MICROSENSORS FOR THE MEASUREMENT OF
SHEAR FORCES IN TURBULENT BOUNDARY LAYERS

by

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B.S., Rensselaer Polytechnic Institute (1981)
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Martin Arnold Schmidt

Submitted to the Department of Electrical Engineering and Computer Science on May 18, 1988 in partial fulfillment of the requirements for the Degree of Doctor of Philosophy in Electrical Engineering and Computer Science

ABSTRACT

This thesis reports on the development of a microfabricated floating-element shear-stress sensor intended for use in turbulent boundary layers in air. The goal of the thesis was to identify a microfabricated structure which could ultimately be used to make a direct measurement of shear stress with a sensitivity of 1 Pa, a bandwidth of 20 kHz, and a spatial resolution of 100 μm. The prototype developed in this thesis has a sensitivity of 0.1 Pa, a bandwidth of 10 kHz and is sized 500 μm x 500 μm.

Surface micromachining was used to fabricate a polyimide/metal composite floating element which will deflect laterally due to shear stress. The structure consists of a square plate and four tethers suspended 3 μm above the silicon substrate. The lateral deflection is detected using an integrated differential capacitor readout scheme. The capacitive coupling between a drive electrode and two sense electrodes is measured using a pair of matched depletion-mode metal-gate MOS transistors on-chip. This coupling is modulated by lateral motion of the polyimide plate with embedded thin conductor. The differential drain current is then measured off-chip. A set of electro-mechanical design equations are developed to model the response of the sensor. Based on this model, a prototype sensor chip measuring 4 mm x 5 mm with a 500 μm x 500 μm element size was layed out. The electrodes and devices were sized to ensure an adequate signal at 1 Pa. Dynamic response models suggest that the device would have a bandwidth of ~10 kHz as determined by the first lateral resonance of the structure.

The mechanical properties of the polyimide/metal composite structure were studied in order to both calibrate the sensor as well as control the shape of the fabricated part. The elastic modulus and residual tensile stress in the polyimide were measured using suspended diaphragms. Cantilevers were fabricated in order to study the out-of-plane warpage of the composite microstructure. A model was developed and fit to the experimental cantilever deflection data. Based on these results a process was identified that allowed the fabrication of a 500 μm edge length polyimide plate (32 μm thick) with 300 Å chrome embedded which warped out-of-plane less than 1 μm.
The fabricated sensors were fully calibrated in laminar flow. A specially designed package and flow cell ensured that the sensor could be exposed to a well calibrated flow. The measured response to laminar flow was 40 μV(a-c)/Pa (±20V - 10 kHz drive) equivalent differential voltage on the transistor gates, and the calculated response was 42 μV(a-c)/Pa. Drifts as well as moisture sensitivity of the structure were identified. The noise floor of the system was approximately 0.1 Pa. The normal pressure response of the sensor was measured and found to be negligible. Several parts were packed for insertion into the wind tunnel. While the sensors responded to flow in the wind tunnel, the magnitude of shear stress generated (~0.1 Pa) was not large enough to allow for dynamic measurements.

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# TABLE OF CONTENTS

<table>
<thead>
<tr>
<th>Section</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>ABSTRACT</td>
<td>2</td>
</tr>
<tr>
<td>ACKNOWLEDGEMENTS</td>
<td>4</td>
</tr>
<tr>
<td>TABLE OF CONTENTS</td>
<td>6</td>
</tr>
<tr>
<td>LIST OF FIGURES</td>
<td>9</td>
</tr>
<tr>
<td>LIST OF TABLES</td>
<td>13</td>
</tr>
<tr>
<td>1 INTRODUCTION</td>
<td>14</td>
</tr>
<tr>
<td>A Shear-Stress in Turbulent Boundary Layers</td>
<td>14</td>
</tr>
<tr>
<td>B Measurement Techniques</td>
<td>19</td>
</tr>
<tr>
<td>C Floating-Element Shear-Stress Sensors</td>
<td>20</td>
</tr>
<tr>
<td>D Micromachining Techniques and Polyimide</td>
<td>29</td>
</tr>
<tr>
<td>E Outline of Thesis</td>
<td>32</td>
</tr>
<tr>
<td>2 SENSOR DESIGN</td>
<td>33</td>
</tr>
<tr>
<td>A Microfabricated Floating Element</td>
<td>33</td>
</tr>
<tr>
<td>B Mechanical Analysis - Static</td>
<td>36</td>
</tr>
<tr>
<td>C Mechanical Analysis - Dynamic</td>
<td>44</td>
</tr>
<tr>
<td>D Normal Pressure, Gap, and Protrusion Effects</td>
<td>50</td>
</tr>
<tr>
<td>E Effect of a Pressure Driven Laminar Flow</td>
<td>54</td>
</tr>
<tr>
<td>F Differential Capacitor Readout</td>
<td>55</td>
</tr>
<tr>
<td>G Sensor Layout</td>
<td>62</td>
</tr>
<tr>
<td>3 FABRICATION AND PACKAGING</td>
<td>65</td>
</tr>
<tr>
<td>A Metal-Gate Depletion-Mode PMOS Process</td>
<td>65</td>
</tr>
<tr>
<td>B Floating Element Fabrication</td>
<td>66</td>
</tr>
<tr>
<td>C Laminar Flow Packaging</td>
<td>73</td>
</tr>
<tr>
<td>D Wind Tunnel Packaging</td>
<td>85</td>
</tr>
</tbody>
</table>
4 MECHANICAL PROPERTIES CHARACTERIZATION...
A Fabrication Sequence and Test Mask......................... 87
B Experimental Methods......................................... 94
C Diaphragm Deflection Model.................................. 99
D Cantilever Deflection Model................................ 103
E Experimental Results - General.............................. 108
F Experimental Results - Diaphragms and Cantilevers...... 113
G Summary......................................................... 119

5 SENSOR CALIBRATION........................................... 120
A Experimental Methods......................................... 120
B Cell Calibration................................................ 125
C Measured Flow Response....................................... 130
D Noise, Drift, and Moisture Effects......................... 135
E Comparison with Model....................................... 137
F Effect of Channel Height..................................... 140
G Acceleration Sensitivity...................................... 143
H Normal Pressure Response.................................... 145
I Wind Tunnel Results........................................... 146
J Summary......................................................... 146

6 CONCLUSIONS................................................. 149

APPENDICES........................................................ 152
A Deflection Equation for a Clamped-Clamped Bridge under
Axial Tension.......................................................... 152
B Computer Model of Sensor Response.......................... 155
C Process Sequences.............................................. 159
  1 PMOS and Floating Element................................. 159
  2 Laminar Cell Wafer........................................... 162
3  Mechanical Properties Wafer ........................................ 163
D  Analysis of Passivation Process .................................... 166
E  Package Designs .......................................................... 169
F  Current to Voltage Converter ........................................ 178
REFERENCES .................................................................... 180
LIST OF FIGURES

1. Velocity profile at the wall ................................................................. 15
2. Structure of a turbulent boundary layer ............................................... 17
3. Preston tube for shear stress measurement .......................................... 21
4. Razor blade Stanton tube reported by East[10] .................................... 22
5. Sublayer fence. (a) Simplest form. (b) Two dimensional measure...... 23
6. Hot film and hot wire shear-stress sensors ........................................... 24
7. Floating-element shear-stress sensor ..................................................... 24
8. Forces acting on floating element due to misalignment ....................... 27
9. Floating element design to eliminate bending moment sensitivity...... 28
10. Floating element design to eliminate acceleration sensitivity .......... 28
11. Surface micromachining process ......................................................... 30
12. Microfabricated floating element concept .......................................... 34
13. Simple fabrication sequence for floating element ............................. 35
14. SEM photograph of a 1 mm x 1 mm floating element with 1 mm x 10 \( \mu \text{m} \) tethers fabricated out of an 8 \( \mu \text{m} \) thick polyimide film .... 37
15. SEM photograph of tether of floating element in Figure 14 ............... 38
16. SEM photograph of anchor point of tether in Figure 15 ................. 39
17. (a) Deflected floating element. (b) Model for deflections. (c) Calculated mechanical sensitivity of floating element ( dimensions given in text ). 40
18. (a) Deformation of pre-stressed released structure. (b) Deformation of floating element ................................................................. 43
19. (a) Mass-spring-dashpot lumped system. (b) Deflection versus frequency of applied load for lumped system ................................. 45
20. Fluid flow under plate during lateral vibrations .................................. 49
21. Fluid flow under plate during normal vibrations ............................... 49
Force acting on a protruding floating element........................................ 53
Additional forces acting on a floating element in a pressurized flow.... 53
(a) Schematic illustration of floating element readout. (b) Definition of
parameters in readout circuit model. (c) Equivalent circuit for readout.. 56
Electrostatic forces acting on a parallel plate capacitor......................... 60
Layout of prototype chip ( 5 mm x 4 mm )......................................... 64
Cross-section through process of fabricating metal-gate PMOS......... 67
Cross-section through process of fabricating the floating element...... 71
SEM photograph of fabricated floating-element shear-stress sensor.... 74
SEM photograph close-up view of element in Figure 29......................... 74
Schematic illustration of importance of sensor placement................... 75
Requirements on packaging for laminar flow..................................... 75
Laminar flow cell assembly.............................................................. 77
Sensor plate...................................................................................... 77
Fabrication and assembly of sensor plate........................................... 78
2 inch wafer used for sensor plate...................................................... 80
Photomicrograph of pressure tap....................................................... 80
Sensor plate...................................................................................... 81
Assembled flow cell........................................................................... 81
Flow cell on probe station................................................................. 82
Illustration of entry length............................................................... 84
Illustration of wind tunnel package.................................................. 86
Photograph of wind tunnel package.................................................. 86
Cross-section of process for fabrication of mechanical properties wafer. 88
Incorporation of spacer in process..................................................... 91
Layout of mechanical properties wafer.............................................. 92
Layout of cantilevers on mechanical properties wafer...................... 93
<table>
<thead>
<tr>
<th>Page</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>48</td>
<td>Mechanical properties wafer after anisotropic etching</td>
</tr>
<tr>
<td>49</td>
<td>Cantilever sections of wafer</td>
</tr>
<tr>
<td>50</td>
<td>Photomicrograph of cantilevers</td>
</tr>
<tr>
<td>51</td>
<td>Illustration of load-deflection apparatus</td>
</tr>
<tr>
<td>52</td>
<td>Photograph of load-deflection apparatus</td>
</tr>
<tr>
<td>53</td>
<td>Two load-deflection diaphragms</td>
</tr>
<tr>
<td>54</td>
<td>Illustration of deflected cantilevers</td>
</tr>
<tr>
<td>55</td>
<td>Deflection versus pressure data for 14 μm thick polyimide diaphragm</td>
</tr>
<tr>
<td>56</td>
<td>Definition of parameters in load-deflection theory</td>
</tr>
<tr>
<td>57</td>
<td>Normalized load-deflection data</td>
</tr>
<tr>
<td>58</td>
<td>Definition of parameters in cantilever model</td>
</tr>
<tr>
<td>59</td>
<td>Deformation of a prestressed composite cantilever</td>
</tr>
<tr>
<td>60</td>
<td>Photomicrograph of 300 Å aluminum film in polyimide</td>
</tr>
<tr>
<td>61</td>
<td>Illustration of aluminum stress relaxation effect</td>
</tr>
<tr>
<td>62</td>
<td>Photomicrograph of 300 Å chrome film in polyimide</td>
</tr>
<tr>
<td>63</td>
<td>Cantilever deflection versus polyimide thickness</td>
</tr>
<tr>
<td>64</td>
<td>Laminar flow testing apparatus</td>
</tr>
<tr>
<td>65</td>
<td>Current-to-voltage converter</td>
</tr>
<tr>
<td>66</td>
<td>Normal pressure test apparatus</td>
</tr>
<tr>
<td>67</td>
<td>Pressure versus flow for laminar cell with 217 μm thick shim</td>
</tr>
<tr>
<td>68</td>
<td>Effect of plate misalignment on shim height</td>
</tr>
<tr>
<td>69</td>
<td>Pressure versus flow for laminar cell with 426 μm thick shim</td>
</tr>
<tr>
<td>70</td>
<td>Differential voltage output versus flow with 217 μm thick shim</td>
</tr>
<tr>
<td>71</td>
<td>Differential voltage output versus shear stress using data in Figure 70.</td>
</tr>
<tr>
<td>72</td>
<td>Illustration of system gain measurement technique</td>
</tr>
<tr>
<td>73</td>
<td>Sensor response at high flow for 217 μm thick shim</td>
</tr>
<tr>
<td>74</td>
<td>Single channel input-output response of sensor</td>
</tr>
</tbody>
</table>
Differential voltage output versus flow with 426 μm thick shim........ 141
Differential voltage output versus flow for both shim thicknesses...... 142
Measured acceleration sensitivity............................................. 144
Single channel response to 84 Pa - 3 kHz normal pressure wave
( sensor is driven at 10 kHz )................................................ 147
Differential response to 84 Pa - 3 kHz normal pressure wave ( sensor
is driven at 10 kHz )............................................................. 148
LIST OF TABLES

1  Metal-gate PMOS process parameters.......................... 69
2  Cantilever deflections for three different samples.............. 115
3  Polyimide thickness for four different samples................... 115
4  Results of load-deflection measurements........................ 117
5  Measured cantilever deflections.................................. 117
CHAPTER 1

INTRODUCTION

This thesis reports on the development of a shear-stress probe intended for use in the study of turbulent boundary layers in air. The sensor is a microfabricated version of what is commonly know as a floating-element balance. In this chapter we will motivate the desire to make shear-stress measurements by summarizing the structure of turbulent boundary layers. The current methods employed to measure shear-stress will be reviewed, with particular emphasis on the floating element technique. Finally, a summary of microfabrication technologies available for the development of microfabricated floating-element structures will be presented.

A) Shear-Stress in Turbulent Boundary Layers

Fluid flow past a boundary will produce a gradient in the velocity normal to the wall (Figure 1). This gradient establishes a shear stress $\tau_w$ at the wall which can be expressed as

$$\tau_w = \mu \frac{\partial u_x}{\partial y}$$

(1)

where $\mu$ is the absolute viscosity of the fluid and $u_x$ is the velocity in the $x$ direction. The wall shear stress is one of the most important parameters in bounded turbulent flows[1]. Both the mean and fluctuating part are manifestations of the overall structure of the flow above the wall. The mean value is related to the drag characteristics of a particular configuration, while the fluctuating part is of importance in sound generation, separated flows, passive or active control of turbulence and, in general, assessment of which types of flow structures are primarily responsible for momentum transfer between the outer part of the boundary layer and the wall. In the Turbulence Research Laboratory wind tunnel at
Fig. 1  Velocity profile at the wall.
MIT we would like to be able to measure shear stresses of 1 Pa with a spatial resolution of 100 μm, and a bandwidth of 20 kHz.

A number of investigators have concentrated on the near-wall structure of turbulence, mostly in channel, pipe and planar boundary-layer flows. Measurements to date show a rather large scatter in measured values of the shear stress, in many instances, using the same probes under almost identical conditions [2-7]. The measurements of the fluctuating shear stress, in particular, cover a range whose extreme values differ by approximately an order of magnitude. It is therefore clear that the measurement of the fluctuating shear stress is not a trivial matter.

In order to understand the requirements of instruments designed to measure wall shear stress it is useful to understand the structure of a turbulent boundary layer. Figure 2(a) is a plot of velocity versus distance from the wall. The velocity in the free stream region above the plate, $U_\infty$, is uniform and drops rapidly to zero at the wall. This region, of thickness $\delta$, where the velocity drop from the free stream value to zero is identified as the boundary layer.

In a turbulent boundary layer there are several distinct regions illustrated in Figure 2(b), which shows a plot of velocity expressed in non-dimensional units $u^+$, versus distance from the wall in non-dimensional units $y^+$ (both $u^+$ and $y^+$ are defined below). A detailed description of the structure of a turbulent boundary layer may be found in Hinze [1]. The outer part of the boundary layer is called the wake region. In this outer region the effect of the wall is felt indirectly through the shear stress at the wall. The inner 20% of the boundary layer is identified as the inner region or wall region. The mean velocity in the inner region, $\bar{u}$, is a function of the shear stress at the wall $\tau_w$, the height above the wall $y$, the fluid density $\rho$, and the absolute viscosity $\mu$. A characteristic velocity in the inner region, called the friction velocity, $u^*$, is defined as

$$\rho u^*^2 = \tau_w$$ (2)
Fig. 2  Structure of a turbulent boundary layer.
Thus, velocity may be expressed in non-dimensional units $u^+$ where

$$ u^+ = \frac{\bar{u}}{u^*} \quad (3) $$

Also a characteristic length or wall unit, $y^*$, can be defined as

$$ y^* = \frac{\mu}{\rho u^*} \quad (4) $$

such that the distance from the wall may be written in non-dimensional units $y^+$ as

$$ y^+ = \frac{y}{y^*} \quad (5) $$

Dimensional analysis yields a relationship for velocity as a function of distance know as the law of the wall;

$$ u^+ = f(y^+) \quad (6) $$

Within an extremely thin layer near the wall where viscous effects dominate, the law of the wall has a linear form $u^+ = y^+$. This relationship holds for $0 < y^+ < 5$ where the region is known as the viscous sublayer or the linear region. For values of $y^+$ greater than approximately 45 the law of the wall is of the form

$$ u^+ = \frac{1}{\kappa} \ln y^+ + B \quad (7) $$
where $\kappa$ and $B$ are constants which depend on the particular flow configuration. This is known as the log region and it extends out to approximately $0.2\delta$. Between the viscous sublayer and the log region is what is known as the buffer region.

Based on this description of turbulent boundary layers it is obvious that in order to measure wall shear stress it is necessary for the probe to be within the inner region, where the effects of the wall dominate. It is most desirable to locate the probe entirely within the viscous sublayer. However, as the velocity increases, the thickness of this layer decreases, making it difficult to remain in the viscous sublayer. If a shear stress of 1 Pa is to be measured, in air for example, this corresponds to a $u^*$ of 0.88 m/s and thus a wall unit of 17 $\mu$m.

B) Measurement Techniques

Several authors[8,9] have written reviews summarizing the variety of techniques used to measure wall shear stress. Hanratty and Cambell[8] summarize the six principle methods for measuring local shear stress as

1) The Preston Tube
2) The Stanton Tube
3) The sublayer fence
4) Thermal methods
5) The electrochemical technique
6) Direct measurement

In addition there are extrapolation techniques based on direct velocity measurements, either by hot wire or flow visualization methods.

Figure 3 illustrates a Preston tube which consists of a round Pitot tube placed on the surface of the wall. The difference between the pressure measured by this tube and the static pressure measured using a tap on the wall is used to indirectly determine the shear stress. A Stanton tube uses a rectangular Pitot tube in which one of the sides of the tube is
formed by the wall. Razor blades are commonly used to make Stanton tubes as illustrated in Figure 4, taken from East[10].

A sublayer fence is an obstruction that is placed in the flow at the wall (Figure 5). The fence is sized such that it only protrudes into the viscous sublayer of the turbulent boundary layer. The pressure difference on either side of the fence is measured from which the shear stress is inferred. Figure 5(a) is a schematic illustration of a sublayer fence, and figure 5(b) shows a fence structure proposed to measure shear stress in two directions[11].

