MECHANICAL COUNTER-PRESSURE SPACE SUIT DESIGN
USING ACTIVE MATERIALS

by

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Mechanical Counter-Pressure Space Suit Design

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Abstract

Mechanical counter-pressure (MCP) space suits have the potential to greatly improve the mobility of astronauts as they conduct planetary exploration activities; however, the underlying technologies required to provide uniform compression in an MCP garment at sufficient pressures (29.6 kPa) for space exploration have not yet been demonstrated, and donning and doffing of such a suit remains a significant challenge. This research effort focuses on the novel use of active material technologies to produce a garment with controllable compression capabilities to address these problems.

We first describe the modeling, development, and testing of low spring index \( C = 3 \) nickel titanium (NiTi) shape memory alloy (SMA) coil actuators designed for use in wearable compression garments. Several actuators were manufactured, annealed, and tested to assess their de-twinning and activation characteristics. We then describe the derivation and development of a complete two-spring model to predict the performance of hybrid compression textiles combining passive elastic fabrics and integrated SMA coil actuators based on 11 design parameters. Design studies (including two specifically tailored for MCP applications) are presented using the derived model to demonstrate the range of possible garment performance outcomes based on strategically chosen SMA and material parameters. Finally, we present a novel methodology for producing modular 3D-printed SMA actuator cartridges designed for use in compression garments, and test 5 active tourniquet prototypes (made using these cartridges and commercially available fabrics) to assess the effect of SMA actuation on the tourniquet compression characteristics.

Our results demonstrate that hybrid active tourniquet prototypes are highly effective, with counter-pressures increasing by an average of 81.9% when activated (taking an average of only 23.7 seconds to achieve steady state). Maximum average counter-pressures reached 34.3 kPa, achieving 115.9% of the target MCP counter-pressure. We observed significant spatial variability in the active counter-pressure profiles, stemming from high friction, asymmetric fabric stretching, and near-field pressure spikes/voids caused by the SMA cartridge. Modifications to reduce tourniquet friction were effective at mitigating a proportion of this variability.
performance and repeatability were found to depend heavily on the passive fabric characteristics, with performance losses attributable to irrecoverable fabric strain, degradation in fabric elastic modulus, and non-linear modulus behavior.

The results of this research open the door to new opportunities to advance the field of MCP spacesuit design, as well as opportunities to improve compression garments used in healthcare therapies, competitive athletics, and battlefield medicine.

This research was supported in part by the National Aeronautics and Space Administration (NASA) Office of the Chief Technologist (OCT) under Grant NNX11AM62H, and by the MIT Portugal Program (MPP).

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I dedicate this thesis to my Grandpa Jerry and Grandma Lorine, each of whom we lost this year. We love you and miss you greatly. Rest in Peace.
# Contents

1 Introduction .......................................................... 25  
   1.1 Background ........................................................ 25  
   1.2 Problem Statement ............................................... 28  
      1.2.1 Motivation .................................................. 28  
      1.2.2 Broader Uses for Controllable 
            Compression Technologies .................................... 29  
      1.2.3 Hypotheses .................................................. 30  
      1.2.4 Research Objectives ....................................... 30  
   1.3 Thesis Overview and Structure .................................. 31  
   1.4 Brief Summary of Thesis Findings .............................. 32  

2 Active Materials for Use in Mechanical Counter-Pressure Garments 33  
   2.1 MCP Design Requirements 
       and Material Considerations .................................. 33  
   2.2 Survey of Active Materials ..................................... 35  
   2.3 Detailed Discussion of Candidate Materials .................. 39  
      2.3.1 Dielectric Elastomer Actuators ............................ 39  
      2.3.2 Shape Memory Alloys ..................................... 41  
      2.3.3 Shape Memory Polymers ................................... 44  
   2.4 Initial Material Investigations .................................. 45  
      2.4.1 Dielectric Elastomer Initial Investigation ................ 45  
      2.4.2 Shape Memory Alloy Initial Investigation ................ 48  
      2.4.3 Shape Memory Polymer Initial Investigation .............. 53
3 Low Spring Index SMA Coil Actuator Design, Development, and Characterization

3.1 Theory and Modeling of NiTi Coil Actuators
   3.1.1 Geometry and Force Modeling of NiTi Coils
   3.1.2 Theoretical Pressure Production of Low Spring Index NiTi Actuators
   3.1.3 Thermal Modeling of NiTi Coils

3.2 NiTi Coil Manufacturing and Shape Setting

3.3 Fundamental NiTi Coil Characterizations
   3.3.1 Differential Scanning Calorimetry (DSC) Testing
   3.3.2 Martensite De-twinning Force Testing
   3.3.3 Active Blocking Force Thermal Testing
   3.3.4 Actuator Response and Path Dependence
   3.3.5 Active Force-Voltage-Extensional Strain Testing
   3.3.6 Actuator Cyclic Performance Degradation
   3.3.7 Actuator Linearity and Thermal Model Efficacy

3.4 Compression Tourniquet Using Integrated NiTi Coil Actuators
   3.4.1 Tourniquet Design and Manufacturing
   3.4.2 Force and Pressure Testing

3.5 Discussion

4 Two-Spring Model for Active Compression Textiles with Integrated SMA Coil Actuators

4.1 Hybrid Active Tourniquet Concept
   4.1.1 SMA Displacement-Force-Pressure Modeling
   4.1.2 Passive Fabric Displacement-Force-Pressure Modeling

4.2 Combined Two Spring Model for Hybrid Active Compression Garments
   4.2.1 Two-Dimensional, Two-Spring Model Concept
5 Compression Textile Design using SMA Coil Actuators

5.1 Modular SMA Cartridge Development

5.2 Active Tourniquet Counter-Pressure Testing
   5.2.1 Test Setup, Objectives and Test Matrix
   5.2.2 Test Results

5.3 Detailed Discussion of Test Results
   5.3.1 Spatial Pressure Distribution
   5.3.2 Model Accuracy, Validation, and Limitations
   5.3.3 System Repeatability
   5.3.4 Analysis of SMA Pressure Augmentation
   5.3.5 Mechanical Limitations of SMA Cartridge

5.4 Discussion of Alternative Active Garment Concepts using SMAs
   5.4.1 Hybrid SMA Biaxial Braid
   5.4.2 Lines of Non-Extension (LoNE) Integration
   5.4.3 SMA Composite Fibers

5.5 Performance Predictions for Soft Surfaces

5.6 Summary
6 Discussion and Summary

6.1 Discussion of Key Findings

6.1.1 Summary of Materials Analysis - Chapter 2

6.1.2 Summary of SMA Actuator Development - Chapter 3

6.1.3 Summary of Analytic Pressure Modeling - Chapter 4

6.1.4 Summary of Active Garment Prototyping - Chapter 5

6.2 Hypotheses Assessment

6.3 Limitations

6.4 Future Research Directions

6.5 Summary of Contributions

6.5.1 List of Associated Publications and Patents

6.6 Final Comments

A Textile Structures of Interest, and Active Material-Textile Combination Analysis

B Additional SMA Characterization Tests

C Calorimetry Data Analysis Example

D SMA Cartridge Manufacturing Methods and Drawings

E Biaxial Braid Dynamics

F Micro-System Applications for SMA Coil Actuators

G Pressure Sensor Variance Assessment

References
# List of Figures

1-1 Original and modern MCP suit designs ........................................... 27

2-1 Model of an MCP cross section assuming a thin-walled pressure vessel 34
2-2 Relative stress-strain performance of several active material categories 38
2-3 Dielectric elastomer actuator schematic ......................................... 39
2-4 Dielectric elastomer actuator architecture concepts ......................... 41
2-5 SMA phase transformation cycle ...................................................... 42
2-6 Time lapse view of SMA activation ................................................ 42
2-7 SMA prototype systems ................................................................. 43
2-8 Example of SMP deformation recovery ......................................... 45
2-9 Example of DEA prototype actuator pre- and post-activation ............. 47
2-10 SMA biaxial braid structures ......................................................... 50
2-11 SMA circular knit structures .......................................................... 51
2-12 SMA active seam and active weave prototypes ............................... 52
2-13 Uniaxial SMP stress vs. time for a fixed strain ............................. 54
3-1 SMA coil actuator shape setting steps and activation cycle ............... 59
3-2 3D SMA activation cycle and spring parameters .............................. 61
3-3 Predicted SMA spring actuator force vs. wire diameter .................... 65
3-4 Schematic of active tourniquet using SMA coils .............................. 67
3-5 SMA coil winding method and example coils .................................. 70
3-6 SMA coil de-twinning shear stress vs. shear strain .......................... 74
3-7 Force vs. temperature for SMA blocking force tests ....................... 76
3-8 SMA path dependency assessment ................................................. 78
3-9 SMA force vs. extensional strain vs. voltage ................. 80
3-10 Active force vs. cycle number, without de-twining ............ 81
3-11 Active force vs. cycle number, with de-twinning .............. 82
3-12 Temperature error vs. voltage as predicted by analytic model . 84
3-13 System schematic of compression tourniquet using integrated NiTi coil actuators and 3D printed structures ..................... 87
3-14 Initial tourniquet pressure testing setup ....................... 89
3-15 Prototype active tourniquet pressure production vs. applied voltage . 90

4-1 Hybrid active tourniquet concept incorporating both SMA spring actuators and passive fabrics ......................... 95
4-2 Two-dimensional decomposition of hybrid active tourniquet system . 99
4-3 Representative force-length relationship for the active tourniquet cuff 100
4-4 Experimental data relating total actuator length to number of coils . 102
4-5 Assumed force-displacement curve for de-twinned martensite SMA actuators based on measured $\epsilon_{Smax}$. ..................... 103
4-6 Effect of shortening $L_{F0}$ on tourniquet system behavior ........ 108
4-7 Effect of increasing $E$ on tourniquet system behavior ............ 109
4-8 Examples of passive elastic fabrics used for uniaxial tensile testing . 112
4-9 Uniaxial force-displacement and stress-strain data for four passive materials ......................................................... 113
4-10 Estimate of Young’s Modulus of each passive material ........... 114
4-11 Investigation of jumbo spandex Young’s Modulus at low extensional strains ......................................................... 115
4-12 Maximum pressure cuff trade study ............................... 118
4-13 Easy don, high pressure cuff trade study .......................... 120
4-14 First low profile, flexible cuff trade study ....................... 122
4-15 Second low profile, flexible cuff trade study ..................... 123
4-16 BioSuit spandex trade study: 1-3 layers .......................... 126
4-17 BioSuit spandex trade study: 4-6 layers .......................... 127
4-18 BioSuit spandex trade study: 7 layers ............................................. 128
4-19 BioSuit neoprene trade study: 1-3 layers ................................. 130
4-20 BioSuit neoprene trade study: 4-6 layers ................................. 131
4-21 BioSuit spandex three dimensional pressure contour plot .......... 133
4-22 BioSuit neoprene three dimensional pressure contour plot .......... 134
4-23 BioSuit spandex active pressure vs. SMA and fabric length ......... 135
4-24 BioSuit neoprene active pressure vs. SMA and fabric length ....... 136

5-1 SMA coil actuator cartridge schematic ........................................ 143
5-2 Example of single-plastic SMA cartridge ............................................... 145
5-3 Example of multi-plastic SMA cartridge ............................................. 145
5-4 Thermally-induced single-plastic SMA cartridge failure .......... 146
5-5 Active tourniquet study test setup ..................................................... 148
5-6 Depiction of generic spandex active tourniquet ....................... 149
5-7 Test 1: jumbo spandex, 5-layer average tourniquet pressure vs. time,
and steady state pressure vs. sensel location ................................. 151
5-8 Test 2: low friction jumbo spandex, 5-layer average tourniquet pressure
vs. time, and steady state pressure vs. sensel location ..................... 152
5-9 Test 3: extended length, low friction jumbo spandex, 5-layer average
tourniquet pressure vs. time, and steady state pressure vs. sensel
location ................................................................................................. 153
5-10 Test 4: extended length, low friction jumbo spandex, 7-layer average
tourniquet pressure vs. time, and steady state pressure vs. sensel
location ................................................................................................. 154
5-11 Test 5: generic spandex, 5-layer average tourniquet pressure vs. time,
and steady state pressure vs. sensel location ....................................... 156
5-12 Test 6: generic spandex, 5-layer average pressure vs. sensel location
with alternative measurement location .............................................. 157
5-13 Test 7: low friction generic spandex, 5-layer average tourniquet pres-
sure vs. time, and steady state pressure vs. sensel location ............... 158
5-14 Test 8: extended length, low friction generic spandex, 7-layer average
tourniquet pressure vs. time, and steady state pressure vs. sensel
location .............................................................. 159
5-15 Test 9: maximum power stress test using 5-layer, low friction jumbo
spandex to determine maximum pressure .......................... 160
5-16 Comparison of jumbo spandex elastic modulus pre- and post-testing . 162
5-17 Effect of modifying passive fabric on stretching behavior .............. 162
5-18 Comparison of generic spandex elastic modulus pre- and post-testing 163
5-19 Total normalized passive and active pressure residuals vs. sensel number 167
5-20 Total normalized passive and active pressure residuals vs. sensel num-
ber, controlling for differences in tourniquet friction ............... 170
5-21 Example of “far-field” spatial pressure evolution for high and low fric-
tion tests .............................................................. 171
5-22 Depiction of SMA cartridge length and unstretched fabric parameters 174
5-23 Depiction of tourniquet stitch pattern ................................ 180
5-24 Percentage increase in pressure during activation vs. “far-field” sensel
number .............................................................. 184
5-25 Structural failure of multi-plastic SMA cartridge at high power ....... 186
5-26 Lines of non-extension concept ....................................... 188
5-27 SMA composite fiber concept ....................................... 190
5-28 SMA composite fiber prototyping methods ............................ 191
5-29 Initial SMA composite fiber characterization .......................... 192
A-1 Woven, knit, and non-woven fabric architectures ...................... 210
A-2 Biaxial braid schematic and example ................................ 211
A-3 Linking, looping, and interlocking architectures ....................... 212
A-4 Material-textile architecture trade space analysis ..................... 213
B-1 Additional SMA characterization tests .............................. 219
C-1 Example of raw and analyzed calorimetry dataset .................... 222
List of Tables

2.1 Summary and assessment of active material categories based on maximum stress and achievable activation strain [1–6] 37

3.1 Summary of NiTi coil calorimetry analysis: average activation temperatures for raw and annealed specimens 71

3.2 Comparison of two-state model parameters 73

4.1 Summary of variables used to derive two-spring active tourniquet model, with model inputs identified 106

4.2 Survey of typical Young’s Moduli of elastomer materials [7] 111

4.3 Summary of experimental Young’s Moduli data for four passive elastic materials between 0-200% strain, with expanded analysis for jumbo spandex at low extensional strains 115

4.4 Summary of eleven design parameters and predicted performance for each theoretical active thigh tourniquet concept 124

5.1 Active tourniquet characterization test matrix 149

5.2 Summary of design parameters for jumbo and generic spandex tourniquet prototypes 155

5.3 Statistical comparison between sensel groups with and without friction 172

5.4 Summary of active tourniquet test results based on pre- and post-testing modulus values for jumbo spandex tests 176

5.5 Summary of active tourniquet test results based on pre- and post-testing modulus values for generic spandex tests 177
<table>
<thead>
<tr>
<th>Acronym</th>
<th>Definition</th>
</tr>
</thead>
<tbody>
<tr>
<td>DEA</td>
<td>Dielectric Elastomer Actuator</td>
</tr>
<tr>
<td>EAP</td>
<td>Electroactive Polymer</td>
</tr>
<tr>
<td>EMU</td>
<td>Extravehicular Mobility Unit</td>
</tr>
<tr>
<td>EVA</td>
<td>Extravehicular Activity</td>
</tr>
<tr>
<td>IPMC</td>
<td>Ionic Polymer Metal Composite</td>
</tr>
<tr>
<td>LoNE</td>
<td>Lines of Non-Extension</td>
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<tr>
<td>MEMS</td>
<td>Micro-electro-mechanical Systems</td>
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<tr>
<td>MIT</td>
<td>Massachusetts Institute of Technology</td>
</tr>
<tr>
<td>MCP</td>
<td>Mechanical Counter-Pressure</td>
</tr>
<tr>
<td>MVL</td>
<td>MIT Man Vehicle Laboratory</td>
</tr>
<tr>
<td>NASA</td>
<td>National Aeronautics and Space Administration</td>
</tr>
<tr>
<td>SAP</td>
<td>Stimulus Active Polymer</td>
</tr>
<tr>
<td>SMA</td>
<td>Shape Memory Alloy</td>
</tr>
<tr>
<td>SMP</td>
<td>Shape Memory Polymer</td>
</tr>
</tbody>
</table>
Chapter 1

Introduction

1.1 Background

Beginning with the very first spacewalk performed by Alexei Leonov in 1965, astronauts conducting Extravehicular Activity (EVA) have donned gas-pressurized space suits to stay safe in the harsh environment of space [8]. These suits function by creating an artificial gas environment that surrounds the user, mimicking the breathable atmosphere and complete-body counter-pressure found on the surface of the Earth. Considerable advances have been made in the field of space suit design since the 1960s, but the fundamental concept of gas-pressurization has remained unchanged, and looks to be the modus operandi of NASA and its contractor community for the foreseeable future [9].

While the primary objective of the space suit is to provide life support to keep the astronaut alive (including things like access to oxygen, counter-pressure, carbon dioxide removal, and thermal and radiation protection) [8], it is also critical that the suit does not prevent the astronaut from physically completing the task at hand. After safety, flexibility and mobility are perhaps the most important design considerations for suit engineers [9]. However, traditional gas-pressurized suits are notoriously inflexible. Gas pressurization causes stiffening of the soft suit materials, and changes in internal volume and pressure caused by deformation of the suit joints during movement force the astronaut to expend energy every time he or she bends the suit away.
Mechanical counter-pressure (MCP) space suits have the potential to greatly improve the mobility of astronauts as they conduct planetary exploration activities. MCP suits differ from traditional gas-pressurized space suits by applying surface pressure to the wearer using tight-fitting materials rather than a pressurized gas enclosure, thus, representing a fundamental change in space suit design. By altering the pressurization mechanism, MCP suits avoid the pitfall of forcing the astronaut to work against the suit during movement; the suit acts as a conformal, wetsuit-like mobile garment rather than an inflexible balloon. MCP suits offer an added benefit of acting like a second-skin to the wearer, vastly reducing the mass of the suit while simultaneously mitigating the risk of catastrophic failures due to puncture or depressurization [12]. As a result, MCP suits represent a promising breakthrough technology for future exploration missions.

