation Between Collective Pitch and Blade Loading.

<table>
<thead>
<tr>
<th>Collective</th>
<th>Thrust (lbs.)</th>
<th>( C )</th>
<th>( \mathcal{T} )</th>
<th>( C_T/\sigma )</th>
</tr>
</thead>
<tbody>
<tr>
<td>0°</td>
<td>18</td>
<td>0.00019</td>
<td>0.0033</td>
<td></td>
</tr>
<tr>
<td>4°</td>
<td>162</td>
<td>0.0017</td>
<td>0.0300</td>
<td></td>
</tr>
<tr>
<td>8°</td>
<td>375</td>
<td>0.0039</td>
<td>0.0693</td>
<td></td>
</tr>
</tbody>
</table>

speeds using the same four actuation frequencies. Static twist measurements were also taken in which a DC voltage was applied to the actuators in hover.

RPM Sweep

Data from the single frequency testing of the integral blade are presented as a function of rotor speed in Figure 7.13. The data were collected at 8° collective pitch at 800 RPM, 1000 RPM, 1200 RPM, and 1336 RPM. The twist actuation performance was evaluated using torsional strain measurements at 4 spanwise locations (0.416R, 0.513R, 0.804R, and 0.901R) and the vertical hub shear force. The rotor torque, \( M_z \), is also plotted. The torsional strain data provide an estimate of the twist rate distribution along the blade, while the load cell data provides a metric of total performance: the aerodynamic loads which the active twist creates.

The data illustrate several interesting results. First, the torsional strain measurements at the four blade stations are roughly equivalent, around 40 microstrain in amplitude. Secondly, the strain levels remain fairly constant with increasing rotor speed as expected. The amplitudes also remain fairly constant for each of the actuation frequencies although some resonant amplification is evident at the higher frequencies.

The measured hub loads show a strong increasing trend with rotor speed. The moment induced as a result of the change of lift in the active blade and constant lift in the passive blade, \( M_x \), also shows a similar trend. The peak hub loads range from 12 to 21 pounds at 1336 RPM. There is significant variation in the amplitude of the response at different actuation frequencies. Several factors contribute to this behavior. First, since the twist performance appears to be roughly constant, the induced hub loads should follow a qua-
dratic trend with rotor speeds. The aerodynamic loads are approximately proportional to the square of the flow velocity. Three other factors may be affecting the response. First, noise in the load signals may be affecting the data. Secondly, the system dynamics will vary the amplitude of the response depending on the rotor speed and actuation frequency. Thirdly, the effects of aerodynamic damping on the dynamic response of the blade will reduce the performance at higher rotor speeds.

Plotting the induced vertical hub load data in terms of the nondimensional coefficient of thrust removes the quadratic dependence of the data on the rotor speed. This data is presented in Figure 7.14. If the effects of system dynamics and damping were insignificant, the data in this plot would be roughly constant. All subsequent data have been collected using a swept sine technique. This will provide a great deal more information about the system dynamics and the behavior of the system in general. The effects of noise will also be reduced because of the Fourier coefficient analysis technique, described in Section 7.2.3, and because the trends in the response will be more clearly illustrated.

**Static Twist**

The static twist capability of the integral blade has been evaluated for an applied voltage of 1kV. Time trace data were collected at several rotor speeds. The average response with the voltage applied was calculated relative to the average response with actuators off for each sensor channel. The results are plotted in Figure 7.15 as a function of rotor speed.

The data show a fairly linear trend in twist response at each rotor speed measured at 0.8R and 0.9R with a slightly increasing slope. The twist measured at 1336 RPM is roughly 5% greater than measured at 400 RPM. The measured vertical hub load does not show the expected increase in thrust with increased rotor speed. Although the data was averaged over a significant number of rotor revolutions, the low signal to noise ratio may affect the data. The loads measured were an order of magnitude smaller than the induced loads measured at the 10 Hz actuation frequency. However, the induced moment, $M_x$, follows a quadratic trend with rotor speed. The magnitude of the static moment generated relative to
Figure 7.13  Single frequency data at 8 deg collective for vertical hub shear, moment, and torsional strain at 4 actuation frequencies.
the 10 Hz data has approximately the same ratio as the torsional strain measured in the static and 10 Hz cases.

7.3.3 Twist Mode Transfer Functions

The majority of the data collected on the integral blade were obtained using the swept sine technique with real time Fourier coefficient analysis. For each data point, data collection continued until a 90% confidence level was reached in 5% accuracy for each channel of data or a total of 5000 averages was reached. Fifty data points were collected across the spectrum from 10 Hz to 150 Hz. The time required to obtain a sufficient number of averages limited the lower bound while the data acquisition speed determined the upper limit. All data were collected with a 2kV peak to peak sinusoidal input to the actuators. Data were collected primarily in twist mode although some data were collected in the bending mode of operation. Twist mode data were collected over a range of rotor speeds and collective pitch settings.
Data from static twist actuation at 1kV DC as a function of the rotor speed.

The data sets are presented in the form of transfer function data from input voltage to the various sensors. The magnitude, phase, and confidence levels are plotted for each frequency tested. The units for the magnitude are presented relative to a 1kV amplitude input voltage. Since this is the actual input used during the testing, the magnitude also represents the true sensor amplitudes measured as well. The confidence levels plotted are relative to the accuracy stated in the y-axis label. For torsional strain signals, the accuracy is 1%. For the coefficient of thrust and flap bending strains, the accuracy is 10%. The
reduced accuracy used for the channels with weaker signal-to-noise ratios provided a more clear presentation of the confidence levels.

Twist mode transfer function data have been collected for a range of rotor speeds from 200 RPM to 1336 RPM including each multiple of 200 RPM in between. Data were also collected at three different collective pitch settings. Data were first collected at 8° collective for each rotor speed. Then, the collective pitch was adjust to 0° and data were again collected at each rotor speed. Finally, data were collected at 4° collective pitch for 1336 RPM only.

The first data sets presented are the transfer functions from voltage to coefficient of thrust, flap bending strain at 0.39R, torsional strain at 0.8R, and torsional strain at 0.9R. Failures of the other strain gage bridges precluded strain measurements at other blade stations. The data sets are presented individually for 1336 RPM at each collective pitch. Additional data sets will be presented in overlay plots in subsequent subsections.

Figure 7.16 through Figure 7.26 show the collected transfer function data at 1336 RPM. In Figure 7.16, the transfer function for the coefficient of thrust is plotted for 0° collective. The magnitude is plotted as the actuation-induced change in the coefficient of thrust per kilovolt of excitation voltage. This first transfer function exhibits a number of characteristics that will be common to the all of the data sets for the collective thrust. First, the variations in the magnitude of the response vary greatly, over an order of magnitude, across the testing bandwidth. It is clear that the system dynamics are strongly affecting the response. A system pole is visible near 50 Hz (2.25/rev). This is the second flap bending mode (first elastic) of the system. Several other less pronounced poles are also evident. The poles also correlate with the sharp drops in the phase (for example near 80 Hz and 135 Hz). An apparent zero is visible near 45 Hz, or approximately 2/rev. Data sets for other sensors and for other rotor speeds will help to clarify the system dynamics.

Another typical feature of the coefficient of thrust transfer function is that the magnitude at 10 Hz is approximately $1 \times 10^{-4}$. The magnitude increases to $2 \times 10^{-4}$ toward the pole near
Figure 7.16 Transfer function from voltage to coefficient of thrust at 0° collective and 1336 RPM.

Figure 7.17 Transfer function from voltage to flap bending strain at 0.39R, 0° collective, and 1336 RPM.

50 Hz and then gradually decreases to a minimum of $1 \times 10^{-5}$ near 130 Hz. As the magnitude drops, the confidence level in the data also decreases.

Figure 7.17 presents the transfer function at 0° collective for the flap bending strain measured at 0.39R. This data set shows similar behavior to the coefficient of thrust data. The system poles are more clearly defined in the magnitude plot.
The torsional strain response of the blade shows significantly different characteristics. The torsional strain, shown in Figure 7.18 and Figure 7.19, has a typical second order response in the bandwidth plotted. The strain at both blade stations, 0.8R and 0.9R, is constant from 10 Hz to about 100 Hz, and then rolls off. A 90° phase change accompanies the change in magnitude. The magnitude at 0.8R is approximately 20 microstrain/kV while the magnitude at 0.9R is 30 microstrain/kV. The apparent pole in the transfer func-
Figure 7.20  Transfer function from voltage to coefficient of thrust at 4° collective and 1336 RPM.

Figure 7.21  Transfer function from voltage to flap bending strain at 0.39R, 4° collective, and 1336 RPM.

tion is slightly above 100 Hz and is critically damped. This is the first torsional mode of the system. Note that the confidence levels for the torsional strain measurements are nearly 100% with an accuracy of 1%.

The next data sets presented are for 1336 RPM at 4° collective pitch. Figure 7.20 displays the transfer function for the coefficient of thrust. The features of this transfer function are
Figure 7.22 Transfer function from voltage to torsional strain at 0.8R, 4° collective, and 1336 RPM. Figure 7.23 Transfer function from voltage to torsional strain at 0.9R, 4° collective, and 1336 RPM.

similar to those of the 0° collective transfer function previously described. One important difference is that the noise levels have increased due to the increased inflow. As a result, the confidence levels have decreased above 1/rev. The system zero near 2/rev is visible but less pronounced. The second bending mode remains clear. The overall magnitude of the response is also similar. Trends in the strain bending and torsional strain measurements
plotted in Figure 7.21, Figure 7.22, and Figure 7.23 also so similar behavior to the $0^\circ$ data sets.

The next data sets presented were collected at $8^\circ$ collective pitch and 1336 RPM. At this test condition, the aerodynamic noise is more severe. The confidence levels in the thrust data are further reduced above 1/rev. However, the general trends and system dynamics are still visible as shown in Figure 7.24. No flap bending strain data were collected at this
Figure 7.25 Transfer function from voltage to torsional strain at 0.8R, 8° collective, and 1336 RPM.

Figure 7.26 Transfer function from voltage to torsional strain at 0.9R, 8° collective, and 1336 RPM.

test condition. The torsional strain data are presented in Figure 7.25 and Figure 7.26. These data sets also show the same trends. The comparisons between the data collected at the three collective pitch setting will be clarified in overlay plots presented later in this section.
**RPM Sweep**

The next data sets presented are the rotor speed sweeps at 0° and 8° collective. Varying the rotor speed provides insight into the system dynamics, especially with regard to the frequency shifts in the modes that occur. This is often referred to as centrifugal stiffening. In addition, it will be shown that the reduced dynamic pressures and reduced inflow will provide a clearer picture of the transfer functions. When compared with transfer functions collected at lower speeds, the characteristics of the transfer function data collected at 1336 RPM will also be clarified. The modal information obtained from the rotor speed sweeps will be summarized in Section 7.3.5.

The rotor speed sweep data sets are presented in two forms. The first is a direct overlay plot in which the transfer function collected at each rotor speed is coded by color and line type. These data sets are presented in Figure 7.27 through Figure 7.34. The second form is the waterfall plot. This is a three-dimensional presentation of the data with rotor speed as an axis on the plots. The waterfall plots, presented in Figure 7.36 through Figure 7.42, better illustrate the shifting of the modes with rotor speed. The direct overlay plots are more useful for detailed comparisons between the transfer functions.

The first overlay plot shown in Figure 7.27 displays the coefficient of thrust data for each rotor speed at 0° collective pitch. Since the coefficient of thrust nondimensionalizes the vertical hub shear by the dynamic pressure, the magnitude of the transfer functions should be roughly equivalent at the various rotor speeds. However, as shown in the figure, the second, third, and fourth bending modes are clearly visible in the data. The peaks in amplitude are approximately an order of magnitude greater at 200 RPM than at 1336 RPM. This is most likely the result of aerodynamic damping. The shifting of the peaks is also visible with increasing rotor speeds. The first bending mode, which is a rigid flapping near 1/rev, is not evident. The first torsion mode also does not stand out. Between the collection of the transfer function at 1336 RPM, which was collected first, and the transfer function at 200 RPM, which was collected last, 4 of the AFC packs were disconnected due to failures. The effect of the reduced actuation is not clearly evident in the data sets.
The next data plot, shown in Figure 7.28, displays the transfer functions from voltage to flap bending strain for each rotor speed. Once again, the second, third, and fourth bending modes are clearly visible, especially at the lower rotor speeds. The shifts in the phase corresponding with the poles are also evident. The second and fourth bending modes are more observable to the strain gage bridge, located at 0.39R.

Figure 7.29 shows the transfer functions for the torsional strain at 0.8R. Two important trends are evident. First, the response at frequencies below the first torsional resonance is fairly constant with rotor speed. The response is actually greatest at 1336 RPM. This is due to the fact that an actuator failure reduced the local twist rate near the strain gage after the data set at 1336 RPM was collected. The second characteristic of interest is that the torsional resonance clearly shows the effect of aerodynamic damping at higher rotor speeds. The damping makes it difficult to estimate the exact location of the pole although it appears to remain in the 105 to 115 Hz range. Similar trends are evident in the strain data from 0.9R in Figure 7.30 although less pronounced. The resonant amplification is also reduced at this blade station.

Figure 7.31 shows the thrust coefficient transfer functions collected at 8° collective. This data differs slightly from the data for 0° shown in Figure 7.27. First, the second, third, and fourth bending modes are more clearly defined at 8° collective. Since the background noise levels on the data signals are much higher at 8°, the clear modes are contrary to expectations. Another factor which may contribute to this effect is that 4 more packs were operational during the 8° collective data collection than during the 0° data collection which would correlate with greater twist.

The flap bending strain plot, shown in Figure 7.32, displays only two transfer functions collected at 200 RPM and 600 RPM. In Figure 7.33, the torsional strain data for 0.8R collected at 8° pitch shows a significant change in the performance between the 1000 RPM and 800 RPM data sets. This resulted from the failure of an additional pack near the strain gage bridge. As with the 0° collective data, the effects of the aerodynamic damping on the
torsional resonance is clearly defined. The data at 0.9R shown in Figure 7.34 displays a consistent behavior across the rotor speed sweep and between the two collectives.

The same data can also be presented using the rotor speed to nondimensionalize the actuation frequency. The data for the coefficient of thrust at 8° collective is presented again in Figure 7.35. In this form, it is evident that the first resonant mode occurs near 1/rev and remains constant with rotor speed. This is indicative of the first flapping mode of the rotor system.

Next, the same data sets are plotted in waterfall form. The discussion of these plots will be limited since most of the same attributes were already discussed with reference to the overlay plots. Note that the magnitude data are plotted in decibels in order to maintain a logarithmic scaling. Refer to the overlay plots for a more detailed display of the magnitudes.

Figure 7.36 through Figure 7.39 show the waterfall plots for the 0° collective data sets. The data for the coefficient of thrust in Figure 7.36 clearly shows the zero near 2/rev and the second, third, and fourth bending mode peaks as a function of rotor speed. The magnitude of the resonances is significantly increased for lower rotor speeds. The phase data also shows the effect of the zero and pole (2nd bending). The trends are less clear for the higher modes where the confidence levels are reduced.

The flap bending strain data plotted in Figure 7.37 illustrates the second and fourth bending modes. Note that in contrast with the thrust data, the amplitude of the second bending mode resonant peak remains relatively constant with changes in rotor speed. The torsional strain data in Figure 7.38 and Figure 7.39 clearly display the torsional resonance and the changes in damping. The changes in the magnitude at the lower frequencies due to the reduced actuator authority are also visible at 0.8R.

Figure 7.40 shows the thrust data for the 8° collective pitch. The bending modes are clearly defined in both magnitude and phase plots. The zero which was evident in the 0°
Figure 7.27  Overlay plot of RPM sweep of transfer functions from voltage to coefficient of thrust at $0^\circ$ collective.
Figure 7.28  Overlay plot of RPM sweep of transfer functions from voltage to flap bending strain at 0.39R and 0° collective.
Figure 7.29  Overlay plot of RPM sweep of transfer functions from voltage to torsional strain at 0.8R and 0° collective.
Figure 7.30  Overlay plot of RPM sweep of transfer functions from voltage to torsional strain at 0.9R and 0° collective.
Figure 7.31  Overlay plot of RPM sweep of transfer functions from voltage to coefficient of thrust at 8° collective.
Figure 7.32 Overlay plot of RPM sweep of transfer functions from voltage to flap bending strain at 0.39R and 8° collective.
Figure 7.33  Overlay plot of RPM sweep of transfer functions from voltage to torsional strain at 0.8R and 8° collective.
Figure 7.34  Overlay plot of RPM sweep of transfer functions from voltage to torsional strain at 0.9R and 8° collective.
Figure 7.35 Overlay plot of RPM sweep of transfer functions from voltage to coefficient of thrust at 8° collective as a function of nondimensional frequency.

Collective data is not visible in this plot. The damping effect with increased rotor speed is most significant in the higher bending modes.

The flap bending strain data for 8° collective is not plotted in waterfall form since only two data sets were obtained. The torsional strain data presented in Figure 7.41 and Figure 7.42 follow similar trends as the 0° collective data.
Figure 7.36  Waterfall plots of RPM sweep in transfer functions from voltage to coefficient of thrust at 0° collective.
Figure 7.37  Waterfall plots of RPM sweep in transfer functions from voltage to flap bending strain at 0.39R at 0° collective.
Figure 7.38 Waterfall plots of RPM sweep in transfer functions from voltage to torsional strain at 0° collective.
Figure 7.39  Waterfall plots of RPM sweep in transfer functions from voltage to torsional strain at 0° at 0.9R collective.
Figure 7.40  Waterfall plots of RPM sweep in transfer functions from voltage to coefficient of thrust at 8° collective.
Figure 7.41  Waterfall plots of RPM sweep in transfer functions from voltage to torsional strain at 0.8R at 8° collective.
Figure 7.42 Waterfall plots of RPM sweep in transfer functions from voltage to torsional strain at 0.9R at 8° collective.
Collective Sweep

Next, the data collected at $0^\circ$, $4^\circ$, and $8^\circ$ collective pitch will be compared directly in overlay plots. The primary comparisons will be made at 1336 RPM. Data also will be presented for 1200 RPM, 800 RPM, and 200 RPM for comparisons under different conditions. The order in which the data sets were collected is significant in the correlation at each collective pitch since the actuator authority decreased slightly during the testing. The $8^\circ$ collective data was obtained first and was followed by the $0^\circ$ and finally the $4^\circ$ data sets. A total of 4 AFC packs were disconnected between the $8^\circ$ and the $0^\circ$ data sets. An additional 4 packs were lost between the $0^\circ$ and the $4^\circ$ data sets.

The first data sets presented are the coefficient of thrust transfer functions for each collective pitch, shown in Figure 7.43. The transfer functions follow the same general trends with a few notable variations. First, the magnitude at the lower actuation frequencies varies significantly at the different collectives. This change does not correlate with increased blade loading. The amplitude is greatest at $8^\circ$ and is the least at $4^\circ$. This correlates with the changes in actuation authority. A second important feature is that the confidence levels are significantly greater at $0^\circ$ collective, where the inflow is minimal. The background noise levels at the higher collectives obscure the zero visible near 2/rev. The phase data clearly shows the poles and zeros of the transfer function at $0^\circ$ collective.

The flap bending strain transfer functions shown in Figure 7.44 show similar trends at $0^\circ$ and $4^\circ$ collective pitch. The reduced amplitude can most likely be attributed to the reduced actuation authority at $4^\circ$. The changes in actuation authority are clearly illustrated in the torsional strain data at 0.8R, as shown in Figure 7.45. As with the thrust data, the best performance was observed at $8^\circ$ collective. The explanation for the strong effect of the actuation authority changes on this sensor is that a pack failed between each of the data sets at the location of the strain gage, which was centered on a group of packs near 0.8R. The data collected at 0.9R do not show a significant change in twist performance with blade loading as shown in Figure 7.46.
Figure 7.43 Transfer functions from voltage to coefficient of thrust at 1336 RPM for 0°, 4°, and 8° collective.
Figure 7.44  Transfer functions from voltage to flap bending strain at 0.39R and 1336 RPM for 0° and 4° collective.
Figure 7.45 Transfer functions from voltage to torsional strain at 0.8R and 1336 RPM for 0°, 4°, and 8° collective.
Figure 7.46  Transfer functions from voltage to torsional strain at 0.9R and 1336 RPM for 0°, 4°, and 8° collective.
Transfer functions from voltage to coefficient of thrust at 1200 RPM for $0^\circ$ and $8^\circ$ collective.

Transfer functions from voltage to torsional strain at 0.8R and 1200 RPM for $0^\circ$ and $8^\circ$ collective.

Transfer functions at $0^\circ$ and $8^\circ$ collective are presented for the coefficient of thrust, torsional strain at 0.8R, and torsional strain at 0.9R at 1200 RPM in Figure 7.47, Figure 7.48, and Figure 7.49, respectively. The trends in the data are similar to those presented for 1336 RPM. At 800 RPM, the peaks in the thrust coefficient transfer function become more clearly defined as shown in Figure 7.50. The torsional resonance is also visible in the torsional strain data presented in Figure 7.51 and Figure 7.52.
The final collective pitch comparisons are presented at a rotor speed of 200 RPM. At this speed, the aerodynamic damping effects are greatly reduced and the peaks in the transfer functions are much more sharp. Figure 7.53 shows the data for the thrust coefficient which shows very little difference at 0° and 8° collective. The bending mode peaks are also clearly and consistently defined in the bending strain data plotted in Figure 7.54. The torsional resonance is shown in Figure 7.55 and Figure 7.56.
Figure 7.51 Transfer functions from voltage to torsional strain at 0.8R and 800 RPM for 0° and 8° collective.

Figure 7.52 Transfer functions from voltage to torsional strain at 0.9R and 800 RPM for 0° and 8° collective.
**Figure 7.53** Transfer functions from voltage to coefficient of thrust at 200 RPM for $0^\circ$ and $8^\circ$ collective.

**Figure 7.54** Transfer functions from voltage to flap bending strain at 0.39R and 200 RPM for $0^\circ$ and $8^\circ$ collective.
Figure 7.55  Transfer functions from voltage to torsional strain at 0.8R and 200 RPM for 0° and 8° collective.

Figure 7.56  Transfer functions from voltage to torsional strain at 0.9R and 200 RPM for 0° and 8° collective.
7.3.4 Bending Mode Transfer Functions

Transfer function data were also collected for the integral blade operating in bending mode. In this mode, the packs on the upper spar surface are driven out of phase with those on the lower surface in order to create a flapwise bending moment. As with the twist mode data, transfer function data were collected from the input voltage to the coefficient of thrust and strain sensors.

Data for the coefficient of thrust are shown in Figure 7.57 at 1336 RPM and 0° collective. Note that the vertical axis scale has changed from that used for the twist mode data plots. The magnitude of the response is two orders of magnitude lower in bending mode than in twist mode away from the resonances. The response near 10 Hz actuation is approximately $1 \times 10^{-6}$ change in the coefficient of thrust per kilovolt. The response is much stronger near the elastic bending modes. The first torsional mode is also visible near 115 Hz. The response is similar for the flap bending data shown in Figure 7.58. Here the third bending mode is less apparent and the overall magnitude is significantly reduced in comparison with the twist mode case. For both data sets, the confidence level in the data is rather poor across the entire bandwidth. The torsional strain response for the bending mode operation is presented in Figure 7.59 and Figure 7.60 for completeness. The response has a small magnitude and is flat at both 0.8R and 0.9R.

The performance of the blade in bending mode is directly compared to the twist mode data in Figure 7.61. Both data sets were collected at 0° pitch and 1336 RPM. For the coefficient of thrust, the transfer functions overlap only at the peak of the third bending mode. Otherwise, it is clear that the bending mode response is much smaller. The flap bending strain data follows similar trends across the bandwidth but is also significantly reduced in the bending mode of operation.
Figure 7.57  Transfer function from voltage to coefficient of thrust in bending mode operation.