Thermal methods include hot-wire and hot-film techniques shown in Figure 6. The heat that is convected away from the film is monitored, from which the shear stress can be inferred. Electrochemical techniques are the mass transfer analog of the hot-film probe. An electrochemical reaction is carried out on the surface of an electrode mounted flush with the wall. The reaction rate can be modified by the flow, from which the shear stress is determined.

The direct measurement schemes are known as skin friction balances or floating element balances. In this scheme, the local tangential force is measured by allowing a section of the wall bounding the flow to move laterally under the shear stress load (Figure 7). The displacement of the structure or the force required to restore it to a null position are measured, from which the shear stress is directly determined. The floating element balance is attractive for turbulent boundary layer measurements since it is a direct measurement and hence does not require that any assumptions be made about the nature of the flow in the vicinity of the device.

C) Floating-Element Shear-Stress Sensors

The usefulness of a direct measurement has promoted the development of a variety of floating-element balance designs. Winter[9] summarizes many of these structures and identifies some of the principal problems associated with floating element balances as
Fig. 3  Preston tube for shear stress measurement.
Fig. 4  Razor blade Stanton tube reported by East[10].
Fig. 5  Sublayer fence. (a) Simplest form. (b) Two dimensional measure.
Fig. 6  Hot film and hot wire shear-stress sensors.

Fig. 7  Floating-element shear-stress sensor.
1) Provision of a transducer for measuring small forces or deflections, and the compromise between the requirement to measure local properties and the necessity of having an element of sufficient size that the force on it can be measured accurately.

2) The effect of the necessary gaps around the floating element.

3) The effects of misalignment of the floating element.

4) Forces arising from pressure gradients.

5) The effect of gravity or of acceleration if the balance is to be used in a moving vehicle.

6) Effects of temperature changes.

7) Effects of heat transfer.

8) Use with boundary layer injection or suction.

9) Effects of leaks.

10) Protection of the measuring system against transient normal forces during starting and stopping in a supersonic tunnel.

A shear stress of 1 Pascal acting on a 1 mm\(^2\) square element produces a force of \(10^{-6}\) Newton, or 102 \(\mu\)g. Thus, to measure shear stress on a 100 \(\mu\)m x 100 \(\mu\)m square with 1% accuracy would require a resolution of 10 picogram. Because of these limits, most of the currently available techniques employ floating elements that are at least 1 cm\(^2\).

The effect of gaps and misalignments of these types of probes has been extensively studied by Allen[12,13]. Gaps between the element and the wall can produce flow around the structure and introduce additional forces that may disturb the measurement. The misalignment of the structure can also introduce forces on the element. Figure 8, taken from Allen[12,13], highlights the forces acting on a structure due to misalignment of the element. The edge forces act identically on the structure as \(\tau_w\), but the normal forces produce a bending moment. Thus, the normal force effect can be reduced by using a
moment insensitive structure like the parallel-linkage balance shown in Figure 9. This type of balance will also exhibit less sensitivity to moments introduced by pressure gradients.

The acceleration sensitivity problem can be particularly acute for a sensor used in a moving system. We can define an equivalent shear stress due to acceleration, \( \tau_a \), as

\[
\tau_a = \frac{F}{A} = \frac{Mg}{A} = \rho t p g
\]

where \( g \) is acceleration, \( M \) is the mass of the element, \( A \) is the surface area, \( \rho \) is the density of the material used to make the element, and \( t_p \) is the thickness of the floating element. Using the density of steel (8000 kg/m\(^3\)) and a thickness of 0.25 cm, the equivalent shear stress for an acceleration of 1 g is 196 Pa! This acceleration sensitivity can be reduced by reducing either the density of the material or its thickness. Another method that has been suggested to reduce acceleration sensitivity is show in figure 10. In this balance design, a counter weight is placed below the pivot point of the balance so that the center of gravity for lateral acceleration is at the pivot point.

In addition to the complications identified by Winter, there are limits on the performance imposed by the mechanical resonance of the structure. Specifically, if it is desired to measure the dynamic shear-stress, the bandwidth will be limited by the first lateral resonant frequency. This is typically on the order of 100 Hz.

In spite of all these difficulties, direct measurement methods remain of interest if only to serve as a method to calibrate an indirect method. Most of the complications of using a floating-element balance can be substantially removed if the dimensions of the balance can be reduced while retaining the ability to measure the forces generated for a given shear-stress. This recognition is what motivates the development of a microfabricated floating-element structure with integrated readout. The approach is to use micromachining techniques in conjunction with polyimide, as described further below.
Fig. 8  Forces acting on floating element due to misalignment.
Fig. 9  Floating element design to eliminate bending moment sensitivity.

Fig. 10  Floating element design to eliminate acceleration sensitivity.
D) Micromachining Techniques and Polyimide

There are two principal techniques employed in microfabrication; bulk micromachining, and surface micromachining. In bulk micromachining, anisotropic etchants such as potassium hydroxide, ethylenediamine-pyrocatechol-water, and hydrazine hydrate are used to etch silicon wafers. These etchants exhibit high selectivity against etching of the <111> crystalline plane. As a consequence, well defined square cavities may be etched in the silicon wafer. The etch rate is also dependent on doping level, such that heavily boron doped layers can be used as etch stops. Electrochemical etch stops are also possible. Peterson[14] provides a very useful summary of bulk micromachining and its applications.

Surface micromachining involves the removal of sacrificial thin films from under other deposited thin films to form free-standing microstructures. Figure 11 shows a simple process for fabricating a cantilever structure with this technology. A sacrificial spacer film is first deposited and patterned (Figure 11(a)). The structural material is then deposited and patterned over the top of the sacrificial layer to form the cantilever (Figure 11(b)). The cantilever structure is then released by the isotropic etching of the sacrificial layer with an etchant that does not attack the structural material (Figure 11(c)). This technique was first exploited by Nathanson[15] in the fabrication of a resonant cantilever beam for an oscillator circuit. The structural material used was gold and the sacrificial spacer was nickel. Since that time a number of interesting structures have been fabricated using a wide variety of materials[16-26]. Howe[25] has used polysilicon as a structural material with a silicon dioxide spacer to form a resonant bridge vapor sensor. Guckel[26] has also used the polysilicon technology along with other material combinations such as aluminum-silicon-copper alloys with a nickel spacer. The planar nature of surface micromachining makes it attractive for a flush-mounted shear-stress sensor.
Fig. 11  Surface micromachining process.
In this thesis we will use polyimide as the structural material with an aluminum sacrificial spacer. Polyimide is a high temperature polymer that is receiving increased attention for use in integrated circuit applications[27]. It is used as both a interlevel insulator for multilevel metallization processes as well as a passivant for the completed integrated circuit.

The term polyimide refers to a general class of polymers which can be synthesized from a dianhydride and a diamine. The polyimide as received from the supplier is a polyamic acid precursor in liquid form in a solvent (N-Methylpyrrolidone or NMP). This liquid is spin cast on the substrate and thermally cured to form the polyimide. The thermal curing is a two-stage process. The wafer is first baked at a low temperature to drive off the solvent and begin the formation of the imide group. This step is performed at 145°C for 15 minutes in this thesis. Following this "soft-bake", the wafer is then cured in N₂ at 400°C for 45 minutes in order to complete the cure cycle. Multiple coats of polyimide may be applied by soft-baking between each coat and then curing the entire film at the end.

Cured polyimide is very resistant to attack by a wide variety of acids. It can be patterned either by plasma etching before or after cure, or by wet chemical etching prior to cure. It is an electrical insulator with a bulk resistivity of approximately $10^{18}$ Ω-cm and a relative dielectric permittivity of 3.5. Polyimide is susceptible to moisture absorption and it has been used as the sensitive element in humidity sensors[28]. The polyimide we will use has an elastic modulus of 3-4 GPa and is typically under residual tensile stress on the order of 30 MPa when deposited on a silicon substrate[30]. There are a number of newer polyimides which can have substantially different modulus and tensile stress numbers[29].

The low temperatures required for use of polyimide make it possible to readily merge it with a standard integrated circuit process. This is attractive from the point of view making microfabricated mechanical components with integrated electronics. In addition, the planarizing properties of polyimide will make it possible to fabricate microstructures which
will be very flat. It is these reasons as well as its desirable mechanical properties that motivated the use of this material system for the floating-element sensor.

E) Outline of Thesis

Chapter 2 will describe the design of the microfabricated floating-element shear-stress sensor. A mechanical analysis of the structure will be presented as well as the design of the readout circuit. Finally, the sensor layout is reviewed. Chapter 3 discusses the fabrication processes used for both the integrated readout and the floating-element. The packaging of the sensor for testing in both laminar and turbulent flow will also be described and the fabrication of the package detailed. The mechanical characterization of the polyimide and polyimide/metal composite element is reported in Chapter 4. Chapter 5 presents the results of the calibration of the sensor. The conclusions drawn from this work are summarized in Chapter 6.
CHAPTER 2
SENSOR DESIGN

This chapter presents the design of a micromachined floating-element shear-stress sensor. The mechanical structure is analyzed in order to develop design equations for the sensor. The expected performance of the structure under normal pressure is discussed. Once the mechanical element is analyzed, a differential capacitor readout scheme will be presented. The electrical model for the circuit is derived and electro-mechanical interactions with the floating element are studied. An estimate of the minimum detectable signal is made based on an equivalent noise model for the circuit. The chapter concludes with a presentation of the layout of the sensor chip.

A) Microfabricated Floating Element

Figure 12 shows the simplest form of a microfabricated structure that will exhibit sensitivity to shear-stress. The structure consists of a square element suspended above the surface of the silicon wafer by four tethers which have their ends attached to the substrate. If the silicon wafer bounds the flow, there will be a shear-stress acting over the entire surface of the square element. This will cause the element to displace laterally against the restoring force of the tethers. Tensile stress in the structural material will help to keep the element suspended above the surface and it will also limit the mechanical deflection for a given shear-stress load.

It is possible to make the microfabricated floating-element very smooth to the flow by leaving some structural material remaining in the field region surrounding the element. Figure 13 presents a top view and cross section of the fabrication sequence that would be employed to do this. This sequence is similar to that shown in the previous chapter. We begin with the deposition, by evaporation, of a 3 μm thick aluminum spacer layer. The layer is patterned using wet chemical etching (Figure 13(a)). Next the polyimide structural
Fig. 12  Microfabricated floating element concept.
Fig. 13 Simple fabrication sequence for floating element.
material is spun-cast in multiple coats to the desired thickness (~30 μm). The polyimide is patterned and etched anisotropically using an O₂ plasma (Figure 13(b)). The polyimide remains in the field, and a "slot" is patterned to define the element and tethers. The width of this slot will be defined either by what is possible lithographically, or the maximum deflection expected for this structure, whichever is larger. The final step is the release of the structure by wet chemical etching of the aluminum spacer (Figure 13(c)). The element is shown deflected under a shear stress load. Note that in cross-section the element can look very smooth to the flow, exhibiting gaps that are as small as can be made using microfabrication. In addition, the element will be very smooth due to the fact that polyimide tends to planarize the structures which it covers.

Figure 14 is an scanning electron microscope (SEM) photograph of a polyimide floating element. The polyimide has been removed from the field region for clarity. This is an 8 μm thick polyimide film which was deposited over a 3 μm thick aluminum layer. The element is 1 mm x 1 mm in area with tethers that are 1 mm x 10 μm. Figure 15 is a closer view at the tether at the anchor point. Figure 16 is a close up of the step up of the polyimide tether. The extent of planarization can be seen in this photograph by comparing the top and bottom surfaces of the polyimide. These structures were placed in a crudely constructed wind tunnel and were observed to deflect laterally with increasing flow.

B) Mechanical Analysis - Static

An analytical model for the lateral deflections of the floating-element structure has been developed and is highlighted in Figure 17. The floating element has a surface area of \( A_p \) and it is loaded by a force \( \tau_w A_p \) which causes it to deflect a lateral distance \( \delta \). The surface area \( A_p \) equals the product of the width of the element \( W_e \) and the length of the element \( L_e \). The tethers are each of length \( L_t \), width \( W_t \), and thickness \( t_p \) (Figure 17(a)).

If we assume that the plate moves rigidly under an applied shear stress load, we can treat each set of tethers as a clamped-clamped bridge of length \( 2L_t \) (Figure 17(b)).
Fig. 14  SEM photograph of a 1 mm x 1 mm floating element with 1 mm x 10 μm tethers fabricated out of an 8 μm thick polyimide film.
Fig. 15  SEM photograph of tether of floating element in Figure 14.
Fig. 16  SEM photograph of anchor point of tether in Figure 15.
Fig. 17  (a) Deflected floating element.  (b) Model for deflections.  (c) Calculated mechanical sensitivity of floating element (dimensions given in text).
force acting on the center of the bridge \( P \) will be one half the total force acting on the square plate, since there are two sets of tethers. There will also be a distributed force \( q \) that would represent the shear stress acting on the face of each tether. Finally, it is possible to incorporate an axial tension load \( S \) on the bridge which would represent the effect of the residual tensile stress in the polyimide.

The solution for the deflection \( \delta \) of the structure shown in Figure 17(b) can be written as (Appendix A)

\[
\delta = \frac{PL_t}{2S} \left[ 1 + \frac{qL_t}{P} \right] \left\{ 1 - \frac{\tanh \left( \frac{u}{2} \right)}{\frac{u}{2}} \right\} 
\]  

(9)

where \( u = L_t \left[ \frac{S}{E_I} \right]^\frac{1}{2}, E_p \) is the modulus of elasticity of polyimide, and \( I \) is the bending moment of inertia of the tether \( \left( \frac{1}{12} t_p W_t^3 \right) \). The loads on the structure are expressed as \( P = \frac{1}{2} \tau_w W_e L_e \), \( q = \tau_w W_t \), and \( S = \sigma_p t_p W_t \), where \( \sigma_p \) is the residual tensile stress in the polyimide. This model assumes the deflections are small compared to the tether width. The sensitivity of this structure \( \delta/\tau_w \) can be calculated as

\[
\frac{\delta}{\tau_w} = \frac{W_e L_e L_t}{4 \sigma_p t_p W_t} \left[ 1 + 2 \frac{W_t L_t}{W_e L_e} \right] \left\{ 1 - \frac{\tanh \left( \frac{u}{2} \right)}{\frac{u}{2}} \right\} 
\]  

(10)

Figure 17(c) is a plot of \( \delta/\tau_w \) as a function of polyimide residual stress for a structure with the dimensions, \( W_e = L_e = 500 \mu m, W_t = 10 \mu m, L_t = 1 \) mm, \( t_p = 30 \) \mu m, and a polyimide modulus of elasticity of 4 GPa. There are two distinct regions of the sensitivity plot. At low values of residual stress the sensitivity is independent of axial tension and is determined only by the modulus and the geometry of the structure. This is the bending beam region. In this region \( u \ll 1 \) and Eqns. 9-10 can be reduced to
\[ \delta = \frac{PL_t}{24E_Pl} \left[ 1 + \frac{qL_t}{P} \right] \]  

(11a)

or

\[ \frac{\delta}{\tau_w} = \frac{W_eL_eL_t}{4E_plp} \left[ \frac{L_t}{W_t} \right]^3 \left[ 1 + 2\frac{W_iL_t}{W_eL_e} \right] \]  

(11b)

At high values of residual stress the sensitivity becomes a linear function of the stress since \( u \gg 1 \) and Eqns. 9-10 become;

\[ \delta = \frac{PL_t}{2S} \left[ 1 + \frac{qL_t}{P} \right] \]  

(12a)

or

\[ \frac{\delta}{\tau_w} = \frac{W_eL_eL_t}{4\sigma_plpW_t} \left[ 1 + 2\frac{W_iL_t}{W_eL_e} \right] \]  

(12b)

In this region the load-deflection characteristics are controlled by the axial tension, and the structure behaves like a tensioned wire. The residual stress for the polyimide chemistry that we will be using is measured to be approximately 30 MPa and thus it is important to recognize the role that this stress plays in the mechanical response. The expected sensitivity for this value of polyimide residual stress is 7.5 nm/Pa.

Because the polyimide film is under uniform tensile stress when deposited, after patterning and release it is possible that the stress in the tethers will be higher than the uniform stress \( \sigma_p \). This can be caused by a load imbalance in the patterned structure.

Figure 18(a) highlights how this might occur in the case of a non-uniform cross-section beam. The point where the cross-section changes will have an unbalanced force due to the difference in width of the two sections. This will cause this point to move in the direction of the wider section in the case of tensile stress. The new equilibrium position by force balance is
Fig. 18  (a) Deformation of pre-stressed released structure. (b) Deformation of floating element.
\[
\left[ \frac{\sigma_p}{E_p} + \frac{\Delta}{L_1} \right] W_1 = \left[ \frac{\sigma_p}{E_p} + \frac{\Delta}{L_2} \right] W_2
\]  

(13a)

\[
\Delta = \frac{\sigma_p}{E_p} \left[ \frac{W_2 - W_1}{W_1 + W_2} \right]
\]  

(13b)

where \( W_1 \) and \( L_1 \) are the width and length of the narrow region, \( W_2 \) and \( L_2 \) are the width and length of the wide region, and \( \Delta \) is the distance the line moved. This phenomena has been exploited by Mehregany[30,31,32] as a technique to measure stress in thin polyimide films.

In the case of the floating element structure, this shrinkage will also occur, potentially increasing the stress in the tethers. We can make a worst-case estimate of this effect by assuming the plate shrinks uniformly to a zero-stress state (Figure 18(b)). In this limit the increase in stress in the tethers can be expressed as

\[
\sigma'_p = \left[ 1 + \frac{L_p}{2L_t} \right] \sigma_p
\]

(14)

Using the parameters in this section, this translates to a stress increase in the tethers of 25%. In actuality, the stress increase will be less than that predicted by equation 13 because the loading of the tethers by the plate shrinkage is much more complex. For subsequent analysis of the structure we will ignore this effect.

C) Mechanical Analysis - Dynamic

Since we are also interested in measuring the time varying component of the shear stress we need to consider the dynamic response of the sensor. Specifically, we will need to determine the first resonance of the structure and make an estimate of the quality of resonance.

For a mass-spring-dashpot system (Figure 19(a)), the equation of motion is
Fig. 19  (a) Mass-spring-dashpot lumped system. (b) Deflection versus frequency of applied load for lumped system.
\[ M \frac{\partial^2 x}{\partial t^2} + c \frac{\partial x}{\partial t} + kx = F_d \]  

(15)

where \( M \) is the mass of the system, \( c \) is the drag coefficient, \( k \) is the spring constant, and \( F_d \) is the driving force. Figure 19(b) schematically illustrates the driven response of a system such as this. By assuming simple harmonic motion where \( x = X\cos(2\pi ft) \), the resonant frequency of this lumped mass-spring system is \([33,34,35]\)

\[ f_r = \frac{1}{2\pi} \sqrt{\frac{k}{M}} \]  

(16)

The critical damping coefficient, \( c_c \), is defined as the damping coefficient for which no oscillations occur and is equal to

\[ c_c = 2\sqrt{kM} \]  

(17)

The ratio \( \zeta = \frac{c}{c_c} \) is defined as the fraction of critical damping, and the quality factor of resonance \( Q \) is

\[ Q = \frac{1}{2\zeta} = \frac{\sqrt{kM}}{c} \]  

(18)

which for values of \( \zeta < 0.1 \) is equivalent to the ratio of the bandwidth over the resonant frequency (\( \Delta f/f_r \)).