The concept of MCP space suits was first proposed and explored in the 1960s and 1970s by Webb and Annis (the Space Activity Suit, see Figure 1-1), but the concept was rejected due to limitations in available materials, comfort, and donning and doffing challenges [13]. More recent research efforts to advance MCP suit design have been conducted at multiple universities, including at the University of San Diego and in the Man Vehicle Laboratory (MVL) at MIT [12, 14–24]. The BioSuit system (see Figure 1-1), designed by Prof. Dava Newman at MIT, represents the current state of the art of MCP suit design. The underlying concepts of this suit have been demonstrated through sub-component static testing, as well as advanced donning/doffing solutions, and full-suit mock-ups have been developed and produced to illustrate advanced restraint patterning [12, 16, 20, 24].

While MCP suits were first proposed over 40 years ago, challenges still exist to realize a flight system [13, 22, 24]. To date, all attempts at developing MCP garments have focused on the use of tight-fitting, passive garments, which impart pressure to the wearer by virtue of the tight fitting material stretching when donned [12, 13, 15, 20, 22–24]. These passive material solutions have produced pressure magnitudes that approach (and in some cases, meet) EVA suit requirements, however
donning/doffing of such suit components have proven too difficult to be operationally feasible. Furthermore, producing uniform compression (i.e., across all areas of the body) at sufficient pressures for space exploration has proven additionally challenging [13, 20]. The most promising solution to both of these problems lies in active materials technology. Active materials, such as shape memory alloys (SMAs) and electroactive polymers (EAPs), possess the ability to change shape when stimulated, and have been considered for application in everything from robotic actuators to self-expanding stents [1, 2, 25–28]. Integrating these technologies into a wearable garment could lead to smart fabrics capable of altering their compression characteristics upon command. Such a technology brings MCP suits much closer to operational viability. To the best of our knowledge, prior to this research effort, the use of integrated active materials in MCP design had never been attempted.
1.2 Problem Statement

1.2.1 Motivation

With the Space Shuttle retired, NASA is working towards an eventual return to human planetary exploration, which will necessarily result in an increase in the number and physicality of astronaut EVAs - previous researchers have estimated that future terrestrial missions will require “more EVA hours than the sum total of the entire history of EVA experience” [12,16,30]. As a result, the need for a highly mobile space suit is continually increasing, and this challenge has been identified as a critical research area in NASA’s 2010 Space Technology Roadmap [31]. Because of the demonstrated space-readiness and reliability of traditional gas-pressurized suits, NASA and its contracting partners have chosen a gas-pressurized architecture for their next-generation suit (despite the inherent mobility problems of such a design) [9]. MCP suits offer the promise of greatly improved mobility [13,19,29,32], but have not yet been seriously considered to replace NASA’s gas-pressurized suits because the underlying technologies needed to develop a functional MCP prototype have not yet been demonstrated.

Active materials may prove to be exactly the technology needed to bridge the gap from MCP concept to a functional, flight-ready prototype. While widely studied for use as robotic actuators and other similar tasks because of their ability to function as ”artificial muscles” [1], active materials have only recently garnered attention for their potential to strategically augment or enhance wearable garments [26,33,34]. Berzowska et al. demonstrated the viability of integrating NiTi SMA wire into wearable garments to change their shape [34]; Hu et al. discussed modern research efforts focusing on morphing textiles with active polymers [26]; and Abel et al. recently produced a variety of shape-changing SMA knit structures with several viable operating modes [33].

The interest in integrating active materials for smart fabrics is growing as the underlying material technologies mature, and avenues for new and innovative research in these areas are being continuously discovered [35]. However, the concept of integrating active materials into a wearable garment with the specific purpose of producing
controllable compression has not been previously studied, and this potential application serves as the primary motivation of this research effort.

1.2.2 Broader Uses for Controllable Compression Technologies

Outside of use in EVA exploration suits, compression garments serve many functions in our daily lives. Compression stockings are used regularly to help patients suffering from venous insufficiency or to manage lymphedema [36,37]; athletes don specifically engineered compression shorts to improve athletic performance and to increase comfort during workouts [38]; pressure garment therapy has been used to support burn victim recovery for over 50 years [39]; and cosmetic shapewear compression garments have recently exploded in popularity among those looking for a more streamlined figure. Compression garments also see use in extreme environment situations. The US military uses compression garments for emergency battlefield medicine [40]; and NASA and its partner universities have been investigating compression technologies as a countermeasure against post-spaceflight orthostatic intolerance in astronauts [41].

These compression garments typically take the form of either tight fitting elastic materials (in the case of [36–39]) or as an inflatable bladder system (in the case of [37,41]). Both designs offer unique benefits and disadvantages. Inflatable systems are adjustable (i.e., the magnitude and location of counter-pressure can be controlled by adjusting the inflation characteristics), but they require bulky plumbing, are subject to leaks, can be very costly, and may require access to high pressure gas sources to function [37,41]. Elastic systems are form fitting, lightweight and streamlined, but are generally static in design (i.e., the amount of pressure produced on a given object is unchanging, and is a direct function of the material properties and the shape/size of the garment relative to the wearer), offer no controllability, and can be difficult to don/doff [12,13,15,20,23].

Success in the development of controllable compression garments using active materials could both fundamentally change space suit design philosophies, and lead
to a variety of new technologies with widespread terrestrial application.

1.2.3 Hypotheses

To examine the feasibility of integrating active materials into a wearable MCP garment, the following hypotheses were tested:

1. Counter-pressure levels of 29.6 kPa (4.3 psi) can be repeatedly and uniformly achieved (i.e., deviations no greater than ±10%) using a combination of passive elastics and one or more active materials integrated into a wearable garment.

2. This garment can be donned and doffed in less than 10 minutes\(^1\).

1.2.4 Research Objectives

To test these hypotheses, the following the research objectives were completed:

1. Investigate active material technologies that demonstrate potential to achieve 29.6 kPa (4.3 PSI) compression throughout a wearable garment.

2. Obtain and modify active material components for integration into a wearable garment.

3. Develop an analytic model to predict the performance characteristics of an active material compression garment.

4. Design and fabricate hybrid materials combining passive and active compression components based on model predictions.

5. Assess the performance of the active compression garments (including the magnitude and uniformity of counter-pressure produced and the ease of donning and doffing).

\(^1\)NASA estimates the current suit, the Extravehicular Mobility Unit (EMU), takes approximately 45 minutes to don, including undergarments: [http://www.nasa.gov/audience/foreducators/spacesuits/facts/facts-index.html](http://www.nasa.gov/audience/foreducators/spacesuits/facts/facts-index.html)
6. Provide recommendations for future hybrid designs based on quantitative measurements.

### 1.3 Thesis Overview and Structure

This thesis is organized into 6 chapters that follow the stated research objectives:

**Chapter 2**: The requirements for MCP space suit design are presented, and a survey of available active materials is presented and discussed to determine viable material candidates for active compression garments. Materials that meet minimum MCP requirements are identified, and initial acquisition and development efforts are presented. Material and garment architectures are down-selected for detailed study.

**Chapter 3**: A detailed investigation into low spring index shape memory alloy (SMA) coil actuators is presented. This includes actuator theory, force-temperature-pressure modeling, manufacturing, and a battery of fundamental material characterizations. A first-generation active tourniquet is developed and its performance is assessed to validate initial counter-pressure models.

**Chapter 4**: A complete analytic model describing hybrid passive-active garments as a linear two-spring system is derived and discussed. Hypothetical garment designs meeting a variety of performance requirements are presented based on strategically selected fabric and SMA parameters. Uniaxial loading tests of various passive fabrics are presented to inform real-world compression garment design. An extensive design exercise is presented detailing the analysis and selection of garment parameters for MCP space suit (and other) applications.

**Chapter 5**: A novel, 3D printed, modular cartridge approach to high-force SMA actuator manufacturing is presented. These cartridges are integrated into multiple prototype compression garments, and the prototypes are tested to assess their performance. A detailed discussion of these tests is presented, including prototype pressure production performance, spatial variability and repeatability. Alternative
active compression garment design concepts are presented, and recommendations for system improvement and optimization are discussed based on the outcome of these tests.

Chapter 6: A summary of the thesis is presented, including major findings, contributions, limitations, and opportunities for future work.

1.4 Brief Summary of Thesis Findings

This thesis demonstrates the viability of integrating active elements (low spring index shape memory alloy coil actuators) in a wearable garment to produce controllable counter-pressure. We present modeling, development, and testing of both individual coil actuators and hybrid wearable compression systems comprised of passive fabrics and modular coil actuator cartridges. These systems are shown to produce active pressures sufficient for MCP applications within 30 seconds of activation. The technologies developed for this thesis bridge the existing MCP performance gap, advancing modern MCP design towards the ultimate goal of full-flight demonstration.
Chapter 2

Active Materials for Use in Mechanical Counter-Pressure Garments

This chapter focuses on the analysis and downselection of candidate active materials for MCP applications based on their ability to meet established MCP design requirements.

2.1 MCP Design Requirements and Material Considerations

The driving design requirement of an MCP suit is that it must provide sufficient counter-pressure to keep the user alive. By setting a target counter-pressure requirement consistent with current gas-pressurized space suit designs, performance requirements can be developed to constrain the choice of constituent active materials. The following are considered baseline MCP design requirements: the target pressure production is 29.6 kPa (4.3 psi), matching the internal gas-pressurization of the current Extravehicular Mobility Unity (EMU) space suit (which is set as a compromise between suit flexibility and risk of decompression sickness) [42,43]; and
desired maximum material thickness is initially set to 5 mm to maintain compliance with the “second-skin” design goal (representing an average wetsuit thickness) [20].

Assuming a thin-walled cylindrical structure, these requirements set a minimum internal hoop stress of the pressure garment, determined by (2.1) and modeled in Figure 2-1 [24]:

\[
\sigma_\theta = \frac{Pr}{t} = \frac{F}{tw} = \frac{T}{t} \tag{2.1}
\]

In (2.1), \(\sigma_\theta\) is hoop stress, \(P\) is counter-pressure, \(r\) is local limb radius, \(t\) is material thickness, \(w\) is the axial length of the cylinder, \(F\) is circumferential force acting on the area described by the thickness and length, and \(T\) is the wall tension. For the largest limb radius (the upper thigh of a 95th percentile male, 10.7 cm), a minimum active hoop stress of 0.633 MPa at 5 mm thickness is required [20, 24, 44]. This corresponds to a wall tension of 31.65 N/cm [17]. For limbs with smaller radii, or for crew members of smaller stature (e.g., a 5th percentile female, with a thigh radius of 7.60 cm), smaller active stress values (0.450 MPa) and wall tensions (22.50 N/cm) are required [44].
Given this active stress requirement, active strain is to be maximized for ease of donning and doffing. A minimum active strain requirement is difficult to quantify, because minimum constriction/closure distances are set both by a given design and by local body geometry (e.g., an initially tight fitting garment may not require much active strain to meet minimum stress requirements vs. an initially loose fitting garment, and donning requirements for an arm sleeve vary from a leg sleeve due to differences in relative geometries of arms vs. legs). A first-order estimate for minimum active strain is the percentage difference between average hand circumference (24.1 cm for 95th percentile men, 16.8 cm for 5th percentile females) and wrist circumference (19.8 cm and 13.5 cm, resulting in percentage differences of 17.8% and 19.6%, respectively), because an arm sleeve will have to be pulled over the hand while still being capable of constricting sufficiently around the wrist [44].

Finally, beyond active stress and strain, several other material characteristics are important when designing an MCP garment. These include: strain rate; activation mechanism type and operating regime; suitability for use in a wearable garment; efficiency; stiffness; tensile strength; hysterisis; usable lifetime; and longitudinal stresses (to be minimized to enable maximum mobility).

2.2 Survey of Active Materials

Several categories of active materials exist, each with different capabilities, limitations, and characteristics. A broad survey of active materials was conducted, and focused on assessing the current state of active material types with respect to the basic design requirements for an MCP garment [1–5,45]:

1. Dielectric Elastomer (Acrylic, Silicon): Materials comprised of multiple elastomer films coated with conductive layers which undergo compressive strains when exposed to electrostatic energy.

2. Piezoelectrics (Ceramics, Polymers): Materials that respond to mechanical stress by producing electricity (and vice-versa).
3. **Shape Memory Alloy**: Bi-phasic metal alloys that return from a deformed state when exposed to thermal stimuli.

4. **Stimulus Active Polymer**: Polymers that change shape when exposed to external stimuli.

5. **Liquid Crystal Elastomer**: Liquid crystal polymers that undergo phase changes when exposed to thermal or electrostatic energy.

6. **Ferroelectric Polymers**: Polymers containing polarized domains that align when exposed to an applied electric field.

7. **Magnetostrictives**: Ferromagnetic materials that internally realign when exposed to an applied magnetic field.

8. **Ionic Polymer Metal Composite (IPMC)**: Polymer electrolytes sandwiched between thin metal layers that can be deflected when internal ion concentrations are affected by the presence of an electric field.

9. **Carbon Nanotubes**: Hollow cylinders comprised of pure carbon, coated in an electrolyte, that undergo dimensional changes when exposed to an electric potential.

Based on the primary MCP requirements of 1) maximum active strain given a strict 2) minimum acceptable stress, each material type was graded in one of three ways: accepted for further study (indicating that it meets minimum stress and strain design requirements); considered for further study (indicating that it may meet design requirements pending further investigation); and not further considered (indicating that it failed one or both design requirements). The results of this survey, and grades for each material type, are included in Table 2.1.

The results of this survey are also presented in Figure 2-2, with materials presented in terms of their reported maximum stress and strain ranges (decreasing performance from top to bottom assuming acceptable minimum stress value). Optimum materials
Table 2.1: Summary and assessment of active material categories based on maximum stress and achievable activation strain [1–6]

<table>
<thead>
<tr>
<th>Material</th>
<th>Maximum Stress (MPa)</th>
<th>Maximum Strain (%)</th>
<th>Actuation Mechanism</th>
<th>Assessment</th>
</tr>
</thead>
<tbody>
<tr>
<td>Dielectric Elastomer</td>
<td>0.3-7.7</td>
<td>120-380</td>
<td>Applied Voltage (&gt;1 kV)</td>
<td>Accepted for further study</td>
</tr>
<tr>
<td>Shape Memory Polymer</td>
<td>4</td>
<td>100</td>
<td>Multiple</td>
<td>Accepted for further study</td>
</tr>
<tr>
<td>Ferroelectric Polymer</td>
<td>20-45</td>
<td>3.5-7</td>
<td>Applied Voltage (&gt;1 kV)</td>
<td>Considered for further study</td>
</tr>
<tr>
<td>Ionic Polymer Metal Composite</td>
<td>0.23-15</td>
<td>0.5-3.3</td>
<td>Applied Voltage (1-7 V)</td>
<td>Considered for further study</td>
</tr>
<tr>
<td>Shape Memory Alloy</td>
<td>&gt;200</td>
<td>4-8</td>
<td>Thermal Stimulus</td>
<td>Considered for further study</td>
</tr>
<tr>
<td>Carbon Nanotubes</td>
<td>&gt;20000</td>
<td>0.2-1</td>
<td>Applied Voltage (1-30 V)</td>
<td>Not further considered</td>
</tr>
<tr>
<td>Liquid Crystal Elastomer</td>
<td>0.01-0.45</td>
<td>19-45</td>
<td>Thermal or Electric Stimulus</td>
<td>Not further considered</td>
</tr>
<tr>
<td>Magnetostrictives</td>
<td>70</td>
<td>0.21</td>
<td>Applied Magnetic Field</td>
<td>Not further considered</td>
</tr>
<tr>
<td>Piezoelectrics</td>
<td>4.8-110</td>
<td>0.2-1.7</td>
<td>Applied Voltage (1500 V)</td>
<td>Not further considered</td>
</tr>
<tr>
<td>Design Goal</td>
<td>0.633 MPa (minimum)</td>
<td>To be maximized (&gt; 10%)</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
Figure 2-2: Relative stress-strain performance of several active material categories (shown on a logarithmic scale), with minimum stress requirement identified with a dashed line. Selected active materials for MCP textile design are identified by asterisk.

(denoted by an asterisk) provide maximum active strain while accommodating at least the minimum calculated hoop stress (denoted by a vertical dashed line).

Four material types (piezoelectrics, liquid crystal elastomers, magnetostrictives, and carbon nanotubes) are not viable for MCP applications due to significant limitations in their performance (i.e., they failed to meet minimum stress levels, or they produce strains too small to be useful for large-stroke compression garments). Dielectric elastomer actuators (DEAs) and shape memory polymers (SMPs) met both design criteria, and were accepted for further study. Shape memory alloys (SMAs), ferroelectric polymers, and ionic polymer metal composites (IPMCs) were examined for further study, due to borderline capabilities in either active stress or strain; ultimately ferroelectric polymers and IPMCs were also rejected (the former due to minimal active strain capability given the required voltages; the latter due to low active...
strains and reliance on embedded fluids that will prove problematic in micro-gravity environments). The three materials ultimately accepted for this study (SMA, SMP, and DEA) correspond to the highest active strain materials that met minimum stress requirements as seen in Figure 2-2. Detailed descriptions of each selected material follow.

2.3 Detailed Discussion of Candidate Materials

2.3.1 Dielectric Elastomer Actuators

Dielectric elastomer actuators (DEAs) are comprised of an elastomer film coated on each side with conductive layers. When exposed to an electric field, the conductive layers experience electrostatic attraction, producing Maxwell pressure, which in turn induces compressive strains on the elastomer film [1, 46]. This concept is illustrated in Figure 2-3.