Figure 7.58  Transfer function from voltage to flap bending strain at 0.39R in bending mode operation.
Figure 7.59  Transfer function from voltage to torsional strain at 0.8R in bending mode operation.

Figure 7.60  Transfer function from voltage to torsional strain at 0.9R in bending mode operation.
Figure 7.61  Comparison of coefficient of thrust and flap bending strain at 0.39R data at 0° collective and 1336 RPM for twist mode and bending mode operation.
7.3.5 System Identification

A blade with integrated actuators and sufficient bandwidth offers a useful means of exciting the various modes of the system for modal identification purposes. In this case, the integral blade was used in twist mode to excite the modes of the system in the 10 Hz to 150 Hz bandwidth. The modes can be observed at any rotor speed. The rotating frame load cell and the blade mounted strain gages were used as sensors. The vertical hub shear and a flap bending strain gage at 0.39R provided the majority of the data on the bending modes of the system. The torsional strain gages were used to observe the first torsional mode of the system. The location of the poles were obtained from the transfer function data presented in the previous sections. The torsional mode was difficult to precisely locate in many cases because of the high damping which eliminated the peak.

Boeing’s TECH-01 rotor model was also used to predict the in vacuo system modes as a function of rotor speed. The model used the baseline design properties of the integral blade as described in Chapter 4 with the stiffness and inertial properties scaled to match the experimentally measured values described in Chapter 6. The data from the transfer function data collected at each rotor speed are plotted along with the model predictions and the N/rev lines in Figure 7.62. This is referred to as a Campbell diagram or fan plot for the rotor system.

The predicted modes at 1336 RPM are compared with data collected at 8° pitch in Table 7.4. The first predicted mode is the lead-lag mode near 0.5/rev. No data were collected for this mode for comparison. Next is the rigid flapping mode which is slightly greater than 1/rev for an articulated rotor. The natural frequencies of the flapwise bending modes were below model predictions. The first torsional mode was slightly below model predictions. Note that the predictions were for in vacuo modes and the actual data were collected in air. Since the resolution of the transfer function data used to find the peaks was roughly 3 Hz, the estimates of the peak locations may vary by ±0.15/rev. In addition, noise in the transfer function data made it difficult to clearly identify the peaks for some of the data points.
Figure 7.62  Campbell diagram for rotor system showing experimentally located modes at 0° and 8° collective for comparison with TECH-01 model predictions.
TABLE 7.4  Comparison of Measured and Predicted Rotor System Modes at 1336 RPM.

<table>
<thead>
<tr>
<th>Mode</th>
<th>Predicted</th>
<th>Measured</th>
</tr>
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<td>1st flap</td>
<td>1.02/rev</td>
<td>1.09/rev</td>
</tr>
<tr>
<td>2nd flap</td>
<td>2.59</td>
<td>2.36</td>
</tr>
<tr>
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<td>4.71</td>
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</tr>
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<td>5.10</td>
</tr>
<tr>
<td>4th flap</td>
<td>7.76</td>
<td>6.07</td>
</tr>
</tbody>
</table>

Additional data on the damping in the system was obtained from the peak amplitudes measured from the transfer function data. The coefficient of thrust peak amplitudes for the bending modes were compared along with the torsional strain peak for the first torsional mode. The peak amplitudes are plotted in Figure 7.63 relative to the amplitude for each mode at 1336 RPM.

![Graph](image)

**Figure 7.63**  Relative resonant amplification of the rotor system modes as a function of rotor speed.
Since the coefficient of thrust data used for the bending mode amplitudes is nondimensionalized by the rotor speed, the peak amplitudes reflect the damping present. The plot shows the strong effect of the rotor speed on the amplitude for the fourth bending mode (B4). During the 8° collective pitch testing, the amplitude of the peak decreased by over two orders of magnitude between 200 RPM and 1336 RPM. The third bending mode demonstrated a factor of two change in magnitude at 8° collective but almost no change at 0°. The torsional mode also showed a factor of two change in magnitude between 200 RPM and 1336 RPM. The first and second bending modes showed virtually no change in amplitude with rotor speed.

7.3.6 Error Bounds

This section is intended to better characterize the confidence levels in the transfer function data presented as well as the baseline noise present in the rotor system. First, the transfer function data collected at each collective pitch level for the coefficient of thrust will be presented along with confidence levels. Then the background noise spectrums for each of the major sensor channels will be presented for each collective pitch. The causes of the noise in the system will also be discussed.

Confidence Intervals

The transfer function data plotted in Section 7.3.3 included a confidence plot corresponding with the magnitude and phase data. The confidence level represented the percent confidence that each data point was within 10% accuracy for the coefficient of thrust and flap bending strains and 1% accuracy for the torsional strain. Another way to view the confidence is to set a confidence interval and then estimate the accuracy. This will result in a confidence interval plot for the magnitude of the transfer function. An upper and lower bound are plotted along with the data to illustrate the possible range of error in the data at the prescribed confidence level.

Figure 7.64 shows the transfer function from voltage to the coefficient of thrust at 0° collective and 1336 RPM. The 90% confidence interval is also plotted. The bounds on the
data are fairly tight except where the magnitude drops into the $1\times10^{-5}$ range. The next data set is for the $4^\circ$ collective case, shown in Figure 7.65. In this case, the confidence bounds are much wider for frequencies above $1/\text{rev}$. Some of the features of the transfer function such as smaller amplitude poles are contained within the bounds which suggests a confidence of less than 90% in their identification. Above $4/\text{rev}$, where the magnitude of the response is decreased, the confidence bounds extend further. The lower bound was off the scale of the plot and is not shown in the figure. At $8^\circ$ collective, the noise levels are greater, and the 90% confidence intervals widen further. This is shown in Figure 7.66. In contrast, a sample of the torsional strain data is plotted in Figure 7.67 with 99.9% confidence bounds. The bounds remain extremely close to the data across the bandwidth with only a slight increase at the higher frequencies.

![Figure 7.64](image1)
![Figure 7.65](image2)

**Figure 7.64** Transfer function from voltage to coefficient of thrust including 90% confidence intervals at $0^\circ$ collective, 1336 RPM.

**Figure 7.65** Transfer function from voltage to coefficient of thrust including 90% confidence intervals at $4^\circ$ collective, 1336 RPM.

Even though the poor confidence levels in the individual data sets illustrate a level of uncertainty in the trends in the data, the overall confidence in the ensemble of transfer function data collected is adequate for drawing conclusions about the performance of the integral blade. Data comparisons for the rotor speed and collective sweeps provide data
sets with suitable confidence (at lower speeds and collectives). The trends in these data sets are clearly visible and are similar to those found in the noisier data sets. The noise will be discussed further in the next section.

**Background Noise**

The background noise levels for the coefficient of thrust and strain gage signals were evaluated using a Fourier analysis. A 30 second time trace of data was collected at a 1 kHz sampling rate at 1336 RPM for each collective pitch level. The resulting noise spectrums are plotted for each sensor as a function of the nondimensionalized rotor speed. Figure 7.68 shows the noise spectrum for the coefficient of thrust and torsional strains at 0.8R and 0.9R for 0° collective. The load cell data clearly shows noise peaks at the harmonics of the rotor speed. The largest peaks are visible at 2/rev and 4/rev. The peaks are fairly broad and have a maximum amplitude of approximately $2 \times 10^{-4}$, which is approximately twice the amplitude of the nominal twist-actuated thrust measured. The noise floor is approximately $1 \times 10^{-6}$.
For the torsional strains, noise peaks are also present at the rotor speed harmonics, though they are less pronounced. The peak at 2.69/rev is 60Hz signal noise. The highest peak occurs at 3/rev and is on the order of 1-2 microstrain which is roughly an order of magnitude less than the nominal twist-actuated strain levels. The noise floor is roughly three orders of magnitude less than the signal.

Figure 7.69 presents similar data for the 4° collective pitch case. For each sensor, the noise floor and peak levels are increased. For the coefficient of thrust, the 2/rev peak is increased to 1x10⁻³ and the noise floor near the peaks is roughly 1x10⁻⁵. The noise levels on the strain gage channels is roughly double at the 4° pitch level.

The worst case noise was measured at the 8° collective pitch setting. The data are plotted in Figure 7.70 for this case. The 2/rev peak in the coefficient of thrust is now greater than 2x10⁻³. The peaks at 2/rev and 4/rev are widened and approaching the 1x10⁻⁴ level. The corresponding flap bending strain data is also presented for 8° collective. The 2/rev peak has a magnitude of 200 microstrain with additional peaks on the order of 10 microstrain at the other harmonics. The peaks in the torsional strain data are approaching 10 microstrain at 2/rev with several other harmonics near 1 microstrain.

In general, the signal to noise ratio for the torsional strain data is sufficient to enable data collection with high accuracy and confidence. In contrast, the noise levels on the load cell and flap bending strain gage exceed the primary signal from the twist actuation. The result is the poor confidence indicated in the transfer function data presented.

The primary cause of the noise has been attributed to nonuniform inflow into the rotor disk. Smoke tests were used to visualize the flow in the testing room (refer to Section 7.2.2). A large scale variation in the inflow was identified in which the flow recirculation is quite strong in two opposite quadrants of the rotor disk, but weak in the others. The recirculation correlates with the proximity of the walls of the room, which are approximately 14’ from the center of rotation near the quadrants with strong recirculation. In these quadrants, the flow enters the rotor vertically and near the center of the rotor. In the
**Figure 7.68** Background noise spectra for thrust coefficient and torsional strains at 0.8R and 0.9R at 0° collective and 1336 RPM.

**Figure 7.69** Background noise spectra for thrust coefficient and torsional strains at 0.8R and 0.9R at 4° collective and 1336 RPM.
other quadrants, the walls are approximately 30° from the center and the flow enters the rotor disk near the blade tips with more of a horizontal velocity.

Other factors disturb the flow on a smaller scale. A column on either end of the test area and pipes hanging from the ceiling add turbulence to the flow. The containment ring surrounding the rotor at a 7' radius also affects the flow. Turbulence is created as the flow passes over the upper edge of the ring.
Modifications to the testing environment are in progress to improve the flow. The improvements include a bellmouth formed on the inside of the containment ring to create the effect of a ducted fan. A mesh will also be placed across the top of the ring to create a pressure drop and thus reduce the turbulence entering the rotor disk.

7.4 Analysis of Results

The objectives of this section are to analyze the twist actuation performance of the integral blade in hover in comparison with model predictions and with other conceptually similar designs. Model predictions from Boeing’s TECH-01 rotor model and from a simple steady aerodynamic calculation will be compared with the measured induced hub loads in hover. The performance will also be compared with data from a twist-actuated rotor tested at the University of Maryland.

7.4.1 Model Predictions

The integral blade induced approximately 12 lbs. of thrust in hover testing at 1336 RPM, with 8° collective pitch during the initial single frequency testing. This was for an applied voltage of 1920 Vpp at 10 Hz. This is equivalent to a change in the coefficient of thrust of 1.3x10⁻⁴. From the transfer function data collected it is evident that induced thrust response in this frequency range is fairly constant and does not appear to be amplified by resonances. During the hover testing, no direct measure of the tip twist was available. Torsional strain measurements at several blade stations during the tests can be used to estimate the tip twist. Nonrotating data collected using laser displacement sensors was correlated with data collected simultaneously from the strain gages. This data was reported in the bench testing discussion of Section 6.3.1. The measured torsional strain was found to be consistent at all rotor speeds.

Using the estimated correlation of 0.0100 degrees/microstrain, the tip twist was estimated from data at 1336 RPM and 8° collective. For the single frequency tests, the estimated tip twist was 0.4° amplitude at 10 Hz. The transfer function data, which was collected after
several packs had failed, was also used to estimate the tip twist transfer function. This estimate is plotted in Figure 7.71.

![Figure 7.71](image)

Figure 7.71 Estimated transfer function from voltage to tip twist at 8° collective based on measured nonrotating correlation with torsional strain at 0.8R.

A simple aerodynamic model was first used to obtain a rough estimate of the induced thrust for a 0.4° tip twist at 1336 RPM and 8° collective pitch. Using the steady aerodynamic calculation,

\[ T = qcbC_{L\alpha} \alpha \]  \hspace{1cm} (7.10)

where \( q \) is the dynamic pressure, \( b \) is the span, \( C_{L\alpha} \) is the lift curve slope, and \( \alpha \) is the angle of attack, the change in lift or thrust was estimated. A linear twist was assumed between 0.27R and 0.95R. Four additional parameters were included in the calculation [Johnson, 1980]. An estimate of 5.7 was used for the lift curve slope. Blade element theory was used to include a uniform inflow effect (\( \lambda = 0.045 \)) for a rotor in hover which changed the effective radial velocity distribution slightly. A ground effect factor was also included using an estimate based on the method of images (\( T/T_\infty = 1.029 \)). A blade tip loss factor
was also incorporated (B=0.97). The resulting thrust prediction is 11.4 lbs. This is 10% less than the measured thrust of 12.6 lbs.

The change in thrust was also predicted using Boeing’s TECH-01 rotor system model [Derham, 1996b]. A linear twist was also assumed between 0.27R and 0.95R. The simplest uniform downwash wake model was used in this prediction, and the rotor was assumed to be out of ground effect. The model predicted 14 lbs of lift per degree of twist or 5.6 lbs. for 0.4°. The measured thrust was more than twice that level.

### 7.4.2 Comparison with Other Twist-Actuated Rotors

Other researchers have pursued and are currently pursuing twist-actuated rotors for IBC. One such rotor has already been tested at the University of Maryland [Chen, 1996; Chen, 1997b]. The blade incorporates narrow piezoceramic wafers embedded beneath the composite structure of the blade at ±45° to the longitudinal axis. Of four configurations tested, the dual-layer B (DLB) design produced the greatest results and will be referenced in the comparison. This blade incorporated 30 pairs of 2 inch by 0.25 inch by 11 mil actuators. The blade model was a 1/8th Froude scale NASA/Boeing ITR blade with a 6 foot rotor diameter. The blade was tested both in hover and in forward flight to evaluate the performance of the twist actuation. Table 7.5 compares the properties and performance of the two blade types.

The comparison is made for fairly low frequency actuation to avoid system resonances and at the maximum speed and maximum blade loading tested. The primary performance metric for the blades is the induced change in the nondimensional coefficient of thrust. For the ITR blade, two active blades were tested. Thus the referenced ΔC_T/blade is 50% of the measured value. For the CH-47D tests, only one active blade was tested. The ITR demonstrated a change of 3.5x10^{-5} while the integral blade demonstrated 1.3x10^{-4}. The two rotor systems compared are quite different. The CH-47D system is much stiffer and was designed to withstand the larger Mach-scale loads in hover. Thus the actual torque
TABLE 7.5  Comparison of Hover Test Results for MIT Integral Blade and U. Maryland Active Twist Rotor.

<table>
<thead>
<tr>
<th>Property</th>
<th>UM ary land DLB</th>
<th>MIT Integral</th>
</tr>
</thead>
<tbody>
<tr>
<td>model</td>
<td>1/8th Froude</td>
<td>1/6th Mach</td>
</tr>
<tr>
<td></td>
<td>Boeing ITR</td>
<td>CH-47D</td>
</tr>
<tr>
<td>rotor type</td>
<td>bearingless</td>
<td>articulated</td>
</tr>
<tr>
<td>rotor speed</td>
<td>900 RPM</td>
<td>1336 RPM</td>
</tr>
<tr>
<td>radius</td>
<td>36 in.</td>
<td>60.6 in.</td>
</tr>
<tr>
<td>number of blades</td>
<td>2</td>
<td>2 (1 active)</td>
</tr>
<tr>
<td>(as tested</td>
<td></td>
<td></td>
</tr>
<tr>
<td>chord</td>
<td>3 in.</td>
<td>5.388 in.</td>
</tr>
<tr>
<td>active span</td>
<td>11.5 in.</td>
<td>41.2 in.</td>
</tr>
<tr>
<td>torsional stiffness</td>
<td>2400 lb-in²</td>
<td>55800 lb-in²</td>
</tr>
<tr>
<td>torsional freq.</td>
<td>4.4/rev</td>
<td>5.1/rev</td>
</tr>
<tr>
<td>max tip twist&lt;sup&gt;a&lt;/sup&gt;</td>
<td>9.25°</td>
<td>0.40°</td>
</tr>
<tr>
<td>ΔC&lt;sub&gt;T&lt;/sub&gt;/blade</td>
<td>3.5x10⁻⁵</td>
<td>1.3x10⁻⁴</td>
</tr>
<tr>
<td>torque (estimated)</td>
<td>0.92 in-lb.</td>
<td>9.4 in-lb.</td>
</tr>
</tbody>
</table>

a. amplitude below resonance, full speed, 8° collective

required to achieve the performance shown was an order of magnitude greater in the CH-47D blade.

Several other important differences were illuminated in this comparison. First, the tests at Maryland found that the twist actuation capability was significantly reduced (~23%) as rotor speed was increased. The twist was reduced further at higher blade loadings (~17% from 4° to 8° collective), although the induced change in the coefficient of thrust was not affected as significantly. Neither trend was observed in the hover testing of the CH-47D integral blade. The twist actuation performance was nearly constant for different rotor speeds and blade loading. This may be due to the significantly greater stiffness and actuator authority of the integral blade. The ITR blade also showed a factor of three amplification of the twist response at resonance, while the integral blade response was critically damped. The aerodynamic damping effects are dependent upon the flow velocity which is significantly greater (2.5 times) for the integral blade. The torsional resonance apparent in
the blade structural response for both rotor systems was also evident in the induced thrust response of the ITR blade, but not in the response of the integral blade. When tested at similar tip speeds, the twist response of the integral blade did show resonant amplification at the first torsional mode.
Chapter 8

ACTUATOR FAILURE ANALYSIS

During the twist actuation testing of the integral blade, electrical failures rendered a large number of AFC packs inoperable. As a result, the data was collected at a reduced voltage and with reduced actuation authority. This chapter will detail the failures of the packs including the chronology, conditions of the failure, and the correlation of the failures with several characteristics of the packs and the blade geometry. In particular, the correlation with manufacturing flaws discovered in the spar structure of the blade will be investigated through analysis of blade cross sections. The possible and probable causes of the defects will also be addressed.

8.1 Pack Failure History

This section will summarize the failures of the AFC packs which occurred during testing including a chronology and details about the type of failure. The information provided can be used to evaluate the actuation capability of the integral blade during each of the tests performed as described in Chapter 7. In addition, the failure data will be used to determine the causality of the failures later in this chapter.

8.1.1 Chronology and Correlation with Hover Data

The pack failures occurred during twist or bend actuation testing resulting in a gradual reduction in the actuation capability of the blade. The failures typically result in a short
circuit condition during the actuation testing. A dielectric breakdown or arcing failure is the typical cause. Capacitance and resistance checks are used to track down the damaged pack and the nature of the failure. In some cases, an internal low resistance path is discovered. Another possibility is a low resistance path to another conductor within the blade, such as another pack electrode or conductive structural element. In order to continue testing, the positive voltage contact is disconnected at the hub connector to eliminate the problem.

Examining the failure history of the packs during operation will aid in finding the causes and will enable estimates of the actuation authority during each of the series of tests performed on the blade. Table 8.1 summarizes the pack failures along with the conditions at failure. The pack failures are ordered chronologically. The pack location is described in terms of the ply number (SP2, SP4, or SP6), spar location (upper/lower), and spanwise center. In actuality, the packs extend approximately ±3 inches from the center. The failure conditions include the voltage applied, the type of electrical breakdown failure, the test in progress, and the rotor speed at the time of failure.

A significant number of the initial pack failures occurred after the initial single frequency hover testing of the blade when the excitation voltage was increased above 2 kVpp. A total of 9 packs failed as the voltage was gradually increased to 2.4 kVpp and then to 600 VDC and 2.4 kVpp. The actual voltage at failure may have been slightly below the referenced value. All the other failures occurred at lower voltages, typically 2 kVpp, and at a gradual rate on the order of one pack per hour of testing. Note that this is 50% of the design operating voltage. This fact will be addressed further in Section 8.3.1.

In addition to the dielectric breakdown failures of the packs, the several of the connections to packs in the integral blade were found to be open circuit prior to testing. The connections were opened during the fairing cure in the manufacturing process. The connection failures may have resulted from excessive heating of the solder connections between the packs and the flex circuit along the web. The result was that eleven packs were discon-
TABLE 8.1 Pack Failure Chronology.

<table>
<thead>
<tr>
<th>Pack</th>
<th>Failure Conditions</th>
<th>Rotor Speed (RPM)</th>
<th>Test in Progress</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Up/Low Bld. Sta.</td>
<td>Voltage Type</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>U 19.3</td>
<td>1500 Vpp external</td>
<td>0</td>
</tr>
<tr>
<td>6</td>
<td>L 37.0</td>
<td>1920 Vpp internal</td>
<td>1000 single freq.</td>
</tr>
<tr>
<td>4</td>
<td>L 37.0</td>
<td>350 Vrms internal</td>
<td>0</td>
</tr>
<tr>
<td>6</td>
<td>U 48.7</td>
<td>2400 Vpp internal</td>
<td>0</td>
</tr>
<tr>
<td>6</td>
<td>L 48.7</td>
<td>2400 Vpp internal</td>
<td>0</td>
</tr>
<tr>
<td>4</td>
<td>L 54.6</td>
<td>2400 Vpp internal</td>
<td>0</td>
</tr>
<tr>
<td>6</td>
<td>L 31.1</td>
<td>2400 Vpp internal</td>
<td>0</td>
</tr>
<tr>
<td>6</td>
<td>U 37.0</td>
<td>2400 Vpp internal</td>
<td>0</td>
</tr>
<tr>
<td>6</td>
<td>L 42.9</td>
<td>2400 Vpp internal</td>
<td>0</td>
</tr>
<tr>
<td>6</td>
<td>L 54.6</td>
<td>2400 Vpp internal</td>
<td>0</td>
</tr>
<tr>
<td>4</td>
<td>U 48.7</td>
<td>600 VDC+ internal</td>
<td>0</td>
</tr>
<tr>
<td>6</td>
<td>L 19.3</td>
<td>600 VDC+ external</td>
<td>0</td>
</tr>
<tr>
<td>6</td>
<td>U 19.3</td>
<td>1300 VDC external</td>
<td>0</td>
</tr>
<tr>
<td>4</td>
<td>L 19.3</td>
<td>2000 Vpp internal</td>
<td>1000 8º TF</td>
</tr>
<tr>
<td>4</td>
<td>L 48.7</td>
<td>2000 Vpp internal</td>
<td>1000 8º TF</td>
</tr>
<tr>
<td>4</td>
<td>U 25.2</td>
<td>2000 Vpp external</td>
<td>1200 0º TF</td>
</tr>
<tr>
<td>6</td>
<td>L 25.2</td>
<td>2000 Vpp internal</td>
<td>1200 0º TF</td>
</tr>
<tr>
<td>4</td>
<td>U 37.0</td>
<td>2000 Vpp internal</td>
<td>1000 0º TF</td>
</tr>
<tr>
<td>4</td>
<td>U 31.1</td>
<td>2000 Vpp external</td>
<td>600 0º TF</td>
</tr>
<tr>
<td>6</td>
<td>U 31.1</td>
<td>2000 Vpp external-shield</td>
<td>400 0º TF</td>
</tr>
<tr>
<td>2</td>
<td>U 42.9</td>
<td>2000 Vpp external-graph</td>
<td>1336 bend TF</td>
</tr>
<tr>
<td>6</td>
<td>U 42.9</td>
<td>2000 Vpp external-graph</td>
<td>1336 bend TF</td>
</tr>
<tr>
<td>2</td>
<td>L 48.7</td>
<td>2000 Vpp internal</td>
<td>0</td>
</tr>
</tbody>
</table>

...nected or had intermittent contacts. Figure 8.1 maps the packs with open circuit connections prior to testing. The shaded packs, divided into the upper and lower spar plies, represent open circuit packs.
Figure 8.1 Map of open circuit packs prior to testing of integral blade.