Assuming the structure is in the tensioned wire region and neglecting the mass of the tethers, we recognize that the spring constant is obtained from Eqn. 12(b) as

\[ k = \frac{4\sigma_p L_t W_t}{L_t} \]  

(19)

and the mass is simply that of the element, \( M = \rho_p L_t W_e L_e \), with \( \rho_p \) = density of polyimide = 1400 kgm\(^{-3}\)\([36,37]\]. Substituting in and calculating using the above values (\( \sigma_p = 200 \) MPa), we obtain
\[ f_r = \frac{1}{2\pi} \sqrt{\frac{4\sigma_p W_I}{\rho_p W e L_e L_t}} = 9.3 \text{ kHz.} \] (20)

Note that this resonance is for motion both lateral and normal to the surface. This is a consequence of the fact that we are in the tensioned wire region. For bending dominated behavior, the two resonances can be different depending upon the aspect ratio of the tether cross-section.

We can make an estimate of the quality of resonance by modelling the motion of the air under the plate assuming it is an incompressible fluid. For the case of lateral motion, the drag on the plate can be estimated by assuming we have Couette flow in the gap. Thus the velocity of the fluid in the gap, \( u_x(y) \), may be written as\([38,39]\)

\[ u_x(y) = u_{\text{plate}} \left( \frac{y}{t_{\text{gap}}} \right) \] (21)

where \( u_{\text{plate}} \) is the velocity of the plate and \( t_{\text{gap}} \) is the channel height (Figure 20). This equation assumes that the time scale of oscillations is much greater than the time scale for viscous propagation through air. The time constant for viscous propagation is \( l^2/\nu \), which for \( l = 3 \mu m \) and \( \nu = 1.5 \times 10^{-5} \text{ m}^2/\text{s} \) gives a value of 0.6 \( \mu \text{s} \). Thus, this fluid mechanical model should be valid up to several hundred kHz. The drag force acting on the plate is simply the shear-stress generated by the fluid times the area of the plate

\[ F = \tau_{\text{drag}} W e L_e = \mu \frac{\partial u_x}{\partial y} W e L_e = \mu \frac{u_{\text{plate}}}{t_{\text{gap}}} W e L_e \] (22)

From equation 15 we can see that \( c = \frac{\mu}{t_{\text{gap}}} W e L_e \). Using this and the spring constant for the tensioned wire region (equation 19), we calculate a quality of resonance of
\[ Q = \frac{\sqrt{4 \sigma_{tp} W_t}}{L_t} \frac{\rho_{tp} W_e L_e}{\frac{\mu}{t_{gap}} W_e L_e} = 378 \]  

(23)

If the structure is in the bending dominated region, then the spring constant may be determined from equation 11b and the quality factor is

\[ Q = \frac{\sqrt{4 E_{tp} \left( \frac{W_t}{L_t} \right)^3}}{\frac{\mu}{t_{gap}} W_e L_e} \frac{\rho_{tp} W_e L_e}{\frac{\mu}{t_{gap}} W_e L_e} = 43.7 \]  

(24)

The bending dominated structure has a lower quality of resonance. This is consistent with the notion that the more "rigid" the structure (higher \( k \)), the higher the quality of resonance.

It is possible to estimate the \( Q \) for normal motion of the plate by using a fluid mechanics model for the flow under the plate as it moves up and down. This is called a squeeze film problem and is illustrated in Figure 21. For a circular plate of radius \( R \) and spacing \( t_{gap} \) from the substrate, the force \( F \) required to move the plate at a velocity \( u_{plate} \) is[39]

\[ F = \frac{3}{2} \mu \pi R \left( \frac{R}{t_{gap}} \right)^3 u_{plate} \]  

(25)

thus

\[ c = \frac{3}{2} \mu \pi R \left( \frac{R}{t_{gap}} \right)^3. \]  

(26)

If we equate the areas of a circle of radius \( R \) and a square of edge length \( L_e \), we can define an effective radius which is

\[ R_{eff} = \sqrt{\pi L_e} \]  

(27)

48
Fig. 20  Fluid flow under plate during lateral vibrations.

Fig. 21  Fluid flow under plate during normal vibrations.
Using this effective radius in equation 26 and the spring constant in the tensioned wire region, we calculate a quality factor of resonance of 0.03. Thus the normal damping force virtually eliminates any resonance.

It is useful to consider the response time of normal motion to a step force. The time constant for an over-damped system is \( \tau = c / k \). Using this expression we calculate a response time of approximately 1 ms. Thus any normal force (e.g., acceleration) operating above 1 kHz will not cause significant normal motion.

As a final note, we would like to be able to maximize the sensitivity to shear stress for a given bandwidth. If we define a bandwidth-sensitivity product

\[
BSP = \omega^2 \left[ \frac{\delta}{\tau_w} \right]
\]

Using the sensitivity \( \delta/\tau_w = A_p / k \) and the radial frequency \( \omega^2 = k / M \) from equation 16 we arrive at

\[
BSP = \frac{1}{\rho t_p}
\]

Therefore, since \( \rho \) is fixed by the material selection, the maximum bandwidth-sensitivity product is achieved by minimizing \( t_p \). In practice, the limit to which we can do this will be set by fabrication constraints.

D) Normal Pressure, Gap, and Protrusion Effects

In the measurement of shear-stress in a turbulent boundary layer, the probe will also experience normal pressures. These normal pressures will be due to acoustic pressure waves or turbulent eddies. In each case we would like the size of the element to be smaller than the scale of these pressure fluctuations so that their effect will be felt uniformly over the area of the plate.

Acoustic pressure waves will have a characteristic wavelength determined by the speed of sound in air and the acoustic frequency. This acoustic wave length \( \lambda \) can be
expressed as $\lambda = \frac{c}{f}$ where $c$ is the speed of sound in air (331 m/s), and $f$ is the acoustic frequency. If our element size is 500 µm, the acoustic frequency corresponding to this length is 0.7 MHz. This is much higher than any frequency that can be expected in the turbulent-boundary-layer, and therefore acoustic pressure fluctuations should not be a problem.

In a turbulent boundary layer the smallest turbulent eddies are of the order of $10y^*$. In high Reynold's Number turbulent flow, the friction velocity $u^*$ can be related to the free stream velocity as $u^* \equiv 0.03U_\infty$. Assuming $U_\infty = 10$ m/s, we can calculate from equation 2-4 that $\tau_w = 0.12$ Pa and $y^* = 50$ µm. Thus, the smallest turbulent eddy will be 500 µm which is of the order of the element. Therefore, as the element is scaled down, this effect will be reduced.

The gaps presented to the flow will be those between the element and the polyimide in the field region. Thus, they will be as small as can be patterned lithographically. Using plasma etching, etching through 30 µm thick polyimide will most likely require a 10-20µm gap. Provided this gap is less than several wall units, it will not disturb the flow[1]. The wall unit for a $U_\infty$ of 20 m/s is 25 µm from above, therefore the gaps should not disturb the flow. The gaps will allow flow around the element if a pressure gradient exists, but as we saw above, for this size element we should not experience large pressure gradients.

It is difficult to directly determine the contribution of protrusion induced forces acting on the element. However, we can make an estimate of what this effect might be by calculating the pressure induced by stopping the flow. This pressure can be expressed as $pux^2$. If we integrate this pressure over the face of the element we can write the total force as

$$F = \rho W_e \int_0^l u_x^2(y) \, dy$$  \hspace{1cm} (30)
where \( l \) is the height of the protrusion, and \( W_e \) is the width of the element (Figure 22).

Assuming the protrusion remains in the viscous sublayer, the velocity can be expressed using the shear stress as

\[
u(x) = \frac{\tau_w}{\mu} y
\]

Substituting this into eqn (30) we get

\[
F = \frac{1}{3} \frac{\rho}{\mu^2} W_e l^3 \tau_{w^2}
\]

The effect of this additional force may be expressed in terms of an effective shear stress by normalizing by the element area such that

\[
\tau_{w,\text{eff}} = \left[ 1 + \frac{1}{3} \frac{\rho}{\mu^2} \frac{l}{L_e} \tau_w \right] \tau_w
\]

The second term in the brackets represents the fractional contribution to the shear stress of the protrusion force. Assuming a \( U_\infty \) of 20 m/s and hence a shear stress, \( \tau_w \), of 0.47 Pa, the effective shear stress for a 3 \( \mu \)m protrusion is \( \tau_{w,\text{eff}} = \left[ 1 + 3 \times 10^{-5} \right] \tau_w \). Therefore, based on this approximation, the effect should be negligible.

It is possible to make a better estimate of this effect by using the results reported by Trilling and Hakkinen for calibration of a Stanton tube[40]. In that report, they found that the pressure acting on a protrusion can be expressed as \( p = 1.2 \tau_w \). This relation is valid provided we are in a Stokes flow region where \( \frac{\tau_w \rho l^2}{\mu^2} < 1 \). Using a shear stress of 1 Pa and a protrusion height, \( l \), of 3 \( \mu \)m, this quantity is 0.03, and thus the Stokes flow assumption is valid. The fractional contribution of this pressure force to the total shear stress will simply be \( 1.2 \frac{l}{L_e} \) which for the numbers used above is 0.7%. This contribution to the total shear stress will be negligible.
Fig. 22  Force acting on a protruding floating element.

Fig. 23  Additional forces acting on a floating element in a pressurized flow.
E) Effect of a Pressure Driven Laminar Flow

In most wind tunnel experiments, the flow pressure gradients are zero or moderate. However, in the case of a laminar flow cell, where large pressure gradients are present, additional forces can be introduced which act on the floating element.

Figure 23 shows a floating element balance placed in a laminar channel flow. The channel is of height $h$, and the element of thickness $t_p$ is above a gap of height $t_{gap}$. There are three forces acting on the element as shown in Figure 23: a shear stress on the upper surface, a shear stress on the lower surface, and a differential pressure acting on the edges. The shear stress in the channel is $[38,39]$

$$\tau_w = \frac{h}{2} \frac{dp}{dx} \quad (34)$$

The shear stress acting on the lower surface, $\tau_g$, due to the flow through the gap is

$$\tau_g = \frac{t_{gap}}{2} \frac{dp}{dx} \quad (35)$$

Finally, the pressure difference force acting on the face of the element is

$$F_p = t_p W_e \Delta p = t_p W_e \frac{dp}{dx} L_e \quad (36)$$

If we sum all of the forces and divide by the surface area of the element, we can define an effective shear stress, $\tau_{w,eff}$, which accounts for these forces.

$$\tau_{w,eff} = \tau_w \left[ 1 + \frac{t_{gap}}{h} + \frac{2t_p}{h} \right] \quad (37)$$

As we will see in Chapter 5, these additional force terms can add substantially to the measured response.
F) Differential Capacitor Readout

Based on the deflection analysis of Section B, a readout scheme is needed which can resolve deflections of 7.5 nm. The differential-capacitor readout scheme which we have chosen is shown in Figure 24(a). Readout schemes similar to this have been used in a dielectric measurement system\[41,42\], and a tactile sensor\[43\]. Three passivated electrodes are located on the surface of the wafer underneath the element and a thin conductor is embedded in the polyimide. The capacitive coupling between the center drive electrode and the two symmetrically placed sense electrodes is modified by motion of the floating element. This change in capacitance is transduced by connecting the sense electrodes to a pair of matched depletion-mode metal-gate p-MOSFETs on chip. A capacitive divider is formed from the line and gate capacitance. The channel current from the matched devices is then measured off-chip using a pair of trans-resistance amplifiers. Figure 24(b) is a more detailed view of the element and electrodes, and Figure 24(c) is an equivalent circuit model of this readout scheme. All of the capacitors are modeled as parallel-plate type and fringing capacitances are neglected. The drive voltage $V_d$ is coupled to the plate ($V_p$) through $C_{dp}$ which can be written as

$$
C_{dp} = \left[ \frac{\varepsilon_o}{t_{gap} + \frac{\varepsilon_o}{\varepsilon_{pi}} t_{p11} + \frac{\varepsilon_o}{\varepsilon_{ox}} t_{po}} \right] L_d W_d = \frac{\varepsilon_o}{t_{gap}} L_d W_d \tag{38}
$$

where $t_{gap}$ is the air gap thickness, $t_{p11}$ is the thickness of polyimide between the air gap and the embedded electrode, $t_{po}$ is the thickness of the electrode passivation (oxide/polyimide), $\varepsilon_o$ is the permittivity of free space (8.85x10^{-14} \text{ F/cm}), $\varepsilon_{pi}$ is the relative permittivity of polyimide (3.5), $\varepsilon_{ox}$ is the relative permittivity of the passivation layer (taken to be that of silicon dioxide, 3.9), $L_d$ is the drive electrode length, $W_d$ is the
Fig. 24  (a) Schematic illustration of floating element readout. (b) Definition of parameters in readout circuit model. (c) Equivalent circuit for readout.
Fig. 24  (a) Schematic illustration of floating element readout. (b) Definition of parameters in readout circuit model. (c) Equivalent circuit for readout.
drive electrode width, and $t'_{gap}$ is the effective air gap thickness for this composite dielectric.

The sense capacitances $C_{ps1}$ and $C_{ps2}$ vary linearly with deflection $\delta$ of the plate and can be written as

$$C_{ps1} = C_{ps0} \left[ 1 - \frac{\delta}{f W_s} \right] \quad C_{ps2} = C_{ps0} \left[ 1 + \frac{\delta}{f W_s} \right] \quad (39)$$

where $C_{ps0}$ is the undeflected capacitance (assuming the channels are matched), $W_s$ is the width of the sense electrode, and $f$ is the fraction of the sense electrode overlapped by the conductor in the element. The undeflected capacitance is written as

$$C_{ps0} = \frac{\varepsilon_{o}}{t'_{gap}} f L_s W_s \quad (40)$$

There are parasitic capacitances $C_{pb}$ from the plate to the substrate due to the gap between the drive and sense electrodes which can be written as

$$C_{pb} = \left[ \frac{\varepsilon_{o}}{t_{gap} + \frac{\varepsilon_{o}}{\varepsilon_{pi}} t_{p1} + \frac{\varepsilon_{o}}{\varepsilon_{ox}} (t_{po} + t_{fo})} \right] L_d W_g = \frac{\varepsilon_{o}}{t''_{gap}} L_d W_g \quad (41)$$

where $W_g$ is the width of the gap between the drive and sense electrodes, $t_{fo}$ is the thickness of the field oxide below the electrodes, and $t''_{gap}$ represents the equivalent air-gap thickness of the composite dielectric.

The sense electrode to substrate capacitance $C_{sb}$ includes the electrode capacitance $C_{sb0}$, line capacitance $C_{line}$, and the gate capacitance of the MOSFET $C_{gate}$, and is written as

$$C_{sb} = C_{sb0} + C_{line} + C_{gate} \quad (42)$$
where

\[ C_{sb0} = \frac{\varepsilon_{ox} \varepsilon_o}{t_{fo}} L_s W_s \]  \hspace{1cm} (43)

\[ C_{line} = \frac{\varepsilon_{ox} \varepsilon_o}{t_{fo}} L_i W_i \]  \hspace{1cm} (44)

\[ C_{gate} = \frac{\varepsilon_{ox} \varepsilon_o}{t_{gate}} L_g W_g \]  \hspace{1cm} (45)

where \( L_i \) and \( W_i \) are the length and width of the interconnect line, \( L_g \) and \( W_g \) are the length and width of the gate, and \( t_{gate} \) is the gate oxide thickness.

We can express the differential voltage on the gates of the transistors as

\[ V_{s1} - V_{s2} = \left[ \frac{C_{ps1}}{C_{ps1} + C_{sb}} - \frac{C_{ps2}}{C_{ps2} + C_{sb}} \right] V_p \]  \hspace{1cm} (46)

where

\[ V_p = \frac{C_{dp}}{C_{dp} + C_t} V_d \]  \hspace{1cm} (47)

and

\[ C_t = 2C_{pb} + \frac{C_{ps1}}{C_{ps1} + C_{sb}} C_{sb} + \frac{C_{ps2}}{C_{ps2} + C_{sb}} C_{sb} \]  \hspace{1cm} (48)

For the design we have considered in this work, \( C_{ps0} = 0.06 \) pF and \( C_{sb} = 1.6 \) pF, thus \( C_{ps0} \ll C_{sb} \) and the expressions may be simplified. In this limit, \( C_t \) is a constant, independent of deflection, and equation 46 reduces to

\[ V_{s1} - V_{s2} = - \left[ \frac{C_{ps0}}{C_{sb}} \right] \left[ \frac{C_{dp}}{C_{dp} + C_t} \right] \left[ \frac{2V_d}{fW_s} \right] \delta \]  \hspace{1cm} (49)

Thus, in this limit, the response is linear in deflection.

The a-c drive voltage will exert both an a-c and a d-c normal electrostatic force on the element. The d-c force will cause a static normal deflection of the plate, as shown in Figure 25. For an a-c drive expressed as \( V = V_m \sin \omega t \), the force \( F \) generated on the plate of a capacitor \( C \) with electrode spacing \( t_{gap} \) and area \( A \) is
Fig. 25  Electrostatic forces acting on a parallel plate capacitor.
\[ F = \frac{1}{2} \varepsilon E E^2 = \frac{1}{2} \frac{\varepsilon}{t_{gap}} V^2 = \frac{1}{4} \frac{C}{t_{gap}} V_m^2 (1 - \cos 2\omega t) \quad (50) \]

We can neglect the a-c force since the very small air gap between the element and the substrate will damp out any dynamic normal motion. In order to determine the total d-c force acting on the floating element, we must include the force generated by the four capacitances; \( C_{dp}, C_{ps1}, C_{ps2} \) and, \( C_{pb} \). Thus the total d-c force acting on the floating element will be

\[ F_{dc} = \frac{1}{4} \left[ \frac{C_{dp}}{t_{gap}} (V_d - V_p)^2 + \frac{C_{ps1}}{t_{gap}} (V_p - V_{s1})^2 + \frac{C_{ps2}}{t_{gap}} (V_p - V_{s2})^2 + \frac{C_{pb}}{t_{gap}} V_p^2 \right] \quad (51) \]

This force will be balanced by the restoring force of the tethers until the voltage exceeds what is known as the pull-in voltage, at which point the element is drawn all the way to the substrate[15]. This effect occurs simply because the electrostatic force is a nonlinear function of the plate spacing. The pull-in voltage for all the structures we have considered is at least twice the drive voltage, but the electrostatic force will still cause a deflection substantial enough that it must be considered in the model. To solve for the electrical output we will iteratively solve for the voltages and plate spacing in the structure.