Maxwell pressure, and thus the induced stress and strain on the elastomer, are governed by the following equation:

\[ p = \varepsilon \varepsilon_0 E^2 = \varepsilon \varepsilon_0 \left(\frac{V}{t}\right)^2 \]  

(2.2)

where \( p \) is Maxwell pressure, \( \varepsilon \) is the dielectric constant of the material, \( \varepsilon_0 \) is the
permittivity of free space, $E$ is the imposed electric field, $V$ is the applied voltage, and $t$ is the polymer thickness. For a given strain, the required voltage and material thickness are directly proportional, meaning that required voltage can be minimized by minimizing the thickness of the elastomer film [1,47,48]. Contraction occurs in the plane that is normal to the conducting layers (i.e., the thickness), and this contraction induces surface expansion in the other two dimensions.

Dielectric elastomers have been shown to produce strains up to 120-380% by area [1,46,49,50]. For this reason, DEAs hold significant promise as artificial muscles or other robotic actuators [49–51]. DEAs require electrodes to be placed on each conducting surface, and these electrodes create the electric potential that leads to activation. Arora et al. proposed several different elastomer cross-sectional configurations that are possible given such an architecture, and tested one of these configurations to determine its actuation behavior [47] (see Figure 2-4a). It was noted that the initial activation behavior of the prototype was not reflective of the steady-state, long term behavior - a stress-softening phenomenon was observed after cyclic loading and unloading, and the material behavior became repeatable only after softening had taken place (as a result Arora recommends 25-50 cycles before fabricating the final actuator). This behavior is depicted in Figure 2-4b [47]. Strain was found to be dependent on the square of the applied electric field, which is consistent with (2.2).

Of particular interest in Figure 2-4a is configuration (c), which Arora refers to as a hollow-concentric sheath/core configuration. This configuration closely aligns with the general design of a wearable compression garment - a hollow interior (to be filled by the body of the wearer) surrounded by a thin, actively controllable sheath. Because dielectric elastomers produce both compression and expansion strains that are orders of magnitude larger than those of other active materials, as an individual element they could provide significant shape change ability to such a sheath when integrated circumferentially. This would be of particular use from a donning/doffing perspective. Additional benefits of DEAs include the fact that they are easy to produce, are inexpensive, rely on simple electrostatic attraction for operation, have high efficiencies, and are shown to be repeatable (after softening) [1,47].
However, dielectric elastomers are not without limitation. They require large voltages (i.e., currently on the order of 1kV), which in the application of a tight-fitting MCP garment would put the wearer in considerable danger, and the large induced strains can lead to durability issues of the elastomer as well as the activation electrodes [1]. Voltage can be minimized by decreasing the elastomer thickness and/or increasing the elastomer dielectric constant, however decreasing the thickness of the elastomer introduces other design challenges [1,52].

### 2.3.2 Shape Memory Alloys

Shape memory alloys are a category of metal alloys that demonstrate a shape-memory effect, which is the ability to return from a deformed (i.e., de-twinned) state to a “remembered” state when exposed to a specific stimulus. This occurs as a result of a diffusionless solid-to-solid transformation between the alloy’s austenitic and martensitic phases that is triggered by an external stimulus [1]. See Figure 2-5a for a representation of this transformation [53]. Stimuli can take several forms, including externally applied stress, heat, or magnetic fields, among others. Shape memory alloys also demonstrate superelasticity, which is the ability to fully recover its strain throughout a loading and unloading cycle, though hysteresis-based energy losses do occur [54].
Figure 2-5: A (left): phase transformation cycle of a SMA. From its deformed (i.e., de-twinned) state, the alloy transforms when heated, causing it to return to an original (i.e., memory) phase configuration. B (right): superelastic behavior of SMAs through a loading and unloading cycle, with hysteresis represented by the area inside the loop [54].

Figure 2-6: Time lapse view of an SMA, de-twinned from its original shape, returning to its shape due to an externally applied thermal stimulus.

See Figure 2-5b for a depiction of a typical superelastic and hysteresis behavior of a SMA. The deformations that can be recovered through the shape memory effect are significant: Figure 2-6 shows a time-lapse view of a SMA wire, deformed from its original configuration then exposed to heat and thus returning to its original, un-deformed shape.

SMAs have been extensively studied, and their shape memory and elastic properties have proven useful in a wide variety of applications, ranging from robotic actuators and prostheses to bridge restraints, valves, deformable glasses frames, biomedical devices, and even wearable garments [34, 55–59]. See Figure 2-7a-d for examples of
these technologies. The memory effect has been demonstrated in several alloy types, though the most common and commercially available alloy produced is Ni-Ti (approximately 55% Nickel), under brands such as Nitinol and Flexinol. Such alloys can be purchased in wire, tube, strip, or sheet form in varying thicknesses and diameters.

To maximize the usefulness of these alloys, studies have been conducted to determine optimal configurations for large strains and forces, optimal designs for bundle actuation schemes, and functional dependencies on fiber diameters. De Laurentis et al. analyzed and optimized SMA actuator bundles consisting of parallel wires of varying diameter (from 100-250 µm), demonstrating forces of up to 38 N [60]. Anadon developed and tested a large force SMA linear actuator capable of lifting 100 lbs with a stroke length of 0.8 in [28]. Chen and Schuh determined that energy dissipation in SMA wires increases as their diameters decrease, and that both the transformation

---

stresses and temperatures are subject to size effects: both stress and temperature 
hysteresis increase with decreasing wire diameters [61].

With proper design and manufacturing, shape memory alloys can produce large 
forces, recover from large deformations, and have been integrated into textiles in 
both fibrous (fine weave) and wire (coarse matrix) configurations [34]. Moreover, 
SMAs are widely available, and relatively inexpensive. These features make them 
attractive for use in a controllable compression garment. A major limitation of SMAs, 
however, is the small magnitude of recoverable axial strain. State of the art SMAs 
demonstrate strains that peak in the single-digit percentage range [1, 61]. This poses 
challenges for applications that require large stroke lengths. This does not eliminate 
SMAs as a candidate material for an active compression garment. It does, however, 
require design architectures that use SMAs to exploit other useful features of SMAs 
to produce compression (namely, their superelasticity and large deformation recovery 
abilities).

2.3.3 Shape Memory Polymers

SMPs can be thought of as analogs to SMAs - polymers with shape memory charac-
teristics that demonstrate deformation recovery capabilities. While SMAs get their 
memory effect from diffusion-less phase changes within the metal alloy, SMP memory 
effects are derived from low-temperature glass or melting transitions (whereby internal-
ized stresses are released), and the polymers themselves are generally physically 
or chemically cross-linked. SMPs are generally highly conductive materials, and their 
shape memory effect is generally irreversible (in order to return to its remembered 
state, the polymer must be externally deformed) [1, 2, 26, 62]. An example of the SMP 
recovery effect is included in Figure 2-8.

Hundreds of polymers have been identified that demonstrate shape memory ef-
fects [2]. As is the case with SMAs, SMPs have been studied for use in countless ap-
plications where actuation is desired, as they are easily configurable to accommodate 
different geometries and critical activation temperatures. Applications range from 
morphing biomedical implants, to integrated temperature sensors, deployable hinges,
and transformable textiles [26,63–65]. SMPs mimic SMAs in most all relevant categories: performance (high recoverable deformation under desired stress conditions that scales with element size); availability (commercially available); versatility (wide variety of types and operating regimes); as well as limitations (low force production and smaller average active strains than DEAs).

2.4 Initial Material Investigations

With DEAs, SMAs, and SMPs identified by the literature as candidate active materials for MCP applications, initial acquisition, development and testing of each type of material was conducted. These initial investigations are detailed in the following subsections.

2.4.1 Dielectric Elastomer Initial Investigation

Dielectric elastomer actuators sport the greatest active strains of all active materials surveyed (see Table 2.1), making them especially attractive as a donning/doffing mechanism for MCP suits. However, the high strains reported in the literature are only achieved by actuators with minimized thickness that are exposed to high (i.e.,
> 1 kV) electric fields (this relationship is expressed in (2.2)) [1]. Consequently, there is significant downside to using DEAs for MCP exploration suits (or any wearable system, for that matter), as their minimized thickness leads to durability issues and their high electric fields pose safety risks to the wearer. Despite these risks, DEAs have been studied as wearable mechanisms, such as an active cuff to be used in SCUBA dry-suits [66].

Initial efforts at manufacturing DEA prototypes were conducted in collaboration with researchers at the MIT Media Lab and Department of Architecture [67]. Actuators comprised of VHB 4905 [3M, Inc.\(^3\)] acrylic elastomers coated with carbon powder electrodes were assembled and exposed to voltages up to 5 kV. These materials were selected for the prototype actuators due to their prevalence in DEA-related literature and their ability to generate triple-digit active strains [1, 46–49, 52, 68–70].

While some prototype actuators demonstrated an ability to expand when exposed to an applied voltage (see Figure 2-9 for an example of electrode expansion during 3 kV actuation) several significant failure modes were encountered:

1. Because pre-stretching is critical to actuator performance (as it leads to thinner actuators that have been “broken-in”), it is necessary to mechanically work the elastomers prior to the application of electrodes. This initial process led to many elastomer failures, attributable in large part to flaws in the VHB acrylic elastomer. A more consistent base elastomer would eliminate these failure modes, but proved beyond the scope of this investigation.

2. Several fully assembled actuators demonstrated dielectric breakdown, which resulted in burning or tearing of the elastomer during activation. Again, these likely stem from flaws in the acrylic and/or flaws in the pre-stretching process [67].

3. The design of the actuator frame is critical to actuator performance. The frame provides structure to the stretched elastomer, but also resists any forces generated during activation. Diamond-shaped, 0.5 mm acrylic frames were used for

\(^3\)http://www.shop3m.com/3m-vhb-tape-4905-group.html
Figure 2-9: Example of DEA prototype actuator pre- and post-activation [67]. Applied voltage (3 kV) induced an expansion of the electrode area, causing a decrease in the passive area between the electrode and the acrylic frame.
the DEA prototypes, based largely on designs proposed by Plante [50], however these designs proved too stiff to enable significant shape change. More flexible frame material (e.g., 0.5 mm polycarbonate) may improve frame flexibility and thus enhance active stroke length, but a tradeoff exists in this design because the frame must support the pre-stretched (and therefore highly stressed) elastomer without bending. Proper frame design for DEAs to be used in MCP applications remains a significant unsolved problem.

4. Perhaps most importantly, fully assembled actuators (free from elastomer flaws) showed very limited durability, for two reasons. First, the VHB acrylic tended to dry out after actuator assembly, significantly reducing actuator flexibility and strength after as short as a few hours. Actuators that were assembled on one day and tested the next showed a high tendency to fail. This lack of durability has been noted in literature [50]. Second, the fundamental design goal of minimizing actuator thickness to increase actuator performance (or to decrease the required electric field) results in actuators that are susceptible to tearing or other mechanical failure. Improvements in material characteristics (e.g., increased Poisson’s ratio, increased dielectric constant, or increased material breakdown strength) may lead to more functional and durable actuators, but these challenges exceed the scope of this study [67].

The results of this investigation indicate DEAs, at their current level of maturity, are poorly suited for MCP applications, due to their limited durability, unreliability, and reliance on very high applied voltages (which may threaten the safety of a potential wearer). This conclusion was also reached by Newie, who led the manufacturing and testing process [67].

2.4.2 Shape Memory Alloy Initial Investigation

Shape memory alloys are commercially available (typically, but not always, in wire form), are well studied as actuators, already appear in commercial and medical products, and have even been demonstrated in textile applications [1, 25, 28, 34, 59, 71–74].
Because of their commercial availability (and the associated material quality control that comes with a commercial-grade product), SMAs do not generally suffer from the manufacturing and assembly problems encountered with DEA prototype development. This does not guarantee that SMAs are well qualified for MCP applications: for example, their published active axial strains are in the single digits [1], which is not necessarily well suited for a constriction garment and is the reason for their initial classification as “considered”, and not “accepted”, for further study according to Table 2.1. This means that initial investigations of SMAs must instead focus on the ability to strategically exploit their shape recovery characteristics.

Two approaches to garment design using SMAs were initially investigated: homogeneous designs, which we define as garments with textile structures entirely comprised of SMA material; and heterogeneous or hybrid designs, which we define as garments that combine separate SMA actuators working in series or parallel with typical, passive fabrics (refer to Appendix A for a detailed discussion of textile structures most suitable for MCP applications, as well as viable active material-textile structure combinations). Two diameters of commercially available SMA wire (101 µm and 305 µm diameter Flexinol wire) were purchased from Dynalloy, Inc.\(^4\), to be used in initial prototyping. Homogeneous SMA knit, braid, and weave structures were manufactured with support from researchers at the Harvard Graduate School of Design and the Navy Clothing and Textile Research Facility [Natick, MA]. Examples of these structures are included in Figure 2-10, Figure 2-11, and Figure 2-12.

Three cylindrical biaxial braid structures, shown in Figure 2-10 comprised of either 101 µm or 305 µm diameter wire, were produced using a traditional rope braiding machine. In order to produce these structures, a substrate material (in this case, nylon ski ropes of various diameter) was fed through the center of the machine, and the braid structure was produced by interlacing 16 separate threads of SMA wire as they wrapped around the center rope. Three diameters of braid were produced (3.175 mm, 9.525 mm, and 22.225 mm), with the largest braid equaling the maximum size that could be produced by the braiding equipment. It was observed that these

\(^4\)http://dynalloy.com/PriceGuide.php
structures collapse when the substrate material is removed or burned away during annealing due to the high wire tension introduced during the braiding process (a collapsed braid with substrate removed is shown in Figure 2-10), which introduces difficulty in annealing, and results in structures that struggle to recover their shape when activated.

A circular knit structure, shown in Figure 2-11, comprised entirely of 101 $\mu$m diameter wire was produced using a Lawson Hemphill Fiber Analysis Knitter (FAK). The thicker gauge wire failed when run through the machine, so no knits of that wire were manufactured. When annealed to remember a fully expanded, cylindrical shape, the knit demonstrated an ability to recover that shape when deformed. It did not, however, demonstrate an ability to produce either measurable forces or measurable circumferential constriction, making this specific circular knit structure poorly suited for MCP design.

Finally, three weave structures were produced using 305 $\mu$m gauge wire: for each
Figure 2-11: Homogeneous SMA circular knit comprised of 101 µm diameter Flexinol wire, produced using a Lawsom Hemphill Fiber Analysis Knitter at the Navy Clothing and Textile Research Facility in Natick, MA.

prototype, SMA was woven as the warp element, with varying elements (nylon, carbon fiber, and SMA wire, respectively) comprising the weft. One example (the SMA-nylon weave) is shown in Figure 2-12b. Similar to the circular knit, these woven structures demonstrated an ability to recover a flattened shape when activated (illustrated in Figure 2-12b), however they did not demonstrate an ability to produce forces or active strains conducive to MCP garment design.

Finally, a hybrid design using 101 µm diameter wire as a lacing element between passive columns was fabricated to assess the ability of the SMA shape-recovery effect to produce a garment with measurable constriction. Both a model of the structure, and the actual prototype pre- and post-activation, are presented in Figure 2-12a. While the SMAs successfully constricted the garment under no load, it easily gave way when exposed to an external force, suggesting that plain SMA wire in this configuration is insufficient on its own to sustain sufficient bending forces to keep a compression garment constricted.

These investigations reveal that, while plain SMA wire lends itself well to homogeneous textile development, these designs in general are poorly suited to produce the types of active strains and forces necessary for MCP applications. Hybrid designs show more promise, though plain SMA wire structures are not optimal for producing sustained forces or active strains. Referring to literature, high-force and high strain...
Figure 2-12: A (top three images): SMA active seam model and prototype (pre- and post-activation). B (bottom three images): SMA-nylon knit demonstrating shape recovery during activation.
SMA actuators are most commonly achieved by winding the actuators into spring actuator forms, and these have been shown to be effective at manipulating wearable structures [71,72,74–78]. Hybrid textile structures comprised of SMA actuators such as these offer the greatest potential for high force, large displacement active compression garments.

2.4.3 Shape Memory Polymer Initial Investigation

In collaboration with materials science researchers at MIT and the University of Massachusetts, SMP samples were produced and tested from commercially available thermoplastic (supplied by Manborui Material Technology[^5]) using standard water bath polymer extrusion methods [79]. SMP filament of diameter 2.6 ± 0.5 mm was produced, and differential scanning calorimetry (DSC) analysis established a glass transition temperature $T_g$ of 51.3°C and a melting point of $T_m$ of 60.0°C. Elastic modulus was found to be temperature dependent (with a log linear dependency), which is consistent with previous SMP findings [79].

Because MCP-type applications will likely require significant active stresses maintained over time, the stress vs. time response for fixed temperature and fixed strain was measured using a uniaxial loading test machine with an attached environmental chamber. When held at 15% strain at 55°C, significant viscoelastic relaxation was measured: active stress was initially measured to be 2.5 MPa (which is consistent in magnitude with values reported by Madden *et al.* [1] and reported in Table 2.1, and sufficiently high to meet minimum MCP hoop stress requirements); however, this value dropped to approximately 1.3 MPa after 15s, a 48% drop [79]. The results of this investigation are presented in Figure 2-13.

Further, Wee [79] developed a model to predict the accumulation of irrecoverable strain for a SMP-based textile as a function of cycle number, active temperature, and active holding time per cycle. As both active holding time increases (modeled up to 8 hours, or within the realm of possibility for astronaut EVA) and number of cycles increases, irrecoverable strain increases, reaching up to 85% irrecoverable

Figure 2-13: Uniaxial SMP stress vs. time for a fixed strain (15%), as measured by Wee [79]. Significant viscoelastic relaxation (48%) is observed as the sample is held at constant strain for 15s.

strain after 50 cycles when held for 6 hours per cycle at the SMP glass transition temperature (which Wee reports as the optimized temperature) [79]. Wee therefore concludes that, while SMPs can theoretically achieve active stress values that meet MCP requirements, viscoelastic and irrecoverable strain characteristics make SMP-based compression garments inappropriate for MCP applications [79].