A similar mapping technique has also been used to illustrate the cumulative history of pack failures. A separate map is provided for each of the major hover and bench tests performed on the integral blade. Each map indicates which packs have an open connection or have electrically failed and were disconnected, including failures which occurred during the tests. Table 8.1 should still be referenced for the exact chronology and test conditions at failure.

Figure 8.2 presents cumulative damage maps for the blade during initial single frequency bench testing and initial single frequency hover testing. The first dielectric breakdown occurred with the first application of voltage during the initial bench tests. A low resistance path was found between adjacent lines on the flex circuit and web and was external to the pack. The second pack failure occurred during single frequency testing at 1000 RPM at 1920 Vpp.

Figure 8.3 shows the cumulative failures during a second period of bench testing. The first map is for failures present during transfer function testing using 350 Vrms white noise input. One pack failed during this testing. The second map is for the first group of high
Figure 8.2  Cumulative damage maps for blade for initial bench testing (upper) and single frequency hover testing (lower).
voltage bench testing as the voltage was increased to 2400 Vpp. This is the period where the most packs failed in a short duration of testing.

Figure 8.4 presents the cumulative pack failures during the second phase of high voltage testing as well as for the transfer function testing of the blade at 8° collective. One additional pack failure which occurred during a static twist test is also included in the second map. A total of two packs failed simultaneously during the hover test at 1000 RPM.

The cumulative failures which occurred during hover testing at 0° collective are presented in Figure 8.5. The first plot shows the map for the twist mode testing, while the second plot includes the subsequent bending mode tests. Four packs failed during the twist testing at 1200, 1200, 600, and 400 RPM. Three of the four failures involved external low resistance paths. With the fourth pack failure, a low resistance path was found between the pack and the aluminum shielding embedded in the web. Two additional failures occurred during the bending mode tests. With both of these failures a low resistance path was found between the pack and the hover test stand. This suggests a short to the graphite ply in the spar which is continuous and directly in contact with the test stand structure. N additional failures occurred during testing at 4° collective (1336 RPM only). One final failure occurred in final bench testing.

### 8.1.2 Maximum Voltage Capability

Once all of the actuation testing had been completed on the integral blade and 55% of the packs had been disconnected due to dielectric breakdown failures, the remaining packs were tested to determine their maximum voltage capability. A sinusoidal voltage was applied to the packs individually at 10 Hz. A DC offset was used to avoid repolarization of the packs at higher voltages. The voltage level was increased in increments of 500 Vpp.

Figure 8.6 maps the maximum voltage level which the packs survived. This includes the packs which failed during the actuation testing of the blade. Since these failures often involved interactions between packs which were not tested in the more recent independent
Figure 8.3  Cumulative damage maps for blade for white noise bench testing (upper) and high voltage bench testing (lower).
Figure 8.4  Cumulative damage maps for blade for additional high voltage bench testing (upper) and hover testing at 8° collective (lower).
Figure 8.5  Cumulative damage maps for blade for additional hover testing at 0° collective (upper) and hover testing at 0° collective in bending mode (lower).
breakdown tests, the data sets are not entirely uniform. However, the purpose of the study is to indicate trends in the voltage at failure as well as to determine what portion if any of the packs were able to operate at the design voltage of 800 VDC and 4kVpp.

![Diagram](image)

**Figure 8.6** Map of blade packs indicating breakdown voltage range.

The packs which survived the greatest voltage before failing were concentrated in the outer spar ply, SP2. This is primarily due the fact that most of the middle and inner ply packs had failed during operation. Only 5 of the packs were operational at 4kV. Two others remained open circuit during the tests. The breakdown voltage is often indicative of the type of failure which occurred in the pack. Experience gained in the proof testing of the packs has shown that failures in the 2kV to 2.6kV range almost always correlate with an internal pack void or exposed electrode fingers. This is because of the 1.125 mm spacing between electrode fingers and the dielectric breakdown strength of the air. This fact will be further investigated in Section 8.3.1.
8.2 Cross-Section Analysis

The integral blade was cut into a number of spanwise sections in order to evaluate the spanwise distribution of inertial properties and to evaluate the cross-sectional geometry of the blade. In particular, this section will investigate manufacturing defects which are apparent in the cross sections. First, each cross section will be presented. Then, the defects present will be characterized.

8.2.1 Cross-Section Survey

The integral blade was cut into 31 spanwise segments using a diamond-abrasive, water-cooled, circular saw in the Technology Laboratory for Advanced Composites at MIT. The rotation speed of the blade was 1100 RPM and the feed rate was approximately 15 cm/min. The cuts were made perpendicular to the leading edge of the blade at predetermined blade stations (measured in inches). Between the root and BS17, cuts were made at every integer blade station. Between BS17 and the tip, cuts were made add odd integer blade stations, or every two inches.

After cutting, the segments were dried at 100°C for two hours to remove excess water from the cutting operation. Then the inertial data were collected. Next, the surfaces were lightly sanded with 600 grit paper. The segments were each placed on a flatbed scanner to be scanned. The resulting photos are shown in Figure 8.7 through Figure 8.11. Each is scaled the same relative to each other and all are scaled to print approximately 1:1.

For reference, a drawing of the blade planform and cross section is presented again in Figure 8.12. The blade cross sections show the spar laminate wrapped around the foam core. A nose block with embedded tungsten weights is at the leading edge of the core while a strain gage wire bundle is located at the heel of the spar core. The flex circuit is located immediately aft of the web structural plies. The images at BS27 and BS29 in Figure 8.8 show the expected blade cross section. In many of the other cross sections, manufacturing defects are visible. These will be addressed in the next section.
Figure 8.7  Blade cross-sections at span stations 9.093, 10, 11, 12, 13, 14, and 15 (top to bottom).
Figure 8.8  Blade cross-sections at span stations 17, 19, 21, 23, 25, 27, and 29 (top to bottom).
Figure 8.9  Blade cross-sections at span stations 31, 33, 35, 37, 39, 41, and 43 (top to bottom).
Figure 8.10  Blade cross-sections at span stations 45, 47, 49, 51, 53, 55, and 57 (top to bottom).
Some additional photographs have been included to illustrate some of the normal features visible in the blade cross sections. Near the leading edge of the sections, the nose block is visible as shown in Figure 8.13. The nose block consists of unidirectional glass plies and embedded tungsten rods. The spar plies wrap around the nose with the exception of the packs, which terminate near the nose block. Near the tip of the nose on the upper spar surface, the epoxy used to spline the leading edge is visible. The graphite inner spar ply has been pushed aft near the leading edge.

The typical spar laminate is shown in Figure 8.14. Each of the 8 spar plies are denoted along with the foam core. The next photomicrograph, Figure 8.15, shows the aft end of the spar including the upper and lower spar laminates, the web plies, the flex circuit, and the wire trough. The strain gage wire shielding is also visible between the flex circuit lay-
ers and the wire trough. The trough is potted with an epoxy to hold the wires in place. All 6 layers of the flex circuit are also shown at this blade station. The gap between adjacent packs within the outer active ply (SP2) is visible in Figure 8.16. The gap is filled with S-glass strips during the lay-up.

Some of the sectioning cuts passed through the solder connections between the flex circuit and the copper strips attached to the pack electrodes. One is shown in Figure 8.17. Two copper strips extend to the right from the pack in spar ply 4 at the top of the photo and then fold downward along the web. A third, auxiliary copper strip, used in the soldering process, is also apparent along the web to the right of the other strips. The fairing skin encapsulates the upper end of this strip during the fairing cure. The copper solder pad is visible on the left edge (forward) of the flex circuit. The solder joint connects each of the copper layers together.
Figure 8.14 Photo of spar laminate including material types.

Figure 8.15 Photomicrograph of aft end of spar at BS17 including upper and lower spar laminates, flex circuit, and wire trough.
8.2.2 Manufacturing Defects

Three major defect types were found in the cross sections of the blade. The first is the core void or bubble found in the foam core of the spar at a number of blade stations. An example at BS45 is shown in Figure 8.18. Three separate voids are shown where the core is
undersized and separated from the inside of the spar laminate. The voids are fairly widespread and range in length from 1 inch to 8 inches spanwise. The voids appear only in the spar core and are distributed between the upper and lower surfaces. The cross-section photos shown in the previous section illustrate the variations in the voids along the span.

![Figure 8.18 Cross section photo of BS45 showing multiple core voids.](image)

The second defect type is a delamination between spar laminate plies. These delaminations are also widespread and may be associated with the core voids. In some cases, it appears that a large void or space is present between plies at the site of the delamination either in addition to or instead of a void at the core. Figure 8.19 shows a core void which merges with a delamination between spar plies 6 and 7 near the nose. The delamination, which is located at the interface between the inner active ply electrode and an E-glass ply, has enlarged to approximately 3 ply thicknesses.

In many cases, the delaminations, which uniformly occur at active ply boundaries, appear to be associated with local damage to the active plies. Figure 8.20 shows the delamination near the nose of the spar at BS47. The inner electrode is separated from the active fibers and matrix. Figure 8.21 illustrates the separation of the E-glass in SP7 from the inner active ply. In Figure 8.22, damage is evident at the outer surface of the middle active ply at the site of a delamination. Similarly, Figure 8.23 shows damage to the inner electrode of the inner active ply.
The damage visible in each of the cross sections of the blade has been mapped in order to better characterize the nature and extent of the damage. The damage map is shown in Figure 8.24. The map shows the 8 spar plies above and below the core in the same orientation through the thickness as a cross section of the spar. The map is essentially a verti-
cally oriented longitudinal section through the spar. The spanwise distribution of defects is illustrated using solid rectangles to indicate core voids and "<" or ">" symbols to indicate delaminations evident at the particular section cut. The dashed vertical lines indicate the spanwise locations of the section cuts. The corresponding numbers indicate the blade station. A red symbol indicates that the delamination occurred within the active ply, between the electrode and the fiber/matrix layer. The three active plies in the upper and lower laminate are shaded. The sections of the active plies hatched in red indicate the locations of pack electrical failures. The internal strain gages are also shown in the diagram. The correlation between the strain gages and the core voids will be discussed in the next section.

The final manufacturing defect was the failure of several connections between the flex circuit and the actuators. The connections failed during the fairing cure which followed the bonding of the flex circuit to the spar. The choice of a low temperature solder was most likely a factor in the failures of the connections. Additional details will be discussed in the next section.

8.3 Suggested Causes and Remediation

This final section will present some possible causes for the pack failures and the blade manufacturing defects. Correlations between pack failures and manufacturing defects will
Figure 8.24  Map of spar damage including core voids, delaminations both within and outside the electrode layers based on sections cut as shown (dashed lines), and pack electrical failures (red).

be investigated along with other possible risk factors. Several possible causes of the manufacturing defects will be explained and data from the cross-sectional analysis and other references will be used to support or opposition. Suggestions will be provided for improving the manufacturing process for future blades.
8.3.1 Pack Failures

Several important facts must be considered in the analysis of the pack failures. The following list summarizes the important points:

- 55% of the packs in the integral blade suffered electrical failures during testing
- the vast majority of failures occurred at voltages between 2kVpp to 2.4kVpp
- all the packs in the blade had passed proof tests at 4kVpp prior to the blade manufacture
- the pack failure rate in the previous half span test article was significantly lower and 86% of the packs were operational at 4kVpp
- the packs in the integral blade were higher in quality (performance and risk)
- the majority of failures occurred on the inner and middle active plies

Several deductions can be made from these facts. First, it would appear that something occurred during the blade manufacturing process that led to the failures. The fact that the failures occurred in the 2kVpp range strongly suggests that the conductive inner surface of the pack electrodes were exposed to air. During the proof testing process, packs will often experience a dielectric breakdown around this voltage level when a void or bubble is present in the matrix. This is because of the 1.125 mm spacing between electrode fingers and the dielectric breakdown strength of the air in between. Since the packs all passed the proof test to 4kV successfully, it is highly unlikely that any other voids were present in the packs that would cause a dielectric breakdown at half that voltage. Also, the majority of the packs in the previously manufactured half span test article, referred to as blade section 3, were operational at 4kV. The packs in the full blade had better strain performance and fewer defects than those in the blade section. The packs in integral section 3 also survived combined loads testing at static levels significantly greater than the hover loads.

The fact that the failures are located almost entirely on the inner two active plies of the upper and lower spar laminates supports the possibility of a manufacturing-related problem. If the packs were failing as a result of hover testing stresses, it would be more likely that the pack failures would be concentrated inboard and in the outer plies where stresses
are highest. The pack failures were also distributed rather uniformly in the spanwise direction.

From the cross-section analysis of the blade, it is clear that a number of manufacturing defects were present in the blade. The distribution of the defects was presented in Figure 8.24, along with the correlation with pack electrical failures. Note that additional undetected damage may be present in between the section cuts which are 2 inches apart. Table 8.2 lists the packs which failed chronologically along with any local damage present. From this comparison, it is evident that the majority of the packs which failed were located near these defects. In particular, 10 of the first 11 pack failures occurred in packs which showed a delamination on one surface.

Another consideration is the failure rate for various groups of packs within the blade. Table 8.3 presents the failure rates for several groups. The overall failure rate among all packs was 55%. For packs which were found to be adjacent to a delamination, the rate jumped to 76%. Dividing the packs by spar ply, the failure rates for the inner and middle plies stand out at 86% and 64%, respectively. In contrast, the failure rate for packs in the outer ply was 14%. Another comparison was made for packs which were found to be initially open circuit. Many of these packs were effectively connected under the application of a sufficient voltage for arcing to occur across the gap which was most likely present between the flex circuit solder pad and the copper strips. The failure rate among these packs was similar to the overall average.

The number of repairs each pack had during the pack preparation and proof testing phase is also considered to be an indicator of failure risk. Packs with a large number of repairs are more likely to suffer further dielectric breakdown problems. Since the highest quality packs were used in the blade, the majority of the packs had either no repairs or one repair. Among this group, the failure rate was 70%. The packs with two or more repairs had a failure rate of only 27%. The trend is the opposite of what would be expected. The reason is that packs with more repairs were purposely placed in the outer spar ply, which showed
TABLE 8.2  Pack Failure Correlation with Blade Defects.

<table>
<thead>
<tr>
<th>Ply</th>
<th>Up/Low</th>
<th>Bld. Sta.</th>
<th>LocaStructural Damage</th>
</tr>
</thead>
<tbody>
<tr>
<td>4</td>
<td>U</td>
<td>19.3</td>
<td>delam, void</td>
</tr>
<tr>
<td>6</td>
<td>L</td>
<td>37.0</td>
<td>delam, void</td>
</tr>
<tr>
<td>4</td>
<td>L</td>
<td>37.0</td>
<td>void</td>
</tr>
<tr>
<td>6</td>
<td>U</td>
<td>48.7</td>
<td>delam, void</td>
</tr>
<tr>
<td>6</td>
<td>L</td>
<td>48.7</td>
<td>delam, void</td>
</tr>
<tr>
<td>4</td>
<td>L</td>
<td>54.6</td>
<td>delam</td>
</tr>
<tr>
<td>6</td>
<td>L</td>
<td>31.1</td>
<td>delam</td>
</tr>
<tr>
<td>6</td>
<td>U</td>
<td>37.0</td>
<td>delam, void</td>
</tr>
<tr>
<td>6</td>
<td>L</td>
<td>42.9</td>
<td>delam, void</td>
</tr>
<tr>
<td>6</td>
<td>L</td>
<td>54.6</td>
<td>delam</td>
</tr>
<tr>
<td>4</td>
<td>U</td>
<td>48.7</td>
<td>delam, void</td>
</tr>
<tr>
<td>6</td>
<td>L</td>
<td>19.3</td>
<td>-</td>
</tr>
<tr>
<td>6</td>
<td>U</td>
<td>19.3</td>
<td>void</td>
</tr>
<tr>
<td>4</td>
<td>L</td>
<td>19.3</td>
<td>-</td>
</tr>
<tr>
<td>4</td>
<td>L</td>
<td>48.7</td>
<td>void</td>
</tr>
<tr>
<td>4</td>
<td>U</td>
<td>25.2</td>
<td>void</td>
</tr>
<tr>
<td>6</td>
<td>L</td>
<td>25.2</td>
<td>delam, void</td>
</tr>
<tr>
<td>4</td>
<td>U</td>
<td>37.0</td>
<td>-</td>
</tr>
<tr>
<td>4</td>
<td>U</td>
<td>31.1</td>
<td>void</td>
</tr>
<tr>
<td>6</td>
<td>U</td>
<td>31.1</td>
<td>delam, void</td>
</tr>
<tr>
<td>2</td>
<td>U</td>
<td>42.9</td>
<td>void</td>
</tr>
<tr>
<td>6</td>
<td>U</td>
<td>42.9</td>
<td>delam, void</td>
</tr>
<tr>
<td>2</td>
<td>L</td>
<td>48.7</td>
<td>void</td>
</tr>
</tbody>
</table>

by far the lowest rate of failure. The packs with fewer repairs were located on the inner plies, which showed the highest rate of failure. This reinforces the importance of the high failure rate on the inner plies.
### TABLE 8.3 Correlation of Pack Failure Rates with Various Pack Groups.

<table>
<thead>
<tr>
<th>Correlation with Pack Groups</th>
<th>Failure Rate</th>
</tr>
</thead>
<tbody>
<tr>
<td>overall</td>
<td>55%</td>
</tr>
<tr>
<td>packs adjacent to delamination</td>
<td>76%</td>
</tr>
<tr>
<td>ply 2 (outer)</td>
<td>14%</td>
</tr>
<tr>
<td>ply 4 (middle)</td>
<td>64%</td>
</tr>
<tr>
<td>ply 6 (inner)</td>
<td>86%</td>
</tr>
<tr>
<td>initial open circuit</td>
<td>58%</td>
</tr>
<tr>
<td>packs with 2+ repairs</td>
<td>27%</td>
</tr>
<tr>
<td>packs with no more than 1 repair</td>
<td>70%</td>
</tr>
</tbody>
</table>

Another consideration in the pack failure analysis is that continuous arcing was occurring during the testing of the blade. The arcing would occur at repeatable voltage levels in the sinusoidal voltage cycle. The arcing occurred with several different packs, at slightly different voltage levels. The current draw during the arcing was not large enough to trip the current limits on the amplifier. The arcing was apparently related to open circuit connections to the packs, but may have also been related to any delaminations which may have been present within the active plies. In either case, it may also be possible that the arcing caused local damage which propagated. Conductive carburized material formed in the process could eventually lead to a more significant low resistance path between conductors of opposite polarity either within a pack, or between a pack and another conductor in the blade. The arcing could have also occurred along the web. The 12.5 μm kapton electrodes and insulative pack perimeters should have inhibited the spread of electrical damage between packs within the spar laminate. However, it is also possible that damage to the kapton electrodes from the pack manufacturing process created weak points in the insulative layer. The spreading of electrical damage could explain the external failures in four of the last five packs listed in Table 8.1, and the reduced correlation with the delamination defects in the latter half of the failed packs. Two improvements which could reduce the risk of electrical failures are changing to 25 μm kapton and a higher temperature and stronger solder for the web connections.
The damage to the packs which appeared in the cross-sectional photos could explain the high overall failure rate of the packs. Assuming that the delaminations and electrode separations occurred during the manufacturing process, then there is a clear explanation for the failures. The delaminations, which are concentrated among the inner active plies, damaged the electrodes on a significant number of packs. This resulted in copper electrode surfaces being exposed to air. Arcing would then occur at the breakdown strength of air between the fingers. The core voids may have been the root cause of the delaminations.

Conversely, if it is assumed that the delaminations were not present immediately after the blade cure, then the failure of the packs could have caused the delaminations. Dielectric breakdown or arcing which occurred could have been the cause. This theory does not explain the reason for so many pack failures at such a low voltage. In addition, there were current and voltage limits on the amplifier which prevented any significant dissipation of power in the dielectric breakdown events. No signs of heat damage were evident near the packs and delaminations. Also, this would not explain the core voids which will be discussed in the next section. The evidence strongly supports the theory that the delaminations contributed to the pack failures.

### 8.3.2 Blade Manufacturing Defects

In this section, each of the three major manufacturing defects, core voids, delaminations, and open circuit connections, will be discussed and possible causes will be investigated. Suggestions for solving the manufacturing problems will also be provided.

#### Core Voids

The possible causes of the core voids can be divided into three general categories: heat, chemical interaction, and outgassing/trapped air. Each of the possibilities will be discussed in the following paragraphs. Supporting and opposing evidence will be provided for each.
Heat. Overheating of the foam is one of the simplest explanations for the voids upon first examination. Two possible sources for the heat are the mold heaters during the cure and heat produced during actuation of the piezoelectric actuators from internal losses or arcing. No correlation was found between the void locations and the heating elements of the mold, which would be the local hot spots. The hysteretic heating of the actuators is fairly small for actuation frequencies below 150 Hz. No changes in temperature were evident during testing. For the arcing theory, the fact remains that the power output of the amplifier was limited so that any damaging effects would be minimal.

Several important characteristics of the foam and the voids are also significant. First, the heat distortion temperature for the Rohacell 71 IG foam is 355°F, which is 100°F higher than the cure temperature. There is no evidence of heat damage from arcing or heating of the actuators near the delaminations. Since the surface of the foam core and the inner surface of the spar laminate are both coated in film adhesive, it is likely that the voids were formed during the spar cure, before the adhesive was cured. The inner surfaces of a core void are shown in Figure 8.25.

Chemical Interaction. Another possibility is that some chemical interaction occurred which dissolved or reduced the volume of the foam. Since the void locations are strongly correlated with strain gage locations, it could be that one of the chemicals used in the strain gaging process interacted with the foam. To investigate this possibility, each of the chemicals used in the strain gaging process were applied to a block of foam along with the film adhesive. Each block was subjected to 250°F for two hours followed by 300°F for two hours. The test was performed in an open air environment. No degradation was found in any of the foam samples.

Another important characteristic of the core voids is that cuts made in the foam surface for routing strain gage wires are still present at the deformed surface of the core. Figure 8.25 shows a strain gage wire positioned in a groove in the foam which is widely separated from the initial core location at the inner surface of the laminate. If a chemical attack had
created the void, the wire track in the foam should have been destroyed. The track is simply displaced. This is shown more closely in Figure 8.26 and Figure 8.27.

**Figure 8.26** Close-up view of strain gage wire in core of BS19.  
**Figure 8.27** Strain gage wire in core at BS49 along with delaminations between plies 1 and 2, 6 and 7.

**Outgassing/Trapped Air**. Trapped air between the core and the spar laminate or some form of outgassing during the spar cure may have formed the core voids. Air may be
trapped during the lay-up process where the prepreg layers do not adequately adhere to the foam core. Hand pressure is used to minimize any trapped air during the lay-up. During the cure, the resin in the prepreg layers liquefies which should allow any trapped air to escape under the high cure pressures in the mold.

The other possibility is that outgassing occurred within the spar during the spar cure. Possible sources of the gas include absorbed moisture in the foam core, or some sort of chemical outgassing in one of the strain gage chemicals. With the latter, it is questionable whether enough outgassing could have occurred from such small volumes of chemicals as were used in the strain gaging process. It is much more likely that water vapor was outgassed from the foam. The core was made from industrial grade foam and no special precautions were taken to remove absorbed moisture prior to the cure. Blade manufacturers at Boeing have suggested that the foam should be heat treated to dry the foam prior to assembly.

In either case, several factors may have contributed to the formation of voids due to trapped gases in the integral blade. The integral blade differs from typical model blades in that it has embedded AFC pack actuators. Because the packs are precured in a flat, uncurved form, the packs tend to straighten out somewhat during the lay-up. The result is that the ends of the packs may pull the laminate away from the core near the forward and aft ends of the spar. The reduced adhesion of the prepregs to the packs, in comparison with the adhesion of the prepreg layers to each other, also may contribute to separations forming between the laminae. Once the cure pressure and temperatures are reached, it is expected that all gaps would close; however, it is possible that air could be trapped. In addition, the packs are nonporous because of their kapton electrode layers. The large extent of the nonporous packs in comparison with the previous half-span test articles may also have inhibited the escape of trapped air during the cure.