A FORTRAN program was written to iteratively solve for the electrical output for a given shear stress. The program is documented in Appendix B. Using the sensor electrical parameters measured on test devices and the mechanical properties measured as described in Chapter 4 we can compare the measured and calculated response.

The final consideration for the readout circuit design is the signal to noise ratio that can be expected. By assuming the dominant noise source is the noise introduced by the MOSFETs, we can solve for the equivalent noise voltage on the gate of the device. We will assume Johnson noise in the channel since we are operating the device in its linear region at frequencies above the 1/f region. For the laminar flow calibration we are looking at a static
signal and hence can operate above the 1/f noise region. In this limit, the equivalent rms noise voltage \( v \) on the gate of the FET may be written as [44]

\[
v = \left[ 4 kT \frac{1}{g_m} \Delta f \right]^{\frac{1}{2}}
\]

where \( g_m = \frac{W_g}{L_g} \frac{\varepsilon_{ox}}{t_{gate}} \frac{\varepsilon_o}{V_{DS}} \). Assuming a 1000 Å gate dielectric, \( \frac{W_g}{L_g} = 100 \), \( \mu_p = 250 \text{ cm}^2/\text{Vs} \), and \( V_{DS} = -1.5 \text{ V} \), the equivalent noise voltage in a 10 kHz bandwidth is 0.36 \( \mu \text{V} \). We will therefore need to design the electrode and element geometries to produce a signal voltage for 1 Pa that exceeds this noise limit.

G) Sensor Layout

A prototype layout based on these design equations is shown in Figure 26. The overall chip dimensions are 4 mm x 5 mm with the floating element size 500 \( \mu \text{m} \) x 500 \( \mu \text{m} \). The tethers are 1 mm long and the width was varied from 10-20 \( \mu \text{m} \). The drive electrode is 450 \( \mu \text{m} \) x 260 \( \mu \text{m} \) and the sense electrodes are each 450 \( \mu \text{m} \) x 100 \( \mu \text{m} \) (\( W_s = 100 \mu \text{m} \)) with the thin conductor in the element overlapping the sense electrodes by 50 \( \mu \text{m} \) (\( f = 0.5 \)). The drive voltage metal line is directly above a diffused line which is connected to ground. In this way the ground currents associated with the drive voltage will be supplied through this low resistance path, minimizing substrate modulation effects. The interconnect length from each sense electrode to device is matched and the devices have a W/L ratio of 100.

The bonding pads have been located well "down-stream" of the sensing element in order to minimize perturbation of the flow when contacting the device. A variety of test structures are included on the chip to measure electrical as well as mechanical properties.

We can use the geometry of the layout shown in Figure 26 to model the expected sensor response using the program in Appendix B. The values used for all parameters are included in Appendix B. For a drive voltage of \( \pm 20 \text{V} \), the predicted differential voltage on
the gates of the FET's is $\pm 50 \mu V/\text{Pa}$. The calculated equivalent noise voltage at the gate from Section 2.F was 0.36 $\mu V$ for a 10 kHz bandwidth, which gives a signal to noise ratio for a 1 Pa signal of approximately 40 dB. Alternatively, the minimum detectable signal will be 0.01 Pa.
Fig. 26  Layout of prototype chip (5 mm x 4 mm).
CHAPTER 3

FABRICATION AND PACKAGING

This chapter presents the fabrication processes used to make the shear-stress sensor as well as the packaging process employed for testing. We begin with a presentation of the PMOS process used to fabricate the on-chip transistors. The fabrication of the polyimide floating element is described next, including the device passivation. The design and construction of the packaging technology follows. Both the laminar flow package, as well as the wind tunnel package are presented.

A) Metal-Gate Depletion-Mode PMOS Process

A four-mask 10 μm design-rule process was used to fabricate the metal gate transistors used in this study. This process is similar to that developed by Garverick[45,46] and it was selected for this application because of its anticipated high yield and ease of fabrication. The detailed process sequence is contained in Appendix C. Figure 27 is a cross-section through the important steps in the fabrication process.

We begin with a 10-20 Ω-cm n-type <100> 2 inch silicon wafer which is oxidized at 1100 °C in steam in order to grow a 1 μm thick field (Figure 27(a)). The oxide is then patterned to form the source and drain regions (Figure 27(b)). This field oxide must be thick in order to minimize the capacitance between the sense electrodes and the substrate. A solid source boron diffusion at 900 °C with a drive-in is performed to create the source and drain. The drive-in is done at 1100 °C in an oxidizing ambient to grow a 5000 Å in the source/drain areas (Figure 27(c)). The frontside is protected with resist at this point and an SF₆ plasma etch of the backside is performed to remove the boron doped region. The next lithography step defines the gate oxide region and removes the oxide grown during the drive-in from the contact regions (Figure 27(d)). The 1000 Å gate oxide is grown at 1100°C in dry O₂. The wafers next receive a boron implant of dose 1.5 x 10¹² cm⁻² at an
energy of 70keV to adjust the threshold of the devices (Figure 27(e)). The third lithography step is to define the contact cuts, which produces the "staircase" oxide profile for the metallization to minimize step coverage problems. After etch of the contact cuts and photoresist strip and clean, the gate aluminum is evaporated to a thickness of 5000 Å. The gate metal is lithographically patterned and wet etched using PAN (Phosphoric-Acetic-Nitric Acid) etch. A 5000 Å aluminum layer is deposited on the backside and the wafer is sintered in forming gas at 450°C for 30 minutes to complete the device process (Figure 27(f)).

Table 1 summarizes the process and device parameters for the PMOS wafers which were fabricated in the CMSE Microelectronics Laboratory at MIT. The process was developed using the SUPREMIII process simulator[47], which proved to very accurately predict the process variables which were measured.

B) Floating Element Fabrication

The floating element fabrication process begins with the metal gate PMOS transistor wafer. The wafer is first passivated to protect the device metallization during the spacer etch, and then the floating element is fabricated.

The requirement on the device passivation layer is that it must protect the gate and electrode aluminum during the aluminum spacer etch. Initially we investigated using atmospheric pressure chemical vapor deposited (APCVD) silicon dioxide. A 7500 Å thick layer of deposited oxide worked well to protect the metallization which was exposed to the spacer etch. It was found, however, that the metal lines which lay underneath the thick aluminum spacer would get attacked by the spacer etch even though they were passivated. A set of experiments were performed (detailed in Appendix D) to study this phenomena. It was concluded that the deposited oxide which was underneath the thick aluminum spacer would crack due to thermal expansion stresses generated during the 400 °C polyimide cure.
Fig. 27  Cross-section through process of fabricating metal-gate PMOS.
Fig. 27 Cross-section through process of fabricating metal-gate PMOS.
**PROCESS PARAMETERS**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gate Oxide Thickness</td>
<td>1000 Å</td>
</tr>
<tr>
<td>Field Oxide Thickness</td>
<td>10,000 Å</td>
</tr>
<tr>
<td>Aluminum Thickness</td>
<td>5,000 Å</td>
</tr>
<tr>
<td>Junction Depth</td>
<td>2 μm</td>
</tr>
<tr>
<td>Channel Length</td>
<td>10 μm</td>
</tr>
<tr>
<td>Transconductance of Device</td>
<td></td>
</tr>
<tr>
<td>with W/L=100 at Vds=0.1V</td>
<td>6×10⁻⁵ Ω⁻¹</td>
</tr>
</tbody>
</table>

Table 1 Metal-gate PMOS process parameters.
Once the oxide layer cracked, the underlying metal lines could be attacked by the spacer etch.

In order to eliminate the stress cracking effect, a 1 µm coat of polyimide was spin-cast and cured on top of the deposited oxide. This layer acts as a "cushion" to absorb most of the thermal stress without cracking. This process was found to work very well. A passivating layer of polyimide is also spin-cast onto the backside of the wafer to protect the backside metallization during subsequent processing.

Once the wafer is passivated, the floating element process begins. Figure 28(a) shows a schematic cross-section through the wafer at the point at which the element process begins. A 3 µm thick aluminum spacer is evaporated and patterned. The spacer is etched using PAN etch and the polyimide passivation layer is then remove from the field regions in an O₂ plasma using the spacer as an etch mask (Figure 28(b)). This is to allow the floating element polyimide to make adhesive contact to the silicon substrate rather than cured polyimide.

The next step is to spin a 1 µm layer of polyimide which is partially imidized by soft baking the film at 160 °C for 15 minutes. The partial imidization is to allow for improved adhesion to subsequent layers of polyimide. A 300 Å layer of chrome is evaporated and patterned to form the shield plate. The choice of metal and thickness was determined by the mechanical properties of the materials, and will be discussed in the following chapter. The chrome is wet-etched using a commercial ceric-sulfate-based chrome etchant (Figure 28(c)). After chrome definition, the remaining polyimide is spin-cast in seven coats and cured to achieve a thickness of approximately 30 µm. A 3000 Å aluminum layer is evaporated to form a non-erodible etch mask. The floating element is patterned and the aluminum etch mask defined. An anisotropic O₂ plasma etch is performed in a parallel-plate reactor to etch the thick polyimide. The lateral etching is approximately 6 µm for a 30 µm thick polyimide layer. Figure 28(d) shows a cross-section through the element at this stage in the processing.
Fig. 28  Cross-section through process of fabricating the floating element.
Fig. 28  Cross-section through process of fabricating the floating element.
The element is now released by placing the wafer in a mildly heated (40-60 °C) solution of PAN etch. Release of a 500 μm x 500 μm element takes approximately 2 hours. During this time a slight loss of adhesion of the polyimide can sometimes be observed at the chip edge, but this does not effect the floating-element. Extended exposure (> 6 hours) can cause the polyimide to completely delaminate from the surface of the chip.

Figure 29 is a SEM photograph of a fabricated chip. The element is towards the front of the chip, and the bonding pad openings can be seen at the trailing edge of the chip. The extent to which the polyimide planarizes the underlying features can be readily seen. Figure 30 is a closer view of the floating element area of the chip.

C) Laminar Flow Packaging

In order to calibrate the fabricated sensor, we must insert the sensor into a well calibrated flow pattern and minimize the disturbances of the flow around the chip. Figure 31 highlights the complications associated with sensor placement where protrusion with respect to the wall can substantially alter the flow pattern. The specific package requirements for the calibration of this sensor in laminar flow are:

1) Ability to measure pressure drop along the flow channel
2) Minimization of protrusion into the flow.
3) Minimization of gaps between the sensor and the wall
4) Electrical access to the trailing edge of the chip

These requirements are schematically illustrated in Figure 32. The sensor chip can not protrude more than several percent of the channel height. In order to satisfy these requirements we have used silicon micromachining techniques to fabricate a first level package for laminar flow calibration.

Figure 33 shows a schematic drawing of the laminar flow cell assembly. The cell consists of a sensor plate made of lucite to which a silicon plate and the sensor chip are attached with epoxy. The flow channel is formed by clamping the sensor plate to a cover
Fig. 29   SEM photograph of fabricated floating-element shear-stress sensor.

Fig. 30   SEM photograph close-up view of element in Figure 29.
Fig. 31  Schematic illustration of importance of sensor placement.

Fig. 32  Requirements on packaging for laminar flow.
plate with inlet manifold. A thin shim between the cover and sensor plates defines the channel dimensions. The sensor chip protrudes from the end of the channel, allowing for contact to be made to the bonding pads. A more detailed drawing of the sensor plate is shown in Figure 34. The lucite plate has a recess machined for the silicon plate. The silicon plate contains an anisotropically etched cavity that precisely matches the dimensions of the sensor chip. In addition, a series of anisotropically etched pressure taps are included for calibration of the flow cell. The entire cell is machined from lucite with overall dimensions of 8 cm x 11 cm x 10 cm which is small enough to be placed on the chuck of a probe station. The silicon micromachined plate is 4 cm x 2.8 cm x 0.275 mm and the channel dimensions are 7.3 cm x 2.6 cm x 0.22 mm. The exact dimensions of all the components are contained in Appendix E.

Figure 35 shows the fabrication and assembly procedure for making the silicon plate and package. The detailed process sequence is contained in Appendix C. We begin with a 2-inch <100> Si wafer which has been boron doped and anisotropically etched to produce 5 µm thick silicon diaphragms in the pressure tap and chip cavities. Polyimide is spun-cast on the wafer in multiple coats to achieve the same thickness as the polyimide on the sensor chip (Fig. 35(a)). The polyimide is patterned over the diaphragms and plasma etched in the same way as the sensor chip (Fig. 35(b)). A 20 µm x 20 µm hole is opened over the pressure tap cavity to form a very small tap that will not perturb the flow. The polyimide in the chip cavity overhangs the cavity and is defined to match the dimensions of the polyimide on the sensor. The silicon in the cavities is removed by SF₆ plasma etch from the backside to release the polyimide films (Fig. 35(c)) and the wafer is scribed and broken into the rectangular plate. The sensor plate is now assembled by attaching the silicon plate and the sensor chip to the lucite with epoxy, allowing the overhanging polyimide on the silicon plate to self-register to the chip edge in the scribe lane as shown in Figure 35(d). In this way, the surfaces are flush mounted to within the difference in polyimide thicknesses on the two parts, which is typically ~1 µm or less than 0.5% of the
Fig. 33  Laminar flow cell assembly.

Fig. 34  Sensor plate.
Fig. 35  Fabrication and assembly of sensor plate.
channel height. Fittings are attached to the pressure tap holes and connected to a commercial pressure transducer.

Figure 36 shows a photograph of a 2 inch wafer with the sensor plate. The anisotropically etched scribe lanes can be seen as well as the pressure taps and the chip cavity. Figure 37 is a microphotograph of one of the pressure tap cavities taken from the backside of the wafer. The sloped sidewalls of the anisotropic etching can be seen. The etched cavity is 200 \text{ \( \mu \)m} \times 200 \text{ \( \mu \)m} at the frontside of the wafer and the pressure tap is 20 \text{ \( \mu \)m} \times 20 \text{ \( \mu \)m}.

An assembled sensor plate can be seen in Figure 38. The pressure tap lines are seen coming from underneath the plate. A syringe needle is used to inject conducting epoxy underneath the sensor chip to press it against the polyimide overhang. The needle also serves as the backside contact after the plate is assembled. Figure 39 shows the fully assembled flow cell and Figure 40 shows the flow cell on the chuck of a probe station.

The shear stress which we can expect to generate in this type of cell can be determined using simple channel flow equations and neglecting end effects. The shear stress in a channel flow with channel height of \( h \) is\[38,39]  

\[
\tau_w = \frac{h}{2} \left(\frac{dP}{dx}\right) 
\] (53)

where \( \frac{dP}{dx} \) is the pressure drop per unit length in the cell. This pressure drop can be expressed as a function of volumetric flow, \( Q \), as\[38,39]  

\[
\frac{dP}{dx} = \frac{12\mu}{h^3W} Q 
\] (54)
Fig. 36  2 inch wafer used for sensor plate.

Fig. 37  Photomicrograph of pressure tap.
Fig. 38  Sensor plate.

Fig. 39  Assembled flow cell.
Fig. 40  Flow cell on probe station.
where \( \mu \) is the absolute viscosity of air (1.95 \( \times \) 10\(^{-5}\) kg/m-s) and \( W \) is the channel width.

We are assuming laminar flow, and we should therefore determine the Reynold's Number, \( N_{RE} \), to verify this assumption. The Reynold's Number can be written as\[38,39\]

\[
N_{RE} = \frac{\rho <u_x> h}{\mu} = \frac{\rho Q}{\mu W} \tag{55}
\]

where \( \rho \) is the air density (1.3 kg/m\(^3\)), and \( <u_x> \) is the average velocity in the channel and equals the flow divided by the cross-sectional area of the channel. Using the channel dimensions given in this section (\( h = 0.22 \) mm, \( W = 2.6 \) cm), we estimate a shear-stress of 1 Pa for a differential pressure drop of 9.09 \( \times \) 10\(^3\) Pa/m (1.32 psi/m), which is produced by a volumetric flow of 1.08 \( \times \) 10\(^{-5}\) m\(^3\)/s (0.65 l/min). The Reynold's number at this flow is 27.6, and the average velocity is 1.9 m/s.

A final criteria in the design is that the flow be fully developed by the time it reaches the sensor chip. In laminar pipe flow, the entry length, \( L_e \), is given approximately by the expression\[39\]

\[
\frac{L_e}{h} = 0.59 + 0.055N_{RE} \tag{56}
\]

This expression is derived by determining the point at which the boundary layers on each wall reach the center of the channel and is highlighted in Figure 41. The highest Reynold's Number for laminar flow will be approximately 2000 giving a maximum entry length of 2.4 cm, which is less than the length of the channel (7.6 cm). The floating element will be approximately 7.2 cm from the entry. The effect of the exit will only be felt approximately one channel height from the exit at low Reynold's Numbers[39], and thus the floating element should be >0.5 mm from the exit. In our package design, the floating element is approximately 3 mm from the exit. The calibration of the package will be described in Chapter 5.
Fig. 41 Illustration of entry length.
D) Wind Tunnel Packaging

Several package schemes have been investigated for use in the wind tunnel. The requirements on the package with regards to the amount of protrusion which is tolerable is slightly more relaxed for the wind tunnel. Since the maximum achievable shear-stress in the Turbulence Research Laboratory at MIT is 0.5 Pa, which corresponds to a wall unit of approximately 25 μm, the sensor need only be flush to several wall units, or 25-50 μm.

The package which was developed to satisfy these requirements is shown schematically in Figure 42 (Detailed drawings are contained in Appendix E). A 0.9 inch diameter lucite plug 1.1 inch long is used to hold the sensor chip. A 0.015 inch recess is machined in the plug into which the sensor chip is attached with conductive epoxy. A heavy gauge copper wire extends from the back of the plug up to the recess on the front surface to form the backside contact. Moulding clay is used to fill in the gaps between the silicon chip and the lucite plug. Electrical contact is made to the chip using low profile wire bonds between the chip and a set of six copper wire which are fed up through holes in the plug and epoxied into place. Figure 43 is a photograph of an assembled plug. The plug is inserted into a hole in the wall of the wind tunnel and fastened into place with a set screw once the front surfaces are registered. This process enables the sensor to be placed in the flow with a maximum of approximately 25 μm of protrusion. Electrical access is made at the back of the wall using the copper feed-through wires. The use of this package will be described in Chapter 5.
Fig. 42 Illustration of wind tunnel package.

Fig. 43 Photograph of wind tunnel package.
CHAPTER 4
MECHANICAL PROPERTIES CHARACTERIZATION

The proper design and calibration of the floating-element shear-stress sensor has required that the mechanical properties of the materials used in this structures be well characterized. There are two components to this materials characterization work. First, we must measure elastic modulus and residual tensile stress in the polyimide to model the mechanical response of the tethers. Second, we must characterize the polyimide/metal composite element in order to control out-of-plane warpage. The suspended diaphragm techniques which Mark Allen[31,48] and Mehran Mehregany[30,32] have developed are used to measure the modulus and stress in the polyimide. Cantilevers with embedded metal are used to study the out-of-plane warpage and a model is developed to understand the results of this study.

This chapter begins with a brief description of the wafer fabrication and the test mask design. The methods employed for extracting data are presented. The diaphragm deflection model is described and a model for composite cantilever deflection is developed. We then present the results of the suspended diaphragm and cantilever experiments.