2.5 Active Materials Summary

Many types of active materials do not currently meet minimum operating requirements for use in MCP garments. And, of those that do meet these requirements, only SMAs when used as high force, large displacement linear actuators (as opposed to homogeneous textile structures) offer real potential to achieve forces and displacements necessary to realize active MCP space suit designs. For the remainder of this thesis, SMA actuators that are optimized for large forces and displacements will take primary focus, and their design, modeling, and integration into compression garments will be examined in detail.
It is important to note that, while these initial investigations into DEAs, SMAs, and SMPs offered significant insight into the current viability of each of these materials as candidates for active compression garments, they were not intended to unequivocally or permanently invalidate specific material classes or textile structures for all future MCP applications, especially as materials and actuator fabrication methods improve. Different polymers or production methods may lead to significantly different conclusions with regard to SMPs; advances in elastomer durability may similarly affect DEA performance and conclusions; and homogeneous SMA textile structures may prove appealing if the structure and composition can be professionally tailored for compression garments.
Chapter 3

Low Spring Index SMA Coil Actuator Design, Development, and Characterization

High force, large displacement SMA actuators were identified in Chapter 2 as optimal structures for active compression garments. In this chapter, the development and testing of strategically designed SMA coil actuators is presented for use in future MCP systems.

3.1 Theory and Modeling of NiTi Coil Actuators

As previously discussed, SMAs exhibit thermally induced shape recovery capabilities as a result of solid state diffusionless phase transformations [1, 73]. As SMAs are heated, they transform from the low-temperature, face-centered tetragonal lattice martensite phase to the high-temperature, body-centered cubic lattice austenite phase. In the absence of external stress, this phase transformation drives a repeatable macro-scale shape change, and both the memory shape and activation temperature thresholds can be tailored for custom applications (by annealing the alloy while fixed in the desired shape, or by modifying the alloy mixture, respectively) [73].

Several alloys exhibit this type of memory characteristic (e.g., Ag-Cd, Cu-Al-
Ni, Mn-Cu, etc.), though NiTi is the most widely studied and is used commercially for a variety of applications [1, 73]. NiTi memory elements have been used (among other things) in: stents [63]; catheters [80]; actuators for robotic and biomimetic systems [56, 58, 71, 75, 76]; morphing aircraft structures [73]; orthopedic implants [73]; systems for structural health monitoring [55]; robotic pumps [81]; and even as integrated elements in clothing [34]. SMAs offer high blocking stresses (200 MPa), high specific power (> 100 kW/kg), and can be actuated using Joule heating via an applied current [1]. Additionally, by varying the alloy composition or annealing parameters, SMA activation temperatures can be tailored for a wide variety of applications [73]. Drawbacks of NiTi and other SMA actuators include low efficiencies, slow response times (as they rely on heat transfer for actuation and cooling), difficulties in precision control in thermally dynamic environments, and moderately small (i.e., typically 1-8%) active strains [1].

To address the problem of small active strains exhibited by SMAs, designers have often trained NiTi wires as coil actuators, such that activation leads to either expansion or contraction of the coil structure [72, 74, 77, 78, 82, 83]. A depiction of a typical SMA coil actuation cycle is included in Figure 3-1. First, a raw NiTi wire is wound into a coil configuration and annealed at high temperature to set the austenite memory state. Once the coil is cooled (either by water quenching or by free convective cooling) it transforms to the twinned martensite state. From there, an external force is required to deform and extend the coil, causing de-twinning of the crystal structure. Subsequent heating above the austenite start and stop temperatures results in contraction of the coil as austenite phase transformation occurs (for NiTi, however, these activation temperatures may be hundreds of degrees above the temperatures experienced in deep space, requiring increased power to sustain activation [67, 73]). As the coil cools, it re-enters the twinned martensite phase, and the cycle can repeat. This can be understood in terms of stress, strain, and temperature using Figure 3-2a.

NiTi coils can achieve displacements that are orders of magnitude greater (> 100%) than those of a typical axially-aligned SMA wire [71, 82]. The combination of high forces, large displacements, simple activation mechanism, low mass, compact
Figure 3-1: SMA coil actuator shape setting steps and activation cycle. Once a coil has been wound and annealed, it follows a 3-step activation cycle: a shape-set twinned martensite actuator is subjected to an external force, causing martensite de-twinning; the actuator is heated, and austenite phase-change occurs leading to activation; subsequent cooling causes martensite phase-change, ending in the original twinned state.

form factor and fiber-like aspect ratio make NiTi SMA coil structures well-suited for inclusion in an active compression textile.

3.1.1 Geometry and Force Modeling of NiTi Coils

NiTi compression coils are defined by several key parameters, just as any other spring. A typical compression spring is presented in Figure 3-2b, with key parameters identified: NiTi wire diameter $d$; spring diameter $D$, as measured by the midpoint between inner and outer diameters; number of active coils $n$; solid spring length $L_S$, defined as the length of a spring that is fully packed; free spring length $L_0$, defined as the zero-load length of the spring (and for our purposes, the length of the SMA actuator when fully actuated with no load); spring pitch $p$, defined as the distance between
adjacent coils; spring pitch angle $\alpha$, defined as the angle between a given coil and the local horizontal; initial and final extended spring length, $L_i$ and $L_f$, defined in this case as the total extended spring length pre- and post-activation (under no load, $L_f \approx L_0$); and initial and final linear displacement $\delta_i$ and $\delta_f$, defined as the difference between initial and final extended spring length and free spring length.

Actuator force follows Hooke’s law, and can be expressed in simplified form as follows, where $G$ is the SMA austenite shear modulus [71,72,74]:

$$F = k\delta = \left(\frac{Gd^4}{8D^3n}\right)\delta. \quad (3.1)$$

This model, which assumes complete austenite transformation, is a simplified version of those presented by An et al. and Seok et al.: An et al. proposes a two-state NiTi coil model modified from the conventional force-displacement model, where $G_M$ and $G_A$ (martensite and austenite shear moduli) are used to describe the system response in either the pure martensite or austenite phase, with additional terms to account for reductions in spring diameter during large displacements, and for detwinning and bending moment effects [72]; Seok et al.’s enhanced model accounts for changes in free length of the spring due to phase transition [71]. However, the basic model described in (3.1) is sufficient to analyze the relationship between coil parameters and actuator performance.

We can modify (3.1) to streamline design by defining three non-dimensionalized parameters: packing density, $\eta$; actuator extensional strain, $\epsilon$; and spring index, $C$ [84]. We define packing density $\eta$ as the ratio of the number of active coils $n$ contained in the free spring length $L_0$ relative to the physical limit. This can also be defined as the ratio of the solid spring length $L_S$ to the free spring length $L_0$:

$$\eta = \frac{L_s}{L_0} = \frac{nd}{L_0}. \quad (3.2)$$

We define actuator extensional strain $\epsilon$ as the ratio of spring displacement $\delta$ to free spring length $L_0$: 
Spring index $C$ is a universal spring parameter defined as the ratio of spring diameter $D$ to wire diameter $d$, which is a measure of coil curvature [85–87]:

$$C = \frac{D}{d}. \quad (3.4)$$

Substituting Eqs. (3.2), (3.3), and (3.4) into (3.1) provides us with the following form:

$$F = \frac{Gd^2}{8C^3\eta} \epsilon. \quad (3.5)$$

With (3.5), we can design actuators to meet specific performance requirements, which may include force targets, size limitations, manufacturing limitations, or desired lengths or extensional strains. For example, force is maximized by maximizing $G$, $d$, and $\epsilon$, and by minimizing $C$ and $\eta$. Physically speaking, maximum force is achieved when a SMA spring actuator is comprised of thick diameter wire wound to
the tightest spring index, and is de-twinned to the mechanical limit with the lowest possible packing density. Such a design, however, requires tradeoffs in terms of actuator size and maximum actuator stroke length (i.e., longer stroke lengths can only be achieved when spring index is increased and packing density is increased, and large diameter SMA wire translates to large coil diameter, even with a minimized spring index). Alternatively, actuator design targets can be achieved by scaling the number of actuators used (if it is not possible to satisfy all constraints with a single actuator). However, increasing the number of actuators in a given system creates both a larger system footprint and greater power requirement. Therefore, specific consideration of each design variable must be given when engineering a system for a desired application.

We are specifically interested in creating high-force, morphing wearable structures using SMA coil actuators, therefore we prioritize maximum force generated (to create maximum counter-pressure) over other design variables, using the following criteria:

1. Maximize force by minimizing spring index $C$. A physical limit to the sharpness of curvature of a spring actuator exists, below which the material experiences structural damage [85–87]. We select actuators that match this minimum ($C = 3$).

2. Maximize force by selecting large $d$, within reason for a wearable system. While actuator force scales with the square of wire diameter (meaning that a maximum wire thickness should be used if force is to be purely maximized), this cannot be simply maximized due to design constraints associated with wearable garments (e.g., overly-thick actuators will encumber the wearer, and this concern necessitates the design requirement of MCP garment thickness $\leq 5$ mm). Commercial NiTi wire is available as thin as $d = 25 \ \mu$m, and as thick as $d = 510 \ \mu$m\(^1\). We select reasonably large SMA wire diameters ($d = 305 \ \mu$m) to balance this tradeoff.

3. Enable large extensional strains $\epsilon$ by selecting high packing densities $\eta$. In ad-

\(^1\)http://www.dynalloy.com/index.php
dition to large active forces, MCP compression suits require significant active stroke lengths to accommodate donning and doffing prior to active compression. These requirements cannot both be maximized, in terms of selecting appropriate \( \eta \) and \( \epsilon \) values (i.e., increased force calls for minimizing \( \eta \), while increased stroke length calls for maximizing \( \eta \) because doing so increases maximum \( \epsilon \)). Consequently, we select maximum \( \eta \) for a fixed \( C \) to provide as much extensional strain margin as possible [84].

Prototype coil actuators with \( C = 3 \) have been shown to be mechanically limited to extensional strains approximately \( \lesssim 3 \) (see Chapter 4 for a greater discussion of this effect) [84]. Because force varies with extensional strain, and extensional strain decreases as a coil actuator contracts, the initial pulling force (i.e., the force generated at the fully de-twinned length, \( \epsilon_i \)) will vary from the steady state pulling force (i.e., the force generated at the partially contracted equilibrium length, \( \epsilon_f \)), unless the actuator is fully blocked from the onset (resulting in no activation stroke and therefore no change in extensional strain, and \( \epsilon_i = \epsilon_f \)). Using conservative estimates for material and actuation parameters (\( G = 25 \text{ GPa}, \eta = 0.9 \), initial de-twinned extensional strain \( \epsilon_i = 3 \), and final equilibrium extensional strain \( \epsilon_f = 0.5 \), representing an activation stroke length of 250\% of the free length \( L_0 \)) and the aforementioned design decisions (\( d = 305 \mu m, C = 3 \)), we predict a single low spring index NiTi SMA actuator will produce the following initial (\( F_i \)) and steady state (\( F_{ss} \)) forces, assuming complete austenite transformation [72]:

\[
F_i = \frac{(25 \cdot 10^9 \text{ Pa})(305 \cdot 10^{-6} \text{ m})^2}{8(3)^3(0.9)}(3) = 35.89 \text{ N}; \quad (3.6)
\]

\[
F_{ss} = \frac{(25 \cdot 10^9 \text{ Pa})(305 \cdot 10^{-6} \text{ m})^2}{8(3)^3(0.9)}(0.5) = 5.98 \text{ N}. \quad (3.7)
\]

These values are based on an actuator comprised of commercially available SMA diameter wire that is slightly larger than average in terms of wire thickness (producing an actuator with an outer diameter of 1.22 mm). As previously stated, upper and lower bounds of SMA wire diameter available from Dynalloy, Inc., are 510 \( \mu m \) and
25 µm, respectively. Actuators with a fixed spring index \((C = 3)\) made from these actuators would have outer diameters of 2.04 mm and 0.1 mm, respectively. Using the same parameter set as used in (3.6) and (3.7), we predict initial and steady state activation forces for the full range of these commercially available SMA wire diameters (results shown in Figure 3-3). Force increases with the square of wire diameter and is linearly multiplied by \(\epsilon\) (from 0.24-99.56 N at maximum \(\epsilon\), and from 0.04-16.59 N at an estimated steady state \(\epsilon\), assuming complete austenite transformation), and actuator outer diameter scales linearly with wire diameter (from 0.10-2.03 mm). These relations provide system designers with considerable freedom in designing actuators for a specific application, from micrometer-scale to millimeter-scale (and beyond). These freedoms in design will only increase as the number of actuators increase and constraints, such as minimum spring index, are relaxed.

### 3.1.2 Theoretical Pressure Production of Low Spring Index NiTi Actuators

Taking each of these relationships into account, we are now able to size an optimized actuator system (i.e., number of parallel actuators \(n_a\) that each produce a maximum activation force \(F_{max}\), determined by (3.5) using \(G_A\) and \(\epsilon_f\)) for use in a textile compression system to achieve a counter-pressure target \(P\) based on the previously defined thin walled hoop stress equation (2.1). A conceptual tourniquet configuration is depicted graphically in Figure 3-4. Assuming a standard single-layer, parallel actuator configuration, the upper limit of total actuators will be constrained by the total axial width and by the fixed minimum spring index \((C = 3)\):

\[
w \geq (D + d)(n_a) = 4d(n_a).
\]  

Substituting (3.5) and (3.8) into (2.1) and rearranging we are left with the following equation for maximum counter-pressure that can be produced using low spring index coils:
Figure 3-3: Predicted initial force (assuming $\epsilon = 3$), steady state force (assuming $\epsilon = 0.5$), and actuator outer diameter (assuming $C = 3$), for SMA spring actuators made from commercially available NiTi Flexinol wire (inset plot magnifies low diameter range). As wire diameter increases, actuator diameter linearly increases, and force increases with the square of wire diameter for a fixed $\epsilon$. A wide range of forces and actuator diameters are possible given the choices in commercially available NiTi wire, providing considerable design freedom.
\[ P \leq \frac{G_A d \epsilon_f}{864 r \eta}. \] (3.9)

We see in (3.9) that maximum counter-pressure scales with SMA wire thickness and actuator extensional strain, is inversely proportional to limb radius and coil packing density, and can be achieved over any width (so long as it is an integer multiple of the outer diameter of the chosen actuator). Putting (3.9) into practice again using real-world values, for an average human thigh radius (9.5 cm), a typical \( G_A \) value (25 GPa), a commercially-available SMA wire diameter (305 µm) and reasonable estimates for coil packing density \( \eta \) and final actuator extensional strain \( \epsilon_f \) (0.9 and 0.5, respectively) we can theoretically produce counter-pressure magnitudes over an arbitrary thigh width as follows [44, 72]:

\[
P \leq \left( \frac{(25 \cdot 10^9 \text{Pa})(305 \cdot 10^{-6} \text{m})(0.5)}{(864)(0.095\text{m})(0.9)} \right) = 51.6\text{kPa}. \] (3.10)

We see that the counter-pressure design goal for an MCP space suit, 29.6 kPa [12, 13, 15, 20, 23], is well within the theoretical capability of a parallel array of low spring index SMA coil actuators (for even the thigh, which is the largest average human limb radius), and that performance can be tuned based on actuator extensional strain, SMA wire thickness, and designed coil actuator packing density.

Note that (3.9) and (3.10) assume that the maximum possible force is generated by the embedded SMA actuator subsystem. In order for this assumption to be valid, several conditions must be met: the actuators must be arranged in a single parallel layer; perfect actuator spacing must be achieved (i.e., no wasted space between adjacent actuators); and complete austenite phase transformation of all actuators must be achieved. If the SMAs are not arranged in a single parallel layer, the geometric relation expressed in (3.8) does not hold, and a new relation between actuator number and compression band width must be derived and substituted into (2.1). If actuator spacing is not optimized, the inequality forms of (3.8) and (3.9) hold, and (3.8) must be modified to better reflect the actual number of actuators contained in the compression band width if an explicit pressure prediction is desired. If complete
Figure 3-4: Schematic of the variables that determine counter-pressure applied by a thin vessel on a solid surface based on the general hoop stress equation using the thin-walled pressure vessel assumption. A proposed architecture of several low spring index NiTi coils aligned in a single, circumferentially-aligned parallel layer is presented. Delta and epsilon values will change as the system contracts (i.e., the initial, pre-activation de-twinned actuator length will be greater than the final, partially-contracted equilibrium actuator length, unless the system is fully blocked from the onset).
3.1.3 Thermal Modeling of NiTi Coils

Thermal activation of NiTi coils is generally achieved through Joule heating (i.e., the heat generated as current passes through a resistor). This provides a direct and controllable input mechanism for SMA activation. To fully model this phenomenon, 5 terms must be considered: power input from the applied voltage; conductive heat flow through the wire; convective heat loss to the environment; transformation heat flux associated with the phase change during activation; and radiation heat loss [56,88–90]. Equation (3.11) provides a simplified baseline model that disregards radiation and transformation heat flux effects (but is analytically solvable):

\[
\frac{U^2}{R(T)} = hA(T(t) - T_\infty) + \rho V c_p \frac{dT}{dt}. \tag{3.11}
\]

In this equation: \(U\) is the applied voltage; \(R\) is the wire resistance as a function of wire temperature \(T\); \(h\) is the heat transfer coefficient; \(A\) is the surface area of the wire; \(T\) is the wire temperature as a function of time; \(T_\infty\) is the ambient temperature; \(c_p\) is the specific heat of NiTi; \(V\) is the volume of the wire; and \(\rho\) is the density of NiTi. NiTi resistivity is dependent on the fraction of austenite and martensite in the material, which changes as the wire is actuated [91], though previous studies have found this effect to be small enough to disregard [88]. Additionally, as NiTi wire actuates it does slightly change length (average active strain is \(\leq 5\%\) [1]), which would affect the volume, surface area, and density terms, though these effects are also assumed to be negligible.

By assuming that wire resistivity can be simplified to the resistivity of pure austenite NiTi, (3.11) can be simplified and rearranged to solve for coil temperature as a function of time and applied voltage:

\[
T(t) = \frac{U^2}{hAR} \left(1 - e^{\frac{hA}{\rho V c_p} t}\right) + T_\infty. \tag{3.12}
\]
Using (3.12) in combination with (3.9), and knowing the activation temperature thresholds and behavior of a given NiTi coil actuator, it becomes possible to predict its voltage-extensional strain-force/pressure characteristics. We expect actuation temperatures that exceed 150°C (see section 3.3.1 for detailed calorimetry analysis), which will require shielding between the wearer and the actuator system (e.g., a fabric liner similar to the tongue of a laced shoe). Additionally, because the steady-state temperature achieved by a given power input is dependent on ambient temperature $T_\infty$, this model is capable of predicting the magnitude of additional power required to achieve the same steady state temperature for varying $T_\infty$, assuming convection heat transfer is viable. For deep-space applications, where heat transfer via convection is unavailable, the model will require additional terms to account for radiative heat transfer.