The void locations are also highly correlated with strain gage locations. Of the sections cut, 68% of the sections which were in close proximity to strain gages also had core voids
(considering upper and lower spar surfaces separately). An example is shown in Figure 8.28. The strain gage is bonded to the inside of spar ply 8, which is a graphite ply. The core is separated from the inside of the laminate. In addition, a large delamination is visible between plies 6 and 7. The delamination occurred at the interface of the inner electrode with the adjacent E-glass ply. The delamination transitions from the outside of the electrode at the left edge of the frame, to the inside of the electrode at the right. In many cases, it would appear that the core voids are centered on the strain gages. In a number of other cases, there is no evidence of a void at a strain gage.

![Photomicrograph showing core void, strain gage, delamination between plies 6 and 7, and electrode separation.](image)

The strong correlation between strain gages and core voids may support the chemical outgassing explanation. A more likely scenario is that the strain gages served as initiation sites for the voids. The strain gages are mounted on the foam core prior to the lay-up. The gages are covered with precured E-glass tabs. In many cases, the completed gage assembly may not have been flush with the surface of the foam core. The result is a small dis-
continuity in the core. This would make it more likely that air would be trapped around the gage during the lay-up. It could also provide an initiation site for gasses to collect during the cure if outgassing occurred. Another important fact is that the previous half-span blade section in which the majority of packs were operational at 4kV did not have internal strain gages.

The appearance of the voids have characteristics which support the trapped gas explanation. The general rounded shape of the voids is similar to what might be expected of a high pressure gas deforming the core. The outer surface of the laminate would not be deformed because of the solid mold against it. The mating surfaces on the foam and the inside of the laminate appear to have foam and film adhesive residue on both surfaces. This would suggest that the surfaces were separated before the adhesive fully cured. Enough of the cure had progressed for the strain gages to preferentially adhere to the laminate. The surface features of the mating surfaces also appear to match in some locations. The cuts in the foam for the surface mounted strain gage wires also show signs of expansion rather than signs of heat or chemical damage. This is illustrated in Figure 8.26. The groove was initially the same width as the wire.

Several steps can be taken to reduce the likelihood of core voids being formed if they are related to trapped air or outgassing. First, the core can be heat treated to remove moisture. To ensure dimensional accuracy, it may be necessary to do an initial heat treatment prior to shaping the foam. If necessary, the core can also be heat treated after the strain gages are installed. This could reduce any outgassing from the core or the strain gage chemicals during the spar cure.

Another solution which could reduce the risk of trapped voids would be to have pressure relief holes in the core. Since the foam is closed-cell, it can hold pressure and will deform. If small holes were drilled through the thickness at regular intervals along the core which would intersect a spanwise hole through the center of the spar, any trapped air pressure could be released to the outside of the mold during the cure. It may also be possible to
pull out trapped air using a vacuum during the manufacture as the spar is placed in the mold until full pressure and temperature are achieved.

**Delaminations**

In addition to the core voids, widespread delaminations were also found in the cross sections. As was shown in Figure 8.24, the delaminations are most prevalent between spar plies 6 and 7. The separation occurs between the inner electrode of the packs (SP6) and either the adjacent E-glass ply or the internal active fibers and matrix layer. Both cases are present in the section shown in Figure 8.28. Delaminations also appeared much less frequently at the interfaces of the other active plies (SP2 and SP4).

The delaminations appear to correlate to some extent with the core voids. In some cases, the appears to transition from the interface of the inner spar ply and the core, to in between plies 6 and 7. This was visible in several of the photos presented in this section. Rather than a slight separation between the delaminated plies, a large gap forms which is several ply thicknesses in size. In several cases, the delamination tend to extend over a greater distance spanwise than the core voids. In the vicinity of a core void, the cure pressure on the spar laminate would be hydrostatic, rather than solid foam, pushing the laminate outward against the mold. The hydrostatic pressure may not have been adequate to ensure a solid cure of the spar laminate, resulting in delaminations or gaps between plies. This would be especially true near the active plies which are not prepregs and do not conform as well as the other prepreg plies in the uncured laminate.

It is possible that air was trapped between the plies or that air trapped at the core interface shifted to form a void between plies. The impermeability of the packs would increase the likelihood that the air would be trapped on the inner surface of the innermost active ply.

Another possibility is that delaminations occurred during the cutting operation. Localized delaminations were found within 0.2 inches of the cut face in the outer E-glass skin. The delaminations strongly correlated with cutting damage in the outermost active ply as well. This would not explain the majority of the delaminations with a large separation, but the
cutting may have contributed to the smaller scale delaminations. Similarly, thermal stresses from the spar cure may have contributed to the delaminations. The thermal stresses in conjunction with the stresses of cutting and the presence of core voids may have also combined to create delaminations. Further analysis is needed to evaluate the possible causes. Further analysis of the thermal stresses and the effects of the cutting process are required.

Other Pack Damage

A third type of damage found in the cross sections is localized cutting damage to the active plies. An example of this type of damage is shown in Figure 8.29. The damage appears to be widespread and distributed evenly between the three active plies. This type of damage is most likely the result of the cutting operation. However, the presence of this type of damage in many areas may indicate that a small delamination was present. During the cutting operation, the delamination may have allowed for the abrasive blade to tear the soft, unsupported electrode material. In some cases, the AFC fiber and matrix material is also damaged. Some localized delamination of the outer skin (SP1) is visible at many of the section edges where cuts were made. The correlation of these small surface delaminations with the cut edges definitely establishes the cutting as the cause. In addition, the localized damage to the outer active ply also appears to correlate with the surface delaminations. This supports the possibility that the cutting operation is responsible for some of the delaminations. The delaminations with larger separation between the plies does not appear to fit this characterization. However, the cutting was responsible for the damage to the fiber/matrix layer near the delaminations.

Open Circuit Connections

The final type of damage and associated manufacturing flaw is the loss of several pack connections during the fairing cure. A total of 11 packs were disconnected during the cure. The opening in the circuit most likely occurred at the weak point in the conductor, at the joint between the flex circuit and the copper strips along the web. The combination of
stresses and temperature during the fairing cure are the most likely causes of the failures. Both relate to the selection of a low temperature solder for the connection. The Indium 290 solder has a melting point of 290°F. During the fairing cure, the blade reached temperatures of 250-265°F. The mold temperature may have been slightly higher. In addition, it is possible that the auxiliary copper strips used in the connection may have contacted the mold in some locations, which would raise the temperature of the solder significantly. For this to occur, the edge of the copper would need to push through the single layer of E-glass fabric of the fairing skin. The blade surface near the web also shows a few spots of solder which indicates that some solder flowed during the fairing cure. The stresses resulting from the cure pressures may have also contributed to the opening of the solder joints in several locations.

The open circuit failures did not occur in the previous half-span blade section. In the blade section, the flex circuit was bonded to the web using a low viscosity epoxy injected after soldering. It is possible, but unlikely that this bond was significantly stronger than that achieved in the full blade. The solder in the full blade may have been exposed to higher temperatures either because of mold overheating or better thermal conductivity through the copper strips. In addition, the increased length of the blade could have contributed to higher cure stresses.
The solution is to avoid the low temperature solder and use a standard lead/tin solder with a melting point of 361°F. This would provide a much greater margin. In addition, the bond strength of the standard solder may be as much as three times greater than that of the low temperature solder.\textsuperscript{1} The low temperature solder was selected in order to enable a stepped soldering process. This prevented the earlier solder connection between the copper strips and the pack electrodes from being damaged during the connection of the flex circuit. However, it is less likely that this connection would be broken since there are no significant stresses present which would separate the two conductors. In contrast, the relatively stiff flex circuit tends to pull away from the web contacts in many places. Reducing the stiffness of the flex circuit would also reduce the risk of failures. Attaching the individual flex circuit layers separately rather than as a preassembled laminate would solve this problem.

**Summary of Suggested Process Improvements**

The following list summarizes the suggested changes to the blade manufacturing procedures to reduce the risk of defects in the integral blade:

- core voids and delaminations
  - heat treat foam
  - eliminate cyanoacrylate adhesive from strain gaging procedure
  - air release holes in core
- open circuit connections
  - use standard solder
  - attach flex circuit in separate layers

Further analysis may provide more certainty in the causes of the defects. Nondestructive testing of several blade sections will provide additional data on the relationship between delaminations, core voids, strain gages, and AFC packs. Blade sections will be analyzed with and without packs and with and without internal strain gages. The heat treating of the foam will also be investigated. Recent testing of the effects of cyanoacrylate adhesive

\textsuperscript{1} manufacturer's data, Indium Corp. of America
present during the cure process strongly indicates that this adhesive was the primary source of the core voids.
The solution is to avoid the low temperature solder and use a standard lead/tin solder with a melting point of 361°F. This would provide a much greater margin. In addition, the bond strength of the standard solder may be as much as three times greater than that of the low temperature solder. The low temperature solder was selected in order to enable a stepped soldering process. This prevented the earlier solder connection between the copper strips and the pack electrodes from being damaged during the connection of the flex. circuit. However, it is less likely that this connection would be broken since there are no significant stresses present which would separate the two conductors. In contrast, the relatively stiff flex circuit tends to pull away from the web contacts in many places. Reducing the stiffness of the flex circuit would also reduce the risk of failures. Attaching the individual flex circuit layers separately rather than as a preassembled laminate would solve this problem.

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  - eliminate cyanoacrylate adhesive from strain gaging procedure
  - air release holes in core
- open circuit connections
  - use standard solder
  - attach flex circuit in separate layers

Further analysis may provide more certainty in the causes of the defects. Nondestructive testing of several blade sections will provide additional data on the relationship between delaminations, core voids, strain gages, and AFC packs. Blade sections will be analyzed with and without packs and with and without internal strain gages. The heat treating of the foam will also be investigated. Recent testing of the effects of cyanoacrylate adhesive

1. manufacturer’s data, Indium Corp. of America
present during the cure process strongly indicates that this adhesive was the primary source of the core voids.
Chapter 9

CONCLUSIONS AND RECOMMENDATIONS

This final chapter will summarize the entire integral blade project described in this thesis. The thesis objectives will be reviewed. Conclusions will be drawn from the results and recommendations for design improvements and for future work will be provided.

9.1 Summary and Conclusions

The primary objectives of this thesis were to design and test an integral twist-actuated rotor blade and to demonstrate the effectiveness of active fiber composite actuators in this application. These objectives were divided into three major sections of the thesis. The first was the development of the AFC actuators for the rotor blade application. This included the development of a manufacturing process for producing the blade actuators and a material system characterization study to quantify the behavior of the actuators and to qualify them for the application. Then next section was the development of the integral blade design. This section included the entire design process as well as the development of the integral blade manufacturing process and the bench testing of representative test articles. The final section is the final system level testing of the integral blade in Mach scale hover tests and the analysis of the data and the actuator failures. The following paragraphs will provide a short summary of each chapter in the thesis.

Chapter 1 of the thesis provided an introduction to the problem of individual blade control (IBC) for vibrations and noise. This motivates the need for blade-mounted actuators and
in particular, for active twist control. The integral twist actuation concept is one potential solution which offers a number of advantages over the more common flap actuation technique. Both aim to achieve the same performance as previous higher harmonic control studies have established as targets for sufficient vibration reduction. A number of competing IBC flap actuators are referenced as well as a direct twist concept involving monolithic piezoceramic wafers. The active fiber composite is introduced as an IBC actuator for integral twist actuation. The AFC offers the advantages of anisotropic actuation, structural integrity, and conformability as well as a simpler design and implementation in comparison with flap actuators.

Chapter 2 begins the material level section of the thesis, describing the development of the AFC actuators for the integral blade application. First, a detailed introduction to the AFC concept is provided. The remainder of the chapter focuses on the development and details of the manufacturing process used to produce the blade actuators, or packs. This includes an outline of the process requirements and the component materials used for the packs. Then the details of the manufacturing process are described, including considerations for higher volume batch processing.

The success of the manufacturing process has formed a base for further development of AFC's for commercial applications. The current work has demonstrated a process for the production of AFC's in sufficient quantity and with acceptable quality for a large-scale, high performance application. Additional research will be necessary to improve the process yield and to reduce the cost of both the raw material, particularly the active fibers, and the process.

Chapter 3 focuses on the secondary objective of characterizing the active fiber composites and demonstrating sufficient structural integrity for the rotor blade application. The operational requirements for the AFC's are established as a guide for the structural integrity testing. A general testing methodology was developed to evaluate the actuators in tests which simulate realistic operating conditions for the application. These include mechani-
cal tests, electrical tests, and electromechanical coupling tests. A major focus of the tests is the characterization of actuator performance changes under simulated operating loads. A subset of these tests which concentrates on induced stress performance and damage tolerance was used to select the optimal component materials for the rotor blade application. Then, a series of additional structural integrity tests is described which target potential failure modes for the integral blade. The final section introduces the pack proof testing regimen which was established to ensure a desired level of performance for the blade packs and to reduce the risk of actuator failures.

Several conclusions can be drawn from the AFC characterization studies. The general testing methodology developed and applied to the material selection process provides a framework for selecting the optimal materials for a specific application. For the model scale integral blade, PZT-5A fibers offered the greatest performance and damage tolerance of available fibers. An alternative composition, PZT-4S, as well as a variety of new higher performance electroceramics under investigation may offer future improvements to the AFC.

Characterization and structural integrity tests have established that the performance and damage tolerance of the actuators are sufficient for the integral blade application. The tests included longitudinal and transverse tensile loading, longitudinal tensile fatigue, longitudinal compression, interlaminar shear, and electrical actuation fatigue. For each test, performance degradation was less than 10% at simulated forward flight limit load conditions. Given the severity of the forward flight material strain requirements, the test results also demonstrate the applicability of AFC's to a wide range of structural actuation implementations.

Chapter 4 presents the design of the model scale integral blade and is the first chapter in the blade level section of the thesis. The baseline model blade was introduced and the design requirements were established. A general design methodology was presented, including considerations for performance, elastic and inertial requirements, and structural
integrity in determining ply lay-ups and actuator placement. Several candidate designs were evaluated and compared. A detailed design was then presented which included system-level considerations for the actuator packs, electrical system, and internal sensors.

The blade design process demonstrated a general technique for designing composite blades with integral actuators. The analysis of the current design has demonstrated that an active blade with integral actuation in the spar laminate can be designed to meet all of the structural and performance requirements for Mach-scale IBC actuation. This design methodology is being applied to similar conceptual designs for the next phase of the integral blade program and also the NASA Langley/MIT Active Twist Rotor program.

Chapter 5 detailed the integral blade manufacturing process. The process includes the fabrication of an instrumented foam mandrel, the spar fabrication, the flexible circuit attachment, the fairing addition, and finally the preparation for hover testing. The process, based on model-scale CH-47D blades fabricated at Boeing Helicopters (Philadelphia), describes the techniques developed for embedding the AFC packs to form active plies within the spar laminate. This process, including the integration of the electrical system, can be applied to the fabrication of future integral twist-actuated blades as well as other applications involving actuators integrated in composite structures.

Chapter 6 presents actuation and structural test data for several representative blade sections. First, a representative spar laminate was tested on a sandwich beam in tension and compression. No performance degradation was observed in tests up to ±4500 microstrain. Three half-span blade sections were fabricated. Each provided performance data and allowed for detailed design and manufacturing process improvements. The third blade section demonstrated approximately 2 deg/m of twist at the design operating voltage levels. The third section was also subjected to combined static tension/torsion loading. Performance degradation after exposure to forward flight limit loads was less than 15%. The tests of the complete integral blade included twist actuation data, detailed stiffness and inertial distribution measurements, and a modal analysis. Twist performance and actuator
admittance were compared as a function of actuation voltage. In comparison with model predictions for twist performance, good correlation was found when the experimentally determined torsional stiffness was used to correct the model predicted stiffness.

Several conclusions can be drawn from the blade bench testing experiments. First, the current integral blade design demonstrated performance on par with model predictions at full operating voltages. The model predictions for the torsional stiffness were found to be fairly inaccurate while axial and bending stiffness predictions were good. Including more accurate cross-sectional geometry in the model was found to improve torsional stiffness predictions. The collected data will be used in the redesign of the integral blade. The second major conclusion is that the combined loading test demonstrated survivability for the blade and the embedded AFC's under worst-case limit load conditions. The embedded actuators had no impact on the integrity of the blade. This implies that the current design for the blade and actuators will be suitable for future forward flight testing.

Chapter 7 provided results for the major objective of the thesis: Mach-scale hover testing. An overview of the MIT Hover Test Stand Facility is provided, including details of the testing environment, the rotor system, and the performance sensors. The testing procedure is outlined, including the collection of transfer function data from input voltage to blade twist and induced vertical hub shear. Data were collected for a range of rotor speeds and blade loading conditions as well as actuation frequencies from 10 to 150 Hz for a single active blade. Blade pack electrical failures limited the performance of the blade. Data were collected at 50% of the design voltage and with many packs inoperable.

Initial single frequency hover data were collected with a 0.8° of peak-to-peak tip twist. The corresponding quasi-steady change in the coefficient of thrust was 1.3x10^{-4}. The majority of transfer function data were collected with 0.6° of peak-to-peak tip twist. The twist response and induced thrust were compared for each rotor speed and blade loading condition. Data were also collected for bending mode operation of the blade. A campbell
diagram or fan plot for the rotor system was also produced and compared with model predictions.

Test room asymmetry caused nonuniform inflow resulting in a poor signal-to-noise ratio for the load measurements. This resulted in reduced confidence levels for data sets collected under high inflow conditions. However, the confidence is sufficient for estimating the system dynamics and performance levels. Background noise levels were documented for each blade loading condition.

The Mach-scale hover testing of the integral blade has enabled a number of interesting conclusions to be drawn from the data. First, the quasi-steady twist response was unaffected by airspeed or collective pitch. The thrust data demonstrated a strong dependence on flap bending modal dynamics, but a minimal dependence on the first torsional resonance. The torsional resonance was found to be critically damped at full speed. Overall, the quasi-steady induced change in the coefficient of thrust was well correlated with model predictions. The induced thrust in the bending mode of operation was significantly less than that of the twist mode. No evidence of any structural degradation was observed as a result of the hover testing.

Chapter 8 provided an analysis of the actuator failures which occurred during the testing of the integral blade. First, the AFC pack failures are documented in detail. The locations of pack failures are mapped for each of the test sequences performed. Then the cross sections of the blade were analyzed to evaluate possible manufacturing defects. Major defects were discovered including large, widespread voids in the foam core and delaminations between the spar laminate plies. The defects were documented and possible causes were discussed in conjunction with correlation studies between the defects, pack failures, strain gage locations, and other characteristics. Recommendations were provided for possible solutions to the problem.

The blade structural defects were found to be a probably cause of the electrical failure of the actuators. The core voids and delaminations, concentrated near the inner active plies,
resulted in the separation of the electrode layer from the active fibers in the active plies. A strong correlation between the locations of the core voids and the internal strain gages suggests a possible outgassing or other trapped gas as the cause. Direct chemical or thermal damage to the foam was less likely. Additional analysis is in progress including non-destructive testing of other blade sections manufactured with and without internal strain gages and embedded AFC packs. The cross-sectional analysis and pack failure analysis will provide useful data for solving the manufacturing problems for future blade projects.

The results presented support the following major conclusions which address the primary objectives of this research:

- integral twist actuation is a viable concept for IBC
- the AFC material system is suitable for large-scale, high stress applications

This research demonstrated the effectiveness of the integral twist actuation in Mach-scaled hover tests. Although model blade manufacturing difficulties resulted in twist performance significantly below design levels, previous tests on half-span sections successfully demonstrated twist at full authority. Correction of the manufacturing problems is expected to result in greater than 2° of peak-to-peak tip twist with the current design. A reduction of the torsional stiffness of the blade to target levels would increase the performance further. Even with the reduced twist, the demonstrated hub load generation at all blade loading conditions and rotor speeds was consistent and near predicted levels. The integral twist actuation concept is worthy of further investigation.

The body of data collected on the structural integrity of the AFC material system, both in direct material testing and in blade-level testing, has shown good survivability, toughness, and damage tolerance for this and other high stress applications. Proper fiber selection can be especially useful in optimizing performance for specific operating environments. New active fibers and electroceramic compositions will offer even greater performance in the future, possibly enabling sufficient actuator authority for full collective control. While improvements in the AFC manufacturing process have improved the scale and reliability
of the packs produced, further advances will be required to reduce material and manufacturing costs and increase yield and volumes to enable full-scale blade applications.

9.2 Recommendations

The results of the current investigation have illuminated a number of areas which require further development and investigation. The recommendations can be grouped into material system, blade level, and system level advances. These will include short-term studies to address current problems and longer-term developments for future applications.

For the active fiber composites, increased performance and decreased costs are the primary goals. Performance includes both the actuation capability as well as the reliability. Recent developments in electroceramic compositions have yielded materials with an order of magnitude greater strain performance in comparison with conventional piezoceramics. These materials will enable larger deflections and higher energy densities. Strain levels are comparable to shape memory alloys but with much greater bandwidth.

These new materials need to be produced in the form of fibers for the AFC actuator. In general, improvements are needed in the processing of fibers, which currently drives the cost of the actuators. Greater consistency and quality in the ceramic will provide benefits in performance and reliability. A transition to smaller diameter fibers will enable higher specific strengths and less constraints on the electrodes required to deliver the driving electric fields. Sufficient performance may be achieved using a single electrode layer, which would greatly simplify the AFC manufacturing process. Voltage requirements could also be reduced. Ideally, continuous piezoceramic fibers would allow AFC’s to be produced in prepreg rolls, in the form that structural composites are available.

Additional improvements are needed to scale-up the AFC manufacturing process without sacrificing the quality of the product. While AFC’s currently offer greater performance than other actuator types, the unit cost must be significantly reduced to be competitive, especially for larger scale applications. Additional studies are in progress for applying the
AFC technology for active structural acoustic control for payload fairings and torpedoes, and for vibration control on large flexible wings.

Further structural integrity testing is needed to fully qualify the AFC, especially for high stress flight applications. Additional data should be collected to complete the high cycle fatigue study of the structural integrity and damage tolerance of the AFC. Similarly, more data is needed on the compressive stress depolarization limits for the actuators.

In the area of modeling, improvements are needed to better predict the torsional stiffness of blade sections. Current models are able to adequately predict the actuator performance when experimentally determined torsional stiffness corrections are included. However, this is an oversimplification of the problem. Higher fidelity models are being investigated which will more accurately account for the solid core and other features beyond the composite structure. More accurate models should include the anisotropic actuation, composite structure, multi-celled beam, warping and other nonlinear effects.

The current blade manufacturing process needs to be modified in order to eliminate the defects exhibited in the model blade. Non-destructive evaluation techniques will be applied to the blade as well as several previously manufactured test articles in order to investigate the correlation between the defects and internal strain gages and AFC packs. Some additional studies may be required to pinpoint the exact cause of the core voids and delaminations. For the open circuit connections on the web, a change should be made to a higher temperature and stronger solder. To eliminate a trapped gas problem, the core can be modified to incorporate pressure release holes possibly with a vacuum suction at the outboard end of the core.

Other suggestions for a general redesign are to eliminate the high voltage connections between the packs and flex circuits along the web. Moving to a modular active ply design in which an entire layer of packs is unified with a flex circuit to be incorporated in the spar would eliminate the need for web connections and would also simplify the spar lay-up procedure. These or similar improvements will be necessary for efficient larger scale inte-
gral blades. Larger scale blades may also be able to efficiently incorporate active material in a larger portion of the cross section such as the web. This can significantly improve the twist performance of the blade. Other variations on the integral actuation concept should also be investigated for performance advantages.

The integral blade concept demonstrated in this work will be investigated further in two separate programs, one continuing with Boeing, and the other with NASA-Langley. Both will fabricate blades of similar scale to the current model. Model-scale wind tunnel testing is also planned. Higher harmonic control algorithms will be investigated to reduce the forward flight vibrations. As a part of the preparations for these tests, additional structural integrity testing will be performed on another blade section. The previous combined loads testing on a half-span blade section, described in this work, tested forward flight limit loads. The future testing will address fatigue loads as well.