A) Fabrication Sequence and Test Mask

The process employed to fabricate the test structures is almost identical to that developed by Mehregany[30]. The details of this process are outlined in Appendix C. Figure 44 is a schematic cross-section through this process. We begin with a 2 inch <100> n- or p-type silicon wafer which has been heavily boron doped on the frontside and subsequently oxidized (Figure 44(a)). The oxide on the back is patterned to form square openings to define the diaphragm regions (Figure 44(b)). The wafer is then anisotropically etched in hydrazine hydrate[49], which stops on the heavily doped boron layer to form a 5 μm thick silicon diaphragm (Figure 44(c)). Next, the oxide is patterned
Fig. 44  Cross-section of process for fabrication of mechanical properties wafer.
Fig. 44 Cross-section of process for fabrication of mechanical properties wafer.
on the frontside to form a frontside alignment marker. Front-to-back alignment is achieved by using an infrared aligner. The first coat of polyimide is deposited on the frontside and partially imidized. The thin metal is evaporated and patterned on some of the cantilevers and diaphragms (Figure 44(d)). The remaining polyimide is spin-cast in multiple coats to the desired thickness, and the entire structure is cured (Figure 44(e)). A 3000 Å aluminum etch mask is evaporated and patterned to define the cantilevers, and the polyimide is etched in an anisotropic O₂ plasma etcher (Figure 44(f)). After stripping the aluminum etch mask, the wafer is placed upside-down in a SF₆ plasma etcher and the silicon in the diaphragm area is removed, releasing the cantilevers and diaphragms (Figure 44(g)).

It is also possible to incorporate an aluminum spacer in the process in order to study the effect that a different substrate might have on the mechanical properties. Figure 45 shows what a structure would look like with an aluminum spacer. The spacer is placed over the diaphragm so that when the structure is released, cantilevers may bend down without contacting the substrate, since it will be removed.

Figure 46 shows the layout of the mechanical properties test mask set which was generated for this work. The mask layout contains 4 duplicate sets of cantilevers with lengths of 250 µm, 500 µm, and 1000 µm (shown in Figure 47). The mask is designed such that cantilevers with and without embedded metal are adjacent to each other to make relative measurements of cantilever deflection. There are also two "T-structures" [30,31,32] in each cantilever set for strain measurements, as well as alignment and thickness measurement structures. There are four 8 mm x 8 mm diaphragms on the wafer, two of which contain embedded metal, as well as one 8 mm x 8 mm diaphragm in the center of the wafer. The center diaphragm was specifically designed for surface micromachining using a spacer, but was not utilized in this thesis. The mask layout also contains scribe lanes to section the wafer after fabrication. There are four masks for the standard set, as well as a fifth mask to allow for the incorporation of an aluminum spacer on two diaphragms and two sets of cantilevers.
Fig. 45  Incorporation of spacer in process.
Fig. 46  Layout of mechanical properties wafer.
Fig. 47  Layout of cantilevers on mechanical properties wafer.
Figure 48 is a photograph of the backside of a silicon wafer after the anisotropic etching step (Figure 44(c)). The completed and sectioned cantilever sets are shown in Figure 49, and Figure 50 is a photomicrograph of one of the cantilever sets.

B) Experimental Methods

The diaphragm structures must be packaged such that differential pressures can be applied across the membrane and the deflection at the center measured. This is accomplished using apparatus developed by Mark Allen[48] and Mehran Mehregany[30] which is shown schematically in Figure 51. The diaphragm is attached with epoxy to a lucite substrate with a pressure inlet hole. This substrate is then clamped into a test jig with a pressure transducer and a pressure input fitting. The entire jig is placed on the stage of a microscope with a calibrated z-axis. Figure 52 is a photograph of the actual test arrangement, and Figure 53 is a photograph of two of the diaphragm sites attached by epoxy to the lucite substrate.

The load-deflection characteristics are measured by focusing the microscope on the center of the diaphragm and recording the deflection magnitude and the pressure. A plot of deflection versus pressure may be generated, from which the stress and modulus can be determined as described in the next section. The resolution of the deflection measurement is determined by the minimum resolvable deflection of the micrometer, as well as the mechanical hysteresis of the micrometer and stage. The micrometer limit is 1 μm. If the data are very carefully collected, it is possible to record an entire up-down cycle with only 1 μm of hysteresis. Thus, it is felt that the accuracy of the deflection measurement is approximately 1-2 μm. It is possible to improve this resolution using optical interference methods, but since the deflections measured range up to 500 μm, it was felt that 1-2 μm represented sufficient accuracy. The pressure is measured using either a Kulite pressure transducer, or a Honeywell Microswitch pressure transducer. In either case, the accuracy is at least 1%.
Fig. 48  Mechanical properties wafer after anisotropic etching.

Fig. 49  Cantilever sections of wafer.
Fig. 50  Photomicrograph of cantilevers.
Fig. 51 Illustration of load-deflection apparatus.
Fig. 11. Photomicrograph of cell-deposition apparatus.

Fig. 11. Two cell-deposition apparatus.
The cantilever data is extracted using the same calibrated z-axis microscope as used in the load-deflection measurements. We first measure the deflection of the tip of each cantilever without metal, shown as $\delta$ is Figure 54. The tip deflection of the cantilevers with metal is then recorded relative to the deflection of the unmetallized cantilever immediately adjacent to it. This relative measurement is shown as $\delta'$ in Figure 54. As in the diaphragm measurements, the accuracy with which deflection of the cantilever can be recorded is approximately 1-2\(\mu\)m.

C) Diaphragm Deflection Model

Figure 55 shows some typical data measured on a diaphragm 14 \(\mu\)m thick. These data can be modeled using an expression developed by Allen[48] for this method. A square diaphragm of edge length 2a and thickness t will deflect a distance d under an applied differential pressure p (Figure 56). The deflection is modeled using a membrane mechanics model which includes the residual tensile stress in the film and is of the form

$$p = c_1 \left[ \frac{E t}{a^4} \right] d^3 + c_2 \left[ \frac{t \sigma_o}{a^2} \right] d$$

(57)

where $E$ is the elastic modulus of the material, $\sigma_o$ is the residual tensile stress, and $c_1$ and $c_2$ are constants equaling 3.0 and 1.8 respectively. The constant $c_1$ is independent of all material parameters, while $c_2$ is weakly dependent upon Poisson's ratio, which is assumed to be equal to 0.25 in this work. These constants were determined using energy minimization methods and were verified to within 8% using finite element modeling[50].

We can see from equation 57 that if we plot $p/d$ versus $d^2$, the equation will be of the form,

$$p/d = c_1 \left[ \frac{E t}{a^4} \right] d^2 + c_2 \left[ \frac{t \sigma_o}{a^2} \right]$$

(58)

99
Fig. 54  Illustration of deflected cantilevers.

Fig. 55  Deflection versus pressure data for 14 μm thick polyimide diaphragm.
Fig. 56 Definition of parameters in load-deflection theory.

Fig. 57 Normalized load-deflection data.
where the slope is proportional to modulus, and the intercept is proportional to residual stress. Figure 57 shows the data in the previous figure replotted in this form. The elastic modulus and residual tensile stress extracted from this plot are 3.4GPa and 30 MPa respectively. Reproducibility of data extraction from wafer to wafer using this technique has proved to be approximately 5-10%.

In addition to polyimide membranes, we will be testing membranes with embedded thin metal. We can estimate the effect that this thin metal will have on the load deflection behavior using a simple composite plate theory. We are interested in determining the change in residual stress and modulus when compared with the homogenecus plate. Figure 58 shows a composite polyimide plate of thickness \( t_p \) with a thin conductor of thickness \( t_m \) embedded inside. Note that \( t_m \) is considered to be much smaller than \( t_p \), and hence will not contribute appreciably to the total thickness \( (t_p + t_m = t_p) \). The distance of the metal from the center of the plate is defined as \( e \). The effective modulus of this composite structure, \( E_{eff} \), when it is acting under stretching forces can be written as[51]

\[
E_{eff} = E_p \frac{t_p - t_m}{t_p} + E_m \frac{t_m}{t_p}
\]  
(59)

where \( E_p \) is the modulus of the polyimide, and \( E_m \) is the metal modulus. We have assumed that the Poisson's ratios of the materials are the same. In an equivalent fashion[51], the effective residual tensile stress, \( \sigma_{eff} \), can be written as

\[
\sigma_{eff} = \sigma_p \frac{t_p - t_m}{t_p} + \sigma_m \frac{t_m}{t_p}
\]  
(60)

where \( \sigma_p \) is the polyimide residual tensile stress and \( \sigma_m \) is the metal residual tensile stress ( compressive stresses would be negative ).

102
Assuming the polyimide is 30 μm thick \( (t_p = 30 \, \mu m) \) and has a residual tensile stress and modulus of 30 MPa and 4 GPa respectively, we can estimate the effect of a typical metal film 300 Å thick \( (t_m = 300 \, \AA) \). Chrome has a bulk elastic modulus of 290 GPa [52] and therefore the effect of this metal would be to increase the elastic modulus by approximately 7%, which is less than our limit of repeatability with the load deflection technique.

Since we do not know in advance what the stress state of the evaporated metal will be, it is difficult to estimate its effect on the tensile stress. We can make a worst case estimate by assuming the metal is under compressive stress, with an effective strain of 1%. In this case the metal stress, \( \sigma_m \), would be -2.9 GPa, and the effective stress would decrease by approximately 9%. Again, this worst case estimate is close to our limit of repeatability.

D) Cantilever Deflection Model

The incorporation of a metal with a different strain state than the polyimide will cause severe out-of-plane warpage of the composite structure. We have developed a model for this behavior which is similar in form to the model used to predict the deflections of bimetallic elements[53], stress in electrodeposits[54], or curvature of prestressed concrete beams[55]. Brenner and Senderoff have studied the models used for determining stress in electrodeposits from the curvature of plated strips[54]. They present several different models depending upon the boundary conditions during the deposition. In certain limits we can use these model for our structure, but in this section we will develop a model which is more generally applicable to our problem.

Figure 59(a) shows the as-deposited condition of the composite structure, with the arrows indicating the direction and magnitude of the residual stress. The metal stress is arbitrarily chosen to be tensile, but this does not affect the validity of the model. The method of solution we will use is as follows;
Fig. 58 Definition of parameters in cantilever model.

Fig. 59 Deformation of a prestressed composite cantilever.
1) Allow the structure to relax until the polyimide is in a zero stress state. We are effectively defining a new reference frame.

2) The embedded metal stress will produce a net internal axial force and bending moment on the structure.

3) Determine the modified stress in the metal as it deforms to its equilibrium state.

4) Use a moment balance equation to determine the radius of curvature of the structure.

If the structure relaxes to a state of zero polyimide stress, the modified stress in the metal, \( \sigma_m' \), will be

\[
\sigma_m' = \sigma_m - E_m \left[ \frac{\sigma_p}{E_p} \right]
\]

(61)

Note that if the metal is under tensile stress, \( \sigma_m > 0 \), the modified stress will be lower than the initial stress. After release of the polyimide stress, we can express the loads on the structure, shown in Figure 59(b) as

\[
F = -\sigma_m' t_m W
\]

(62)

\[
M = -\sigma_m' e t_m W
\]

(63)

The force is negative since it would cause a contraction of the structure. The moment is negative since it would cause a concave down curvature of the structure, and hence a negative radius of curvature.

The force and moment will cause the element to strain as shown in Figure 59(c). The strain caused by the axial force can be written as
\[ \epsilon_a = \left[ \frac{1}{E_p} \right] \left[ \frac{F}{r_p W} \right] = - \left[ \frac{\sigma_m}{E_p} \right] \left[ \frac{t_m}{r_p} \right] \]  

(64)

The strain due to bending can be expressed in terms of the radius of curvature of bending, \( \rho \), as

\[ \epsilon_b = \left[ \frac{e}{\rho} \right] \]  

(65)

where \( e \) is the distance of the thin metal from the center, as shown in Figure 58.

The use of this expression implies the assumption that the embedded metal does not substantially alter the bending stiffness of the structure, and hence that the neutral axis of bending is through the geometric center of the beam. Given the fact that the metal is only 0.1% of the plate thickness, and that bending stiffness goes as thickness to the third power, any difference in modulus between the materials can be neglected. Brenner and Senderoff[54] have modelled this modulus difference in the case of a plated film by assuming the modulus is the same and setting the cross-section of bending to be that of a 'T'. In this way the width of the two sections will reflect the difference in modulus of the materials. As we will show, their model reduces to the one we will derive in the limit of a thin metal.

The two components of strain will modify the metal stress such that we can write an expression, using equations 61, 64, and 65, for the modified stress of the thin metal as

\[ \sigma'_m = \sigma_m - E_m \left[ \frac{\sigma_p}{E_p} \right] + E_m (\epsilon_a + \epsilon_b) = \sigma_m - E_m \left[ \frac{\sigma_p - \sigma'_m t_m}{E_p} + \frac{e}{\rho} \right] \]  

(66)

We can relate the radius of curvature to the bending moment using the expression[51]
\[
\frac{1}{\rho} = \frac{M}{E_p I}
\]  

(67)

where the moment of inertia of bending is \( I = \frac{1}{12} W t_p^3 \), and the moment \( M \) is given in equation 63. Substituting equation 67 into equation 66 using equation 63 and solving for the radius of curvature we arrive at

\[
\rho = \frac{t_p}{12} \frac{t_p}{t_m} e \left[ \frac{E_p}{\sigma_p \left( \frac{E_m}{E_p} \right) - \sigma_m} \right] \left[ 1 + \frac{t_m}{t_p} \frac{E_m}{E_p} + 12 \left( \frac{e}{t_p} \right)^2 \frac{t_m}{t_p} \frac{E_m}{E_p} \right]
\]  

(68)

We can compare this with the expression reported by Brenner and Senderoff

\[
\rho = \frac{E_p \left[ R (t_p - t_m)^4 - (R - 1) t_p^4 \right]}{6 \sigma_m t_m t_p (t_p + t_m)}
\]  

(69)

where \( R = E_m / E_p \). If we take the limit of equation 68 where the metal is at the surface ( \( e = t_p / 2 \) ), and the polyimide stress is zero, and compare it with the limit of equation 61 when \( t_m \ll t_p \) they both reduce to

\[
\rho = -\frac{t_p}{6} \frac{t_p}{t_m} \left[ \frac{E_p}{\sigma_m} \right] \left[ 1 + 4 \frac{t_m}{t_p} \frac{E_m}{E_p} \right]
\]  

(70)

As we will see in the next section, experimentally the metal stress appears to be zero, and in this limit equation 68 reduces to

\[
\rho = \frac{t_p}{12} \frac{t_p}{t_m} e \left[ \frac{E_p}{\sigma_p \left( \frac{E_m}{E_p} \right)} \right] \left[ 1 + \frac{t_m}{t_p} \frac{E_m}{E_p} + 12 \left( \frac{e}{t_p} \right)^2 \frac{t_m}{t_p} \frac{E_m}{E_p} \right]
\]  

(71)
where if the metal is at the surface, it further simplifies to

$$\rho = \frac{(t_pE_p)}{12\sigma_p} \frac{1}{t_mE_m} \left[ 1 + 4 \frac{t_mE_m}{t_pE_p} \right]$$

(72)

We will use this equation in modeling the cantilever measurements in Section 4.F. The values for polyimide stress and modulus can be extracted from diaphragms on the same wafer. The fitting parameter will be the product $t_mE_m$, which represents the effective axial rigidity of the metal when embedded in the polyimide. Given that we know the metal thickness from the metal deposition monitor, we can estimate the elastic modulus of the metal in this system. We can relate the radius of curvature to the cantilever tip deflection using simple geometry as

$$\delta = \frac{L^2}{8\rho}$$

(73)

Thus cantilever deflections should go as $L^2$ for a distributed bending moment.

The remainder of this chapter is a summary of the experimental results which were obtained. We will begin with some general observations regarding the effect of metal stresses in the processing of these structures. We will then present the specific results obtained on polyimide/chrome mechanical microstructures.

E) Experimental Results - General

The development of the process sequence for the floating-element shear-stress sensor yielded some interesting insights into the effect of metal stress. The initial attempts at making the floating element used aluminum as the thin embedded metal. We found that aluminum films evaporated in an electron-beam evaporator or a tungsten-filament
evaporator exhibited very high compressive stresses. This high compressive stress caused delamination of the film from the underlying polyimide.

Figure 60 is a photomicrograph of a 1 mm x 1 mm, 300 Å thick aluminum layer which has begun to delaminate from the surface. The 'wrinkling' of the aluminum film at the edges occurs when polyimide in spun on top of the metal film. Figure 61 highlights schematically the mechanism by which this delamination occurs. We believe that polyimide solvent (NMP) from the upper coat diffuses in to the lower polyimide and softens the under layer. Once softened, the metal can deform and delaminate to release the compressive stress. The release mechanism is the formation of wrinkles in the film.

Floating elements and cantilevers which were fabricated with this compressive aluminum exhibited very large out-of-plane deformations. These deformations were so large that it was determined that a flat floating element could not be made with this material. An additional problem we observed was that these out-of-plane deformations were not very reproducible in that cantilever deflections would vary quite substantially from run to run. We attribute this to a lack of reproducibility in the metal stress.

After the unsuccessful attempts with aluminum we began to investigate chrome, which was chosen because of its good adhesion to polyimide. The electron-beam evaporated chrome which we have used exhibits tensile stress. This is indicated by the presence of microcracks in the chrome layer when it is evaporated on the first coat of polyimide. We believe that the chrome is able to crack because the partially cured polyimide is a relatively compliant substrate. We have seen that the same 300 Å thick film will not develop cracks if it is deposited directly on silicon or on cured polyimide.

Figure 62 is a photomicrograph of the microcracked chrome. We have found that in spite of the cracks, the chrome remains a good conductor for our purposes. Using this film we were able to fabricate floating-elements which were flat. Furthermore, the cantilever deflections were very reproducible from run to run.
Fig. 60  Photomicrograph of 300 Å aluminum film in polyimide.
Fig. 61 Illustration of aluminum stress relaxation effect.
Fig. 62  Photomicrograph of 300 Å chrome film in polyimide.
We now believe that the microcracking allows for almost total release of the tensile stress in the metal film. This release to a zero stress state would account for the reproducibility we observe in our structures. The metal may be evaporated in very different tensile stress states from run to run, but the cracking returns the system to a reference state. This represents an important advantage over other metals which might not stress release.

F) Experimental Results - Diaphragms and Cantilevers

The remainder of this chapter will report on measured results using the polyimide/chrome system. The initial work performed to characterize the floating element process used DuPont 2545 as the first coat, and DuPont 2540 for the remaining coats. Some of the cantilever data are from this materials system. We later switched to DuPont 2555 as the first coat and DuPont 2525 for the remaining coats because of its superior adhesion properties. All of the diaphragm data and most of the cantilever data were collected using this system. In all cases the first coat is approximately 1 μm thick.