3.2 NiTi Coil Manufacturing and Shape Setting

To manufacture low spring index actuators, we adapted the method developed by Kim et al. [74] and Seok et al. [71]. First, the low spring index spring form is established prior to annealing by winding 305 $\mu$m (0.012”) NiTi Flexinol muscle wire [Dynalloy Inc.] around a 635 $\mu$m (0.025”) stainless steel core, resulting in a spring index $C \sim 3.08$. Winding is accomplished by hanging the steel core under tension from a variable speed DC motor, and progressively feeding the NiTi wire along the length of the core using a packing rod as the core rotates. Downward tension is induced in the NiTi wire manually, and upward tension is provided by the packing rod at the point of winding to ensure tight packing density and consistent pitch angle. This method, which is represented in Figure 3-5a, was found to repeatably produce coils with average $\eta = 0.887 \pm 0.02$, at 95% confidence).

The specific NiTi wire diameter (305 $\mu$m) selected is a compromise between maximum force (and therefore maximum pressure) and coil thickness (coil outer diameter, in this case OD $\sim 1.25$ mm, determines the bulkiness of the actuator system relative to the passive textile thickness). There is evidence that as-drawn Nitinol provides
larger deflections for a given force and spring shape [76] and is significantly cheaper than Flexinol muscle wire; however, Flexinol was chosen based on previous research that served to best inform our annealing parameters [71].

Once wound at room temperature, each coil is clamped on both ends to retain its shape and annealed at 450°C for 10 minutes to set the austenite memory state, after which it is water quenched and the steel core and clamps are removed. These annealing parameters were selected as a balance between minimizing de-twinning force and minimizing permanent plastic deformation after actuation [71]. Examples of finished twinned and de-twinned actuators are included as Figure 3-5b.

### 3.3 Fundamental NiTi Coil Characterizations

The following subsections describe a battery of characterization tests conducted with the SMA prototype actuators. Pilot test data is also included in Appendix B.
Table 3.1: Summary of NiTi coil calorimetry analysis: average activation temperatures for raw and annealed specimens

<table>
<thead>
<tr>
<th>Material Type</th>
<th>Martensite Starting State</th>
<th>$A_s$ [$^\circ$C]</th>
<th>$A_f$ [$^\circ$C]</th>
<th>$M_s$ [$^\circ$C]</th>
<th>$M_f$ [$^\circ$C]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Raw Flexinol</td>
<td>De-Twinned</td>
<td>63.3</td>
<td>74.2</td>
<td>81.4</td>
<td>–</td>
</tr>
<tr>
<td>Raw Flexinol</td>
<td>Twinned</td>
<td>41.3</td>
<td>75.3</td>
<td>83.5</td>
<td>–</td>
</tr>
<tr>
<td>Annealed Coil</td>
<td>De-Twinned</td>
<td>48.5</td>
<td>69.8</td>
<td>50.7</td>
<td>39.3</td>
</tr>
<tr>
<td>Annealed Coil</td>
<td>Twinned</td>
<td>44.1</td>
<td>58.4</td>
<td>49.6</td>
<td>39.5</td>
</tr>
</tbody>
</table>

3.3.1 Differential Scanning Calorimetry (DSC) Testing

Calorimetry analysis using a Mettler thermogravimetric analyzer/differential scanning calorimetry machine (TGA/DSC 1) [Mettler Toledo International Inc.] was performed to determine the stress-free critical temperatures (austenite start and finish temperatures, $A_s$ and $A_f$, and martensite start and finish temperatures, $M_s$ and $M_f$) of both raw Flexinol NiTi wire and our custom annealed coils. Five samples (2 raw Flexinol wire samples, and 3 annealed coil samples) were individually heated from 20°C to 195°C, then cooled from 195°C to 35°C, at a rate of 5°C/min, and normalized heat flow (W/g) was measured at 2 Hz. Each test was repeated twice for comparison, resulting in 20 total datasets (10 martensite to austenite transformations, and 10 austenite to martensite transformations). Critical temperatures were identified as the temperatures that mark the beginning and end of spikes in heat flow (signaling an endo- or exo-thermic process that stems from an internal phase change during activation) [73]. An example of this analysis is included in Appendix C.

The results of these tests are shown in Table 3.1. The specific raw Flexinol specimens tested are reported by the manufacturer to actuate at 70°C, and we see that in both the de-twinned and twinned starting condition, the raw wire austenite finish temperature is near that value (austenite starting temperature is significantly lower, and varies depending on whether the sample begins de-twinned or twinned). When cooling raw wire from the activated (austenite) state, we see that martensite transformation begins near the reported activation temperature (approximately 80°C), though it does not fully complete at temperatures as low as 35°C (explaining the voids in Table 3.1, indicating the final transformation temperature is < 35°C).
We see different behavior for annealed coil samples: the austenite activation window (start and stop temperatures) is downshifted, and the martensite activation window begins at a lower temperature and is completed at approximately 39°C.

Correcting for multiple comparisons using Bonferroni corrections, the dataset shows no statistically significant effects. Annealing appears to affect critical temperatures for both de-twinned and twinned samples, as does starting martensite condition on austenite start temperatures, but these effects were not found to be significant given the small dataset. Previous studies have found initial martensite state (de-twinned vs. twinned) and annealing history to be statistically significant influences on SMA activation behavior; however, additional data collection and analysis is necessary before these effects can be seen in the data [73].

3.3.2 Martensite De-twinning Force Testing

To characterize the de-twinning behavior of the NiTi coil actuators, tensile tests were performed using a Zwick/Roell tensile test machine [Zwick GmbH & Co.]. For each test, a twinned martensite coil was placed into the test machine, with clamps used to hold the coil in place at both ends. The coil was then stretched to 150% of its initial length (i.e., $\epsilon = 1.5$) at a constant rate of 10 mm/min and tensile force was recorded. Tests were repeated twice on 25, 30, and 35 mm actuators, for a total of 6 tests. Shear stress $\tau$ and shear strain $\gamma$ were determined using the initial pitch angle $\alpha_i$, the final pitch angle $\alpha_f$, Poisson’s ratio $v$, and the known spring index [72]:

$$\tau = \frac{8CF}{\pi d^2}; \quad (3.13)$$

$$\gamma = \frac{1}{C} \left( \frac{\cos^2 \alpha_i (\sin \alpha_f - \sin \alpha_i)}{\cos^2 \alpha_f (\cos^2 \alpha_f + \sin^2 \alpha_f/(1 + v))} \right). \quad (3.14)$$

The two-state martensite model from An et al. [72], which uses the residual strain $\gamma_L$ and shear-stress induced de-twinned martensitic volume fraction $\psi_{S\tau}$, was used to compare the experimental results between studies:
Table 3.2: Comparison of two-state model parameters

<table>
<thead>
<tr>
<th>Data</th>
<th>$G_M$ [MPa]</th>
<th>$\tau_s$ [MPa]</th>
<th>$\tau_f$ [MPa]</th>
<th>$\gamma_L$</th>
</tr>
</thead>
<tbody>
<tr>
<td>An et al.</td>
<td>6980</td>
<td>72.4</td>
<td>114</td>
<td>0.05</td>
</tr>
<tr>
<td>An et al.*</td>
<td>8932</td>
<td>43.4</td>
<td>86.45</td>
<td>0.045</td>
</tr>
</tbody>
</table>

$$
F = \frac{G_M d}{8C^3 n} \left( \frac{\cos^3 \alpha_i}{\cos^2 \alpha_f (\cos^2 \alpha_f + \sin^2 \alpha_f / (1 + v))} \right) \delta
$$

(3.15)

$$
-\frac{\pi d^3}{8D} G_M \gamma_L \psi_{S\tau} \gamma_L \psi_{S\tau}
$$

where

$$
\psi_{S\tau} = \frac{1}{2} \cos \left[ \frac{\pi}{(\tau_s - \tau_f)(\tau - \tau_f)} \right] + \frac{1}{2}.
$$

(3.16)

The results of the de-twinning tests are included in Figure 3-6, which shows how shear stress varies with respect to shear strain (data is presented as the average over 6 tests, with 95% confidence bounds). Each coil undergoes three distinct regimes as it de-twins: the first regime of the curve is a linear, elastic response, while the coil is still twinned; the second regime, and the first non-linearity, corresponds to the de-twinning process; and the final regime, and second linearity, corresponds to the elastic de-twinned response [72]. A coil stretched through this process will remain de-twinned until thermal re-activation, resulting in irrecoverable extensional strains measured up to approximately 300% of starting length ($\epsilon = 3$).

The parameters for the two state model, as reported by An et al. and as calculated based on our experimental data (identified by the label An et al.*), are displayed in Table 3.2. Figure 3-6 demonstrates that the two state model developed by An et al. holds for our coil actuators, which are lower spring index and ten times longer than those used in their study.
Figure 3-6: De-twinning shear stress vs. shear strain. The two state model from An et al. is overlaid twice on the data, once using their reported parameters (referred to as An et al.) and once with parameters calculated from our experimental data (referred to as An et al.*). Dotted lines represent 95% confidence intervals. Note that An et al. tested larger spring index specimens than those tested for this study, which explains the deviations in model parameters.
3.3.3 Active Blocking Force Thermal Testing

To characterize the blocking force response of the coil actuators (i.e., the force generated by a coil actuator when held at a fixed displacement when activated), multiple actuators were stretched to 100% their initial length (i.e., $\epsilon = 1$) using a Zwick/Roell tensile test machine equipped with a thermal chamber. Force was recorded while the thermal chamber was heated from 25°C to 80°C (to trigger activation according to Table 3.1). Seven tests were performed using two actuators to assess the repeatability of the blocking force response within and between actuators. One actuator was tested 5 times between 25-80°C, and the second actuator was tested twice, including once up to 160°C, to determine the magnitude of stress-induced martensitic transformation that occurs beyond the temperature bounds determined in calorimetry testing. The results of these tests are shown in Figure 3-7 (the data is presented as averages with 95% confidence bounds).

As can be seen in Figure 3-7, the coil actuators demonstrate repeatable force generation as a function of temperature, both within and between specimens. Note that forces are generated as low as 30°C, and that force generation slope increases once the actuators reach approximately 60°C (which is close, but not equal, to the reported off-the-shelf activation temperature of 70°C). Of particular interest is actuator behavior as temperature increases beyond 60°C: force continues to increase with temperature to the limit of the test window (160°C), producing maximum forces of 6.24 N. This is different than what was measured in Table 3.1, and is indicative of stress-induced martensitic transformation (i.e., in a blocking-force configuration, the stress generated as the coil actuates induces a reverse austenite-martensite phase change, effectively increasing the critical temperature required for full austenite activation) [61,73,76,92–94]. In practice, large external forces will resist the actuators when used as compression garment elements (upwards of 31.6 N for a 1 cm MCP band), so this effect is likely to affect MCP systems. This is a well-documented effect, and is well described by the Clausius-Clapeyron relation [73,76].

This finding has several implications: when used in a real-life setting, where ex-
Figure 3-7: Force vs. temperature for blocking force tests. Actuators were initially held at 100% their initial length, and were repeatedly heated from 25°C to 80°C (magnified above). The listed activation temperature for the Flexinol wire provided by the manufacturer (70°C), and the austenite start temperature as determined by calorimetry testing, (49°C), are included for reference. A second actuator was heated from 25°C to 160°C and is overlaid on the original data for comparison. Dotted lines represent 95% confidence intervals.
ternal stresses are present, coil force generation is a continuous response over a wide range of input temperatures that is affected by the presence and magnitude of said stress; SMA coils, when used in a blocking-force configuration, provide opportunities for precision force control through applied voltage, as the force response of the coil can be tuned continuously as a result of the extended temperature range over which the coil activates (i.e., a narrow activation temperature range results in actuators that are more difficult to control precisely); and finally, force generation models that predict maximum force generated based on the assumption of 100% austenite transformation (and therefore use pure austenite shear modulus values to calculate force) are limited in their practical predictive power if used in this regime where the transformation window is extended beyond the expected or published bounds (i.e., such models will over-predict the generated force in these cases).

3.3.4 Actuator Response and Path Dependence

For the coil actuator design discussed in this paper, the shape memory effect is utilized by first de-twinning the actuators to a given extensional strain at room temperature, and then applying a voltage to trigger resistive heating to produce a blocking force. If the steps are reversed (i.e., if the actuator is heated to trigger complete austenite activation at zero extensional strain, then stretched) the force profile and maximum force generated will differ.

This is shown in Figure 3-8. De-twinning followed by blocking force resistive heating results in a maximum shear stress that is significantly lower than that measured when the same actuator is strain-cycled entirely at high temperature (235 MPa vs. 333 MPa, a decrease of 29.4%). This demonstrates that actuator performance is highly path dependent, which is important for both modeling actuator performance and for system design: compression garments require both high forces (which are most effectively achieved if the actuators are heated first then strained) and easy donning/doffing (which is most effectively achieved if the actuators are de-twinned at low temperature, then heated to constrict).

This effect is not yet fully understood, and additional study is required. It is possi-
Figure 3-8: Actuators de-twinned at room temperature then heated at a fixed extensional strain generate a specific shear stress value (235 MPa in this case). If the same actuators are instead displacement-cycled entirely at high temperature, a greater shear stress value (333 MPa) is measured at the same temperature-strain combination.
ble that this is not actually a hysteresis effect, but is instead an example of incomplete phase transformation due to stress-induced reverse martensitic transformation unique to blocking-force activation (i.e., extending the orange path to a greater temperature level may result in active shear stress values that converge to the load-cycling value). Additional study is necessary to test this hypothesis. In the absence of a full understanding of this effect, it is prudent for system designers who intend to use the stretch-then-heat (instead of the heat-then-stretch) architecture to anticipate a potential loss in actuator performance and to upscale system requirements accordingly.

3.3.5 Active Force-Voltage-Extensional Strain Testing

A final set of tests was completed to characterize the joint effect of extensional strain and applied voltage (and therefore applied power) on average blocking force, and to compare these results to the previously described voltage-thermal and extensional strain-force models. Four coil actuators of equal length ($L_0 = 8.255$ cm, $n = 245 \pm 3$, packing density $\eta = 0.9 \pm 0.015$) were each exposed to an applied voltage for 60s at a fixed extensional strain, and average steady-state blocking forces were measured. After 60s, the voltage was increased, and the test was repeated (6 applied voltages were tested, from 3-8 V). Upon completion of a full voltage sweep, each actuator was removed from the test stand and the fixed extensional strain was varied (5 total strains were tested), and the voltage sweep was repeated. Average steady state blocking force was thus measured at 30 voltage-extensional strain combinations for each actuator. Data was collected using a tabletop variable height test stand, a tunable DC voltage power supply, and a Futek LTH350 donut load cell [Futek Inc.].

Figure 3-9 presents the data collected from these tests, averaged across all four actuators. We see that for a fixed extensional strain, force increases with increasing voltage up to a maximum recorded force of 7.24 N, which is consistent with the relationship from Figure 3-7: voltage drives an increase in temperature, which in turn causes an increase in blocking force, and this is an extended and continuous response due to stress-induced martensite transformation. We see that for voltages $\leq 3$ V, the heat generated is largely insufficient to trigger austenite activation (and nearly zero
Figure 3-9: Average blocking force generated by four coil actuators when exposed to a 60s applied voltage at a fixed extensional strain $\epsilon$. Force increases as voltage increases (for a fixed $\epsilon$) and force increases as $\epsilon$ increases (for a fixed voltage), which validates the force-extensional strain model for $\epsilon < 1.77$.

force is recorded). Similarly, for a fixed voltage (and therefore fixed temperature), force increases with increasing extensional strain, which is consistent with (3.1) and all subsequently derived force equations. Combining the findings from these results, force is maximized for a given actuator by maximizing both extensional strain (up to the point of mechanical failure) and applied voltage (up to the point that full austenite activation occurs, or the temperature exceeds the memory-resetting temperature).

### 3.3.6 Actuator Cyclic Performance Degradation

Of particular concern is the degradation of SMA actuator performance over its lifetime, which is a known limitation of systems that incorporate SMA actuators [73]. Two actuators ($d = 305 \mu m$, $\eta = 0.9$) were subjected to several consecutive blocking force cycles ($\epsilon_f = 1.5$) [71, 84]. Power was cycled at 60s intervals (8.6 W), with 60 powered-unpowered intervals comprising one full test. One actuator was subjected to repeated manual de-twinning; each cycle began with a manually de-twinned (i.e., $\epsilon_i \gg 1.5$) martensite coil actuator; as power was applied, an active stroke during
Figure 3-10: Average active force vs. cycle number for two SMA actuator blocking force tests, with linear best fit lines included. Cycles were repeated at 60s active/de-active intervals, and two datasets are presented, representing tests where an actuator was not de-twinned between cycles, and tests where an actuator was manually de-twinned after each cycle. Error bars (representing $\pm 2\sigma$ of average active force, sampled at 2.5 Hz) are included only for the test with no manual de-twinning (for readability). See Figure 3-11 for similar error bars for the test with manual de-twinning.

martensite-austenite transformation was measured (resulting in $\epsilon_f = 1.5$); while held in a blocking configuration in the austenite phase at $\epsilon_f = 1.5$, average force was measured; and the cycle ended with austenite-martensite cooling and manual de-twinning. The second actuator was run through an identical battery of tests, but it was not manually de-twinned between cycles. In these tests, activation resulted in measurable forces (but no active stroke). The results of these tests are presented in Figure 3-10 and Figure 3-11.