Additional research effort is also ongoing in the area of high voltage power supplies and slip rings which will be required for implementing the integral actuation on a flight vehicle. Nonlinear switching amplifier technology appears to be a viable means of delivering the required voltages to the actuators with extremely high efficiencies. This will translate into low mass and power consumption. High voltage slip ring technology is also available for eventual full-scale implementation.
REFERENCES


Appendix A

BLADE DESIGN AND MANUFACTURING DETAILS

This appendix provides additional details of the blade design described in Chapter 4. Design drawings and a parts list are provided for the blade composite structure. The flex circuit design and printed circuit board interface with the hub are also described.

A.1 Blade Lay-up

This section will provide the design drawings and parts list for the integral blade. The original drawings and parts list were prepared by Doug Weems at Boeing Helicopters (Philadelphia). The drawings and parts list are intended as supplements to the manufacturing description of Chapter 5. The final subsection will describe how each of the three blade sections manufactured differed from the design of the integral blade.

A.1.1 Parts List

The following pages will present a parts list for the integral blade. Modified versions of this parts list were used for the three other half-span blade sections fabricated. The modifications will be addressed in the next subsection.

A.1.2 Design Drawings

The following pages will present several design drawings which include details of the integral blade and its fabrication.

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A.1.3 Spar Lay-up Variations for Blade Sections

This subsection will point out the differences in the designs of each of the three blade sections in comparison with the final integral blade design. The changes reflect improvements in the design resulting from the manufacturing and testing of each test article.

Blade Section 1

The first blade section manufactured was the first attempt at manufacturing the active spar of the model scale CH-47D blades. To simplify the manufacture, the section was designed to be two packs long in the spanwise direction, with inboard and outboard filler plies at either end. The root of the blade was not included. Other than the ply changes at the ends of the packs, the other spar plies represented the typical section of the blade. A total of 4 AFC packs were incorporated in the section, only in spar ply 2. For plies 4 and 6, the active plies were substituted with single ±45° E-glass plies. No strain gages or leading edge weights were included. The flex circuit and fairing were not included.

In this first blade section, the inboard and outboard filler plies consisted of inboard and outboard IM7 graphite in plies 2 and 4, oriented at +45° and -45°, respectively. No filler plies were used in ply 6. The web plies were placed on the foam core before the graphite in ply 8. In the final design, the graphite was applied first so that the web E-glass plies would provide insulation between the conductive graphite and the packs. The blade section suffered electrical failures when the copper exposed on the edges of the packs along the aft edge of the spar laminate shorted to the exposed graphite along the same edge.

Blade Section 2

This blade section incorporated several design changes and moved closer to the final blade design. The root was included, as well as all three active plies. Only 2 spanwise pack groups were incorporated once again. A tip fitting was added to enable tensile testing. The graphite unidirectional ply, SP8, was applied first to be inside the web plies. The graphite filler plies were trimmed back 0.25 inch from the web where pack edges were present. The goal was to eliminate any conductive graphite from the aft edge of the spar
lamine. This blade section also incorporated 6 internal strain gage bridges. The leading edge weights included a continuous 40 mil, segmented 40 mil, and continuous 20 mil diameter tungsten rod spanning from BS12.966 to the outboard end of the section. Once again, the flex circuit and fairing were not included.

**Blade Section 3**

The third and final blade section made additional design improvements over the previous sections and also included the flex circuit and fairing cures. The graphite filler plies at the ends of the active plies were replaced with a combination of S-glass and E-glass plies. A +45° and -45° S-glass ply were used in place of the graphite in plies 2 and 4. Because of the reduced torsional stiffness, 2 additional E-glass ±45° plies were added as filler plies in SP6. In addition, an insulative E-glass perimeter was built into the packs used in this blade section to reduce the risk of interpack dielectric breakdown. Reinforcing copper strips were also added to the packs to improve the connections on the web. No strain gages were included in this blade section. The tip fitting for the test machine mounting was again included.

The flex circuit was bonded to the spar by first soldering the electrical connections, as with the integral blade. Then, an epoxy resin, Shell Epon 828 and 3223, was injected into the gap between the flex circuit and web. The RTV mold was then used to apply pressure for the room temperature cure of the epoxy.

**A.2 Blade Strain Gages**

The following pages will present lay-out drawings used for installing the internal strain gages on the foam core of the integral blade. Four views of the blade are included: upper, lower, leading edge, and trailing edge. The latter two drawings include only the root section as there is only one strain gage involved. The drawings of the upper and lower surfaces of the spar are divided spanwise onto two separate foldout sheets.
The drawings show the placement and wiring for the twelve internal gages. This includes chordwise bending gages, flapwise bending gages, and torsional gages distributed along the span of the blade spar. The wiring includes connections between each gage in the full bridge to a terminal strip and then the connection from the terminal strip to the wire harness at the heel of the spar. The wire harness extends within the foam core and exits at the root. The wiring is color coded to indicate the position of each strain element in the wheatstone bridge. The procedure for installation was provided in Chapter 5.
TRAILING EDGE (WEB)

LEADING EDGE

CB 0.196R

Flap Bending

Chordwise Bending

Torsion

Solder Pad

red

black

green

Integral Blade
Strain Gage Installation

File: gage locations  Rev. Final

Drawn by: John Rodgers

2/3/96  To Scale  Sheet 1
Nose Block Assy
(Not to Scale - Weight Diameter Exaggerated for Clarity)

Airfoil Leading Edge

Root Block

Root Fairing Core (Ref)

Fairing Core (Ref)

Power Bus
(Not to Scale - Thickness Exaggerated for Clarity)
A.3 Power Distribution System

This section will provide additional details about the power distribution system including the design drawings for the flexible circuit and photographs of the printed circuit board connector.

A.3.1 Flexible Circuit

The flexible circuit was used to deliver high voltage power to the AFC packs embedded in the integral blade. The inboard end of the circuit interfaces with the printed circuit board (PCB) connector at the hub. The flex circuit is mounted on the aft face of the spar, along the web. Each of the 84 individual leads within the flex circuit terminates in a solder pad which interfaces with a particular pack electrode contact.

The flex circuit consists of 6 layers, each having 14 leads. Each layer has a 0.7 mil thick copper trace sandwiched between 1 mil kapton insulating layers. An epoxy adhesive was used to join all of the layers. The individual copper lines were 10 mil wide and had a 15 mil gap between lines. The flex circuit was 0.6 inches wide in the region where solder pads were located. Inboard, the flex circuit tapered to 0.4 inches and then widened at the PCB interface. Here the copper traces were increased to 50 mil in width and the lines were spaced 0.100 inches on centerline, as shown below. The flex circuit was designed in AutoCAD\(^1\) and was fabricated at All Flex, Inc. The following pages will provide drawings of the flex circuit for reference. First, a wide view of the six flex circuit layers is shown in place along the web of the spar, with the upper and lower spar surfaces unwrapped. The termination of each layer is visible. A close-up of the flex circuit root is also shown where the 14 lines of each layer fan out to interface with the 0.100 inch centerline spacing of the connector. Next, a fold-out page present a close-up of the outboard end of the flex circuit, including labels. Finally, another fold-out page shows the flex circuit near the root of the blade.

---

1. AutoCAD, Autodesk Corp., San Rafael, CA
A.3.2 Printed Circuit Board Connector

A two-sided printed circuit board was used as an interface between the 6-layer flexible circuit in the integral blade and a 5-pin AMP Mate-N-Lok high voltage connector on the hub of the hover test stand. The circuit board connects each of the 84 pack leads within the flex circuit to the appropriate pin of the high voltage connector. The 6-flex circuit layers terminate in individual 14-pin, single row socket connectors which plug into 6 through-pin headers, separated by 0.25 inch each. The headers are soldered to the board. The 5-pin high voltage connector is mounted flush with the lower end of the board to mate with the connector in the leads shell. The board was potted in epoxy to improve the high voltage capability. The pin spacing was only 0.100 inch. On the side opposite the 14-pin connectors, which faces radially outward, the potting epoxy was also used as a structural interface. The epoxy was molded to match the cylindrical contour of the leads shell. The flex circuit layers face initially inward and immediately turn 90° upward toward the pitch shaft en route to the blade root. An aluminum clip locks the PCB connector into the mating 5-pin connector.
A.4 Blade Section Tip Fitting

The second and third blade sections incorporated a tip fitting designed to interface with the grips of an Instron 8500 tensile testing machine. The inboard end of the part matches the contour of the spar core while the outboard end has a 0.25 inch thick rectangular cross section. The part was machined in the Laboratory for Manufacturing Productivity at MIT on a CNC machine. The material used was a standard aluminum of type 6061 or 2024. The machine code for the part was developed using a ProEngineer\(^1\) model. The part drawings are shown on the following page.

---

1. ProEngineer, Parametric Technology Corp., Waltham, MA
1/2" BALL MILL USED TO OBTAIN CURVED SURFACE.

CORNER OF 1/4" PIECE IS SET AS 0.0 FOR CNC.

<table>
<thead>
<tr>
<th>DRAWING</th>
<th>DR.</th>
<th>DATE</th>
</tr>
</thead>
<tbody>
<tr>
<td>CNC-1</td>
<td>WCF</td>
<td>3-24-97</td>
</tr>
</tbody>
</table>

SCALE: 1.000
## A.5 Pack Manufacturing Checklist

<table>
<thead>
<tr>
<th>Check</th>
<th>No.</th>
<th>Step</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td></td>
<td>Clean cure plate</td>
</tr>
<tr>
<td>2</td>
<td></td>
<td>Apply mold release (if necessary)</td>
</tr>
<tr>
<td>3</td>
<td></td>
<td>Tape GNPT to plate after cutting with template</td>
</tr>
<tr>
<td>4</td>
<td></td>
<td>Clean electrodes</td>
</tr>
<tr>
<td>5</td>
<td></td>
<td>Bond copper strips to electrode flaps</td>
</tr>
<tr>
<td>6</td>
<td></td>
<td>Align and tape electrodes to plates</td>
</tr>
<tr>
<td>7</td>
<td></td>
<td>Defrost E-glass and place around perimeter of lower electrode</td>
</tr>
<tr>
<td>8</td>
<td></td>
<td>Cover copper with GNPT and kapton tape mold around/over E-glass</td>
</tr>
<tr>
<td>9</td>
<td></td>
<td>Cut vent holes in electrodes</td>
</tr>
<tr>
<td>10</td>
<td></td>
<td>Cut fibers to length batch: g</td>
</tr>
<tr>
<td>11</td>
<td></td>
<td>Wash fibers in acetone</td>
</tr>
<tr>
<td>12</td>
<td></td>
<td>Align and distribute fibers in mold</td>
</tr>
<tr>
<td>13</td>
<td></td>
<td>Attach vac bag and Unistrut to top cover</td>
</tr>
<tr>
<td>14</td>
<td></td>
<td>Preheat top cover to 60°C in oven</td>
</tr>
<tr>
<td>15</td>
<td></td>
<td>Preheat cure plate to 50°C</td>
</tr>
<tr>
<td>16</td>
<td></td>
<td>Mix epoxy</td>
</tr>
<tr>
<td>17</td>
<td></td>
<td>Add epoxy to mold and adjust fibers</td>
</tr>
<tr>
<td>18</td>
<td></td>
<td>Set Unistrut support nuts to 1.2&quot;</td>
</tr>
<tr>
<td>19</td>
<td></td>
<td>Apply vac tape to cure plate</td>
</tr>
<tr>
<td>20</td>
<td></td>
<td>Position angles or rubber around location of top cover</td>
</tr>
<tr>
<td>21</td>
<td></td>
<td>Place top cover in position</td>
</tr>
<tr>
<td>22</td>
<td></td>
<td>Lower Unistrut to 1.1&quot; and ensure alignment pin fit</td>
</tr>
<tr>
<td>23</td>
<td></td>
<td>Seal vac bag and check top plate bolts</td>
</tr>
<tr>
<td>24</td>
<td></td>
<td>Pull vacuum and hold for 5 minute degas</td>
</tr>
<tr>
<td>25</td>
<td></td>
<td>Lower Unistrut 1/2 turn at a time until no load on nuts</td>
</tr>
<tr>
<td>26</td>
<td></td>
<td>Tighten clamping nuts</td>
</tr>
<tr>
<td>27</td>
<td></td>
<td>Release vacuum</td>
</tr>
<tr>
<td>28</td>
<td></td>
<td>Cure (check temp. sensor)</td>
</tr>
<tr>
<td>29</td>
<td></td>
<td>Release top cover and remove pack from cure plate</td>
</tr>
<tr>
<td>30</td>
<td></td>
<td>Trim and peel back copper strips</td>
</tr>
<tr>
<td>31</td>
<td></td>
<td>Measure: ( t = \text{mm} \quad C = \text{nF} \quad m = \text{g} )</td>
</tr>
<tr>
<td>32</td>
<td></td>
<td>Electrode alignment =</td>
</tr>
<tr>
<td>33</td>
<td></td>
<td>Solder lead wires</td>
</tr>
<tr>
<td>34</td>
<td></td>
<td>Pole @ ( V \quad ^\circ C \quad \text{min.} )</td>
</tr>
<tr>
<td>35</td>
<td></td>
<td>Characterize-Rep Cycle: ( C = \text{nF} \quad V_{dc} \quad V_{pp} )</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Microstrain: 1Hz: 10Hz: 100Hz:</td>
</tr>
<tr>
<td>36</td>
<td></td>
<td>Proof Test ( V_{min} \quad V_{max} \quad \mu \varepsilon )</td>
</tr>
<tr>
<td>37</td>
<td></td>
<td>Prepare electrode flaps for blade</td>
</tr>
<tr>
<td>38</td>
<td></td>
<td>Clean pack for embedding</td>
</tr>
</tbody>
</table>
A.6 Blade Mold Modification

The aluminum blade mold was modified to allow the flexible circuit to pass from the web of the blade to the outer edge of the mold near the root. To accomplish this, a 2-dimen-
sional CAD drawing of the mold layout was made. The required trough for the flex circuit was added to the drawing on both the upper and lower mold halves. The lead-lag pin hole at the root of the mold was used as a reference point. The coordinates were then converted to machine code for cutting automatically with an end mill. A 1/16th inch end mill was used. Several passes were made to achieve a depth of 0.3 inches. The edges of the groove on each side of the mold were then chamfered to ensure proper mating of the mold halves on the flex circuit. The CAD drawing is shown on the next page.
A.7 Integral Twist Actuation Scaling

In order to obtain a preliminary estimate of the actuator authority required for integral twist actuation on various blade types, a simple scaling law has been developed. The scaling law can be used to estimate the volume of actuator material required for a rotor blade of a given size, as compared with the 1/6th Mach-scale CH-47D used in this thesis.

Several assumptions are applied in this development. First, the objective of the design is assumed to be equal tip twist in comparison with the current integral blade. Secondly, the distribution of the active plies is uniform spanwise and around the spar contour. Finally, this estimate is based on a single-cell model of the blade spar. Thus, it neglects multi-cell and other more complex effects on the twist actuation.

The following parameters are used in the scaling law development:

- GJ - torsional stiffness (typical section)
- R - active ply length (spanwise)
- s - perimeter of active material in spar contour
- z - average radius used in moment calculation (based on spar geometry)
- t - active ply thickness (total of all plies)
- \( E_{e33} \) - piezo-induced stress
- \( M_t \) - torsional moment
- \( \phi \) - tip twist
- ( ) - relative scale factor for given parameter

The calculation is based on a simple formulation of a rod in torsion:

\[
\phi = \frac{(M_t R)}{(GJ)}
\]

For equal tip twist, the induced moment will scale as GJ/R,

\[
(M_t) = \left(\frac{(GJ)}{R}\right)
\]

where the parentheses indicate a scaling or proportional relationship. An approximate definition of the induced moment in a single-cell beam is:
\[ M_t = \oint_{s} t_z t E e_{33} ds \]

In terms of the scale factors,

\[ (M_t) = (z)(t)(s) = (GJ)/(R) \]

The total volume of active material is then,

\[ V_{AFC} = tsR \]

or,

\[ (V_{AFC}) = (t)(s)(R) = (GJ)/(z) \]

This relation can then be used to estimate the total volume of active material required to achieve the same tip twist as the current integral blade. The only required parameters are the ratio of the torsional stiffnesses and a cross-sectional geometry scale factor.
Appendix B

HOVER TEST STAND AND ELECTRONICS

This appendix will provide additional details regarding the MIT Hover Test Stand Facility and the particular equipment used to collect the hover data provided in Chapter 7. This also includes the data acquisition and the high voltage amplifier developed specifically for the integral blade.

B.1 Test Stand Design

The test stand was designed\(^1\) to spin 1/6th Mach-scale CH-47D blades in air at 1336 RPM. The system is capable of operating with 3 blades at maximum blade loading conditions. The current configuration includes only a two-bladed hub. The testing room also constrained the design because of the relatively low ceiling and floor vibrations. The next section will summarize the major systems of the test stand. The second section will provide a cross-section drawing of the test room in the shorter dimension described in Chapter 7.

B.1.1 Capabilities

This section will provide a brief overview of each of the major subsystems of the test stand. These include the rotating frame components or hub, the sliprings, the motor, the structure and isolation system, and the containment barriers.

\(^1\) Design team consisted of Eric Prechtl, Paul Bauer, and John Rodgers with additional input from faculty, other students, and Boeing representatives
Hub

The hub consists of all parts in the rotating frame between the main shaft and the blades. The hub is shown in the photograph on the next page. The parts will be described working upward from the shaft. First, the shaft interfaces with the shaft-load cell coupling plate. This plate is designed to interface with the load cell, which will be described in detail in the next section. The load cell is mounted between this plate, and the hub-load cell interface plate. Thus the load cell is sandwiched between two stiff plates to ensure that the loads transferred are uniform. A leads shell, mounted to the shaft-load cell coupling plate, surrounds the load cell and houses all rotating frame connectors. The top of the hub-load cell interface engages the blade hub, which houses bearings for the flapping pins.

The pitch shaft assemblies mount to the hub through the flapping pin. The outer end of the pitch shaft assembly interfaces with the blades through the lead-lag pin. The pitch shaft assembly is designed to enable manual adjustment of the blade collective pitch. On the side of the passive blade used in the testing of the integral blade, additional mass had to be added to balance the centrifugal loads. The pitch shaft assembly shown in the photo on the next page includes an oversized key clamp and additional steel ring to achieve the necessary balance. The pitch shaft assemblies were also instrumented with strain gages to serve as safety of flight monitors during testing.

A rotating frame camera is mounted above the hub, pointing towards the active blade. This camera uses an RF link to transmit the video signal to the control room. Power is supplied to the camera and transmitter through the slip rings.
**Slipring Assembly**

The slipring assembly consists of the main shaft, an upper and lower bearing, and the slip rings. The main shaft engages the motor through a gear coupling which allows for axial and angular misalignment. The upper bearing restrains radial and axial motion, while the lower bearing restrains only radial motion to allow for thermal expansion. The inner race of the sliprings interface with the main shaft. The rotating frame leads go through a hollow bushing in the upper bearing to reach the leads shell. The outer portion of the sliprings is locked to the stand frame. The entire slipring assembly is housed in a steel tube structure and is modular in design. The main shaft, bearings, hub, and sliprings can be removed from the stand frame as a unit. A mechanical drawing is provided on the following page.

The sliprings\(^1\) were custom designed and have 138 signal channels and 24 high voltage channels. The high voltage channels can tolerate 8kV between rings. Each of the rings is engaged with two pairs of nonrotating brushes. The sliprings are totally enclosed and are air-cooled.

**Motor**

The motor was selected to provide the necessary torque and power for hover at 1336 RPM. A direct coupling design was selected which placed an additional length constraint on the motor. This was necessary to maximize the distance between the rotor plane and the ceiling of the test room. For this reason, an AC powered motor\(^2\) was selected. The 150 HP motor has a base speed of 1200 RPM but can be driven up to 1500 RPM. A flux vector control system\(^3\) enables 0.1% speed control accuracy. The accuracy can be further improved using tachometer feedback.

---

1. Ref. No. SK1015A, Fabricast, Inc., So. El Monte, CA
2. Lincoln Electric 150HP ODP Motor, Cleveland, OH
3. IDM Powermaster, Houston, TX
Structure and Isolation

The stand structure was designed to avoid resonances within the rotor and actuation frequency bandwidth, roughly from 10 Hz to 150 Hz. A modal analysis was performed using ANSYS\(^1\) software. The stand has a pyramid structure composed of square steel tubes. The upper and lower halves are welded subassemblies which are bolted together through the lower legs. The vertically oriented motor is supported at the bottom through its base and also through a face mount at the top using I-beams. A modular slipring assembly consisting of bearings, shaft, and steel tube housing bolts into the upper frame. The entire test stand is supported on air isolators to attenuate the transmission of floor vibrations present in the building to the stand. Torsional restraints and dampers are also included. A ground resonance analysis was performed to ensure stability of the system. The design drawings for the stand structure are presented on the following pages.

The stand dynamics were evaluated using accelerometers mounted on the top of the stand frame, near the upper bearing. The isolator modes (bouncing and rocking) were between 2 Hz and 7 Hz and had approximately 8% damping. Vibration data were also collected at 1336 RPM in the same location. The acceleration is plotted below as a function of the frequency relative to the rotor speed. The in-plane or horizontal vibrations, shown in the left-hand plot, were much larger than the vertical axis vibrations during the tests. The largest vibrations were excited near 3/rev with a magnitude of approximately 0.7g. The damping ratio measured for the structural modes was approximately 0.5%.

---

1. ANSYS, Jordan, Apostal, Ritter Associates, Inc., N. Kingstown, RI
Containment

In the event of a rotating component of the test stand, two layers of containment structure were installed. First, a 14 foot diameter containment ring surrounds the rotor plane. The ring consists of 0.375 inch steel with an inner 0.5 inch plywood lining to absorb ricochets. The ring extends 2 feet above and below the rotor plane. The containment ring was designed for worst case impact loads from a projectile thrown from a rotating blade. A second safety was was positioned between the test room and control room consisting of 0.125 inch thick steel and a lexan window.
B.1.2 Test Room Cross Section
B.2 Instrumentation

This section will describe some of the electronics used during the hover testing. First, a brief description of the load cell and strain gages will be provided. Then the data acquisition system will be discussed. Finally, the design of the high voltage amplifier is presented. The complete control room with supporting electronics is shown in the photo below.

![Test room window with data acquisition system and amplifier](image)

B.2.1 Sensors

The load cell used was used as a primary sensor for the evaluation of the integral blade in hover. The load cell provided data for three forces and three moments transferred between the blade hub and the main shaft of the test stand. The cylindrical shaped load cell was mounted between 2 inch thick steel plates which interfaced with the shaft and hub.

The JR3 load sensor\(^1\) was designed to provide ±10V outputs for each of the three forces and moments. The signals are conditioned in the rotating frame. The sensitivity of each

---

1. JR3, Inc., Model No. 75E30-E45B 1350L10125, Woodland, CA
channel and zero point were also selected based on the maximum expected loads in hover. The range for the in-plane forces was ±300 lbs. For the vertical force, the range was -500 to 2000 lbs. For the pitch and flapping moments, the range was ±3000 in-lbs. For the lag moment, the range was -9600 to 2400 in-lbs. The manufacturer provided a load calibration matrix for the sensor. For the primary performance metric, the vertical hub shear, the sensor output was approximately 164.1 lbs./volt.

The strain gage signals are conditioned at the input to the data acquisition system. A low pass filter was applied at the input to each channel. Common excitation was used for the blade strain gages and the pitch shaft gages. The system was also designed to include Hall effect transducers on the flapping and lead-lag pins. These were not operational during the current study. Accelerometers were also used to measure stand vibrations during the testing. Rotating and nonrotating video images were available as safety and performance monitors.