Table 2 is a summary of some of the original data collected on polyimide cantilevers with and without embedded chrome using DuPont 2545/2540 polyimide. These cantilevers were fabricated using an aluminum spacer over the diaphragm. The values for $\delta$ and $\delta'$ are shown, as defined in Figure 52, where a positive value indicates an upward curvature. In samples A and B, the polyimide thickness was held nominally constant, and the chrome thickness was doubled from 300 Å to 600 Å. In samples A and C, the chrome thickness was held constant, and the polyimide thickness was doubled. The tip deflections were measured for three cantilever lengths.

The unmetallized cantilever deflections $\delta$ decrease with increasing polyimide thickness. The deflections also are nearly dependent on length squared, suggesting a distributed bending moment effect due to a stress eccentricity in the polyimide. It is also possible that there could be a linear dependence due to loads acting at the cantilever
edge[56], but this effect is not observable. For the 30 μm thick polyimide, the deflections are effectively zero at a length of 250 μm.

Some general trends may be observed in the δ' data. The tip deflections decrease with decreasing length, with a $L^2$ dependence. Again, this dependence is consistent with a distributed bending moment acting on the structure. The deflection decreases with increasing polyimide thickness, which is attributed to the increased bending stiffness of the structure. The deflection increases with increasing metal thickness, consistent with a bimorphic stress model. Finally, and perhaps most importantly, it is possible to fabricate a 250 μm cantilever which is flat to less than 1 μm using 30 μm thick polyimide and 300 Å thick chrome. Therefore, a plate of twice this length, 500 μm, can be made with less than 1 μm of out of plane warpage. We found when we switched to the 2555/2525 polyimide system that the same combination of polyimide/metal thickness would give us a flat cantilever.

After arriving at a process sequence which would yield a flat plate, we fabricated a series of test structures to systematically characterize the mechanical properties of the materials. This work was all performed using 2555/2525. The polyimide thickness was varied by spinning different numbers of coats of 2525 after the metal was deposited and patterned. Table 3 shows the number of coats deposited on the wafer and the resulting thickness determined by surface profilometry at four points on the wafer. The diaphragms with and without thin metal were tested using load-deflection, and the cantilever tip deflection on all wafers were measured.

Table 4 shows the results of the load-deflection measurements. Only one of the two diaphragms with thin metal were measured per wafer. Both unmetallized diaphragms on each wafer were measured, except in the case of wafer #3 where one of the diaphragms failed. The average value for stress and modulus are tabulated at the bottom. There is a slight difference between the metallized and unmetallized samples, but the difference is less than the standard deviation of the results. Based on these results, we will assume that the
<table>
<thead>
<tr>
<th>SAMPLE</th>
<th>POLYIMIDE THICKNESS</th>
<th>CHROME THICKNESS</th>
<th>δ</th>
<th>δ'</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>LENGTH (μm)</td>
<td>LENGTH (μm)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>A</td>
<td>18 μm</td>
<td>300 Å</td>
<td>-120</td>
<td>-37.25</td>
</tr>
<tr>
<td>B</td>
<td>16 μm</td>
<td>600 Å</td>
<td>-128</td>
<td>-42.5</td>
</tr>
<tr>
<td>C</td>
<td>32 μm</td>
<td>300 Å</td>
<td>-15.25</td>
<td>-2.5</td>
</tr>
</tbody>
</table>

Table 2 Cantilever deflections for three different samples.

<table>
<thead>
<tr>
<th>WAFER</th>
<th>NUMBER OF COATS</th>
<th>POLYIMIDE THICKNESS (μm)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>SITE 1</td>
<td>SITE 2</td>
</tr>
<tr>
<td>1</td>
<td>3</td>
<td>12.4</td>
</tr>
<tr>
<td>2</td>
<td>4</td>
<td>18.9</td>
</tr>
<tr>
<td>3</td>
<td>5</td>
<td>22.7</td>
</tr>
<tr>
<td>4</td>
<td>7</td>
<td>31.0</td>
</tr>
</tbody>
</table>

Table 3 Polyimide thickness for four different samples.
correct values of polyimide elastic modulus and tensile stress for this process are 4.5 GPa and 34 MPa respectively. These diaphragms did not have an aluminum spacer but other samples which were fabricated with the spacer indicate that it has very little effect on these properties.

Table 5 presents the values obtained for cantilever deflections on these wafers. These cantilevers were fabricated on top of an aluminum spacer. Cantilevers without the spacer showed similar trends but the absolute deflections differed by ~10-20%. The unmetallized cantilever deflections, \( \delta \), show a marked difference compared to the cantilevers with 2545/2540 (Table 2). In particular, the deflections do not typically show a \( L^2 \) dependence. In fact, for the thickest polyimide plate, the deflections go more like \( L \), which would imply an edge moment effect [56]. This edge moment is caused by a stress non-uniformity at the clamped boundary. This non-uniformity will not be present in a plate because it has a free boundary condition. Thus, the 3 \( \mu \)m deflection value does not imply that a flat plate could not be made. The floating-element structures which we have fabricated verify this assertion.

The difference in deflection between the metallized and unmetallized cantilevers, \( \delta' \), show a similar trend to the 2545/2540 cantilevers. There is a strong \( L^2 \) dependence, and the absolute deflection decreases with increasing polyimide thickness. Again, when the thickness reaches 30 \( \mu \)m the deflections are less than 1 \( \mu \)m for a length of 250 \( \mu \)m. We can use the data in Table 5 along with the values for stress and modulus from Table 4 to fit this data to the model of equations 71 and 73. Figure 63 shows the result of this fit. The fitting parameter for this model is \( E_m \), the elastic modulus of the embedded thin chrome. We have assumed a value of 600 GPa, which is approximately twice the bulk value for chrome. Given the simplicity of the model and the fact that this is a thin coat of microcracked material we are not surprised to find such a difference from the bulk value.
<table>
<thead>
<tr>
<th>WAFER</th>
<th>POLYIMIDE THICKNESS</th>
<th>WITHOUT CHROME</th>
<th>WITH CHROME</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>E (GPa)</td>
<td>σ (MPa)</td>
<td>E (GPa)</td>
</tr>
<tr>
<td>1</td>
<td>13.825</td>
<td>4.72 / 3.42</td>
<td>34.7 / 26.2</td>
</tr>
<tr>
<td>2</td>
<td>18.875</td>
<td>5.06 / 5.35</td>
<td>35.1 / 32.6</td>
</tr>
<tr>
<td>3</td>
<td>22.625</td>
<td>4.74</td>
<td>34.4</td>
</tr>
<tr>
<td>4</td>
<td>31.05</td>
<td>4.12 / 4.17</td>
<td>37.1 / 37.0</td>
</tr>
<tr>
<td>AVERAGE</td>
<td>4.51±0.65</td>
<td>33.9±3.7</td>
<td>4.74±0.4</td>
</tr>
<tr>
<td>GRAND AVERAGE</td>
<td>4.60±0.56</td>
<td>34.0±2.9</td>
<td></td>
</tr>
</tbody>
</table>

Table 4 Results of load-deflection measurements.

<table>
<thead>
<tr>
<th>WAFER</th>
<th>POLYIMIDE THICKNESS</th>
<th>CANTILEVER DEFLECTION (µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>δ</td>
<td>δ'</td>
</tr>
<tr>
<td></td>
<td>LENGTH (µm)</td>
<td>LENGTH (µm)</td>
</tr>
<tr>
<td></td>
<td>1000</td>
<td>500</td>
</tr>
<tr>
<td>1</td>
<td>13.825 µm</td>
<td>-2</td>
</tr>
<tr>
<td>2</td>
<td>18.875 µm</td>
<td>-9.75</td>
</tr>
<tr>
<td>3</td>
<td>22.625 µm</td>
<td>0</td>
</tr>
<tr>
<td>4</td>
<td>31.05 µm</td>
<td>6.75</td>
</tr>
</tbody>
</table>

Table 5 Measured cantilever deflections.
Fig. 63  Cantilever deflection versus polyimide thickness.
G) Summary

We have used micromechanical measurement techniques in order to characterize the materials system which we have employed to fabricate the floating-element shear-stress sensor. It was found that a polyimide/chrome system yielded a functioning part. Microcracking of the thin chrome film appears to have assisted in the process of making this structure. One concern would be the electrical integrity of such a structure. Based on a flat frequency response of the final sensor, we have concluded that the thin film is sufficiently conductive up to the maximum frequency of the off chip circuitry (~ 200 kHz).

The result of this work was the identification of a process which would yield a flat element. We have also collected the necessary mechanical properties data to allow us to calibrate the sensor. Finally, we have a model and a data set to serve as a guide for future designs. Clearly more work need to be done in the refinement of the data collection and the modelling, but the original goal of understanding the mechanics of the structure has been achieved.
CHAPTER 5
SENSOR CALIBRATION

This chapter will present the results of the calibration of the floating-element shear-stress sensor. The laminar flow cell described in Chapter 3 is characterized in order to ensure the presentation of a known flow field to the sensor. The sensor response is measured in this calibrated laminar flow, and these results are compared with the results from the theoretical model. An additional experiment measuring the acceleration sensitivity of the device is performed to lend further confidence to the calibration. Finally, the response of the floating element to normal pressure fluctuations is determined.

A) Experimental Methods

Figure 64 shows the apparatus used in the laminar flow calibration experiments. Dry compressed air is introduced through a pressure regulator to a set of three rotameters for measurement of volumetric flow. The factory-calibrated 150 mm flowtube rotameters (Cole-Parmer) provide us with flow range from 0.1 liter/minute to 64 l/min. The air is directed into the laminar cell and across the sensor chip. The pressure drop in the cell is measured using a Honeywell Microswitch 160 Series pressure transducer which is calibrated with water and mercury manometers.

The sensor response is measured using the off-chip I-V converter shown in Figure 65. The detailed layout of this circuit is contained in Appendix E. The mid-band gain of each of the channels is set by resistor $R_1$, which was chosen to be $10 \text{ k}\Omega$. The dc drain current of the depletion-mode MOSFET is supplied through the feedback loop so that the output of each channel is only the ac signal. The low frequency roll-off of this circuit is 100 Hz and the high frequency roll-off is 250 kHz. Batteries are used for both the I-V converter supply voltages as well as the -1.5V drain voltage bias to minimize power supply noise.
Fig. 64  Laminar flow testing apparatus.
Fig. 65  Current-to-voltage converter.
The sensor is driven with a Hewlett-Packard 3325A Synthesizer/Function Generator. Unless otherwise stated, the drive voltage is ±20V at 10 kHz. The output of the I-V converter is recorded using a Keithley 175 Autoranging Multimeter operating in AC voltage mode with a minimum resolution of 10 µV and a bandwidth of 100 kHz. We have also used a lock-in amplifier with no observable difference in the measured response. The differential output can be trimmed externally using resistor R14. Any mismatch in phase between the channels will set the minimum null that can be achieved. In practice this is on the order of the signal being measured, and thus the system is intentionally unbalanced by approximately 10 mV prior to collecting data so that the signal will be 180° with respect to the drive. The data are then corrected by subtracting out the bias voltage from the measured response.

The acceleration sensitivity experiments are performed using a device which has been packaged in a 24-pin ceramic chip carrier. The packaged device is then placed inside a shielded light-tight box to minimize interference signals. This box is rotated in increments of 0°, 30°, 45°, 60°, 90°, and the electrical output recorded.

The test apparatus for measurement of normal pressure response is shown in Figure 66. The sensor in packaged in a ceramic chip carrier which is attached to a lucite plate. The plate also holds a Brüel & Kjaer microphone (Model 4138 with a UA160 adaptor, 2633 preamp, and a 2807 power supply) immediately adjacent to the ceramic carrier. The microphone is calibrated using a Brüel & Kjaer 4220 Pistonphone pressure cell. A speaker is held directly above the sensor and microphone, and is driven by a function generator through an audio amplifier. The output of the I-V converter response is digitized using a Phoenix Data Analog-to-Digital Converter, which has a maximum sampling rate of 330 kHz. The signal is sampled 2048 times with a sampling rate that is twice the maximum frequency of interest, from which a frequency spectrum is generated using digital signal processing techniques. The experiments are performed by driving the speaker at different
Fig. 66  Normal pressure test apparatus.
frequencies and measuring the sensor response versus the normal pressure as measured with the microphone.

B) Cell Calibration

In order to calibrate the sensor we must first calibrate the flow environment to which we are exposing it. This is done by measuring the volumetric flow versus pressure drop in the cell. Given the channel dimensions we can use this data to verify the existence of fully developed laminar flow. As we saw in Chapter 3, the pressure drop in laminar channel flow from a volumetric flow of $Q$ in a channel of height $h$, width $W$ (where $h \ll W$), and length $L$ can be expressed as

$$\frac{\Delta p}{L} = \frac{12 \mu}{h^3W} Q$$

(74)

Thus, the existence of laminar flow can be verified by looking for a linear pressure versus flow relationship. Furthermore, by examining the pressure drop along the length of the cell we can determine if the flow is fully developed. Finally, the slope of a pressure-flow plot will be inversely proportional to the cube of the channel height, and thus we can independently determine the channel height.

Figure 67(a,b,c) is a plot of differential pressure drop measured at four point along the flow channel using the three rotameters. These data are collected by measuring the pressure difference between two adjacent pressure taps as a function of volumetric flow. Since there are six pressure taps in the laminar cell, five differential pressures are measured, with the spacing between taps set at 5 mm. The data in Figure 67 show only four points because the first tap was found to be defective.

The pressure drops appear very linear up to a volumetric flow of approximately 10 liter/minute. The pressure measurements are also very constant along the length of the cell,
Fig. 67  Pressure versus flow for laminar cell with 217 μm thick shim.
Fig. 67  Pressure versus flow for laminar cell with 217 μm thick shim.
implying that the flow is fully developed. Furthermore, taking the slope of these lines we extract an effective channel height of 217 μm, which compares with a measured shim thickness of 240 μm. The difference in these number can be attributed to two factors, compression of the shim in the assembled cell, and protrusion of silicon plate above the lucite plate. The later effect is highlighted schematically in Figure 68. We will use the extracted channel height in all data analysis.

Beyond the 10 liter/minute point the pressure increases at a greater than linear rate. We believe that this pressure non-linearity can be attributed to a transition to turbulent flow. As we saw in Chapter 3, the Reynolds Number, \( N_{RE} \), in the channel may be expressed as

\[
N_{RE} = \frac{\rho <u_x> h}{\mu}
\]  
(75)

where \(<u_x>\) is the average velocity in the channel, \( \mu \) is the absolute viscosity of the air (1.95 x 10^{-5} \text{ kg/m-s}) , and \( \rho \) is the density of the air (1.3 kg/m^3). The average velocity is determined as

\[
<u_x> = \frac{Q}{hW}
\]  
(76)

Note that the Reynolds Number is independent of channel height. The actual channel width, \( W \), is equal to 2.6 cm, and thus we can express the Reynolds Number in terms of volumetric flow as

\[
N_{RE} = 42.7Q \quad [\text{l/min}]
\]  
(77)

The transition to turbulence in pipe flow occurs at a Reynolds Number of \( \sim 2000 \), which translates for our case to a volumetric flow of 40-50 l/min. This is consistent with
Fig. 68  Effect of plate misalignment on shim height.
what we see in Figure 67(c). As further evidence, Figure 69 shows a plot of pressure drop versus volumetric flow for the flow cell with a different shim thickness. The channel height as determined from the slope is 426 μm. Based on equation 77, the transition to turbulence should occur at the same volumetric flow, as it does.

C) Measured Flow Response

After the calibration of the laminar flow cell, we can calibrate the sensor. Figure 70 shows the measured output of the I-V converter versus volumetric flow for the sensor with a channel height of 217 μm. Figure 70(a) shows the measured response over the flow range of two of the rotameters. A slight discontinuity exist at the 1 liter/minute point where the rotameters overlap. Figure 70(b) is an expanded plot of the lowest flow range. These results are collected in a time interval that is sufficiently short to minimize long term drift problems. Drifts of several microvolts per minute have been observed with this sensor, requiring that the data be collected in the span of approximately 5 minutes for the lowest flow range. More will be said about the drift effects in Section 5.D.

Using the cell calibration data from the previous section we can calculate the shear stress for a given flow for the results in Figure 70. The calculated results are shown in Figure 71, where we can see that 1 Pa shear stresses can be readily determined. The slope of this graph is 470 μV(a-c)/Pa. We can determine the equivalent voltage on the gates of the devices by using the test transistors which are on the same chip with the sensor. We connect the test transistors in the configuration shown in Figure 72. The driven voltage is adjusted such that the single channel output voltage matches that of the sensor. In this way the ratio of drive to output voltage will determine the voltage gain of the system. For the sensor used to measure the results in Figures 70-71, the single channel output voltage was 0.84 V(a-c). A drive voltage of 93 mV(a-c) on the test transistors produced an output voltage of the same magnitude as the sensor, from which the gain is determined to be 9.0.
Fig. 69  Pressure versus flow for laminar cell with 426 μm thick shim.
Fig. 70  Differential voltage output versus flow with 217 μm thick shim.
Fig. 71 Differential voltage output versus shear stress using data in Figure 70.
Fig. 72 Illustration of system gain measurement technique.
This gain implies an equivalent voltage on the gate of the MOSFETs of 52 µV(a-c)/Pa, or 114 µV(p-p)/Pa.

At very large flows the measured differential response exhibits very nonlinear behavior as shown in Figure 73. The voltage output actually begins to decrease in magnitude beyond a certain point. We have observed using the microscope, however, that the element continues to move in the direction of the flow. Initially it was felt that the transition to turbulence in the cell might cause this behavior. However, this effect was not observed in the measurements made with the larger channel height at the same Reynold’s Number (discussed further in Section F). Thus the transition to turbulence does not explain the turn-around. We believe this output voltage turn-around is caused by out-of-plane deformation of the plate at very large deflections (~1 µm). Such deformations could explain why the signal changes direction while the plate continues to move in the same direction. Also, for the same volumetric flow, and hence Reynold’s Number, the shear stress will be less for a larger channel height. Thus the large deflections that produce this effect are not observed.

D) Noise, Drift, and Moisture Effects

The minimum detectable signal obtainable from this sensor will be determined by the noise floor or drift of the sensor and measurement circuit. The noise of the sensor and off-chip readout circuit was measured (with the drive grounded) to be approximately 100-250 µV. As was stated in Section C, the sensors have been observed to drift on the order of several microvolts per minute. We have also noticed that the sensor has a very large photosensitivity. Direct illumination can cause shifts in the differential output voltage of the order of several millivolts. Finally, we have noticed a moisture dependence in the sensor behavior.

The moisture effect is observed during the start-up of the laminar flow testing. The sensor is stored in the laboratory with moisture levels of ~50% RH. The flow testing is
Fig. 73  Sensor response at high flow for 217 μm thick shim.
performed using dry compressed air. When the flow is initiated in the laminar cell, a transient of approximately 5-10 mV is observed in the differential output signal. The transient has a characteristic decay time of approximately 1-2 minutes. After this initial transient, the response stabilizes to the steady state drift reported above. The flow can be decreased below the minimum detectable threshold and the sensor remains stable, but once the dry air is turned off the sensor returns through a transient to the initial state.