Average force vs. cycle number (with $\pm 2\sigma$ error bars included) is presented for
Figure 3-11: An identical plot as Figure 3-10, with error bars included only for the test with repeated manual de-twinning (again, for readability). De-twinning causes a 310% increase in variance in average force, as well as 7.33 times greater degradation in actuator performance over 60 cycles.
each test, along with linear best fit lines. After 60 cycles, the actuator that was not subject to continual de-twining experienced 9.1% loss in average active force (4.64 N vs. 5.07 N). Conversely, the actuator that was subject to cyclic de-twining experienced 23.1% loss in average active force (3.72 N vs. 4.83 N). This is evident in the marked difference in trend between tests (e.g., best fit predictions suggest that cycles that incorporate de-twining cause actuators to degrade 7.33 times faster than those that do not). Additionally, we observe an increase in variability in output at each cycle point for the de-twinned actuator (i.e., average $2\sigma$ values are 310% greater in de-twinned actuator cycles than in non de-twinned actuator cycles). This suggests that tourniquet designs that minimize active stroke length (i.e., designs that fully block the actuator) will be less susceptible to cyclic performance degradation, average force variability, and associated counter-pressure variability or loss.

### 3.3.7 Actuator Linearity and Thermal Model Efficacy

The prevailing SMA coil actuator force-displacement model developed by An et al., which serves as the basis for many of the equations in this paper, bases its activation force predictions on the assumption that the actuator is operating within the linear region of the shear stress-shear strain curve [72]. The tests conducted in Figure 3-7 through Figure 3-9 operate at large extensional strains that may extend beyond the intended limits of such models (and many envisioned uses of SMA coils in future compression garments would require similarly large extensional strains). To assess the linearity of the coil actuators in the tests represented in Figure 3-9, linear fits were matched to the extensional strain-force data (controlling for voltage), resulting in an average $R^2 = 0.82$ for extensional strains $\epsilon \leq 1.77$. This suggests that linear force-displacement models that are used to predict forces at large extensional strains (at least up to $\epsilon = 1.77$) can predict actuator behavior.

To assess the efficacy of the thermal model presented in this paper, steady state temperatures were predicted using (3.11) and (3.12) for the actuators used in the voltage-blocking force tests (see Figure 3-9). Force-temperature data from the thermal chamber blocking force tests (see Figure 3-7) were treated as truth reference values (for
Figure 3-12: Average temperature error vs. voltage, as predicted by the voltage-temperature model, compared against estimated temperatures achieved in voltage-force tests (linearly corrected for $\epsilon$ between 0.33-1.77).

$\epsilon = 1$), and temperature values were estimated for each measurement by normalizing the measured force to a reference $\epsilon = 1$ extensional strain, then cross-referencing this force value against the truth force-temperature table. This method is based on the assumption of force-extensional strain linearity through the extensional strain envelope of interest.

The results of this assessment are included in Figure 3-12, assuming coil packing density $\eta = 0.9$, a convective heat transfer coefficient $h = 90 \text{ W/(m}^2\text{C)}$, a specific heat $c = 837.36 \text{ J/(kg}^\circ\text{C)}$, and a static wire austenite resistivity = 100 $\mu\Omega$-cm [95]. Figure 3-12 presents the average temperature error between the model output and predicted values using the force-temperature reference method for all extensional strains as a function of voltage. The general under-prediction of the model (and the associated upward trend with increasing voltage) likely stems from limitations in the assumption that SMA resistance can be treated as simply the resistance of austenite phase NiTi: as temperature increases and a greater percentage of the material exists in the austenite phase, the wire resistance approaches the resistance of pure NiTi austenite; at lower temperatures, where this is not the case, the model will under-
predict wire temperature (martensite resistivity is lower than that of austenite, so a pure martensite wire will reach higher temperatures than a pure austenite wire for a fixed voltage). This manifests as large errors in predicted blocking force at low temperatures. The small average error recorded at the lowest voltage setting, which is inconsistent with this theory, is likely a unique result of a superposition of both the resistivity error (which causes the model to under-predict temperature) and errors stemming from a non-uniform heating effect that was observed only at low voltages (leading to lower than anticipated measured actuation forces, and therefore under-estimations of temperature using the ground truth table, which artificially brings the model into closer alignment).

3.4 Compression Tourniquet Using Integrated NiTi Coil Actuators

A first-generation compression tourniquet was developed and tested using low spring index coil actuators that were described in the preceding sections and is depicted in Figure 3-13. This prototype consisted of three subcomponents: four parallel-aligned NiTi coil actuators, to provide activation forces and displacements; a band of breathable and compliant fabric, to serve as the cuff that imparts counter-pressure on the body through tension produced by the actuators; and a 3D-printed housing structure, to isolate the actuators from the tourniquet system and to provide structure to couple the actuators to the fabric via a sliding block. This prototype is similar in concept to that depicted in Figure 3-4, with the actuators aligned axially rather than circumferentially.

3.4.1 Tourniquet Design and Manufacturing

The prototype tourniquet system features several strategic design decisions. First, the NiTi coil actuators are housed in an axially-aligned 3D printed plastic compartment that physically separates the actuators from the circumferential fabric cuff that
directly interacts with the wearer. Instead of direct coupling (as is the case in Figure 3-4), these subcomponents are connected through an intermediate, axially-aligned free-sliding block, which is connected on one face to the actuators and on the opposing face to passive cabling that is routed through the housing structure to the cuff. The actuators are fixed to both the sliding block and the back wall of the housing using 3D printed press-fit components that are epoxied together for added grip strength. As the actuators contract, the sliding block is pulled axially up the length of the housing, which reels in and stretches the cuff around an object, creating tension and counter-pressure. Equilibrium is reached when the force generated by the actuator system is balanced by the tension from the stretched cuff as it wraps around the underlying limb.

This architecture was chosen for several reasons: to provide thermal and electrical insulation between the wearer and the actuators; to provide a compact and stable electrical path for voltage-based actuation; and also to maximize the circumferential coverage of the fabric cuff on the wearer. The sliding block contains embedded copper tape, which closes the circuit by separating the actuators into two parallel bundles, and bridging the bundles in series. The geometries of the system (i.e., the number of actuators, the extensional strain stroke length, the passive cabling length, and the desired fabric cuff stretch ratio) can be tuned for specific counter-pressure requirements, donning/doffing requirements, and/or power consumption requirements.

This system was manufactured and assembled using the following components: 4 Flexinol NiTi coil actuators \( C = 3.08, d = 305 \, \mu m, \text{average } L_0 = 5.00 \, \text{cm, average } n = 139, \text{average } \eta = 0.85 \) produced using the method described in Figure 3-5; a 3D printed housing and slider block made from acrylonitrile butadiene styrene (ABS) thermoplastic, produced using a Stratasys Fortus 250mc printer [Stratasys, Ltd.]; 200 \, \mu m diameter stainless steel passive cabling; and a 2.54 cm wide band of breathable compression fabric [BioSkin, inc.].
Figure 3-13: Schematic of compression tourniquet using integrated NiTi coil actuators, 3D printed housing and slider block, and passive fabric.

3.4.2 Force and Pressure Testing

Force and pressure tests were conducted using the tourniquet prototype system to determine its effectiveness at producing counter-pressure against a rigid object (simulating a limb). Blocking force tests using the four-actuator system were shown to produce forces up to 24.75 N at 5 V (4.55 W) at an extensional strain $\epsilon = 1.36$ (an average force per actuator of 6.19 N). The system performs as expected: previous testing of actuators at $\epsilon \approx 1.35$ at the same activation temperature (see Figure 3-9) produced forces of 6.57 N, a difference of only 5.7%.

Pressure production testing was completed by wrapping the tourniquet cuff around a rigid PVC pipe (outer diameter = 11.43 cm), then applying an increasing step voltage to stretch the cuff around the pipe. This setup is represented in Figure 3-14. Voltage was applied at 60s intervals, from 0-10 V, and counter-pressure between the cuff and the pipe was recorded using a Novel S2075 Pliance pressure sensor (a 15.24 cm x 15.24 cm sensor with 256 sensing pixels, range of 2-200 kPa and accuracy > 95% of the measured value) [Novel Electronics, Inc.]. Two different cuff bands were tested:
one measuring 20.6 cm when completely unstretched (and stretched 34% to 27.6 cm when wrapped around the pipe), and another measuring 24.1 cm (and stretched 16% to 27.9 cm when wrapped). The remaining pipe circumference was occupied by the tourniquet casing and the connective steel thread.

The results of the pressure production tests are shown in Figure 3-15, and represent the average pressure produced (with 95% confidence) as a function of voltage (with curvature effects removed). The pressure production profile is significantly affected by the initial cuff parameters: the longer cuff, which required less initial stretching to be donned on the pipe (16% vs. 34% for the shorter cuff), produced less pressure at all conditions (including the 0 V condition, representing the passive pressure applied by the cuff before activation). This is consistent with expectations, as the actuator system will reach equilibrium at a smaller extensional strain when paired with a longer, less-stretched cuff, resulting in a lower circumferential tension. Conversely, the shorter cuff, which had to be stretched a greater percentage when donned (and thus produced a larger passive pressure and provided a larger resistance to the actuator system), produced greater pressure at all voltages because the final strain of the actuator system was greater ($\epsilon_f = 0.37$ vs. 0.17, respectively).

In each case, the system reaches steady state as voltage is increased: for both cuffs, pressure values stabilize for voltages $\geq 6$ V (to within $\pm 5.6\%$ for the tight cuff, and $\pm 10.0\%$ for the loose cuff). The steady state pressure produced is consistent with the values predicted by (3.9) using an estimated shear modulus $G = 7.5$ GPa when corrected for cycling-induced creep in $L_0$ (and thus changes in $\eta$ and $\epsilon$). This modulus term is lower than average [72], which is likely attributable to both fatigue effects and path dependency losses (see Figure 3-8). Additionally, because each cuff is initially stretched (producing a baseline counter-pressure that does not rely on actuation), it is important to assess the magnitude of additional pressure that is produced as a result of the NiTi actuators: with the short cuff, steady state activation equilibrium produces 6.05 kPa (compared to 3.45 kPa when deactivated), an increase of 75%; with the long cuff, activation produces 1.85 kPa (compared to 0.49 kPa when deactivated), an increase of 277%. 

88
Figure 3-14: Initial pressure testing setup. The prototype tourniquet cuff is stretched circumferentially around a PVC pipe, with the actuators and housing held orthogonally along its length. The pressure sensor mat is placed underneath the cuff on the surface of the pipe. Note that the position of the pressure sensor in this figure is for illustrative purposes: the sensor was placed exactly opposite the actuator housing during testing.
Figure 3-15: Prototype active tourniquet pressure production as a function of voltage, for two different initial cuff sizes, with predicted values. Once voltage is shut down, pressure will drop as long as the captured tension exceeds the required SMA detwinning force.
3.5 Discussion

The performance of the prototype tourniquet system provides several insights into future system design. First, the system clearly augments the performance of the passive elastic cuff: at zero voltage, pressures were measured purely due to stretching of the passive elastic fabric when donned on the PVC pipe; as voltage was applied, SMA activation occurred causing contraction of the actuators and additional stretching of the passive fabric, and we measured significant increases in counter-pressure over the passive pressure baseline as a result of this activation. It is also clear that the amount of initial stretching/tension in the cuff affects the magnitude of additional counter-pressure that can be produced by the actuator system. Assuming an elastic fabric is used, to produce maximum forces (and pressures) the effective spring index and/or initial tension of the passive cuff should be sufficiently large to block the actuators during activation. However, this comes at the cost of easy donning/doffing. If an inelastic fabric is used, high pressures can be generated without impeding donning and doffing by simply over-sizing the garment, as the activation stroke will be entirely blocked once the stroke successfully recovers the slack in the garment.

Based on these findings, the prototype tourniquet system can be significantly improved with minor modifications. Reducing the packing density of the coil actuators, increasing the number of parallel actuators, and further shortening the initial length of the passive cuff (or choosing a material with higher stiffness) would all lead to higher counter-pressure levels than those measured in this study. Each of these changes would push the system performance higher, as predicted by (3.9). The housing used to isolate the actuators could also be considerably reduced in size with improved manufacturing and assembly methods, and a passive locking mechanism could be implemented to catch the slider block after actuation to maintain the applied pressure without the need of continuous power. Finally, smart sensors (e.g., a piezoelectric film) could be applied to the inner cuff layer to act as in-situ pressure monitors, providing feedback to the voltage control system to precisely control applied pressure.
The study presented in this chapter demonstrates the viability of low spring index NiTi coil actuators for use in controllable compression garment architectures. A novel framework for modeling the pressure production characteristics of a compression garment comprised of multiple coil actuators is presented that combines the prevailing models of SMA actuator performance [71, 72] with traditional models for counter-pressure systems [20, 24]. These equations are non-dimensionalized to describe coil design in terms of extensional strain ($\epsilon$), packing density ($\eta$), and spring index ($C$). The coil actuators we produced were tested and found to be consistent with previously published literature [71–74].

While much effort was dedicated to fundamentally characterize the performance of the low spring index actuators, additional aspects of actuator behavior require further study. Specifically, performance sensitivities to manufacturing (e.g., Flexinol vs. as-drawn Nitinol), annealing temperature, and alloy content also warrant further study. Finally, textiling and manufacturing challenges remain (as many textile machines are not designed to process coiled metal alloys). A viable manufacturing approach that warrants additional study is to combine low spring index coil actuators with 3D printed structures (as was the case in the tourniquet prototype system) to produce either traditional (e.g., weave, knit, or braid) or hybrid (e.g., tourniquet-style) meta-structures. Customized systems using this approach could incorporate multiple materials, stiffnesses, and complexities, opening up design possibilities that may not be achievable with typical industrial textiling equipment. We explore these types of systems (both modeling and prototyping) in greater detail in Chapters 4-5.

Ultimately, this investigation demonstrates that low spring index NiTi coil actuators offer a low mass, low bulk, large displacement, and large force solution to improve MCP garment performance, both in terms of maximum pressure production and ease of donning/doffing.
Chapter 4

Two-Spring Model for Active Compression Textiles with Integrated SMA Coil Actuators

In Chapter 3 we demonstrated the viability of low spring index SMA coil actuators for active compression textile applications through detailed modeling and characterization at the actuator-level. In this chapter, we examine hybrid compression garments comprised of SMA actuators and passive elastic fabrics at the garment-level, and derive and discuss a two-spring model to predict the performance of these active textiles based on a variety of design parameters.

4.1 Hybrid Active Tourniquet Concept

SMA spring actuators produce both forces and displacements aligned with their length. The most simple architecture for producing active compression with integrated coil actuators is a hybrid, circumferential tourniquet design with NiTi actuators (aligned in parallel) collectively attached in series with a passive elastic fabric: as the actuators contract, they stretch the passive elastic fabric and create circumferential tension, producing counter-pressure. This concept is depicted and described in Figure 4-1, and is analogous to the concept previously presented in Figure 3-4. Sev-
eral twinned martensite SMA coil actuators (of length $L_{S0}$), aligned in parallel, are attached to one end of a passive fabric strip (of length $L_{F0}$). The fabric and actuators are stretched around an object (of radius $r$) and the free ends are attached, resulting in initial displacements and extensional strains of both the SMAs and the passive fabric ($\delta_{Si}$ and $\epsilon_{Si}$, and $\delta_{Fi}$ and $\epsilon_{Fi}$, respectively). A passive equilibrium position ($X_P$) is reached that creates a passive tension ($T_P$) and passive pressure ($P_P$). As the actuators are heated and contract, the passive fabric stretches further, and new active equilibrium quantities – position ($X_A$), tension ($T_A$), and pressure ($P_A$) – are reached.

Mechanical counter-pressure (MCP) garments follow the general thin-walled hoop stress equation (2.1) previously discussed. Counter-pressure scales with circumferential tension, and increasing radii require increasing tensions to produce constant pressure. In order to use (2.1) to properly model and design an active compression tourniquet like that depicted in Figure 4-1, we must understand the relationships between force, displacement, and pressure for both SMA actuators (as was presented in Chapter 3) and passive elastic fabrics.

### 4.1.1 SMA Displacement-Force-Pressure Modeling

As previously derived in Chapter 3, the total tension produced ($T_T$) by several ($n_a$) identical and parallel SMA coil actuators that each produce a known active force ($F_A$) is as follows:

$$T_T = F_A n_a.$$  \hspace{1cm} (4.1)

Using this equation in combination with (3.5) and substituted into (2.1), we produce a final generalized analytic form for SMA-actuated counter-pressure [84]:

$$P = \left( \frac{G_A d^2 n_a}{8 C^3 \eta} \right) \frac{\epsilon_S}{r w}.$$  \hspace{1cm} (4.2)

We see that counter-pressure scales linearly with spring extensional strain (retaining the general form of Hooke’s Law), is inversely proportional to radius, and
Figure 4-1: Hybrid active tourniquet concept incorporating both SMA spring actuators and passive fabrics. Bundled actuators (grouped in parallel) are attached in series to a passive fabric cuff, and the system is stretched around an object, creating an initial passive pressure based on the resultant passive equilibrium position and tension. As the actuators contract, a new equilibrium position is reached as the fabric stretches, and new equilibrium tensions and pressure are achieved. Concept is illustrated in both isometric and top view.
can be modified based on coil geometry and material properties. While this non-dimensionalized spring model can be used to predict the final counter-pressure achieved by a tourniquet system based on the actuator properties and a known final extensional strain of the actuator system when fully activated (as was done in (3.10) in Chapter 3), it is insufficient to predict the final equilibrium position itself (which dictates the final force and therefore final pressure) because the equilibrium position depends heavily on the characteristics of the passive fabric used in the system. Most compression garment applications have target compression magnitude requirements (e.g., MCP planetary exploration suits must achieve 29.6 kPa compression to keep astronauts alive in the vacuum of space [12, 13, 20, 96]), as well as requirements regarding the passive materials used, therefore it is critical that models to predict counter-pressure be developed that incorporate material properties as part of the analysis such that they are able to predict active equilibrium positions (and, therefore, predict the ultimate counter-pressure produced). Consequently, we expand upon this model in the following sections by examining and integrating the force-pressure relationship for passive fabrics into our model.