B.2.2 Data Acquisition System

The data acquisition system consists of a 200 MHz Pentium-based computer and a National Instruments data acquisition card\(^1\). The card receives a single multiplexed input from an SCXI interface with a chassis\(^2\) containing several input modules. Three 8-channel analog input modules\(^3\) are used for preconditioned load cell and other sensor inputs. Six 4-channel strain gage modules\(^4\) were used for strain gage bridges mounted in the integral blade and on the pitch shaft assemblies. The time between channel reads during the acquisition was set to 2.5 microseconds, the minimum allowable settling time. Time trace data was collected at scan rates from 1 to 2 kHz. Data were streamed directly to the hard disk. This data was later postprocessed in MATLAB. The majority of the transfer function data

---

1. National Instruments, AT-MIO-16E-1, Austin, TX
2. National Instruments SCXI-1001
3. National Instruments SCXI-1120
4. National Instruments SCXI-1121
was analyzed in real-time as described in Chapter 7, eliminating the need for large data files.

**B.2.3 High Voltage Amplifier**

The high voltage amplifier was designed to provide the required 4kV for the AFC packs with 1 Amp of current output. Four channels were required. The bandwidth was approximately 10 Hz to 150 Hz. Two of the channels were driving an approximate capacitive load of 40 nF and the other two were 20 nF. The system consisted of a high power audio amplifier\(^1\) and 25:1 voltage transformer\(^2\) for each channel. A single DC power supply\(^3\) was used to provide the DC offset for all 4 channels. A large 1 μF capacitor was used to prevent current flow to the DC supply. The wiring diagram is provided on the next page.

Because of the high power capability of the amplifier system, several precautions were taken to limit both the voltage and current outputs. Voltage limiting transorsbs were applied across the outputs to limit the output voltage to a desired level. These diode pairs essentially draw a large current when the design voltage is exceeded, preventing any increase in voltage. A large current draw is a greater risk for the integral blade in the event of a dielectric breakdown failure of the actuators. The second blade section was damaged when a breakdown occurred and the amplifier provided a large current across the arc. To address this, assive fuses were used in each output channel and electronic circuit breakers\(^4\) were added. The fast-acting circuit breakers could be tuned to a desired maximum current level. The circuits were modified\(^5\) for the desired voltage and current levels. The circuit breakers were placed on the primary side of the transformers while the fuses provided a back-up on the secondary.

---

1. Yorkville Audiopro 3400, Pickering, Ontario, Canada
2. TECO, Winnisquam, NH
3. Model 3000R, Gencom Division, Emitronics, Inc, Plainview, NY
4. NASA Tech Brief, LEW-16027, "Bidirectional Electronic Circuit Breaker"
5. Modified by Paul Bauer and Kamyar Ghandi, MIT
Appendix C

ACTUATION MODEL

This appendix presents the modified Rehfield beam model used to predict the twist performance of the integral blade in benchtop testing. The MATLAB code is presented in the first section and the input files are presented in the second section.

C.1 Modified Rehfield Model Code

This section presents the modified Rehfield beam model applied to the integral blade. The model theory, which is described in Chapter 4, was developed in a previous study [du Plessis, 1995]. As a part of the current research, some minor modifications were incorporated into the MATLAB code. The complete code is presented in this section. The primary subroutines are elem.m, used for the 2-D sectional analysis, and beam.m, used for the 1-D beam finite element formulation. Further details of the code can be found in a previous publication [du Plessis, 1996].

ELEM.M

% % Composite Box Beam Analysis - Element % % ELEM do the analysis of a composite single cell box beam element. % The analysis consists of mass, cg, pitch inertia as well % as a finite element structural matrix. Properties may vary around the % circumference of the beam and any cross sectional shape can % be handled. The cross sectional shape is built from linear % pieces(panels) and in every panel the properties must be constant. % The result from this m-file is the cross sectional properties for
clear;
clc;
format short e;
printout=0;

%----- Read Input ---------------------------------------------
inputf = input('What is the name of the input file? (No extensions!) :
'
';'s');
fout = sprintf('%s.out',inputf);
idout = fopen(fout,'wt');
eval(inputf);

%----- Input Set Printing
if printout
fprintf(idout,'Input File: %s
',inputf);
fprintf(idout,'--------

');
fprintf(idout,'Cross Section
');
fprintf(idout,'--------

');
fprintf(idout,' Y Z

');
fprintf(idout,' %6.1f %6.1f
',yz);
fprintf(idout,'
');
fprintf(idout,'Materials
');
fprintf(idout,'--------

');
for i = 1:m,
    for j = 1:n,
        fprintf(idout,'%2d ',mat(i,j));
    end
    fprintf(idout,'
');
end
fprintf(idout,'
');
fprintf(idout,'Thicknesses
');
fprintf(idout,'--------

');
for i = 1:m,
    for j = 1:n,
        fprintf(idout,'%7.5f ',thick(i,j));
    end
    fprintf(idout,'
');
end
fprintf(idout,'
');
fprintf(idout,'Angles of Plies
');
fprintf(idout,'--------

');
for i = 1:m,
    for j = 1:n,
        fprintf(idout,'%5.1f ',ang(i,j));
    end
    fprintf(idout,'
');
end
fprintf(idout,'
');
fprintf(idout,'Voltages Top & Bottom Plies\n');
fprintf(idout,'-----------------------------------\n');
for i = 1:m,
    for j = 1:2*n,
        fprintf(idout,'%5d ',volt(i,j));
    end
    fprintf(idout,'
');
end
fprintf(idout,'
');
fprintf(idout,'Distributed Forces\n');
fprintf(idout,'---------------------\n');
fprintf(idout,'%4.1f %4.1f %4.1f %4.1f %4.1f %4.1f
',q);
fprintf(idout,'
');
end
%----------------------------------------------------------
%------ Cross Sectional Properties ------------------------
%----------------------------------------------------------
%------ Geometric Properties of Panels and Cross Section ------
%------ Lengths of linear panel pieces
leng = zeros(m,1);
for i = 1:m,
    leng(i) = sqrt((yz(i+1,1)-yz(i,1))^2+(yz(i+1,2)-yz(i,2))^2);
end
%------ Derivatives of linear panel pieces
deriv = zeros(m,2);
for i=1:m,
    deriv(i,1) = (yz(i+1,1)-yz(i,1))/leng(i);
    deriv(i,2) = (yz(i+1,2)-yz(i,2))/leng(i);
end
%------ Normal projections of the linear panels
normp = zeros(m,1);
for i=1:m,
    normp(i) = -yz(i,1)*deriv(i,2)+yz(i,2)*deriv(i,1);
end
%------ Cross section perimeter length
c = sum(leng);
%------ Cross sectional area
A=0;
for i = 1:m,
    dA = 0.5*normp(i)*leng(i);
    A = A + dA;
end
% Physical Properties of the Panels and Cross Section

% Panel mass/unit length (rho*thickness - mass/unit area)
pmassl = zeros(m,1);
for i = 1:m,
    for j = 1:n,
        if thick(i,j)>0
            rho = matprop(2,mat(i,j));
            mul = rho*thick(i,j);
            pmassl(i) = pmassl(i) + mul;
        end
    end
end
mul= 0;

% Cross sectional mass per unit length
mass = leng'*pmassl; % Based on a per unit length piece of the beam
nscg=0;
nmass=0;
for i=1:size(nonspar,1)
    nscg=(nsmass*nscg+nonspar(i,11)*nonspar(i,12))/(nsmass+nonspar(i,11));
    nsmass=nsmass+nonspar(i,11);
end
% Cross Section CG (Relative to Neutral Axis)
sumcg =0;
for i=1:m,
    dgc = leng(i)*pmassl(i)*(yz(i+1,1)+yz(i,1))/2;
    sumcg = sumcg+dgc;
end
CG = sumcg/mass;
CG=(mass*CG+nsmass*nscg)/(mass+nsmass);
% Cross Section Pitch Inertia (Relative to Neutral Axis)
% Based on a unit length of the beam
Iner = 0;
for i = 1:m,
    dI = leng(i)*pmassl(i)*(((yz(i+1,2)+yz(i,2))/2)^2+((yz(i+1,1)+yz(i,1))/2)^2) + leng(i)^3*pmassl(i)/12;
    Iner = Iner+dI;
end
for i=1:size(nonspar,1)
    Iner=Iner+nonspar(i,11)*nonspar(i,12)^2;
end
mass=mass+nsmass;
cprop = [A mass CG Iner];

% Stiffness and Actuation Properties of Panels and Cross Section
% Panel laminate properties
% Panel plate stiffness and stress actuation properties
pstiff = zeros(m,6);
pstract = zeros(m,3);
pGeff = zeros(m,1);
for i = 1:m,
  k = 0;
  layup = [ ];
  prop = [ ];
  E = [ ];
  for j = 1:n,
    %-- Exclude the layers with zero thickness/properties etc.
    if thick(i,j) > 0
      k = k+1;
      layup(k,1) = ang(i,j);
      prop(k,1) = thick(i,j);
      prop(k,2:7) = matprop(1,mat(i,j));
      prop(k,8:9) = matprop(3,mat(i,j));
      %------ Be sure to use the right thicknesses (eg: IDEPFC) for
      Field E
      E(k,1) = (volt(i,2*j-1)-volt(i,2*j))/((45e-3)*0.0254);
    %thick(i,j);
    end
  end
end

[L,P,Geff] = panstif(prop,layup); % Can rewrite this not to transfer
[L]
  pstiff(i,1:6) = [L(1,1) L(1,2) L(1,3) L(2,2) L(2,3) L(3,3)];
  pstract(i,1:3) = (P*E)';
  pGeff(i) = Geff;
end

%------ Warping Function for the Cross Section -------------------------
%------ Warping specified at the coordinates around the contour
[psi,alf]=warpfunc(A,c,leng,normp,pGeff);

%------ Cross sectional stiffness
ord = 4; %------ Order of guassian integration
[Kc,Fac] = Cstiff(yz,leng,deriv,A,pstiff,pstract,ord,psi,alf);
%------ Add Contributions from Non-Spar Elements
Kns=zeros(7);
if size(nonspar,1)==0
  for i=1:size(nonspar,1)
    Kns=Kns+[nonspar(i,4) zeros(1,6);
          0 nonspar(i,10) 0 0 0 0 0;
          0 0 nonspar(i,10) 0 0 0 0;
          0 0 0 nonspar(i,9) 0 0 0;
          0 0 0 0 nonspar(i,7) 0 0;
          0 0 0 0 0 nonspar(i,8) 0;
          zeros(1,7)];
  end
Kc=Kc+Kns;
end
sparprop=[mass CG*xna*.0254 Kc(1,1) Kc(2,2) Kc(3,3) Kc(4,4) Kc(5,5)
          Kc(6,6) Fac(4) Fac(4)/Kc(4,4)*180/pi]'
\% Element Property Output
\%----------------------------------
\%----------------------------------
fprintf(idout,'Cross Sectional Properties\n');
fprintf(idout,'\n');
fprintf(idout,'Area [m^2]: %7.4f\n',A);
fprintf(idout,'Perimeter Length [m]: %7.4f\n',c);
fprintf(idout,'Mass [kg/m]: %7.4f\n',mass);
fprintf(idout,'Pitch Inertia [kgm^2]: %10.6f\n',Iner);
fprintf(idout,'CG [m]: %7.4f\n',CG);
\%----------------------------------
\%----------------------------------
\% Element Stiffness
\%----------------------------------
\%----------------------------------
\% Integration to obtain element stiffness matrix
ord = 4; \% Order of Gaussian integration (2<n<5)
Kens = nselemstif(dx,Kns,ord);
Ke = elemstif(dx,xyz,leng,deriv,A,pstiff,psi,alf,ord)+Kens;
\%----------------------------------
\%----------------------------------
\% Element Electrical Forcing
\%----------------------------------
\%----------------------------------
\% Integration to obtain electrical forcing
ord = 4; \% Order of Gaussian integration (2<n<5)
Fa = elecmech(dx,xyz,leng,deriv,A,pstract,psi,alf,ord);
\%----------------------------------
\%----------------------------------
\% Distributed Loading
\%----------------------------------
\%----------------------------------
\% Integration of the distributed loading to obtain nodal loads
ord = 3; \% Order of Gaussian integration (2<n<5)
Fq = distrib(dx,q,ord);

\% BEAM.M
\%----------------------------------
\% Composite Box Beam Analysis
\%----------------------------------
\% BEAM do the analysis of a composite single cell box beam.
\% The analysis consists of mass, cg, pitch inertia as well
\% as a finite element analysis to determine displacements given
\% a certain load condition. Properties may vary around the
\% circumference of the beam and any cross sectional shape can
\% be handled. The cross sectional shape is built from linear
\% pieces(panels) and in every panel the properties must be constant.
\% The elements used in the analysis constructed earlier by the m-file
\% "elem.m"
%----------------------------------
\% Author:Andries J. du Plessis
\% Original:March 29 1994
clear;
cif;
clic;
%
%
% Define the beam ---------------------------------------------
%------------------------------------------------------------------------
% [Element-Names]
% elements = ['integral0';
%    'integral1';
%    'integral2';
%    'integral3';
%    'integral4';
%    'integral5';
%    'integral6';
%    'integral7';
%    'integral8';]; %Input file names
% Should use metric (mks)--check mat prop definitions, and E-field calc!
% [Beam length for specific element Element length];
def = [.42 .02; .10 .02; .16 .02; .16 .02; .16 .02; .16 .02; .16 .02; .16 .02; .16 .02;];

numel = size(elements,1); % Number of different element types used in analysis
n = def(:,1)/def(:,2);
%---- Point Loads at the end of the beam
Fp = [0 0 0 0 0 0 0]; %----[N Qy Qz Mx My Mz Qw]
%---- Distributed Loads along beam (Air Loads and Centripetal Forces)
loadfile='loads'; % file containing load tables
loadopt=5; % 1=MaxUlt, 2=MinUlt, 3=Stdy, 4=Alt, 5=Hover
loadscale=[0 0 0 0 0 0 0]; % scale factors for applied loads
x0=0.0; % unmodeled length from root

% Construct spanwise location vectors for analysis ---------------------
%------------------------------------------------------------------------
nod = 1;
x = [0];
for i = 1:numel,
    for j = 1:n(i),
        if nod == 1
            xi = 0;
            xe = def(i,2);
            xp = [xi;xe];
            x = [x;xe];
            nod = nod + 1;
        else
            xi = xi+def(i,2);
xe = xe+def(i,2);
xp = [xp; xi; xe];
x = [x; xe];
nod = nod+1;
end
end

%--------------------------------------------------------------------
%----- Read element properties and construct global variables -------
%--------------------------------------------------------------------
%----- Global variables
%
%      Prop - matrix with mass/length, CG, etc.
%          K - Stiffness matrix
%          F - Element forcing
Prop = zeros(length(xp),4);  % - [Area, mass/length, CG, pitch inertia]
K = sparse(7*nod,7*nod);
F = zeros(7*nod,1);
Equilibrium=[-1 0 0 0 0 0 0 0; 0 -1 0 0 0 0 0 0; 0 0 -1 0 0 0 0 0;... 0 0 0 -1 0 0 0 0; 0 0 0 0 -1 0 0 0; 0 0 0 0 0 -1 0 -1 0;... 0 0 0 0 0 0 0 0];  %Equil.Eqs.

nodc1 = 1;
nodc2 = 14;
pc = 1;
for i = 1:nunel,
    fin = sprintf('%s',deblank(elements(i,:)));  % Inputfile
    eval(['load ' fin]);
    %*****************************************************************************
    if dx ~= def(i,2)
        error('Specified element length does not correlate with length read');
    end
    for j = 1:n(i),
        Prop(i,j,:) = [cprop; cprop];
        K(nodc1:nodc2, nodc1:nodc2) = K(nodc1:nodc2, nodc1:nodc2) + Ke;
        [qd] = loadinterp((x((nodc1-1)/7+1)+x0)/(max(x)+x0),...
            (x((nodc1-1)/7+2)+x0)/(max(x)+x0), loadopt, loadfile);
        qd(:,1) = qd(:,1)/dx;  %dF/dx
        qd = loadscale.*((Equilibrium*[qd(:,1); qd(2:3,2)]));  %neglect warping
    qds = [qds qd qd];
        F(nodc1:nodc2,1) = F(nodc1:nodc2,1) + Fq + Fa + distrib(dx, qd, 3);
        nodc1 = nodc1+7;
        nodc2 = nodc2+7;
        pc = pc+2;
    end
end
rcs = (nod-1)*7+1;
F(rcs:7*nod,1) = F(rcs:7*nod,1) + Fp;  %----- Add the point loads

%--------------------------------------------------------------------
%----- Force Displacement Analysis ----------------------------------
%----- Remove the constraints for the cantilevered beam

uu = K(8:7*nod, 8:7*nod) \ F(8:7*nod, 1); % Static Solution
u = [0; 0; 0; 0; 0; 0; 0; uu];
U = zeros(nod, 1);
V = zeros(nod, 1);
W = zeros(nod, 1);
Phi = zeros(nod, 1);
By = zeros(nod, 1);
Bz = zeros(nod, 1);
Phi_x = zeros(nod, 1);
W_x = zeros(nod, 1);
for i = 2:nod,
    U(i) = u(7*(i-1)+1, 1);
    V(i) = u(7*(i-1)+2, 1);
    W(i) = u(7*(i-1)+3, 1);
    Phi(i) = u(7*(i-1)+4, 1);
    By(i) = u(7*(i-1)+5, 1);
    Bz(i) = u(7*(i-1)+6, 1);
    Phi_x(i) = u(7*(i-1)+7, 1);
    W_x(i) = (W(i)-W(i-1))/(x(i)-x(i-1));
end

CSTIFF.M

function [Kc, Fac] = Cstiff(yz, leng, deriv, A, pstiff, pstrain, n, psi, alf);
% Cstiff
%
% Cross sectional stiffness
%
% Cstiff: function that do the integration to obtain the cross section
% stiffness. Integration is done with Gauss Quads with variable
% order of integration.
%
% A - Area
% pstiff - reduced plate stiffness
% pstrain - reduced induced stress actuation
% psi - warping function description
% alf - extra constants used in warping function description
% n - order of the gaussian integration
%
% Author: Andries J. du Plessis
% Date: March 31 1994
% Modified: October 12 1995
% Copyright: MIT-SERC-AMS (c) 1994
%

m = length(leng);
%------ Read Abisscissa and Weights for order n gaussian integration
AW = gausstab(n);

% -------------- Integration for the Panels
Kc=zeros(7,7);
Fac = zeros(7,1);
dF = zeros(7,1);
for i = 1:m, %------ Integration across all the panels
    J = leng(i)/2; %---- Jacobian for transformation to [-1,1]
    %------ Determine the slope and intersection on [-1,1]
    myg = (yz(i+1,1)-yz(i,1))/2;
    cyg = (yz(i+1,1)+yz(i,1))/2;
    mzg = (yz(i+1,2)-yz(i,2))/2;
    czg = (yz(i+1,2)+yz(i,2))/2;
    mpsig = (psi(i,2)-psi(i,1))/2;
    cpsig = (psi(i,2)+psi(i,1))/2;
    alfa = alf(i);
    dy = deriv(i,1);
    dz = deriv(i,2);
    L11 = pstiff(i,1);
    L12 = pstiff(i,2);
    L13 = pstiff(i,3);
    L22 = pstiff(i,4);
    L23 = pstiff(i,5);
    L33 = pstiff(i,6);
    Nxxa = pstrand(i,1);
    Nxsa = pstrand(i,2);
    Mxxa = pstrand(1,3);
for j = 1:n, %---- Gaussian integration of order n, 1-Dimensional
    y = myg*AW(j,1)+cyg;
    z = mzg*AW(j,1)+czg;
    p = mpsig*AW(j,1)+cpsig;
    dk(1,1) = L11;
    dk(1,2) = L12*dy;
    dk(1,3) = L12*dz;
    dk(1,4) = L12*(-2*A*alfa);
    dk(1,5) = L11*z+L13*dy;
    dk(1,6) = -L11*y+L13*dz;
    dk(1,7) = L11*p;
    dk(2,1) = dk(1,2);
    dk(2,2) = L22*dy^2;
    dk(2,3) = L22*dz*dy;
    dk(2,4) = L22*(-2*A*alfa)*dy;
    dk(2,5) = (L12*z+L23*dy)*dy;
    dk(2,6) = (-L12*y+L23*dz)*dy;
    dk(2,7) = L12*p*dy;
\[ \begin{align*} 
\text{d}k(3,1) &= \text{d}k(1,3); \\
\text{d}k(3,2) &= \text{d}k(2,3); \\
\text{d}k(3,3) &= L22*\text{d}z^2; \\
\text{d}k(3,4) &= L22*(-2*A*\alpha)*\text{d}z; \\
\text{d}k(3,5) &= (L12*z+L23*dy)*\text{d}z; \\
\text{d}k(3,6) &= (-L12*y+L23*dy)^2; \\
\text{d}k(3,7) &= L12*p*\text{d}z; \\
\text{d}k(4,1) &= \text{d}k(1,4); \\
\text{d}k(4,2) &= \text{d}k(2,4); \\
\text{d}k(4,3) &= \text{d}k(3,4); \\
\text{d}k(4,4) &= L22*(-2*A*\alpha)^2; \\
\text{d}k(4,5) &= (L12*z+L23*dy)^2; \\
\text{d}k(4,6) &= (-L12*y+L23*dy)^2; \\
\text{d}k(4,7) &= L12*p*(-2*A*\alpha); \\
\text{d}k(5,1) &= \text{d}k(1,5); \\
\text{d}k(5,2) &= \text{d}k(2,5); \\
\text{d}k(5,3) &= \text{d}k(3,5); \\
\text{d}k(5,4) &= \text{d}k(4,5); \\
\text{d}k(5,5) &= (L11*z+L13*dy)^2; \\
\text{d}k(5,6) &= (-L11*y+L13*dy)^2; \\
\text{d}k(5,7) &= L11*z*p+L13*dy*p; \\
\text{d}k(6,1) &= \text{d}k(1,6); \\
\text{d}k(6,2) &= \text{d}k(2,6); \\
\text{d}k(6,3) &= \text{d}k(3,6); \\
\text{d}k(6,4) &= \text{d}k(4,6); \\
\text{d}k(6,5) &= \text{d}k(5,6); \\
\text{d}k(6,6) &= (-L11*y+L13*dy)^2; \\
\text{d}k(6,7) &= L11*(-y)^2; \\
\text{d}k(7,1) &= \text{d}k(1,7); \\
\text{d}k(7,2) &= \text{d}k(2,7); \\
\text{d}k(7,3) &= \text{d}k(3,7); \\
\text{d}k(7,4) &= \text{d}k(4,7); \\
\text{d}k(7,5) &= \text{d}k(5,7); \\
\text{d}k(7,6) &= \text{d}k(6,7); \\
\text{d}k(7,7) &= L11*p^2; \\
\text{d}F(1) &= Nxxa; \\
\text{d}F(2) &= Nxxa*dy; \\
\text{d}F(3) &= Nxxa*dz; \\
\text{d}F(4) &= Nxxa*(-2*A*\alpha); \\
\text{d}F(5) &= Nxxa*z+Mxxa*dy; \\
\text{d}F(6) &= Nxxa*(-y)+Mxxa*dz; \\
\text{d}F(7) &= Nxxa*p; \\
Kc &= Kc + AW(j,2)*\text{d}k*J; \\
\text{Fac} &= \text{Fac} + AW(j,2)*\text{d}F*J; 
\end{align*} \]
ELECMEECH.M

function T = elecmech(1,yz,leng,deriv,A,pstract,psi,alf,n)
% ELECMEECH
% % Panel electro-mechanical coupling matrix construction
% % ELECMEECH: function that does the integration to obtain the coupling
% % matrices.
% % Integration is done with Gauss Quads with variable order of
% % integration.
% % A - Area
% % pstract - reduced induced stress actuation
% % psi - warping function description
% % alf - extra constants used in warping function description
% % n - order of the gaussian integration