It is known that polyimide absorbs water in proportion to the relative humidity of the ambient[36,37]. There are two possible mechanisms to explain the moisture sensitivity of this sensor. First, the moisture absorption produces an increase in the dielectric permittivity of the polyimide which could cause the un-balanced differential output voltage to shift. Second, it is possible that the moisture absorption would cause a change in the residual tensile stress in the polyimide, which would produce a conformational change in the microstructure and hence a change in the capacitive coupling.

E) Comparison with Model

Given the equivalent voltage on the gates of the sensor versus shear-stress, we can compare the measured results with those determined using the model of Chapter 2, which is implemented in Appendix B. In order to model the sensor we will first compare the single channel response of the sensor with the model as a function of drive voltage. The model parameters, namely the gate capacitance, will be adjusted to match this response. Once this is done the differential shear stress sensitivities may be compared. The results of the mechanical properties characterization of Chapter 4 will be used in the model. Specifically, a polyimide elastic modulus and residual tensile stress of 4.5 GPa and 34 MPa respectively, will be assumed.

Figure 74 is a plot of the single channel output of the sensor versus drive voltage. The output voltage shown is the equivalent voltage on the gate in mV(a-c), which is obtained by dividing the output of the I-V converter by the system gain measured in the
Fig. 74  Single channel input-output response of sensor.
previous section (9.0). Also shown in Figure 74 is the fit of the model to this data. The parameters used to achieve this fit are contained in Appendix E. All of the parameters used are based on the mask dimensions, the process data, and the mechanical properties of the polyimide. The capacitance of the MOSFET was used as the fitting parameter, since this value is not known with a great deal of certainty. The gate capacitance was decreased by approximately 10% from the estimated value based on the process parameters and the mask dimensions.

With the single channel output fit to the model, we can then compare the shear stress sensitivity. The measured shear stress sensitivity was 52 μV(a-c)/Pa. If we account for the additional plate forces as outlined in Chapter 2, the true shear stress sensitivity will be reduced by the factor

\[ \tau_{w,\text{eff}} = [1 + \frac{t_{\text{gap}}}{h} + \frac{2t_{p}}{h}] \tau_{w} = 1.31 \tau_{w} \]  

(78)

where \( t_{\text{gap}} = 3 \ \mu\text{m} \) and \( t_{p} = 32 \ \mu\text{m} \). Thus the sensitivity to shear stress acting on one face is 40 μV(a-c)/Pa. The calculated sensitivity is linear with a value of 42 μV(a-c)/Pa assuming a tether width of 5 μm. This tether width is substantially less than the mask dimension of 20 μm for two reasons. First, the topography of the wafer at the polyimide definition lithography step requires substantial overexposure of the photoresist. Second, the plasma etch through 32 μm of polyimide produces 6-7 μm of lateral under-etch from each side. The tethers have been inspected under an optical microscope, and the width was measured to be approximately 5 μm. A table of the calculated values from the model is contained in Appendix E.

The good agreement with the model is encouraging, but we can lend further confidence to the measured results by comparing these results with two other measured responses. First, we will double the channel height to investigate if the additional plate
forces scale correctly. Second, we will measure the acceleration sensitivity of the device as an additional way to calibrate the mechanical sensitivity of the sensor.

F) Effect of Channel Height

The additional plate forces which were discussed in Chapter 2 can add 31% to the measured response for a channel height of 217 \( \mu m \). We have doubled the channel height and measured the sensor response in order to further confirm this model. Figure 75(a) shows the measured response as a function of volumetric flow for two flow ranges, and Figure 75(b) is an expanded view of the lowest flow range. At very high flows where the cell becomes turbulent the measures response increases at a greater than linear rate. This is in contrast to the results measured at this Reynold's Number with the 217 \( \mu m \) channel height. If we concentrate on the low Reynold's Number flow region we can compare the results measured for the two channel heights as shown in Figure 76.

The new shear stress \( \tau'_w \), caused by a change in channel height from \( h \) to \( h' \) can be related to the old shear stress \( \tau_w \) as

\[
\tau'_w = \left( \frac{h}{h'} \right)^2 \tau_w
\]  

(79)

Thus, using the expression in equation 37, the effective shear stresses can be related as

\[
\tau'_{w,\text{eff}} = \left( \frac{h}{h'} \right)^2 \frac{\left[ 1 + \frac{f_{gap}}{h'} + \frac{f_p}{h'} \right]}{\left[ 1 + \frac{f_{gap}}{h} + \frac{f_p}{h} \right]} \tau_{w,\text{eff}}
\]  

(80)

The ratio of the slopes of the lines in Figure 76 should go as the ratio of the effective shear stresses as calculated using equation 80. The measured slopes are 743 \( \mu V \cdot \text{min}/l \) and 156 \( \mu V \cdot \text{min}/l \), and the ratio is 0.21. The calculated ratio using channel heights of 217 \( \mu m \) and 426 \( \mu m \) is 0.23. Thus they are in good agreement.
Fig. 75  Differential voltage output versus flow with 426 μm thick shim.
Fig. 76  Differential voltage output versus flow for both shim thicknesses.
G) Acceleration Sensitivity

It is possible to consider the effect of an acceleration force acting on the floating element in terms of an effective shear-stress. The force on the element due to acceleration is simply

\[ F = M\alpha \]  

(81)

where \( M \) is the mass of the floating element and \( \alpha \) is the acceleration. For the case of the floating element the mass is

\[ M = \rho t_p W_e L_e \]  

(82)

where \( \rho \) is the density of the polyimide. If we normalize the force in equation 81 by the surface area of the floating element, \( W_e L_e \), we can define an effective shear stress for the structure as

\[ \tau_{\text{eff}} = \frac{\rho t_p W_e L_e \alpha}{W_e L_e} = \rho t_p \alpha \]  

(83)

For an acceleration of gravity (9.8 m/s\(^2\)), a floating element thickness of 32 \( \mu \)m, and using a density of polyimide of 1400 kg/m\(^3\), the effective shear stress is 0.44 Pa.

Figure 77 shows the measured response of the sensor as it is rotated through 180°. The response traces out a sine curve as we would expect. The maximum amplitude is \( \pm 120 \mu \text{V(a-c)} \) at \( \pm 90° \). This output was recorded using a drive voltage of \( \pm 10 \text{ V} \), which is half that used in measuring the flow response. Given the linear relationship between drive and output voltages shown in Figure 74, the system gain of 9.0, and the ratio of 0.44 Pa/g, the
Fig. 77  Measured acceleration sensitivity.
equivalent gate voltage sensitivity works out to be 61 μV(a-c)/Pa. This value is larger than that measured in the laminar flow, however, the functional dependence upon angle is correct. The uncertainty regarding the polyimide density as well as errors in aligning the sensor such that it is parallel to the acceleration of gravity could explain the difference in the two measurements. Also, the part used for the flow measurement is not the same as the part used for the acceleration tests. More work needs to be done in order to fully calibrate this measurement.

H) Normal Pressure Response

Normal motion of the plate will amplitude modulate the single channel output signal. The differential signal to first order should be insensitive to normal motion of the plate. Therefore if the plate is oscillating normal to the surface at a frequency \( f_s \), and the sensor is driven by a signal at the frequency \( f_d \), we would expect to see a carrier at \( f_d \) and side-bands at \( f_d - f_s \) and \( f_d + f_s \) in the single channel output spectrum. The differential output should have only the carrier.

In these experiments the speaker frequency, \( f_s \), was adjusted to the acoustic resonances where a maximum amplitude from the microphone was achieved. These resonances occurred typically around 50 Hz, 100 Hz, 600 Hz, and 3.1 kHz. The maximum microphone amplitude at these frequencies was approximately 100 mV(p-p), which corresponds to a normal pressure fluctuation of 84 Pa. The sensor drive signal frequency, \( f_d \), was set at 1 kHz, 10 kHz, and 20 kHz.

The only amplitude modulations which were observed occurred with \( f_d = 10 \) kHz, \( f_s = 3.1 \) kHz, and a normal pressure of 84 Pa. The measured output spectrum is shown in Figure 78. The side-bands at 7 kHz and 13 kHz are more than 60 dB down from the carrier. The measured amplitude of these signals is 177 μV(a-c). This corresponds to a sensitivity of 2 μV(a-c)/Pa which when we account for the gain of 9.0 through the system gives an equivalent gate voltage of 0.2 μV/Pa. This signal is two orders of magnitude less
than the shear stress sensitivity, and based on a simple calculation would correspond to plate motions of less than 0.1 Å! Also, some amount of this side-band amplitude could be due to other forms of coupling to the gain element. Finally, we can see in Figure 79 that the differential signal shows no evidence of amplitude modulation by the pressure fluctuations.

I) Wind Tunnel Results

Several sensors were packaged and tested in the wind tunnel using the package described in Chapter 4. While a differential voltage change was observed as a function of flow, the drift was too large to enable accurate measurement of the 0.4 Pa maximum shear stresses which were generated. It was found that the parts packaged for the wind tunnel drifted tens to hundreds of microvolts per minute. Given this drift, and the length of time needed to establish a given flow in the wind tunnel, it was determined that turbulent shear stress forces could not be measured with these parts. More work is being done to attempt to identify the reason for the large drifts in this package.

J) Summary

The floating-element shear stress sensor has been completely calibrated in laminar flow. Particular attention was paid to calibration of the flow environment in order to lend confidence to the measured flow response. The response was studied as a function of channel dimension and was compared to the model. The predicted response was in good agreement with the measurements. The acceleration sensitivity of the structure was also measured and compared with a model. The response of the floating-element to normal pressure was measured and shown to be insignificant. Finally, an attempt was made to place the sensor in the wall of the MIT wind tunnel, but the response was limited by large drifts which appear to be package induced.
Fig. 78  Single channel response to 84 Pa - 3 kHz normal pressure wave

(sensor is driven at 10 kHz).
Fig. 79  Differential response to 84 Pa - 3 kHz normal pressure wave (sensor is driven at 10 kHz).

$V_{out}$ (p-p)

FREQUENCY

0  10 kHz  20 kHz

0.1 mV  1 mV  10 mV  0.1 V  1 V  10 V
CHAPTER 6
CONCLUSIONS

The goal of this thesis was to investigate the feasibility of microfabricated sensors for turbulent boundary layer research. In particular, a floating-element shear-stress sensor was selected as a candidate for microfabrication. The work required attention be paid to several critical areas; sensor design with integrated readout, mechanical characterization of the constituent materials, and packaging for calibration and testing. These efforts converged in the testing of the sensor.

The design of the sensor with integrated readout consisted of the development of an electro-mechanical model for the behavior of the microfabricated mechanical structure. A set of analytical expressions were developed to predict both the static and dynamic response of the sensor. Using the predicted mechanical response as a guide, a differential capacitor readout scheme was designed. The readout was chosen with the minimum amount of integration allowed by the measurement requirements. A set of design criteria were developed from which a final sensor layout was generated.

Two key mechanical property issues were identified during the development of the design criteria. First, it was found that the modulus and residual stress of the polyimide used to make the element directly coupled into the sensor performance. Secondly, the integrated readout scheme required the fabrication of a composite microstructure. A set of experiments were designed to characterize the mechanical properties of the polyimide floating element. Mechanical property measurements which had been previously developed were employed along with a special test set to characterize the composite structure. The result of this portion of the study was the development of a data set to be used in the modelling and calibration of the sensor, as well as a design criteria for fabricating a polyimide microstructure with control of the shape.
The desire to calibrate this sensor necessitated the development of a packaging technology. Microfabrication of the package was employed to satisfy the requirement for flush mounting and minimum gaps in a laminar flow. Pressure taps were included in the package design to facilitate calibration of the flow. A package was also designed for the wind tunnel.

The calibration of the sensor in laminar flow first required calibration of the flow environment. This was accomplished by measuring the pressure drop along the channel as a function of volumetric flow, from which it is possible to verify the existence of fully-developed laminar flow. Furthermore, this calibration provides us with an independent determination of shear stress which we can use in calibrating the sensor.

The sensor was calibrated in laminar flow and found to behave as predicted by the models in Chapter 2. The channel dimensions were varied and the response measured to improve the confidence in the calibration. Also, acceleration sensitivity measurement were found to be in good agreement with the predicted sensitivity.

The response of the sensor to normal pressure was studied by introducing large normal pressure waves and observing the output. No significant motion of the plate was detected.

The wind tunnel package described in Chapter 3 was employed in an attempt to measure a turbulent shear stress signal. It was found that these packaged parts had drifts that were too large compared to the signal of interest to allow for measurement of a turbulent signal. Further work needs to be done to characterize this package induced drift phenomena.

The conclusion to be drawn from this work is that it is indeed possible to microfabricate a version of the floating-element shear-stress sensor. The sensor behaves as expected in laminar flow. The next stage is the incorporation of this technology into a turbulent boundary layer research effort. Based on the results to date this will require investigation of several key issues. The drifts and moisture effects will need to be better
understood in order to allow for turbulence measurements. It is possible that a more sophisticated on-chip readout circuit will reduce the drift problems. The process which has been developed should readily allow for the incorporation of a more substantial read-out circuit.

Finally, it is possible that in future designs, a redesign of the mechanical structure and readout circuit will allow for the detection of shear stress in two dimensions. Ultimately, as the sensor design becomes more sophisticated one could hope to incorporate arrays of these structures which would permit the measurement of the shear stress "footprint" of a turbulent boundary layer.
APPENDIX A
DEFLECTION EQUATION FOR A CLAMPED-CLAMPED BRIDGE UNDER AXIAL TENSION

The equation derived in this appendix follows from the general derivation contained in Timoshenko's *Strength of Materials*, Part II. The beam of length $L$, width $W$, and thickness $d$, is shown in Figure A1, where it is loaded by a point load $P$, a distributed load $q$, and the axial loads $S$. The bridge is shown in Figure A2 with all of the forces acting on it, including an edge moment $M$, which represents the effect of the built-in boundary condition. The differential equation for bending of this structure may be written as

$$EI \frac{d^2y}{dx^2} = Sy - \frac{qLx}{2} + \frac{qx^2}{2} - \frac{Px}{2} + M$$  \hspace{1cm} (A1)

for $0 < x < \frac{L}{2}$, where $EI$ is the flexural rigidity of bending with $E$ being the modulus of elasticity and $I$ being the moment of inertia in the plane of bending ($I = \frac{1}{12}Wd^3$). If we let

$$p^2 = \frac{S}{EI}$$  \hspace{1cm} (A2)

then equation A1 may be expressed as

$$\frac{1}{p^2} \frac{d^2y}{dx^2} = y - \frac{qL + P}{2S}x + \frac{qx^2}{2S}x^2 + \frac{M}{S}$$  \hspace{1cm} (A2)

The form of the solution to this equation is
\[ y = C_1 \cosh px + C_2 \sinh px - \frac{q}{2S}x^2 + \frac{qL + P}{2S}x - \frac{M}{S} - \frac{q}{p} \frac{L}{2S} \] (A3)

The boundary conditions for this problem are

(i) \( y = 0 \) @ \( x = 0 \)

(ii) \( \frac{dy}{dx} = 0 \) @ \( x = 0 \)

(iii) \( \frac{dy}{dx} = 0 \) @ \( x = \frac{L}{2} \)

We can apply these boundary conditions and substitute equation A3 into A2 to solve for \( C_1, C_2, \) and \( M \). In doing this we arrive at an expression for \( y \) which is written as

\[ y = \frac{qL + P}{2S} \frac{p \sinh u}{p \sinh u} \frac{\cosh px}{(\cosh px - 1)} - \frac{qL + P}{2pS} \frac{\sinh px}{p \sinh u} - \frac{q}{2S}x^2 + \frac{qL + P}{2S}x \] (A4)

where \( u = \frac{pL}{2} \). We can now determine what the center point deflection should be by evaluating equation A4 at \( x = \frac{L}{2} \). The solution then reduces to

\[ y(x = \frac{L}{2}) = \frac{PL}{4S} \left[ 1 + \frac{qL}{2P} \right] \left[ 1 - \frac{\tanh \frac{u}{2}}{\frac{u}{2}} \right] \] (A5)

REFERENCE

Fig. A1  Axially loaded beam.

Fig. A2  Free body diagram.
APPENDIX B
COMPUTER MODEL OF SENSOR RESPONSE

This appendix contains the source code for the Fortran program which was written in order to model the sensor response. The input variable which were used are listed below.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>ld</td>
<td>450E-6 m</td>
</tr>
<tr>
<td>ls</td>
<td>450E-6 m</td>
</tr>
<tr>
<td>lg</td>
<td>10.63E-6 m</td>
</tr>
<tr>
<td>li</td>
<td>1270E-6 m</td>
</tr>
<tr>
<td>wo</td>
<td>10E-6 m</td>
</tr>
<tr>
<td>tfo</td>
<td>1E-6 m</td>
</tr>
<tr>
<td>tgate</td>
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</tr>
<tr>
<td>tp</td>
<td>32E-6 m</td>
</tr>
<tr>
<td>sigpi</td>
<td>34E6 Pa</td>
</tr>
<tr>
<td>lt</td>
<td>1000E-6 m</td>
</tr>
<tr>
<td>le</td>
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</tr>
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<td>vd</td>
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</tr>
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</tr>
<tr>
<td>f</td>
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<tr>
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</tr>
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</tr>
<tr>
<td>tpi1</td>
<td>1E-6 m</td>
</tr>
<tr>
<td>modpi</td>
<td>4.5E9 Pa</td>
</tr>
<tr>
<td>wt</td>
<td>5E-6 m</td>
</tr>
<tr>
<td>we</td>
<td>500E-6 m</td>
</tr>
<tr>
<td>tauw</td>
<td>1 Pa</td>
</tr>
</tbody>
</table>

The calculated results for these set of input parameters are

- Mechanical Sensitivity: 115 Å/Pa
- Gate Capacitance: 3.68 pF
- Total Sense to Substrate Capacitance: 5.67 pF
- Sense Capacitance: 54.5 fF
- Voltage on Gate: 0.1312 V  0.928 V(a-c)

Differential Shear-Stress Sensitivity: 42.4 μV(a-c)/Pa
PROGRAM TO CALCULATE WALL SHEAR SENSOR RESPONSE

Marty Schmidt  4/13/88
Version 1.0

DEFINITION OF VARIABLES

ld,wd  length and width of drive electrode
ls,ws  length and width of sense electrode
li,wi  length and width of interconnect to device
lg,wg  length and width of MOSFET gate
wo  width of gap between drive and sense electrodes
fo  fraction of sense electrode covered by shield plate
tfo  thickness of field oxide
tpo  thickness of polyimide/oxide passivation layer
tgate  thickness of gate oxide
tgap0  initial air gap thickness
tgap  air gap thickness after electrostatic pull-in
tgap1  equiv air gap thickness between plate and electrode
tgap2  equiv air gap thickness between plate and substrate
tp  thickness of floating element plate
tpl  thickness of polyimide between gap and shield electrode
sigpl  residual tensile stress in polyimide
modpl  elastic modulus of polyimide
lt,wt  length and width of tethers
le,we  length and width of element
vd  drive voltage amplitude
vs1  voltage on sense electrode #1
vs2  voltage on sense electrode #2
vout  differential output voltage
vave  average of vs1,vs2
tauw  shear stress
mininc  minimum increment for convergence of pull-in calc.
epsilon  relative permittivity of oxide (also oxide/polyimide passv)
epi  permittivity of polyimide
il,im  bending moment for lateral and normal deflections
ul,un  lateral spring constant
knormal  normal spring constant
deltalat  lateral deflection (normalized)
deladslat  absolute lateral deflection
deltan  absolute normal deflection
csb0  sense electrode to substrate capacitance
cgate  total gate capacitance
cline  interconnect capacitance
csb  total sense to subs capacitance
cdp  drive to plate capacitance
cpb  plate to substrate capacitance
cps1  plate to sense1 capacitance
cps2  plate to sense2 capacitance
cet  total equivalent capacitance from plate to subs
cdp  electrostatic force from drive to plate
fps1  force from plate to sense1
fps2  force from plate to sense2
fpb  force from plate to substrate
felec  total electrostatic force
iter  # of iterations to converge
inc  increment in iteration