4.1.2 Passive Fabric Displacement-Force-Pressure Modeling

The relationship between passive material properties and counter-pressure can be developed as follows:

1. Simple mechanical stress $\sigma$ can be defined in two separate ways. It is both the ratio of force $F$ to applied area $A$ (in this scenario, area is equal to the material axial width $w$ times the material thickness $t$), and the product of Young’s Modulus $E$ and fabric strain $\epsilon_F$ (in this scenario, strain is defined as the ratio of longitudinal displacement $\delta_F$ to the initial fabric length $L_{F0}$) [20]:

$$\sigma = \frac{F}{A} = \frac{F}{wt} = \frac{E\epsilon_F}{L_{F0}}. \quad (4.3)$$

2. Re-arranging (4.3) provides the following linear form (which closely matches
the form derived for SMA actuators in (3.5)):

\[ F = (EA)\epsilon_f = (Ewt)\epsilon_F = \left(\frac{Ewt}{L_{F0}}\right)\delta_F. \]  \hspace{1cm} (4.4)

3. Combining (4.4) and (2.1) gives us the following form:

\[ P = \frac{Et}{rL_{F0}}\delta_F = \left(\frac{Et}{r}\right)\epsilon_F. \]  \hspace{1cm} (4.5)

We now have an analytic relationship for counter-pressure based on the stretching of a passive fabric (with known material properties) around a given radius, similar to the relationship between actuator force and pressure derived in (4.2). Pressure scales linearly with fabric extensional strain (again, retaining a general form of Hooke’s Law), is inversely proportional to radius, and can be modified based on material thickness and modulus. Equipped with (4.2) and (4.5), we can now develop a predictive model that combines SMA and fabric behavior as a two-spring model to fully predict the performance of a hybrid active compression garment.

4.2 Combined Two Spring Model for Hybrid Active Compression Garments

4.2.1 Two-Dimensional, Two-Spring Model Concept

In theory, a hybrid compression garment comprised of SMA actuators in series with passive fabrics can be modeled as a simple two-spring system, as both elements are assumed to be linear force-displacement subsystems. A two-dimensional decomposition of the tourniquet concept from Figure 4-1 is presented in Figure 4-2, and a representative force-length plot that maps a typical activation stroke is presented in Figure 4-3. Identified in Figure 4-2 are the following physical quantities: initial SMA and passive fabric lengths \( (L_{S0} \text{ and } L_{F0}) \); initial, passive equilibrium SMA and fabric displacements \( (\delta_{S_i} \text{ and } \delta_{F_i}) \); final, active equilibrium SMA and passive fabric displace-
ments ($\delta_S$ and $\delta_F$); passive and active equilibrium positions ($X_P$ and $X_A$); target limb circumference ($2\pi r$); and a quantity we define as the system delta ($\Delta X_{System}$), which corresponds to the difference between limb circumference and the total unstretched system length (this can also be thought of as the starting system closure gap).

In order to graphically model this two-dimensional representation, the following conventions are adopted: the SMA coordinate axis originates at the left edge of the two-dimensional system (with positive length measured to the right); the fabric coordinate axis originates at the right edge of the two-dimensional system (with positive length measured to the left); and the distance separating these two axes corresponds to the circumference of the object on which the hybrid garment will be donned. Physically speaking, the origins of both the SMA and fabric axes occupy the same location in 3D space, as the cuff attaches to itself once wrapped around an object (i.e., $L_{S0} = 0$ and $L_{F0} = 0$ represent the same position, the junction between the SMA actuators and the passive fabric before it is donned on an object). These conventions are simply an abstraction necessary to model the three-dimensional system in two-dimensions.

Figure 4-3 presents hypothetical linear force vs. length curves for both the passive fabric (shown in green) and the SMA actuators when in the fully activated (i.e., austenite) state (shown in gray). The x-axis represents length, with the left and right y-axes (marking the origin points of the SMA and fabric coordinate systems, respectively) representing force. SMA force increases linearly with displacement starting at $x = L_{S0}$ (in the positive, or right-ward, direction determined by the SMA coordinate system). Passive fabric force increases linearly with displacement starting at $x = L_{F0}$ (in the positive, or left-ward, direction determined by the passive fabric coordinate system). The passive equilibrium condition is identified by the bold numeral 1, and the corresponding position ($X_P$) and force ($F_{Passive}$) are identified. The active equilibrium condition is identified by the bold numeral 2, and the corresponding position ($X_A$) and force ($F_{Active}$) are identified. Beneath the plot, the physical quantities from Figure 4-2 are similarly mapped.
Figure 4-2: Two-dimensional decomposition of hybrid active tourniquet system comprised of SMA actuators and passive fabric, with passive donning and active compression states identified by the numerals 1 and 2, respectively.
Figure 4-3: Representative force-length relationship for the active tourniquet cuff. When donned on an object, the tourniquet system will passively equilibrate at position $X_P$, identified with the numeral 1, creating a passive force $F_{\text{Passive}}$ that produces an initial, static counter-pressure. Upon activation, the system will equilibrate at position $X_A$, identified with numeral 2, creating an active force $F_{\text{Active}}$ that produces a final, static counter-pressure.

The circumference length is constant and is given by:

$$L_{\text{Circumference}} = \text{Constant} = 2\pi r = L_{S_0} + \delta_{S_i} + L_{F_0} + \delta_{F_i} = L_{S_0} + \delta_{S_f} + L_{F_0} + \delta_{F_f}$$
4.2.2 Two-Spring Model Derivation

First, the passive equilibrium position must be determined. As SMA actuators in the martensite phase are forcibly de-twinned, irrecoverable strain develops in the material [73,84]. This de-twinning is a precondition for shape recovery during activation. For SMA spring actuators, the magnitude of maximum irrecoverable strain during de-twinning is a function of spring index $C$ and packing density $\eta$. To maximize total force generated by the SMA actuators according to (3.5), we select actuators that operate at the lowest achievable spring index, $C = 3$ [84–87]. Based on experimental data collected from prototype SMA actuators produced at MIT (presented in Figure 4-4), actuators with $C = 3$ and $\eta = 0.9$, consistently achieve $\epsilon_{Smax} = 2.98 \pm 0.14$ across several $L_{S0}$.

After de-twinning to the irrecoverable strain limit, the actuator permanently rests at that length, and additional stretching of the actuator results in steep elastic resistance [84]. Based on these conditions, we assume the following: SMA actuators will be manually de-twinned to their limit as they are stretched around a limb/object (i.e., limb circumference $> 4L_{S0}$); and the SMA martensite force-displacement curve follows the path depicted in Figure 4-5 (i.e., up to an extensional strain $\epsilon_{Si} = 3$, $F = 0$; and $\epsilon_{Si} = 3$ for all non-zero forces imparted by the stretched fabric). Mathematically, this can be expressed as follows:

$$\delta_{Si} = 3L_{S0}; \quad (4.6)$$

$$X_P = 4L_{S0}; \quad (4.7)$$

$$\delta_{Fi} = 2\pi r - X_P - L_{F0} = 2\pi r - 4L_{S0} - L_{F0}. \quad (4.8)$$

Substituting (4.8) into (4.4) and (4.5) gives us the final forms for $F_{Passive}$ and $P_{Passive}$:

$$F_{Passive} = \left( \frac{E_{wt}}{L_{F0}} \right) (2\pi r - 4L_{S0} - L_{F0}); \quad (4.9)$$
Figure 4-4: Experimental data relating total actuator length to number of coils, for SMA coil actuators with a spring index $C = 3$, SMA wire diameter $d = 305 \mu m$, and packing density $\eta = 0.9$. For this wire diameter, both starting length $L_{S0}$ and maximum irrecoverable length linearly increase with number of coils $n$, with a consistent extensional strain $\epsilon_{Smax}$ across all $n$ ($\epsilon_{Smax} = 2.98 \pm 0.14$). This extensional strain value will be consistent for all springs with identical $C$ and $\eta$ (increasing $C$ will increase $\epsilon_{Smax}$; decreasing packing density will decrease $\epsilon_{Smax}$).

$$P_{Passive} = \left( \frac{Et}{r} \right) \left( \frac{2\pi r - 4L_{S0}}{L_{F0}} - 1 \right).$$ (4.10)

Next, the active equilibrium position $X_A$ must be determined. The passive equilibrium position $X_P$ represents the position that balances the forces between the de-twinned martensite SMA actuators and the stretched passive fabric. The active equilibrium position $X_A$ represents the position that balances the forces between the austenite actuators and the stretched fabric. The pressure evolution will follow the fabric force-displacement curve until it intersects the SMA austenite force-displacement curve. Mathematically, we first solve for $\delta_{Sf}$ by setting active SMA force equal to passive fabric force assuming general linear force-displacement relationships (with placeholder
Figure 4-5: Assumed force-displacement curve for de-twinned martensite SMA actu-
ators based on measured $\epsilon_{S_{\text{max}}}$.

Values for each representative spring constant), then substitute for known values:

\[ F_{\text{Austenite}} = k_1 \delta_{Sf}; \]  
(4.11)

\[ F_{\text{Fabric}} = -k_2(\delta_{Sf} - \Delta X_{\text{System}}); \]  
(4.12)

\[ k_1 \delta_{Sf} = -k_2(\delta_{Sf} - \Delta X_{\text{System}}); \]  
(4.13)

\[ \delta_{Sf} = \left( \frac{k_2}{k_1 + k_2} \right) \Delta X_{\text{System}}; \]  
(4.14)

\[ X_A = L_{S0} + \delta_{Sf} \leq 4L_{S0}. \]  
(4.15)

Values for $\Delta X_{\text{System}}, k_1$ and $k_2$ are known:
\[ \Delta X_{\text{System}} = 2\pi r - L_{S0} - L_{F0} = \delta_{ff} + \delta_{sf}; \quad (4.16) \]

\[ k_1 = \left( \frac{G_A d^2 n_a}{8 C^3 \eta L_{S0}} \right); \quad (4.17) \]

\[ k_2 = \left( \frac{E_{wt}}{L_{F0}} \right). \quad (4.18) \]

The final active equilibrium position, activation stroke (i.e., the difference between passive and active positions), and active force are:

\[ X_A = L_{S0} + \left( \frac{E_{wt}}{G_A d^2 n_a} + \frac{E_{wt}}{L_{F0}} \right) \Delta X_{\text{System}}; \quad (4.19) \]

\[ X_A = L_{S0} + \left( \frac{\Delta X_{\text{System}}(E_{wt})(8C^3\eta L_{S0})}{G_A d^2 n_a L_{F0} + E_{wt}8C^3\eta L_{S0}} \right); \quad (4.20) \]

\[ \Delta X_{\text{Activation}} = X_P - X_A = 3L_{S0} - \left( \frac{\Delta X_{\text{System}}(E_{wt})(8C^3\eta L_{S0})}{G_A d^2 n_a L_{F0} + E_{wt}8C^3\eta L_{S0}} \right); \quad (4.21) \]

\[ F_A = \left( \frac{G_A d^2 n_a}{8 C^3 \eta L_{S0}} \right) \left( \frac{\Delta X_{\text{System}}(E_{wt})(8C^3\eta L_{S0})}{G_A d^2 n_a L_{F0} + E_{wt}8C^3\eta L_{S0}} \right); \quad (4.22) \]

\[ F_A = \left( \frac{\Delta X_{\text{System}}G_A d^2 n_a E_{wt}}{G_A d^2 n_a L_{F0} + E_{wt}8C^3\eta L_{S0}} \right). \quad (4.23) \]

Finally, we calculate the active pressure equivalently in two ways: either using (4.23) in combination with the equation for counter-pressure (2.1); or using (4.15), (4.16), and (4.20) with the derived equation for stretched passive fabric pressure (4.5):

\[ P_{\text{Active-SMA}} = \frac{\Delta X_{\text{System}}G_A d^2 n_a E_{t}}{r(G_A d^2 n_a L_{F0} + E_{wt}8C^3\eta L_{S0})}; \quad (4.24) \]
\[ P_{Active−Fabric} = \frac{Et}{rL_{F0}} \left( \Delta X_{System} - \left( \frac{\Delta X_{System}(Ewt)(8C^3\eta L_{S0})}{G_Ad^2n_a L_{F0} + Ewt8C^3\eta L_{S0}} \right) \right). \] (4.25)

These equations, derived from the SMA and passive fabric length-force-pressure relationships, respectively, provide identical output. A summary of the variables included in the model, and its derivation, is included in Table 4.1. Beyond the assumptions discussed in the derivation of the model, we also assume the full circumference is completely covered by either SMA actuators or passive fabric (i.e., the connections between the materials occupy no length, and no other materials comprise the system), the passive fabric thickness does not change during stretching, and both the SMAs and fabric operate in their linear region. Corrections to the analytic model to incorporate structural connections are provided in Chapter 5.

### 4.3 Model Output and Design Implications

With (4.10), (4.24) and (4.25) it is now possible to predict passive and active pressures based on any tourniquet design, and to analyze the effect of each design variable on counter-pressure magnitudes. Five SMA actuator parameters \((G_A, C, d, \eta, L_{S0})\), four passive fabric material parameters \((E, L_{F0}, t, w)\) one environmental parameter \((r)\), and two system parameters \((n_a, \text{ and } \Delta X_{System}, \text{ a derived parameter which is dependent on SMA length, fabric length, and circumference})\) determine active tourniquet pressure; only one SMA actuator parameter \((L_{S0})\), three passive fabric material parameters \((E, t, \text{ and } L_{F0})\) and one system parameter \((r)\) determine passive tourniquet pressure. Examining (4.10), (4.24) and (4.24) in detail, we see the following:

1. To minimize passive pressure for a fixed \(r\) (for ease of donning and doffing), maximize \(L_{F0}\) and \(L_{S0}\) (and therefore minimize \(\Delta X_{System}\)). Decreasing \(E\) and \(t\) also decreases passive pressure.

2. All else equal, active pressure increases with increasing \(G_A, d, n_a, E\) and \(t\).
Table 4.1: Summary of variables used to derive two-spring active tourniquet model, with model inputs identified.

<table>
<thead>
<tr>
<th>Variable</th>
<th>Units</th>
<th>Input</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$A$</td>
<td>$[m^2]$</td>
<td></td>
<td>Passive fabric area ($w \times t$)</td>
</tr>
<tr>
<td>$C$</td>
<td></td>
<td>*</td>
<td>SMA spring index ($D/d$)</td>
</tr>
<tr>
<td>$d$</td>
<td>$[m]$</td>
<td>*</td>
<td>SMA wire diameter</td>
</tr>
<tr>
<td>$\delta_{Fi}$</td>
<td>$[m]$</td>
<td></td>
<td>Initial, passive fabric displacement</td>
</tr>
<tr>
<td>$\delta_{Ff}$</td>
<td>$[m]$</td>
<td></td>
<td>Final, active fabric displacement</td>
</tr>
<tr>
<td>$\delta_{Si}$</td>
<td>$[m]$</td>
<td></td>
<td>Initial, passive SMA de-twinned displacement</td>
</tr>
<tr>
<td>$\delta_{Sf}$</td>
<td>$[m]$</td>
<td></td>
<td>Final, active SMA austenite displacement</td>
</tr>
<tr>
<td>$E$</td>
<td>$[Pa]$</td>
<td>*</td>
<td>Passive fabric Young’s Modulus</td>
</tr>
<tr>
<td>$\epsilon_{Fi}$</td>
<td></td>
<td></td>
<td>Initial, passive fabric extensional strain</td>
</tr>
<tr>
<td>$\epsilon_{Ff}$</td>
<td></td>
<td></td>
<td>Final, active fabric extensional strain</td>
</tr>
<tr>
<td>$\epsilon_{Si}$</td>
<td></td>
<td></td>
<td>Initial, passive SMA de-twinned extensional strain</td>
</tr>
<tr>
<td>$\epsilon_{Sf}$</td>
<td></td>
<td></td>
<td>Final, active SMA austenite extensional strain</td>
</tr>
<tr>
<td>$\epsilon_{Smax}$</td>
<td></td>
<td></td>
<td>Maximum SMA de-twinned extensional strain</td>
</tr>
<tr>
<td>$F_A$</td>
<td>$[N]$</td>
<td></td>
<td>Circumferential force during activation</td>
</tr>
<tr>
<td>$F_P$</td>
<td>$[N]$</td>
<td></td>
<td>Passive circumferential force</td>
</tr>
<tr>
<td>$\eta$</td>
<td></td>
<td>*</td>
<td>SMA spring packing density</td>
</tr>
<tr>
<td>$G_A$</td>
<td>$[Pa]$</td>
<td>*</td>
<td>SMA austenite shear modulus</td>
</tr>
<tr>
<td>$L_{F0}$</td>
<td>$[m]$</td>
<td>*</td>
<td>Passive fabric unstretched length</td>
</tr>
<tr>
<td>$L_{S0}$</td>
<td>$[m]$</td>
<td>*</td>
<td>SMA twinned-martensite length</td>
</tr>
<tr>
<td>$n$</td>
<td></td>
<td></td>
<td>Number of active coils in SMA spring</td>
</tr>
<tr>
<td>$n_a$</td>
<td></td>
<td>*</td>
<td>Number of parallel actuators in system</td>
</tr>
<tr>
<td>$P_{Passive}$</td>
<td>$[Pa]$</td>
<td></td>
<td>Passive counter-pressure</td>
</tr>
<tr>
<td>$P_{Active}$</td>
<td>$[Pa]$</td>
<td></td>
<td>Active counter-pressure</td>
</tr>
<tr>
<td>$r$</td>
<td>$[m]$</td>
<td>*</td>
<td>Limb radius</td>
</tr>
<tr>
<td>$t$</td>
<td>$[m]$</td>
<td>*</td>
<td>Passive fabric material thickness</td>
</tr>
<tr>
<td>$w$</td>
<td>$[m]$</td>
<td>*</td>
<td>Passive fabric axial width</td>
</tr>
<tr>
<td>$X_A$</td>
<td>$[m]$</td>
<td></td>
<td>Active equilibrium position</td>
</tr>
<tr>
<td>$X_P$</td>
<td>$[m]$</td>
<td></td>
<td>Passive equilibrium position</td>
</tr>
<tr>
<td>$\Delta X_{Activation}$</td>
<td>$[m]$</td>
<td></td>
<td>SMA stroke length during activation ($X_P - X_A$)</td>
</tr>
<tr>
<td>$\Delta X_{System}$</td>
<td>$[m]$</td>
<td>**</td>
<td>Unstretched system closure gap ($2\pi r - (L_{S0} + L_{F0})$)</td>
</tr>
</tbody>
</table>

** $\Delta X_{System}$ is an input, but it is set by $L_{F0}$, $L_{S0}$, and $r$
3. All else equal, active pressure decreases with increasing $w$, $C$, and $\eta$.