------------------------------------------------------------------------
% Author: Andries J. du Plessis
% Date: April 4 1994
% Modified: October 12 '95
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------------------------------------------------------------------------

m = length(leng);

%----- Read Abiscissa and Weights for order n gaussian integration
AW = gausstab(n);

% ----------- Integration for the Panels
T = zeros(14,1);
Bu = zeros(3,14);
for i = 1:m, %----- Integration across all the panels
    J = 1*leng(i)/4; %----- Jacobian for transformation to [-1,1]
    %----- Determine the slope and intersection on [-1,1]
    myg = (yz(i+1,1)-yz(i,1))/2;
    cyg = (yz(i+1,1)+yz(i,1))/2;
    mzg = (yz(i+1,2)-yz(i,2))/2;
    czg = (yz(i+1,2)+yz(i,2))/2;
    mpsig = (psi(i,2)-psi(i,1))/2;
    cpsig = (psi(i,2)+psi(i,1))/2;
    alf a = alf(i);
    dy = deriv(i,1);
    dz = deriv(i,2);
    dk = [pstract(i,1)
          pstract(i,2)
          pstract(i,3)];
end
end
for j = 1:n,
    xj = (1*AW(j,1)+1)/2;
for k = 1:n,
    y = myg*AW(k,1)+cyg;
    z = mzg*AW(k,1)+czg;
    p = mpsig*AW(k,1)+cpsig;
    Bu(1,1) =-1/1;
    Bu(2,1) = 0;
    Bu(3,1) = 0;
    Bu(1,2) = 0;
    Bu(2,2) =-1/1*dy;
    Bu(3,2) = 0;
    Bu(1,3) = 0;
    Bu(2,3) =-1/1*dz;
    Bu(3,3) = 0;
    Bu(1,4) = p*([12/1^3 -6/1^2]*[xj 1]');
    Bu(2,4) = -2*A*alfa*((6/1^3 -6/1^2)*[xj^2 xj]');
    Bu(3,4) = 0;
    Bu(1,5) =-1/1*z';
    Bu(2,5) = dz*[1/1 1]*[xj 1]';
    Bu(3,5) =-1/1*dy;
    Bu(1,6) = 1/1*y;
    Bu(2,6) = dy*[1/1 -1]*[xj 1]';
    Bu(3,6) = -dz/1;
    Bu(1,7) = p*([6/1^2 -4/1]*[xj 1]');
    Bu(2,7) = -2*A*alfa*[3/1^2 -4/1 1]*[xj^2 xj 1]';
    Bu(3,7) = 0;
    Bu(1,8) = 1/1;
    Bu(2,8) = 0;
    Bu(3,8) = 0;
    Bu(1,9) = 0;
    Bu(2,9) = 1/1*dy;
    Bu(3,9) = 0;
    Bu(1,10) = 0;
    Bu(2,10) = 1/1*dz;
    Bu(3,10) = 0;
    Bu(1,11) = p*([-12/1^3 6/1^2]*[xj 1]');
    Bu(2,11) = -2*A*alfa*([-6/1^3 6/1^2]*[xj^2 xj]');
    Bu(3,11) = 0;
    Bu(1,12) = 1/1*z;
    Bu(2,12) = dz*[1/1 1]*[xj 0]';
    Bu(3,12) = 1/1*dy;
    Bu(1,13) =-1/1*y;
    Bu(2,13) = -dy*[1/1 -1]*[xj 0]';
    Bu(3,14) = dz/1;
    Bu(1,14) = p*([6/1^2 -2/1]*[xj 1]');
    Bu(2,14) = -2*A*alfa*[3/1^2 -2/1]*[xj^2 xj]';
    Bu(3,14) = 0;
T = T + AW(j,2)*AW(k,2)*Bu'*dk*J;
Bu = zeros(3,14);
ELEMSTIF.M

function K = elemstif(l,yz,leng,deriv,A,pstiff,psi,alf,n)
% ELEMSTIF
%
% Element Stiffness Integration
%
% ELEMSTIF: function that do the integration to obtain the element stiffness.
% Integration is done with Gauss Quads with variable order of integration.

Author: Andries J. du Plessis
Date: March 31 1994
Modified: October 12 1995
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m = length(leng);

%----- Read Abisscissa and Weights for order n gaussian integration
AW = gaussstab(n);

%------- Integration for the Panels
K = zeros(14,14);
Bu = zeros(3,14);
for i = 1:m, %----- Integration across all the panels
  J = l*leng(i)/4; %----- Jacobian for transformation to [-1,1]
  %----- Determine the slope and intersection on [-1,1]
  myg = (yz(i+1,1)-yz(i,1))/2;
  cyg = (yz(i+1,1)+yz(i,1))/2;
  mzk = (yz(i+1,2)-yz(i,2))/2;
  czg = (yz(i+1,2)+yz(i,2))/2;
  mpsig = (psi(i,2)-psi(i,1))/2;
  cpsig = (psi(i,2)+psi(i,1))/2;
  alfa = alf(i);
  dy = deriv(i,1);
  dz = deriv(i,2);
  dk = [pstiff(i,1) pstiff(i,2) pstiff(i,3) pstiff(i,2) pstiff(i,4) pstiff(i,5) pstiff(i,3) pstiff(i,5) pstiff(i,6)];
for j = 1:n, %----- First part of 2-D Gaussian integration.
  xj = (1*AW(j,1)+1)/2;
  for k = 1:n, %----- Second part of 2-D Gaussian integration.
    y = myg*AW(k,1)+cyg;
    % Calculation of stiffness matrix

end
end
\[ z = mzg \cdot AW(k, 1) + czg; \]
\[ p = mpsig \cdot AW(k, 1) + cpsig; \]
\[ Bu(1, 1) = -1/l; \]
\[ Bu(2, 1) = 0; \]
\[ Bu(3, 1) = 0; \]
\[ Bu(1, 2) = 0; \]
\[ Bu(2, 2) = -1/l \cdot dy; \]
\[ Bu(3, 2) = 0; \]
\[ Bu(1, 3) = 0; \]
\[ Bu(2, 3) = -1/l \cdot dz; \]
\[ Bu(3, 3) = 0; \]
\[ Bu(1, 4) = p \cdot ([12/l^3 \ -6/l^2] \cdot [xj 1]'); \]
\[ Bu(2, 4) = -2 \cdot A \cdot alfa \cdot ([6/l^3 \ -6/l^2] \cdot [xj^2 xj]'); \]
\[ Bu(3, 4) = 0; \]
\[ Bu(1, 5) = -1/l \cdot z; \]
\[ Bu(2, 5) = dz \cdot ([1/l 1] \cdot [xj 1]'); \]
\[ Bu(3, 5) = -1/l \cdot dy; \]
\[ Bu(1, 6) = 1/l \cdot y; \]
\[ Bu(2, 6) = dy \cdot ([1/l 1] \cdot [xj 1]'); \]
\[ Bu(3, 6) = -dz/l; \]
\[ Bu(1, 7) = p \cdot ([6/l^2 \ -4/l] \cdot [xj 1]'); \]
\[ Bu(2, 7) = -2 \cdot A \cdot alfa \cdot ([3/l^2 \ -4/l 1] \cdot [xj^2 xj 1]'); \]
\[ Bu(3, 7) = 0; \]
\[ Bu(1, 8) = 1/l; \]
\[ Bu(2, 8) = 0; \]
\[ Bu(3, 8) = 0; \]
\[ Bu(1, 9) = 0; \]
\[ Bu(2, 9) = 1/l \cdot dy; \]
\[ Bu(3, 9) = 0; \]
\[ Bu(1, 10) = 0; \]
\[ Bu(2, 10) = 1/l \cdot dz; \]
\[ Bu(3, 10) = 0; \]
\[ Bu(1, 11) = p \cdot ([12/l^3 \ 6/l^2] \cdot [xj 1]'); \]
\[ Bu(2, 11) = -2 \cdot A \cdot alfa \cdot ([6/l^3 \ 6/l^2] \cdot [xj^2 xj]'); \]
\[ Bu(3, 11) = 0; \]
\[ Bu(1, 12) = 1/l \cdot z; \]
\[ Bu(2, 12) = dz \cdot ([1/l 1] \cdot [xj 0]'); \]
\[ Bu(3, 12) = 1/l \cdot dy; \]
\[ Bu(1, 13) = -1/l \cdot y; \]
\[ Bu(2, 13) = -dy \cdot ([1/l 1] \cdot [xj 0]'); \]
\[ Bu(3, 13) = dz/l; \]
\[ Bu(1, 14) = p \cdot ([6/l^2 \ -2/l] \cdot [xj 1]'); \]
\[ Bu(2, 14) = -2 \cdot A \cdot alfa \cdot ([3/l^2 \ -2/l] \cdot [xj^2 xj]'); \]
\[ Bu(3, 14) = 0; \]

\[ K = K + AW(j, 2) \cdot AW(k, 2) \cdot Bu' \cdot dk \cdot Bu \cdot J; \]
\[ Bu = zeros(3, 14); \]
end
end
GAUSSTAB.M

function AW = gausstab(n);
% GAUSSTAB - Look up table for abscissa and weights for the gaussian
% quadrature integration scheme. Only allow for orders 2 to 5.

%------------------------------------------------------------------------
% Author: Andries J. du Plessis
% Date: March 31 1994
% Copyright: MIT-SERC-AMSL (c) 1994
%------------------------------------------------------------------------

if n == 2
    AW = [1/sqrt(3) 1
        -1/sqrt(3) 1];
end
if n == 3
    AW = [0 8/9
        sqrt(0.6) 5/9
        -sqrt(0.6) 5/9];
end
if n == 4
    AW = [0.3399810436 0.6521451549
        -0.3399810436 0.6521451549
        0.8611363116 0.3478548451
        -0.8611363116 0.3478548451];
end
if n == 5
    AW = [0.0000000000 0.5688888889
        0.5384693101 0.4786286705
        -0.5384693101 0.4786286705
        0.9061798459 0.2369268850
        -0.9061798459 0.2369268850];
end

MATPROP.M

function outp=matprop(type,varb)
% MATPROP: Subroutine that returns the material properties
% based on material selection which is done during the
% initialization phase. The output from this subroutine
% gives the stiffness matrix [C], the inplane actuation matrix [d],
% and the density. Other options would be to include the dielectric
% for use when the full coupled electro-mechanical problem are
% considered.
% Type 1 - [C]
% Type 2 - Density
% Type 3 - Electrical
%
% Units : SI except were specified otherwise
if varb<0
    error('Material specification positive number')
elseif varb > 17
    error('Unspecified material used')
end

% Index of Materials
% 0 - Air
% 1 - Steel
% 2 - Aluminum
% 3 - AS/4-3501-6
% 4 - E-Glass (Wet layup used for 1/16th model blade)
% 5 - Kapton Electrode
% 6 - AS4 (Smith values in English)
% 7 - IM7/SP381 Tape used by Boeing
% 8 - Kevlar 49 Style 120
% 9 - S2-Glass used by Boeing
% 10 - E/Glass used by Boeing
% 11 - Titanium (BIN)
% 12 - SP250 E/Glass (BIN)
% 13 - IDEPFC M-Series Model values
% 14 - IDEPFC x2=0.8, 5A fibers, integral blade
% 15 - IDEPFC x2=0.9, 5H fibers, 75wt% powder
% 16 - IDEPFC x2=0.8, 4S fibers, 75wt% powder
% 17 - IDEPFC x2=0.9, 4S fibers, 75wt% powder

% rho d31 d32 E1 E2 vlt Glt 0 0
mater1=[
    7800 0 0 205e9 205e9 0.3 77e9 0 0,
    2800 0 0 69e9 69e9 0.33 26e9 0 0,
    1520 0 0 142e9 9.81e9 0.3 6e9 0 0,
    1800 0 0 14.8e9 13.6e9 0.19 1.9e9 0 0,
    1100 0 0 1e9 1e9 0.3 0.7e9 0 0,
    0.055 0 0 20.59e6 1.42e6 0.42 0.87e6 0 0,
    1545 0 0 142e9 8.3e9 0.34 4.9e9 0 0,
    1370 0 0 29e9 29e9 0.05 2.1e9 0 0,
    1855 0 0 43.4e9 12e9 0.28 3.6e9 0 0,
    1716 0 0 19.3e9 19.3e9 0.148 4.1e9 0 0,
    0.16 0 0 103e9 103e9 0.3 42.7e9 0 0,
    0.067 -3.6667e-9 0 6.3e6 1.74e6 0.3 0.52e6 0 0
]

% rho d31 d32 Q11 Q12 Q13 Q22 Q23 Q33 Plane Stress Props!
4810 147e-12 -61e-12 44.5e9 6.56e9 0 18.5e9 0 5.5e9
4060 329e-12 -137e-12 33.6e9 7.54e9 0 16.6e9 0 5.13e9
5240 229e-12 -95.5e-12 38.5e9 6.88e9 0 18.9e9 0 4.46e9
if type == 1
  if varb < 13
    Q = plyp(materl(varb,4:7));
    outp = [Q(1,1) Q(1,2) Q(1,3) Q(2,2) Q(2,3) Q(3,3)];
  else
    outp = materl(varb,4:9);
  end
end

if type == 2
  outp = materl(varb,1);
end

if type == 3
  outp = materl(varb,2:3);
end

NEWOML.M
function [xo,yo]=newoml(xi,yi,d)
% Written by Kamyar Ghandi, 6/26/96
n=length(xi);
xi=xi';
yi=yi';

dx=diff(xi);
dy=diff(yi);

dx=[dx dx(n-1)];
dy=[dy dy(n-1)];

norm=(dx.^2+dy.^2).^0.5;

xo=(xi-d*dy./norm)';
yo=(yi+d*dx./norm)';

end

NSELEMSTIF.M
function dBn = nselemstif(l,Kns,n);
% NSELEMSTIF
%
% Non-Spar element stiffness integration
% nselemstif: function that does the integration to obtain the element stiffness
% due to non-spar components. Integration is done with Gauss Quads
% with variable order of integration.
% l - length of of element
% Kn - matrix containing constant non-spar stiffness contributions
% n - order of the gaussian integration

%------------------------------------------------------------------------

% Author: John P. Rodgers
% Date: April 7 1994
% Modified: July 12 1996 (John P. Rodgers)
% Copyright: MIT-SERC-AMS (C) 1996

%------------------------------------------------------------------------

%----- Read Abiscissa and Weights for order n gaussian integration
A W = gausstab(n);

%----- Integration
J = 1/2;
dBn = zeros(14,14);
N = zeros(7,14);
for j = 1:n,
   xj = (1*AW(j,1)+1)/2;
   N(1,1) = [-1/1]*[1]';
   N(2,2) = [-1/1]*[1]';
   N(3,3) = [-1/1]*[1]';
   N(4,4) = [6/1^3 -6/1^2]*[xj^2 xj]';
   N(4,7) = [3/1^2 -4/1 1]*[xj^2 xj 1]';
   N(5,5) = [-1/1]*[1]';
   N(6,6) = [-1/1]*[1]';
   N(7,4) = [12/1^3 -6/1^2]*[xj 1]';
   N(7,7) = [6/1^2 -4/1]*[xj 1]';
   N(1,8) = [1/1]*[1]';
   N(2,9) = [1/1]*[1]';
   N(3,10) = [1/1]*[1]';
   N(4,11) = [-6/1^3 6/1^2]*[xj^2 xj]';
   N(4,14) = [3/1^2 -2/1]*[xj^2 xj]';
   N(5,12) = [1/1]*[1]';
   N(6,13) = [1/1]*[1]';
   N(7,11) = [-12/1^3 6/1^2]*[xj 1]';
   N(7,14) = [6/1^2 -2/1]*[xj 1]';
dBn = dBn + AW(j,2)*N'*Kns*N*J;
N = zeros(7,14);
end
function [L,P,Geff] = panstif(prop,layup)

% PANSTIF

% Panel stiffness

% PANSTIF determines the stiffness of a composite panel with the usage of
% CLPT. Certain stiffnesses are ignored in the analysis due to the
% plane nature of the problem. The reduced stiffnesses determined
% through static condensation on the original stiffness matrix.
% The effective electric properties are also determined
% in this function.
% Assumption: Layup is such that there will always be a ply
% boundary at the midplane.
% Q - Plane stress lamina properties

%---------------------------------------------------------------------
% Author: Andries J. du Plessis
% Date: April 4 1994
% Modified: October 10 1995
% Copyright: MIT-SERC-AMSL (c) 1994
%---------------------------------------------------------------------

%---- Error messages
if narg < 2
    error('Need property matrix and layups to determine panel stiffness');
end

[mx,nx] = size(prop);
[my,ny] = size(layup);
if mx ~= my
    error('Property matrix and Layup matrix do not correlate');
end

n = length(prop(1,1));
A = zeros(n,3);
B = zeros(n,3);
D = zeros(n,3);

%---- Ply location matrices (relative to midline plane)
zu = zeros(n,1);
zl = zeros(n,1);
for i = 1:n,
    thick = thick + prop(1,i);
end
zu(1) = thick/2;
zl(1) = zu(1) - prop(1,1);
for i = 2:n,
    zu(i) = zu(i-1) - prop(i-1,1);
    zl(i) = zl(i-1) - prop(i,1);
end
%------ Stiffness calculation
for i=1:n,
    Q = [prop(i,2) prop(i,3) prop(i,4);
         prop(i,3) prop(i,5) prop(i,6);
         prop(i,4) prop(i,6) prop(i,7)];
    Q = plyp(prop(i,2:5));
    Qr = plyrp(Q,layup(i)); % Rotate the ply properties
    z1 = zu(i)-zl(i);
    z2 = (1/2)*(zu(i)^2-zl(i)^2);
    z3 = (1/3)*(zu(i)^3-zl(i)^3);
    A = A+Qr*z1;
    B = B+Qr*z2;
    D = D+Qr*z3;
end
if A(2,2)==0
    A(2,2)=0.00001;
end

%------ Stiffness reduction
L = zeros(3);
L(1,1) = A(1,1) - (A(1,2)^2)/A(2,2);
L(1,2) = A(1,3) - (A(1,2)*A(2,3))/A(2,2);
L(1,3) = B(1,1) - (A(1,2)*B(1,2))/A(2,2);
L(2,1) = L(1,2);
L(2,2) = A(3,3) - (A(2,3)^2)/A(2,2);
L(2,3) = B(1,3) - (A(2,3)*B(1,2))/A(2,2);
L(3,1) = L(1,3);
L(3,2) = L(2,3);
L(3,3) = D(1,1) - (B(1,2)^2)/A(2,2);
for i=1:3,
    for j=1:3,
        if abs(L(i,j))<0.00001
            L(i,j)=0.0;
        end
    end
end

%------ Electrical properties
P = zeros(3,n);
GAM = zeros(3,1);
PI = zeros(3,1);
for i=1:n,
    % Q = plyp(prop(i,2:5));
    % e = Q*([0 0 prop(i,6); 0 0 prop(i,7)]; 0 0 0]);
    Q = [prop(i,2) prop(i,3) prop(i,4);
         prop(i,3) prop(i,5) prop(i,6);
         prop(i,4) prop(i,6) prop(i,7)];
    e = Q*([0 0 prop(i,8); 0 0 prop(i,9); 0 0 0]);
    er = vectrr(e,layup(i));
    z1 = zu(i)-zl(i);
\[ z_2 = (1/2)*(zu(i)^2-z1(i)^2); \]
\[ \text{GAM} = \text{er}*[0 0 1]'*z1; \quad \text{Only have field applied in the n-direction} \]
\[ \text{PI} = \text{er}*[0 0 1]'*z2; \quad \text{Only have field applied in the n-direction} \]
\[ P(1,i) = \text{GAM}(1)-\text{GAM}(2)*A(1,2)/A(2,2); \]
\[ P(2,i) = \text{GAM}(3)-\text{GAM}(2)*A(2,3)/A(2,2); \]
\[ P(3,i) = \text{PI}(1)-\text{GAM}(2)*B(1,2)/A(2,2); \]

%------ Effective shear stiffness for warping function
\[ \text{Geff}=L(2,2)+((2*L(1,2)*L(1,3)*L(2,3)-L(1,2)^2*L(3,3)-L(1,1)*L(2,3)^2).../(L(1,1)*L(3,3)-L(1,3)^2)); \]

**WARPF.M**

```matlab
function [psi,alf]=warfunc(A,c,leng,normp,Geff);
% WARPF
% % Warping function around the circumference
% % WARPF determines the warping function around the circumference of the
% % composite boxbeam. The function does allow for variations in the
% % stiffness around the circumference of the boxbeam.
% % phi - matrix that contain warping function slopes and constants
% % in the four panels
% % alf - warping parameter (differs in every panel)
% % Geff - Effective shear stiffness for the panel
%---------------------------------------------------------------
Author: Andries J. du Plessis
Date: March 29 1994
Modified: October 10 1995
Copyright: MIT-SERC-AMSL (c) 1994
%---------------------------------------------------------------

m = length(leng);
%------ Calculate the warping parameter
alfsum = sum(leng./Geff);
alf = (1./Geff)/alfsum;
%------ Calculate the interim warping function
psie = zeros(m,2);
pzie(1,:) = [0 leng(1)*(-2*A*alf(1)+normp(1))];
for i = 2:m,
    psie(i,1)=psie(i-1,2);
    psie(i,2)=leng(i)*(-2*A*alf(i)+normp(i))+psie(i,1);
end
%------ Adjust the warping function to reflect symmetry and integral req.
kint = sum((0.5*(psie(:,1)+psie(:,2))).*leng);
Kpsi = kint/c;
psi(:,1) = psie(:,1)-Kpsi;
psi(:,2) = psie(:,2)-Kpsi;
```
PLYPM
function Q = plyp(x)
% PLYP
% Ply properties in the ply coordinate system. See [Rogers]
% x - matrix containing El,Et,nu,G

Q = zeros(3);
num = 1-(x(3)^2)*(x(2)/x(1));
Q(1,1) = x(1)/num;
Q(1,2) = (x(3)*x(2))/num;
Q(2,1) = Q(1,2);
Q(2,2) = x(2)/num;
Q(3,3) = x(4);

PLYRP.M
function Qr = plyrp(Q,deg)
% PLYRP
% Rotated ply properties [Jones p.48]
% Qr - rotated ply properties
% Q - properties in the ply coordinate system
% deg - angle of major ply direction relative to structural axis. See [Jones]

% Inclusion of error messages
if nargin==2
    error('Too few arguments');
end
if abs(deg)>180
    error('Angle of ply incorrectly specified');
end
[mx,nx]=size(Q);
if((mx~=3)&(nx~=3))
    error('Stiffness property matrix incorrect');
end

%----- Rotation
Qr = zeros(3);
the = (pi/180)*deg;
c = cos(the);
s = sin(the);
T = [c^2 s^2 2*s*c
    s^2 c^2 -2*s*c
    -s*c s*c (c^2-s^2)];
R = [1 0 0
    0 1 0
    0 0 2];
Qr = inv(T)*Q*R*T*inv(R);

DISTRIB.M
function dBn = distrib(l,q,n);
% DISTRIB
% % Distributed force integration
% % DISTRIB: function that do the integration to obtain the consistant element
% % nodal loads for the analysis. Forcing constant along length.
% % Integration is done with Gauss Quads with variable order of
% % integration.
% l - length of of element
% q - vector that obtain the distributed loading
% n - order of the gaussian integration
%-----------------------------------------------------------------------
% Author: Andries J. du Plessis
% Date: April 7 1994
% Modified: October 2 1995
% Copyright: MIT-SERC-AMS (c) 1994
%-----------------------------------------------------------------------
%------- Read Abiscissa and Weights for order n gaussian integration
AW = gausstab(n);

%------- Integration
J = 1/2;
dBn = zeros(14,1);
N = zeros(7,14);
for j = 1:n,
xj = (1*AW(j,1)+1)/2;
    N(1,1) = [-1/l 1]*[xj 1]';
    N(2,2) = [-1/l 1]*[xj 1]';
    N(3,3) = [-1/l 1]*[xj 1]';
    N(4,4) = [2/l^3 -3/l^2 1]*[xj^3 xj^2 1]';
    N(4,7) = [1/l^2 -2/l 1]*[xj^3 xj^2 xj]';
    N(5,5) = [-1/l 1]*[xj 1]';
    N(6,6) = [-1/l 1]*[xj 1]';
\[
N(7,4) = [6/1^3 -6/1^2]*[xj^2 xj]'; \\
N(7,7) = [3/1^2 -4/1 1]*[xj^2 xj 1]'; \\
N(1,8) = [1/1 1]*[xj 0]'; \\
N(2,9) = [1/1 1]*[xj 0]'; \\
N(3,10) = [1/1 1]*[xj 0]'; \\
N(4,11) = [-2/1^3 3/1^2]*[xj^3 xj^2]'; \\
N(4,14) = [1/1^2 -1/1]*[xj^3 xj^2]'; \\
N(5,12) = [1/1 1]*[xj 0]'; \\
N(6,13) = [1/1 1]*[xj 0]'; \\
N(7,11) = [-6/1^3 6/1^2]*[xj^2 xj]'; \\
N(7,14) = [3/1^2 -2/1]*[xj^2 xj]'; \\
dBn = dBn + AW(j,2)*N'*q*J; \\
N = zeros(7,14); \\
\]

**VECTR.R.M**

function Qr = vectrr(Q,deg)

%-------------------------------------------------------------------------
% Rotated vector properties function
% %
% % AJ du Plessis - AMSL - Dept of Aero+Astro - MIT.
% %-------------------------------------------------------------------------
Qr = zeros(3,3);
the = (pi/180)*deg;
c = cos(the);
s = sin(the);
T = [c^2 s^2 -2*s*c \\
    s^2 c^2 2*s*c \\
    s*c -s*c (c^2-s^2)];
K = [c s 0 \\
    -s c 0 \\
    0 0 1];
Qr = T*Q*K;
C.2 Input Files for Integral Blade Predictions

This section presents the MATLAB input files used in the final benchtop twist predictions for the integral blade in Chapter 6. The input files account for the experimentally determined torsional stiffness as well as the pack failures which occurred. A total of nine files were used to describe the blade from the inboard extent to the outboard extent. Each individual file represents an approximately constant section of the blade. The first file, integral0.m, represents the non-active inboard section of the blade. The next seven files correspond with each of the seven spanwise pack groups along the blade. The last file, integral8.m, represents the non-active outboard section of the blade. The complete input file is presented only for the first inboard section. For each of the remaining files, only the significant lines of the code are printed. These include the lay-up details, the active packs, and the torsional stiffness correction (experimental). For the last two files, the blade taper is also accounted for using a linear interpolation between VR7 and VR8 airfoil coordinates for the center of the discretized section. The file called beam.m, presented in the previous section, calls each of the data files created by elem.m for each section of the blade.