READ INPUT VARIABLES
implicit real(a-z)
OPEN(7,FILE='fdata')
read(7,*)ld,wd,ls,ws,lg,wg,li,wi,wo,f
read(7,*)tfo,tpo,tgate,tgap0,tp,tpil
read(7,*)sigpi,modpi,lt,wt,le,we
read(7,*)vd
read(7,*)tauw
read(7,*)mininc

iter=0
eo=8.85e-12
reo=3.9*eo
epi=3.5*eo
tgap=tgap0
delta0=0

DEFLECTION SPRING CONSTANTS
il=(tp*wt*wt)/12
ul=((sigpi*tp*wt*lt*lt)/(modpi*il))**0.5
alpha=(1+we*lt)/(4*sigpi*tp*wt)
beta=(2*wt*lt)/(we*le)
gamma=(2*tanh(ul/2))/ul
klat=alpha*(1+beta)*(1-gamma)
in=(wt*tp*tp*tp)/12
un=((sigpi*tp*wt*lt*lt)/(modpi*in))**0.5
alpha=lt/(4*sigpi*tp*wt)
gamma=(2*tanh(un/2))/un
knorm=alpha*(1-gamma)

CALCULATE LATERAL DEFLECTION
deltal=(klat*tauw)/ws
delabs=klat*tauw

CONSTANT CAPACITANCES
csb0=eo*x1*ls*ws/tfo
cgate=eo*x1*lg*wgtgate
cint=eo*x1*li*wt/tfo
csb=csb0+cgate+cint

EFFECTIVE GAP SPACING
continue
tgap1=tgap+(eo/epe)*tpil+(eo/eox)*tpo
tgap2=tgap1+(eo/eox)*tfo

VARIABLE CAPACITANCES
cdp=eo*ld*wd/tgap1
cpb=eo*ld*wo/tgap2
cps1=eo*fl*ls*ws*(1+(deltal/f))/tgap1
cps2=eo*fl*ls*ws*(1-(deltal/f))/tgap1
cp=csps+cpb+((cps1*csb)/(cps1+csb))+(cps2*csb)/(cps2+csb)

VOLTAGE CALCULATIONS
vp=(cdp/(cdp+ct))*vd
vs1=(cps1/(cps1+csb))*vp
vs2=(cps2/(cps2+csb))*vp
vout=vs1-vs2
vave=(vs1+vs2)/2
NORMAL FORCE CALCULATION

\[ \text{fdp} = \frac{(cdp*(vd-vp)*(vd-vp))}{tgap1} \]
\[ \text{fps1} = \frac{(cps1*(vp-vs1)*(vp-vs1))}{tgap1} \]
\[ \text{fps2} = \frac{(cps2*(vp-vs2)*(vp-vs2))}{tgap1} \]
\[ \text{fpb} = \frac{(cpb*vp*vp)}{tgap2} \]
\[ \text{flecc} = 0.25*(\text{fdp} + \text{fps1} + \text{fps2} + \text{fpb}) \]

CALCULATE NORMAL DEFLECTION

\[ \text{deltan} = \text{knorm}\*\text{flecc} \]
\[ \text{tgap} = \text{tgap0} - \text{deltan} \]
\[ \text{iter} = 1 + \text{iter} \]

CHECK FOR PULL-IN

if (tgap .lt. 0) goto 75

CHECK FOR ITERATION CONVERGENCE

\[ \text{inc} = \text{deltan} - \text{delta0} \]
if (inc .lt. 0) inc = -inc
if (inc .lt. mininc) goto 100
\[ \text{delta0} = \text{deltan} \]
goto 50

PRINT PULL-IN MESSAGE

75 continue
print*, "Pull-in of Plate for Vplate = ", vd
print*, "Vplate = ", vp
print*, "Number of iterations = ", iter
goto 999

PRINT RESULTS

100 continue
print*, "Vdrive = ", vd, "Tauw = ", tauw
print*, "Lateral Spring Constant (m/Pa) = ", klat
print*, "Deflection = ", delabs
print*, "Differential output = ", vout
print*, "Averaged output = ", vave
print*, "Gap spacing = ", tgap
print*, "Gate capacitance = ", cgate
print*, "Total substrate capacitance = ", csb
print*, "Sense Capacitance = ", cps1
print*, "Number of iter. = ", iter

999 continue
stop
end
APPENDIX C
PROCESS SEQUENCES

This appendix contains the detailed process sequences used in fabricating all of the samples used in this thesis.

1) PMOS and Floating Element

Starting Material  2 inch (100) Silicon Wafers n-type 10-20 Ω-cm.

1) Scribe Run # and Wafer # on edge of frontside of wafer.
2) RCA Clean.
3) Oxidation at 1100 °C, 15 min - 180 min - 15 min, dry-wet-dry. Will grow 1 μm thermal oxide.
4) Photolithography step to pattern diffusion regions.
5) Buffered Oxide Etch of 1 μm thermal oxide.
6) Photoresist Strip.
7) RCA Clean.
8) Solid Source Boron Deposition at 900 °C for 30 minutes.
9) Borosilicate glass strip in 10:1 HF till dewet on monitor wafer(1.5-2 min).
10) RCA Clean
11) Boron drive-in at 1100 °C, 60 min - 30 min, dry-wet. Will grow approximately 5000Å.
12) Photolithography step to pattern thin oxide gate regions and source/drain contact areas.
13) Buffered Oxide Etch to remove 1 μm of thermal oxide.
14) Photoresist strip.
15) RCA Clean.
16) Gate oxidation at 1100 °C, 60 min - 30 min, dry-nitrogen. Will grow 1000Å.

17) Photolithography step to pattern contact cuts.

18) Buffered Oxide Etch to open contact cuts(1 min).

19) Photoresist strip.


21) RCA Clean.

22) Short (5-15 sec) 10:1 HF Dip to dewet scribe lanes.

23) Electron-beam evaporate 5000Å aluminum on frontside.

24) Photoresist coat front of wafer.

25) Plasma etch(SF6) backside to remove any boron doping. Check with hot point probe.

26) Strip photoresist.

27) Photolithography step to pattern aluminum.


29) Strip Resist.

30) Clean back of wafer with 10:1 HF using a swab.

31) Electron-beam evaporate 1µm of aluminum on backside of wafer.

32) Alloy front and back metal. 450 °C for 30 min in forming gas.


34) Deposit 7500 Å APCVD Oxide in PYROX system. Temperature = 400 °C, time = 10 minutes.

35) Spin cast polyimide on front and backside of wafer. DuPont 2545, 1 coat, 6000 RPM. Soft bake 145 °C, 14 minutes.

36) Cure polyimide at 400 °C for 45 minutes.
37) Electron-beam evaporate 3 μm of aluminum on frontside.
38) Photolithography step to pattern spacer.
39) Etch spacer in PAN etch. Use slight heating to accelerate etch time.
40) Strip resist in O2 plasma. Over etch in order to clear polyimide from field regions.
41) Spin cast polyimide on frontside. DuPont 2455, 1 coat, 6000 RPM. Soft bake 160 °C, 15 minutes.
42) Electron-beam evaporate 300 Å of Chrome on frontside.
43) Photolithography step to pattern thin chrome. Photoresist is overexposed by approximately 100% in time to minimize the time required in developing solution. Prolonged exposure to developer will cause attack of un-cured polyimide under chrome. Also, photoresist is not hard-baked prior to etch of chrome.
44) Chrome etch using commercial (TRANSENE) chrome mask etchant.
45) Photoresist strip in acetone, methanol rinse. Follow with a low power, 30 second O2 plasma 'descum'.
46) Spin cast polyimide on frontside. DuPont 2525, 3000 RPM, 7 coats, soft bake 145 °C 15 minutes between each coat.
47) Cure polyimide at 400 °C for 45 minutes.
48) Electron beam evaporate 3000 Å of aluminum on the frontside.
49) Photolithography step to pattern aluminum etch mask. Resist must be overexposed (25-50%) because of the topology of the wafer.
50) PAN etch aluminum etch mask.
51) O2 plasma etch polyimide. 400 W, 100 mtorr, 35 minutes.
52) Use diamond saw to partially cut through wafer. Wafer will remain intact until after spacer etch.
53) PAN etch spacer. Etch takes approximately 2 hours (for 500 μm x 500 μm element) when solution is mildly heated. After etch, wafer must be rinsed in DI water and methanol for approximately 15 minutes each and then left to air dry.

54) O2 plasma etch polyimide passivation layer off back of wafer to expose backside metal.

55) Break wafer into individual die.

56) Oxide over bonding pads is removed most effectively using a vapor HF etch. The individual die are held above a beaker of 48wt% HF, with only the pads exposed. The rest of the chip is physically masked from the vapors using a specially designed holder.

2) Laminar Cell Wafer

Steps 1-4 and 9 follow from Mchregany SM Thesis, 1986, MIT.

Starting Material 2 inch (100) Silicon Wafers p-type 10-20 Ω-cm.

1) RCA Clean

2) Solid source boron deposition at 1175 °C for 2 hours. Wafers are loaded at 900 °C and ramped up and down. The tube is flushed with 90% nitrogen and 10% oxygen during deposition to minimize surface damage.

3) Strip borosilicate glass in 10:1 HF till dewet (4 minutes).

4) Oxidation at 990 °C, 15 min - 45 min - 15 min, dry - wet - dry. Final thickness is 3200 Å.

5) Coat frontside of wafer with positive photoresist and hardbake.

6) Photolithography step on backside of wafer to define pressure tap and chip cavity regions. Thick photoresist is used to ensure good coverage of the rough backside.

7) Buffered Oxide Etch of the backside oxide (5 minute).
8) Photoresist strip.
9) Anisotropic etch of silicon in hydrazine.
10) Buffered Oxide Etch to strip oxide from wafer.
11) Spin cast polyimide. DuPont 2555, one coat, 6000 RPM, followed by DuPont 2525, seven coats, 3000 RPM. Softbake 145 °C for 15 minutes between coats.
12) Cure polyimide at 400 °C for 45 minutes.
13) Photolithography step to define polyimide. Infrared aligner is used to allow for front-to-back alignment.
14) Low power O2 plasma etch until photoresist is cleared from wafer. This will produce a shallow relief pattern on the surface of the polyimide.
15) Electron-beam evaporate 3000 Å of aluminum.
16) Photolithography step to define aluminum etch mask.
17) PAN etch aluminum etch mask.
18) O2 plasma etch polyimide. 400 W, 100 mtorr, 35 minutes.
19) PAN etch to remove aluminum etch mask.
20) SF6 plasma etch of backside to remove thin silicon diaphragms. 400 W, 125 mtorr, 10-15 minutes.
21) Break wafer along etched scribe lanes to form silicon plate.

3) Mechanical Properties Wafer

1) Form diaphragms in silicon using steps 1-9 in previous process.
2) Photolithography step to define alignment patterns in the oxide on the frontside of the wafer. The infrared aligner is used in this step.
3) Buffered Oxide Etch of the patterned oxide.
4) Photoresist strip.

NOTE: Steps 5-10 are used only if a spacer is to be incorporated in the process.
5) Spin cast polyimide. DuPont 2545, one coat, 6000 RPM, softbake 145 °C 15 min.
6) Cure polyimide at 400 °C for 45 min.
7) Electron-beam evaporate 3 μm of aluminum.
8) Photolithography step to pattern spacer.
9) PAN etch of spacer aluminum. Mild heating of PAN to accelerate etch.
10) O2 Plasma etch to strip photoresist and remove polyimide from the field region.
11) Spin cast polyimide. DuPont 2555, one coat, 6000 RPM, soft bake 145 °C 15 min.
12) Electron-beam evaporate 300 Å of Chrome on frontside.
13) Photolithography step to pattern thin chrome. Photoresist is overexposed by approximately 100% in time to minimize the time required in developing solution. Prolonged exposure to developer will cause attack of un-cured polyimide under chrome. Also, photoresist is not hard-baked prior to etch of chrome.
14) Chrome etch using commercial (TRANSENE) chrome mask etchant.
15) Photoresist strip in acetone, methanol rinse. Follow with a low power, 30 second O2 plasma 'descum'.
16) Spin cast polyimide on frontside. DuPont 2525, 3000 RPM, 7 coats, soft bake 145 °C 15 minutes between each coat.
17) Cure polyimide at 400 °C for 45 minutes.
18) Electron beam evaporate 3000 Å of aluminum on the frontside.
19) Photolithography step to pattern aluminum etch mask. Resist must be overexposed (25-50%) because of the topology of the wafer.
20) PAN etch aluminum etch mask.
21) O2 plasma etch polyimide. 400 W, 100 mtorr, 35 minutes.
22) PAN etch to strip aluminum etch mask.

23) SF6 plasma etch of backside to remove thin silicon and release cantilevers and diaphragms.

24) If a spacer is used, and O2 plasma backside etch is needed to remove the polyimide which is underneath the spacer, and then the spacer must be etched in PAN.
APPENDIX D

ANALYSIS OF PASSIVATION PROCESS

A series of experiments were performed to study the integrity of the passivation process which is used to protect the integrated readout circuitry. It was found that thermal expansion mismatch between the thick aluminum spacer and the silicon substrate could introduce very substantial stress cracking in the oxide passivation layer underneath the spacer. This cracking occurred during the polyimide cure process.

A) Experimental

Standard 2-in (100) silicon wafers were oxidized to form a 1 μm thick thermal silicon dioxide layer. A 5000 Å thick aluminum layer was electron-beam evaporated onto the substrates and patterned using the metal mask used in the floating element process. Next, an APCVD silicon dioxide film was deposited on the wafer. Two thicknesses of APCVD oxide were studied, 7500 Å and 15,000 Å. Some samples were coated with a 1 μm spin-cast film of polyimide which was cured at 400 °C for 45 min, to form a composite passivation layer of deposited oxide and polyimide. A 3 μm thick aluminum layer was electron beam evaporated on all substrates and patterned using the floating element process mask. One sample at each condition was then thermally cycled to 400 °C for 45 minutes to simulate the curing cycle of the floating element polyimide. Next, the spacer was etched off of the samples and the samples which contained polyimide on top of APCVD oxide were etched on an O2 plasma to remove the polyimide. Finally, the wafers were placed in heated PAN etch for three hours.

In areas where a defect in the passivation exists over a metal line, the defect was very visible due to the attack of the underlying metal film by the three hour PAN etch. The wafers were inspected and the number of defects in 17 sites of 500 μm x 500 μm size were recorded. Areas of the wafer where there was no spacer were also monitored for defects.
B) Results

Table D1 presents the measured defect counts for the twelve wafers which were fabricated. Six samples at each APCVD oxide thickness were fabricated. Wafers 1,2 and 7,8 had no polyimide layer, while wafers 3,4 and 9,10 had a polyimide layer between the APCVD oxide and the spacer. Wafers 5,6 and 11,12 were control wafers which had no spacer but were processed along with the other samples. One wafer in each subset was given a 400 °C thermal cycle to simulate the polyimide cure.

The conclusion from these results is that the thermal cycling of the wafers with an aluminum spacer directly on the APCVD oxide introduced a substantial number of defects. Furthermore the defect count was a strong function of oxide thickness. The incorporation of a polyimide layer between the oxide and the aluminum effectively eliminates the stress cracking problem. This polyimide layer acts as a stress absorbing, or load-bearing layer to minimize the shear loading of the relatively brittle oxide.
<table>
<thead>
<tr>
<th>WAFER #</th>
<th>OXIDE THICKNESS</th>
<th>SPACER</th>
<th>POLYIMIDE</th>
<th>400 °C CYCLE</th>
<th>UNDER SPACER</th>
<th>FIELD REGION</th>
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</thead>
<tbody>
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<td>82</td>
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<td>0</td>
<td>1</td>
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<td>✓</td>
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<td>NA</td>
<td>1</td>
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<td>NA</td>
<td>4</td>
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</tbody>
</table>

Table D1   Passivation Analysis.
APPENDIX E

PACKAGE DESIGNS

This appendix contains the detailed drawings for all components used in the shear stress sensor packages. The following drawings are included:

Figure E1    Silicon plate
Figure E2    Lucite sensor plate
Figure E3    Shim
Figure E4    Lucite base plate
Figure E5    Lucite cover plate
Figure E6    Wind tunnel plug
Fig. E1  Silicon plate.
Fig. E2  Sensor plate.
Fig. E3  Shim.
Fig. E4  Base plate.
Fig. E5  Cover plate.
Fig. E5  Cover plate.
Fig. E5  Cover plate.
Fig. E6  Wind tunnel plug.
APPENDIX F
CURRENT TO VOLTAGE CONVERTER

The schematic of the current to voltage converter circuit is shown in Figure F1. The components used are listed below. The supply voltages of all IC's were bypassed to ground with 0.1 μF ceramic capacitors. The circuit supply was provided by a ±16.5 V battery.

<table>
<thead>
<tr>
<th>COMPONENT</th>
<th>DESCRIPTION</th>
</tr>
</thead>
<tbody>
<tr>
<td>R1, R6</td>
<td>10kΩ metal film resistor (1/2watt)</td>
</tr>
<tr>
<td>R2, R7</td>
<td>10kΩ carbon resistor (1/4watt)</td>
</tr>
<tr>
<td>R3, R8</td>
<td>10kΩ carbon resistor (1/4watt)</td>
</tr>
<tr>
<td>R4, R9</td>
<td>1kΩ carbon resistor (1/4watt)</td>
</tr>
<tr>
<td>R5, R10</td>
<td>1kΩ metal film resistor (1/2watt)</td>
</tr>
<tr>
<td>R11, R12, R13</td>
<td>10kΩ metal film resistor (1/2watt)</td>
</tr>
<tr>
<td>R14</td>
<td>100kΩ Clarostat trim pot</td>
</tr>
<tr>
<td>C1, C2</td>
<td>10 μF - 25V tantalum capacitor</td>
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<td>A1, A6</td>
<td>LM318 Operational Amplifier</td>
</tr>
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<td>A2, A3, A4</td>
<td>μA741 Operational Amplifier</td>
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<td>μA741 Operational Amplifier</td>
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<td>A5, A10, A11</td>
<td>LM318 Operational Amplifier</td>
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</tbody>
</table>
Fig. F1  Current-Voltage Converter.
REFERENCES


183