INTEGRAL0.M
% INPUTF
%
% Input file for the element analysis
%
% INPUTF defines the element cross section, materials, layups used in the
% different linear panels and the actuation
%---------------------------------------------------------------------
% Author: John P. Rodgers
% Original: July 13 1996
% Modified: September 1998
% Copyright: MIT-SERC-AMSL (c) 1996
%---------------------------------------------------------------------
%------- Inputfile designation and element number ---------------------
dx = 0.02; %------- Element length in analysis
sparonly=0;

%------- Coordinates--------------------------------------------------
% Given with the axis located at the leading edge and at chord line
% Can change this location by supplying a pitch axis location
yzboeing=[0.0 0.0 0.0005 0.0092]
0.0018  0.0119
0.0067  0.0185
0.0155  0.0265
0.0241  0.0321
0.0280  0.0350
0.0443  0.0435
0.064   0.0518
0.120   0.0691
0.160   0.07755
0.200   0.08380
0.255   0.0892
0.330   0.0914
0.3322  0.0914
0.380   0.0897
0.380   -0.0286
0.3322  -0.0289
0.330   -0.0289
0.255   -0.02795
0.200   -0.0266
0.160   -0.0251
0.120   -0.02285
0.0382  -0.0142
0.0241  -0.01173
0.0233  -0.01155
0.0131  -0.0091
0.0065  -0.0066
0.00155 -0.00275
0.0    0.0]*5.388;

pitchx = 1.347;
% pitch axis
pitchy = 0.16;

xna = -.24;
% neutral axis relative to pitch axis (est.)
yna = -.039;

th=0.024;   % half of layup thickness to subtract from OML (in) for CLPT
yz1=[yzboeing(:,:1)-pitchx-xna yzboeing(:,2)-pitchy-yna];
[xnew,ynew]=newoml(flipud([yz1(17:29,1);yz1(1:16,1)]),flipud([yz1(17:29,2);yz1(1:16,2)]),th);
yz=[flipud([xnew(16:29);xnew(1:16)]);
flipud([ynew(16:29);ynew(1:16)])]*.0254;

th2=(5.0+4.5*4)*1e-3;           % web +fairing+adhesive

yz(15:16,2)=yz(15:16,2)-[th2/2 th2/2]'.*0.0254; % subtract half of web/fairing

yz(17:18,2)=yz(17:18,2)+[th2/2 th2/2]'.*0.0254;

yz(16:17,1)=(2.05-pitchx-xna-1.5*4.5e-3*[1 1]'.*0.0254; % web

%------ Laminate Panels ---------------------------------------------
% mat = [material plies in panels] %------Size(pannels,max #plies)
% Index of Materials - Look in matprop.m to get the current values
 nose=[1:5 25:29];
 spar=[6:14 18:24];
 overlap=[15 17];
 web=[16];
 mat=zeros(29,14);
for i=1:length(nose)
    mat(nose(i), :) = [10 9 10 10 10 10 7 9 9 0 0 0 0 0];
end
for i=1:length(spar)
    mat(spar(i), :) = [10 9 9 10 10 10 10 7 9 9 0 0 0 0];
end
for i=1:2
    mat(overlap(i), :) = [10 9 9 9 10 10 10 10 7 9 9 10 10 10];
end
mat(web, :) = [10 10 10 0 0 0 0 0 0 0 0 0 0 0 0 0];
[m, n] = size(mat);
%thick = [ply thicknesses] %---- Size(panels, max #plies)
thick = zeros(size(mat));
for i=1:length(nose)
    thick(nose(i), :) = [4.5 9.0 4.5 4.5 4.5 4.5 5.5 9.0 9.0 0 0 0 0 0];
end
for i=1:length(spar)
    thick(spar(i), :) = [4.5 9.0 9.0 9.0 4.5 4.5 4.5 4.5 5.5 9.0 9.0 0 0 0];
end
for i=1:2
    thick(overlap(i), :) = [4.5 9.0 9.0 9.0 4.5 4.5 4.5 4.5 4.5 5.5 9.0 9.0 4.5 4.5];
end
thick(web, :) = [4.5 4.5 4.5 0 0 0 0 0 0 0 0 0 0 0 0 0];
thick = thick * 1e-3 * .0254;
%ang = [Angles of plies] %---- Size(panels, max #plies)
ang = zeros(size(mat));
for i=1:length(nose)
    ang(nose(i), :) = [45 0 45 45 45 45 0 0 0 0 0 0 0 0];
end
for i=1:length(spar)
    ang(spar(i), :) = [45 45 0 -45 45 45 45 45 0 0 0 0 0 0];
end
for i=1:2
    ang(overlap(i), :) = [45 45 0 -45 45 45 45 45 0 0 0 45 45 45];
end
ang(web, :) = [45 45 45 0 0 0 0 0 0 0 0 0 0 0 0 0];
%volt = [Voltages at the top and bottom of every ply]
V = 2000;  % 2000Vpp for 584 free strain, integral blade testing
volt = zeros(length(mat(:, 1)), 2 * length(mat(:, 1)));
elucp = [6:15];
elclo = [17:24];
posup = [];
negup = [];
poslo = [];
negllo = [];
for i=1:length(elecup)
    for j=1:length(posup)
volt(elecup(i),2*posup(j)-1)=1;
end
for j=1:length(negup)
  volt(elecup(i),2*negup(j)-1)=-1;
end
end
for i=1:length(eleclo)
for j=1:length(poslo)
  volt(eleclo(i),2*poslo(j)-1)=1;
end
for j=1:length(neglo)
  volt(eleclo(i),2*neglo(j)-1)=-1;
end
end
volt=volt*V;
%------- Element loading -----------------------------------------
%------- Distributed loading (assume constant distributed loads)
q = [0 0 0 0 0 0 0 0 1.03 2.339 19.9 0 0.137 0.0315 0;  
     0 0 0 8.26e5 0 0 0 429 113 3.17e5 0.0534 -.0228 0];
%mat,thick,angle,EA,Xna,Yna,E1xx,EEyy,G1J,GA,m,xcg,ycg
if ~sparonly
  nonspar=[0 0 0 0.50e6 0 0 10.3 2339 19.9 0 0.137 0.0315 0;  
            0 0 0 8.26e5 0 0 0 429 113 3.17e5 0.0534 -.0228 0];
else nonspar=[];
end
%upper fairing skin, lower fairing skin
%TE tab, TE stiffener, spar core, fairing core, LE weights

INTEGRAL1.M
mat(nose(i,:),:)=[10 9 9 10 9 10 7 9 0 0 0 0 0];
mat(spar(i,:),:)=[10 15 9 15 10 15 10 7 9 0 0 0 0];
mat(overlap(i,:),:)=[10 15 9 15 10 15 10 7 9 0 0 0 0 0];
mat(web,:)=[10 10 10 0 0 0 0 0 0 0 0 0 0 0];
thick(nose(i,:),:)=[4.5 9.0 9.0 4.5 9.0 4.5 5.5 5.5 9.0 0 0 0 0;  
                     4.5 6.5 9.0 6.5 4.5 6.5 4.5 6.5 4.5 9.0 0 0 0];
thick(spar(i,:),:)=[4.5 6.5 9.0 6.5 4.5 6.5 4.5 6.5 4.5 5.5 9.0 0 0 0];
thick(overlap(i,:),:)=[4.5 6.5 9.0 6.5 4.5 6.5 4.5 6.5 4.5 4.5 5.5 9.0 0 4.5 4.5 4.5];
thick(web,:)=[4.5 4.5 4.5 0 0 0 0 0 0 0 0 0 0 0 0 0 0];
ang(nose(i,:),:)=[45 0 0 45 0 45 0 0 0 0 0 0 0 0];
ang(spar(i,:),:)=[45 45 0 -45 45 45 45 0 0 0 0 0 0];
ang(overlap(i,:),:)=[45 45 0 -45 45 45 45 0 0 45 45 45];
ang(web,:)=[45 45 45 0 0 0 0 0 0 0 0 0 0 0 0];
posup = [2 6];
negup = [];
poslo = [2 6];
neglo = [4];
nonspar=[0 0 0 0.50e6 0 0 10.3 2339 19.9 0 0.137 0.0315 0;  
          0 0 0 8.56e5 0 0 0 445 0 3.3e5 0.0397 -.0228 0;  
          0 0 0 0 0 0 57 0 0 0 0 0];};
INTEGRAL2.M
mat(nose(i,:), :) = [10 9 9 10 9 10 7 0 0 0 0];
mat(spar(i,:), :) = [10 15 9 15 10 15 10 7 0 0 0];
mat(overlap(i,:), :) = [10 15 9 15 10 15 10 7 10 10 10];
mat(web, :) = [10 10 10 0 0 0 0 0 0 0 0];
thick(nose(i,:), :) = [4.5 9.0 9.0 4.5 9.0 4.5 5.5 0 0 0 0];
thick(spar(i,:), :) = [4.5 6.5 9.0 6.5 4.5 6.5 4.5 5.5 0 0 0 0];
thick(overlap(i,:), :) = [4.5 6.5 9.0 6.5 4.5 6.5 4.5 5.5 4.5 4.5 4.5];
thick(web, :) = [4.5 4.5 4.5 0 0 0 0 0 0 0 0 0];
ang(nose(i,:), :) = [45 0 0 45 0 45 0 0 0 0 0 0];
ang(spar(i,:), :) = [45 45 0 -45 45 45 45 0 0 0 0];
ang(overlap(i,:), :) = [45 45 0 -45 45 45 45 0 45 45 45];
ang(web, :) = [45 45 45 0 0 0 0 0 0 0];
posup = [6];
*negup = [4];
poslo = [6];
*neglo = [4];
nonspar = [0 0 0 0.50e6 0 0 10.3 2339 19.9 0 0.137 0.0315 0; 0 0 8.56e5 0 0 0 445 0 3.3e5 0.0397 -0.0228 0; 0 0 0 0 0 0 45 0 0 0 0 0];

INTEGRAL3.M
mat(nose(i,:), :) = [10 9 9 10 9 10 7 0 0 0 0];
mat(spar(i,:), :) = [10 15 9 15 10 15 10 7 0 0 0];
mat(overlap(i,:), :) = [10 15 9 15 10 15 10 7 10 10 10];
mat(web, :) = [10 10 10 0 0 0 0 0 0 0 0 0];
thick(nose(i,:), :) = [4.5 9.0 9.0 4.5 9.0 4.5 5.5 0 0 0 0 0];
thick(spar(i,:), :) = [4.5 6.5 9.0 6.5 4.5 6.5 4.5 5.5 0 0 0 0 0];
thick(overlap(i,:), :) = [4.5 6.5 9.0 6.5 4.5 6.5 4.5 5.5 4.5 4.5 4.5];
thick(web, :) = [4.5 4.5 4.5 0 0 0 0 0 0 0 0 0];
ang(nose(i,:), :) = [45 0 0 45 0 45 0 0 0 0 0 0];
ang(spar(i,:), :) = [45 45 0 -45 45 45 45 0 0 0 0];
ang(overlap(i,:), :) = [45 45 0 -45 45 45 45 0 45 45 45];
ang(web, :) = [45 45 45 0 0 0 0 0 0 0];
posup = [2];
*negup = [1];
poslo = [2];
*neglo = [4];
nonspar = [0 0 0 0.50e6 0 0 10.3 2339 19.9 0 0.137 0.0315 0; 0 0 8.56e5 0 0 0 445 0 3.3e5 0.0397 -0.0228 0; 0 0 0 0 0 0 45 0 0 0 0 0];

INTEGRAL4.M
mat(nose(i,:), :) = [10 9 9 10 9 10 7 0 0 0 0];
mat(spar(i,:), :) = [10 15 9 15 10 15 10 7 0 0 0];
mat(overlap(i,:), :) = [10 15 9 15 10 15 10 7 10 10 10];
mat(web, :) = [10 10 10 0 0 0 0 0 0 0 0 0];
thick(nose(i,:), :) = [4.5 9.0 9.0 4.5 9.0 4.5 5.5 0 0 0 0 0];
thick(spar(i,:), :) = [4.5 6.5 9.0 6.5 4.5 6.5 4.5 5.5 0 0 0 0 0];
thick(overlap(i,:), :) = [4.5 6.5 9.0 6.5 4.5 6.5 4.5 5.5 4.5 4.5 4.5];
thick(web,:)=[4.5 4.5 4.5 0 0 0 0 0 0 0];
ang(nose(i,:),:=[45 0 0 45 0 45 0 0 0 0];
ang(spar(i,:),:=[45 45 0 -45 45 45 45 0 0 0 0];
ang(overlap(i,:),:=[45 45 0 -45 45 45 45 0 45 45 45];
ang(web,:):=[45 45 45 0 0 0 0 0 0 0];
posup = [2 6];
nequp = [1];
poslo = [1];
neglo = [4];
nonspar=[0 0 0 0.50e6 0 0 10.3 2339 19.9 0 0.137 .0315 0;
0 0 0 8.56e5 0 0 0 445 0 3.3e5 0.0397 -.0228 0;
0 0 0 0 0 0 0 0 45 0 0 0 0];

INTEGRAL5.M
mat(nose(i,:),:=[10 9 10 10 10 9 10 7 0 0 0 0];
mat(spar(i,:),:=[10 15 10 10 15 10 15 10 7 0 0 0];
mat(overlap(i,:),:=[10 15 10 10 15 10 15 10 7 10 10 10];
mat(web,:):=[10 10 10 0 0 0 0 0 0 0 0];
thick(nose(i,:),:=[4.5 9.0 4.5 4.5 4.5 9.0 4.5 5.0 0 0 0];
thick(spar(i,:),:=[4.5 6.5 4.5 4.5 4.5 6.5 4.5 5.5 0 0 0];
thick(overlap(i,:),:=[4.5 6.5 4.5 4.5 6.5 4.5 6.5 4.5 5.5 4.5 4.5];
thick(web,:):=[4.5 4.5 4.5 0 0 0 0 0 0 0 0];
ang(nose(i,:),:=[45 0 0 0 45 0 45 0 0 0 0];
ang(spar(i,:),:=[45 45 0 0 -45 45 45 0 0 0 0];
ang(overlap(i,:),:=[45 45 0 0 -45 45 45 45 0 0 0];
ang(web,:):=[45 45 45 0 0 0 0 0 0 0];
posup = [2 7];
nequp = [5];
poslo = [2];
neglo = [1];
nonspar=[0 0 0 0.50e6 0 0 10.3 2339 19.9 0 0.137 .0315 0;
0 0 0 8.56e5 0 0 0 445 0 3.3e5 0.0397 -.0228 0;
0 0 0 0 0 0 0 0 32 0 0 0 0];

INTEGRAL6.M
mat(nose(i,:),:=[10 9 10 10 10 9 10 7 0 0 0 0];
mat(spar(i,:),:=[10 15 10 10 15 10 15 10 7 0 0 0];
mat(overlap(i,:),:=[10 15 10 10 15 10 15 10 7 10 10 10];
mat(web,:):=[10 10 10 0 0 0 0 0 0 0 0];
thick(nose(i,:),:=[4.5 9.0 4.5 4.5 4.5 9.0 4.5 5.5 0 0 0];
thick(spar(i,:),:=[4.5 6.5 4.5 4.5 4.5 6.5 4.5 5.5 0 0 0];
thick(overlap(i,:),:=[4.5 6.5 4.5 4.5 6.5 4.5 6.5 4.5 5.5 4.5 4.5];
thick(web,:):=[4.5 4.5 4.5 0 0 0 0 0 0 0 0];
ang(nose(i,:),:=[45 0 0 0 45 0 45 0 0 0 0];
ang(spar(i,:),:=[45 45 0 0 -45 45 45 0 0 0 0];
ang(overlap(i,:),:=[45 45 0 0 -45 45 45 45 0 0 0];
ang(web,:):=[45 45 45 0 0 0 0 0 0 0];
posup = [2 7];
nequp = [5];
poslo = [2 7];
neglo = [5];
nonspar=[0 0 0 0.50e6 0 0 10.3 239 19.9 0 0.137 .0315 0;
0 0 0 8.56e5 0 0 0 445 0 3.3e5 0.0397 -.0228 0;
0 0 0 0 0 0 32 0 0 0 0 0 0];

INTEGRAL7.M
yzboeing=[0
0 0.0027 0.0178
0.0097 0.0431
0.0361 0.0852
0.0835 0.1211
0.1299 0.1493
0.1509 0.1605
0.2387 0.2003
0.3448 0.2392
0.6466 0.3192
0.8621 0.3583
1.0776 0.3872
1.3739 0.4124
1.7780 0.4244
1.7899 0.4244-.0135
2.05 0.4196-.0135
2.05 -0.1537+.0135
1.7899 -0.1541+.0135
1.7780 -0.1540
1.3739 -0.1483
1.0776 -0.1403
0.8621 -0.1317
0.6466 -0.1193
0.2058 -0.0738
0.1299 -0.0607
0.1255 -0.0598
0.0706 -0.0467
0.0350 -0.0325
0.0084 -0.0121
0 0.000]; % interpolation of VR7/VR8 at x/r=0.9
mat(nose(i,:),)=[10 9 10 10 10 9 10 7 0 0 0 0 0];
mat(spar(i,:),)=[10 15 10 10 10 15 10 7 0 0 0 0 0];
mat(overlap(i,:),)=[10 15 10 10 10 15 10 10 7 10 10 10 10];
mat(web, :)=[10 10 10 0 0 0 0 0 0 0 0 0 0 0];
thick(nose(i,:),)=[4.5 9.0 4.5 4.5 4.5 9.0 4.5 5.5 0 0 0 0 0];
thick(spar(i,:),)=[4.5 6.5 4.5 4.5 4.5 6.5 4.5 6.5 4.5 5.5 0 0 0 0];
thick(overlap(i,:),)=[4.5 6.5 4.5 4.5 4.5 6.5 4.5 6.5 4.5 5.5 4.5 4.5 4.5 4.5];
thick(web, :)=[4.5 4.5 4.5 0 0 0 0 0 0 0 0 0 0 0 0];
ang(nose(i,:),)=[45 0 0 45 0 45 0 0 0 0 0 0 0 0 0];
ang(spar(i,:),)=[45 45 0 0 -45 45 45 45 0 0 0 0 0 0 0 0];
ang(overlap(i,:),)=[45 45 0 0 -45 45 45 45 0 45 45 45 45 45 45 45 45];
posup = [2 7];
.negup = [5];
poslo = [2 7];
neglo = [];
nonspar=[0 0 0 0.50e6 0 0 10.3 2339 19.9 0 0.137 0.0315 0; 0 0 0 1.49e6 0 0 0 774 0 5.6e5 0.0709 -.0228 0; 0 0 0 0 0 0 19 0 0 0 0 0];

INTEGRAL8.M

yzboeing=[0 0
0.0027 0.0203
0.0097 0.0341
0.0361 0.0621
0.0835 0.0887
0.1299 0.1101
0.1509 0.1184
0.2387 0.1492
0.3448 0.1793
0.6466 0.2395
0.8621 0.2690
1.0776 0.2908
1.3739 0.3102
1.7780 0.3222
1.7899 0.3232-.0135
2.0474 0.3195-.0135
2.0474 -0.1509+.0135
1.7899 -0.1516+.0135
1.7780 -0.1516
1.3739 -0.1451
1.0776 -0.1361
0.8621 -0.1267
0.6466 -0.1141
0.2058 -0.0709
0.1299 -0.0586
0.1255 -0.0578
0.0706 -0.0453
0.0350 -0.0323
0.0084 -0.0163
0 0.0000];

% interpolation of VR7/VR8 at x/r=0.975

th=0.0195; % half of layup thickness to subtract from OML (in)

mat(nose(i),:)=[10 10 10 10 10 7 0 0 0 0 0 0];
mat(spar(i),:)=[10 9 10 10 9 10 10 7 0 0 0 0];
mat(overlap(i),:)=[10 9 10 10 9 10 10 7 10 10 10 10];
mat(web,:)=[10 10 10 0 0 0 0 0 0 0 0 0];

thick(nose(i),:)=[4.5 4.5 4.5 4.5 4.5 5.5 0 0 0 0 0 0];
thick(spar(i),:)=[4.5 9.0 4.5 4.5 9.0 4.5 4.5 5.5 0 0 0 0];
thick(overlap(i),:)=[4.5 9.0 4.5 4.5 9.0 4.5 4.5 5.5 4.5 4.5 4.5];

thick(web,:)=[4.5 4.5 4.5 0 0 0 0 0 0 0 0 0];

ang(nose(i),:)=[45 0 0 45 0 45 0 0 0 0 0 0];
ang(spar(i),:)=[45 45 0 0 -45 -45 45 0 0 0 0 0];
ang(overlap(i),:)=[45 45 0 0 -45 45 45 0 45 45 45 45];
ang(web,:)=[45 45 45 0 0 0 0 0 0 0 0 0];

nonspar=10 0 0 0.50e6 0 0 10.3 2339 19.9 0 0.137 0.0315 0; 0 0 0 1.49e6 0 0 0 774 0 5.6e5 0.0709 -.0228 0; 0 0 0 0 0 0 24 0 0 0 0 0];
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