4. Performance is highly sensitive to $L_{F0}$ and $L_{S0}$ relative to limb circumference (which is set by $r$ and sets $\Delta X_{\text{System}}$). These decisions must be considered for each application individually.

We can examine some of these relationships graphically, as presented in Figure 4-6 and Figure 4-7. Figure 4-6 and Figure 4-7 illustrate the effect of varying either $L_{F0}$ or $E$ (all else equal) on $X_P$, $X_A$, $F_{\text{Passive}}$, $F_{\text{Active}}$, and $\Delta X_{\text{Activation}}$. As $L_{F0}$ is shortened (assuming a fixed passive equilibrium position determined by $L_{S0}$), the passive fabric force-displacement curve is right shifted (increasing $\Delta X_{\text{System}}$), causing increases in $F_{\text{Passive}}$ proportional to the reduction in $L_{F0}$. This right-shifting of the fabric force-displacement curve creates new intersections with the SMA force-displacement curve. Upon activation, a new $X_A$ is established based on this intersection point, leading to increases in $F_{\text{Active}}$ and decreases in $\Delta X_{\text{Activation}}$. Physically, these conclusions make sense: if the length of the unstretched passive fabric is shortened, it must be initially stretched a greater distance to achieve passive equilibrium (leading to an increased passive force); additional stretching upon activation leads to a further increase in force, resulting in an equilibrium point that is greater in force (occurring at a larger $X_A$).

Similarly, increasing $E$ steepens the slope of the fabric force-displacement curve, which increases $F_{\text{Passive}}$, increases $X_A$, increases $F_{\text{Active}}$, and decreases $\Delta X_{\text{Activation}}$. While qualitatively similar in terms of the effect on system performance, modifying $L_{F0}$ and $E$ are physically distinct effects that are best understood by examining their effect on the fabric force-displacement curve (as shown in Figure 4-6 and Figure 4-7). This is true for all design parameters, and is one benefit of modeling the system as conjoined linear systems.

These findings provide garment engineers with a wide range of flexibility in design to meet a given set of requirements. Beyond minimum pressure targets, design criteria that may be critical for a particular application include: constraining $\Delta X_{\text{System}}$ and minimizing $P_{\text{Passive}}$ (to aid donning/doffing); limitations on $t$ and $E$ due to a limited
Figure 4-6: Effect of shortening $L_{F0}$ on tourniquet system behavior, which right-shifts the passive fabric force-displacement curve. Passive force and active force increase as passive fabric length decreases, and the active equilibrium position is shifted such that SMA active displacement is larger.
Figure 4-7: Effect of increasing $E$ on tourniquet system behavior, which steepens the slope of the passive fabric force-displacement curve. Passive force and active force increase as passive fabric Young’s modulus increases, and the active equilibrium position is shifted such that SMA active displacement is larger.
range of appropriate passive materials (e.g., when designing a space suit that needs materials with specific thermal, radiation, and durability capabilities); adjusting $n_o$ based on limitations in available power; and minimizing $L_{S0}$ and/or maximizing $L_{F0}$ (to maximize circumferential fabric coverage to protect the wearer).

4.4 Passive Fabric Material Characterization

The two-spring model derived in this chapter is predicated on the assumption of linear behavior of both the SMA actuators and the passive fabrics. We have demonstrated in the preceding analysis that SMA actuator linearity is a justifiable assumption. In this section we investigate the validity of the linearity assumption as it applies to passive fabrics by characterizing the modulus properties of a variety of typical passive fabrics used in compression garments.

Passive compression garments are typically constructed using elastic fabrics (e.g., spandex/elastene, polyester, or even rubbers like neoprene), which are comprised of synthetic polymers or elastomers. An initial survey of the Young’s Modulus of typical elastomeric materials is presented in Table 4.2 [7]. Modulus data only applies to the linear stress-strain regime, which can be limited to small strains depending on the specific material [7].

We conducted uniaxial tensile tests on a variety of commercially available rubber and textile elements to compare against this historical data. Four materials were tested, and are shown in Figure 4-8:

1. EL196 Flat Polyester Elastic. Dimensions: 74.9 mm x 25.4 mm x 1.5 mm. Sourced from Lowry Enterprises, Inc.

2. Jumbo Spandex (90% nylon, 10% spandex). Dimensions: 74.9 mm x 38.1 mm x 0.7 mm. Sourced from Costume Works, Inc.

3. BioSkin Compression Fabric (tri-laminate Lycra with polyurethane midlayer). Dimensions: 74.9 mm x 38.1 mm x 1.15 mm. Sourced from BioSkin, Inc.
Table 4.2: Survey of typical Young’s Moduli of elastomer materials [7].

<table>
<thead>
<tr>
<th>Elastomer</th>
<th>Young’s Modulus $E$ (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Butyl Rubber</td>
<td>1-2</td>
</tr>
<tr>
<td>Ethylene-Vinyl-Acetate (EVA)</td>
<td>10-40</td>
</tr>
<tr>
<td>Isoprene</td>
<td>1.4-4</td>
</tr>
<tr>
<td>Natural Rubber</td>
<td>1.5-2.5</td>
</tr>
<tr>
<td>Polychloroprene (Neoprene)</td>
<td>0.7-2</td>
</tr>
<tr>
<td>Polyurethane Elastomers</td>
<td>2-3</td>
</tr>
<tr>
<td>Silicone Elastomers</td>
<td>5-20</td>
</tr>
</tbody>
</table>

4. Medium-Strength Neoprene (chloroprene) Rubber. Dimensions: 74.9 mm x 38.1 mm x 0.79 mm. Sourced from McMaster-Carr, Inc.

Each sample was stretched at a constant rate of 1 cm/s, and samples were stretched until failure or until stretched to 600-700%. The raw results of this test (force vs. displacement) are included as Figure 4-9a, and the converted results (stress vs. strain, assuming constant sample width and thickness) are included as Figure 4-9b. We see a range of behavior across these materials: neoprene shows highly linear behavior though the full 600% stretch window; each other material appears to behave linearly initially (with spandex having the highest force for a given displacement) and transitioning into non-linear behavior at varying strains $> 200\%$. Given these results, Young’s Modulus for each material was estimated assuming a linear region between 0-200% extensional strain (a realistic envelope for most compression garment applications). Linear fits beginning at the origin were applied to the stress-strain data, and $R^2$ values were calculated. These fits are included as Figure 4-10, and the findings are summarized in Table 4.3.

An inspection of the data reveals several key insights. First, two materials (the jumbo spandex and neoprene) are highly linear up to at least 200% strain, evidenced by their high correlation coefficients (with moduli of 1.41 MPa and 0.359 MPa, respectively). Conversely, the other two materials (BioSkin and the flat polyester elastic) are non-linear even within this strain envelope. This is clear both by comparing the correlation coefficients, and by examining the stress-strain paths shown in Figure
Figure 4-8: Examples of passive elastic fabrics used for uniaxial tensile testing. From top to bottom: EL196 Flat Polyester Elastic [Lowry Enterprises, Inc.]; Jumbo Spandex [Costume Works, Inc.]; BioSkin Compression Fabric [BioSkin, Inc.]; Medium-Strength Neoprene Rubber [McMaster-Carr, Inc.].

4-10: the polyester elastic becomes non-linear at strains as low as 5%, whereas the BioSkin material follows a slightly more linear path. However, these deviate from a plain linear relationship in this envelope to the point that it would inadvisable to quote a fixed modulus value.

Second, examining the jumbo spandex data in even greater detail (see Figure 4-11), we see that a change in modulus slope exists at approximately $\epsilon = 0.15$, and that the modulus is considerably different (1.02 MPa vs. 1.65 MPa) above and below this point. While the total modulus estimate (1.41 MPa) fits the data well ($R^2 = 0.997$), even greater accuracy can be achieved if the modulus is broken down into each of these regimes.

Additionally, the experimental neoprene modulus is less than that reported in the literature (0.359 MPa vs. 0.7-2 MPa). This is likely attributable to breakdowns in the assumption that the material width and thickness are constant throughout the stretching process. Consequently, experimental moduli values listed in Table 4.3 may under-estimate actual modulus values.
Figure 4-9: A (top): raw uniaxial force-displacement data for four passive materials of interest for compression garment design. B (bottom): calculated uniaxial stress-strain data from the force-displacement fabric tensile tests.
Figure 4-10: Estimate of Young’s Modulus of each passive material. Linear fits are applied to each dataset for extensional strains up to 200%. Some materials (spandex, neoprene) are highly linear through this strain envelope; others (BioSkin, flat elastics) are progressively non-linear. The 0-200% envelope is shown in detail.
Figure 4-11: Young’s Modulus estimates for jumbo spandex at low extensional strains. We see a change in modulus slope at approximately $\epsilon = 0.15$, above and below which the modulus differs (1.02 MPa vs. 1.65 MPa, respectively). The global modulus estimate (1.41 MPa) is still a good estimate of material modulus, but additional accuracy can be achieved if the extensional strain is known to fall above or below this inflection point.

Table 4.3: Summary of experimental Young’s Moduli data for four passive elastic materials between 0-200% strain, with expanded analysis for jumbo spandex at low extensional strains.

<table>
<thead>
<tr>
<th>Material</th>
<th>Range</th>
<th>Young’s Modulus $E$ (MPa)</th>
<th>Goodness of Fit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flat Elastic</td>
<td>0-200%</td>
<td>0.478</td>
<td>0.254</td>
</tr>
<tr>
<td>Jumbo Spandex</td>
<td>0-200%</td>
<td>1.41</td>
<td>0.997</td>
</tr>
<tr>
<td></td>
<td>0-15%</td>
<td>1.02</td>
<td>0.995</td>
</tr>
<tr>
<td></td>
<td>20-200%</td>
<td>1.65</td>
<td>0.999</td>
</tr>
<tr>
<td>BioSkin</td>
<td>0-200%</td>
<td>0.467</td>
<td>0.614</td>
</tr>
<tr>
<td>Neoprene</td>
<td>0-200%</td>
<td>0.359</td>
<td>0.865</td>
</tr>
</tbody>
</table>
Finally, given the variability in material linearity, it is imperative that the material properties of a given passive fabric be clearly understood before applying the two-spring linear model for compression garment design. Garments comprised of highly non-linear passive fabrics will not behave as predicted using the two-spring model (modifications to equation (4.12) and all subsequent substitutions are necessary to expand the model to accommodate non-linear materials).

4.5 Example Tourniquet Design Methodology

With this passive materials analysis, we now have the ability to design active compression systems to meet specific performance requirements or specifications. In this section, we present multiple tourniquet designs, each comprised of a suite of parameters selected to achieve a given design criteria, arrived upon using the derived two-spring performance model. Note that for all designs using jumbo spandex, we assume the global modulus value (1.41 MPa).

4.5.1 Tourniquet Design 1: Maximum Counter-Pressure Cuff

The first tourniquet design focuses on developing a 1 cm wide tourniquet capable of producing the highest possible active counter-pressure on an average human thigh for a given set of pre-selected SMA actuators. As demonstrated in Table 4.1, a complete design requires 11 parameter selections. We first select the relevant SMA parameters \((C, d, \eta, G_A\) and \(L_{S0}\)), system parameters \((w\) and \(n_a\)), and environmental parameter \((r)\), to understand and tailor the remaining fabric material parameters \((t, E,\) and \(L_{F0}\)). These initial system parameter selections are identical for each design we present, and are summarized in Table 4.4, which appears at the end of this section. SMA parameters are selected according to design recommendations developed in Chapter 3: low spring index \((C = 3)\); moderate SMA wire diameter \((d = 305 \mu m)\); feasible packing density \((\eta = 0.9)\); representative austenite shear modulus \((G_A = 25 \text{ GPa})\); and a reasonable SMA twinned length given the circumference of the human thigh \((L_{S0} = 8 \text{ cm}, \text{ with a maximum de-twinned length } L_{S0} + \delta_{Si} = 32 \text{ cm})\). The number of
actuators \((n_a = 8)\) is set by the target tourniquet width \((w = 1 \text{ cm})\), and represents the most actuators that fit this width in a single parallel layer. Finally, we set the target radius \((r = 9.5 \text{ cm})\) based on NASA anthropometric standards [44].

In order to achieve maximum counter-pressure, we can examine (4.24) to strategically select the remaining passive fabric material parameters. Counter-pressure increases as material modulus and thickness increase, therefore we select the material with the highest measured modulus from our previous tests (jumbo spandex, \(E = 1.41 \text{ MPa}\)), and the greatest thickness allowed for MCP specifications \((4.9 \text{ mm},\) corresponding to 7 spandex layers each 0.7 mm thick, approaching the design limit of 5 mm).

The remaining passive fabric parameter, initial fabric length \((L_{F_0})\), is selected to maximize active pressure while constrained to not exceed the known linearity limit \((200\% \text{ stretch})\) when active (in order to operate within the known limits of the two-spring model). Selecting this parameter requires an assessment of passive and active fabric stretch percentages (determined by the difference between circumference and de-twinned SMA length, and SMA activation stroke length, respectively). This relationship is shown in Figure 4-12a, including the prescribed stretch constraint. We see that, given these constrains, \(L_{F_0}\) is set to 13.4 cm, resulting in 200% active stretch and 43% circumferential fabric coverage (i.e., the percentage of the total limb circumference that is covered by fabric, as opposed to SMA actuators, when fully activated).

With this selection, we calculate passive and active counter-pressures, as well as activation stroke length, in Figure 4-12b. For this set of parameters: passive pressure reaches 77.6 kPa; active pressure reaches 145.2 kPa; and activation stroke reaches 12.5 cm. These are summarized in Table 4.4 at the end of this section.

Examining the predicted performance of this tourniquet system, we see that massive \((> 145 \text{ kPa})\) active counter-pressures are theoretically possible, if the system is fully optimized towards this goal. This performance comes at the significant detriment to donning/doffing simplicity, as it is accompanied by a still-massive \((> 77 \text{ kPa})\) passive counter-pressure, which is likely impossible to achieve without signifi-
Figure 4-12: A (top): effect of varying initial fabric length $L_{F0}$ on passive and active fabric stretch percentages, and total circumference fabric coverage, for maximum compression tourniquet design. Seeking to maximize active stretch while constrained to a 200% stretch limit sets the initial fabric length to 13.4 cm, representing a 43% circumferential fabric coverage when activated. B (bottom): predicted passive and active pressures, and activation stroke length, based on a 13.4 cm initial fabric length. Passive pressure reaches 77.6 kPa and active pressure reaches 145.2 kPa (denoted by red triangle markers), and activation stroke reaches 12.5 cm (denoted by a blue square marker).
cant effort or external support. Similarly, this garment nearly exceeds the maximum allowable garment thickness, making this design more massive and bulky than other more streamlined options. Finally, total limb fabric coverage is quite limited, achieving < 43% coverage when activated (however, this is partially affected by the choice of twinned SMA length, which is a quantity that can be varied if needed).

4.5.2 Tourniquet Design 2: Easy Don, High Pressure Cuff

Following the same methodology as the first tourniquet design, we next design a compression garment that minimizes passive pressure (to aid donning/doffing) while aiming for maximum active counter-pressure given this constraint. For comparison purposes, all SMA, system, and environmental parameters remain unchanged, as well as material and thickness parameters. To minimize passive pressure, we select the largest $L_{F0}$ necessary to remain unstretched when donned on the human limb (corresponding to a $L_{F0} = 27.6$ cm). We again model the same 6 performance parameters (passive and active stretch percentage, percentage fabric coverage, passive and active counter-pressure, and activation stroke length). These relationships are presented in Figure 4-13a-b, and are summarized in Table 4.4.

We see with this design that high counter-pressures (> 52 kPa) are achievable without inhibiting donning/doffing (passive pressures are effectively zero in this design). Similar bulk and mass limitations exist with this design as did in the maximum counter-pressure design (as seven layers of spandex are employed), but this design improves fabric coverage percentage (79% vs. 43%). Additionally, fabric stretch percentages are significantly reduced (0% vs. 107% for the passive condition, 72% vs. 199.7% for the active condition). This is beneficial, as the material will be comfortably in the linear stress-strain region (as opposed to the maximum pressure design, which approached the linear limit).
Figure 4-13: A (top): effect of varying initial fabric length $L_{F0}$ on passive and active fabric stretch percentages, and total circumference fabric coverage, for easy don/doff, high active pressure tourniquet design. Seeking to minimize passive stretch and pressure sets the initial fabric length to 27.6 cm, representing a 79% circumferential fabric coverage when activated. B (bottom): predicted passive and active pressures, and activation stroke length, based on a 27.6 cm initial fabric length. Passive pressure is effectively zero (0.25 kPa) and active pressure reaches 52.5 kPa (denoted by red triangle markers), and activation stroke reaches 19.8 cm (denoted by a blue square marker).
4.5.3 Tourniquet Designs 3a and 3b: Low Profile, Flexible Cuffs

Next, we design two tourniquets with vastly different objectives than the previous two designs: minimize garment profile, and maximize both material flexibility and active stroke length. Again, all SMA, system, and environment parameters are unchanged, but instead of selecting a multi-layer spandex architecture, we select a single neoprene layer to achieve these design objectives (thickness $t = 0.79$ mm, and modulus $E = 0.359$ MPa). In the first low profile design, to maximize active stroke length and flexibility, again we select $L_{F0} = 27.6$ cm to avoid any initial stretching. Stretching values, fabric coverage, and pressure/activation stroke analyses are included in Figure 4-14. Predicted performance results are included in Table 4.4.

We see drastically different performance with this design compared to the previous two designs. Both passive and active pressures are very low ($< 3$ kPa), however a very large active stroke length occurs (23.8 cm), resulting in a final fabric coverage of 86%. For applications where counter-pressure does not need to be great, but active closure, fabric coverage and/or minimized garment thickness are of high interest, this type of design will outperform the powerful, bulky garments previously presented. And again, as is the case in the previous designs, the stroke lengths and fabric coverage percentages in this design can be further improved by modifying the shape memory alloy twinned length ($L_{S0}$).

If low garment thickness and greater active counter-pressure are of primary interest, and fabric coverage percentage is of less interest, a second low profile design that sets $L_{F0}$ based on the previously discussed 200% active stretch limit (instead of the zero-stretch passive condition) was also explored. This is presented in Figure 4-15 with results included in Table 4.4. With this design philosophy, we achieve 5.97 kPa active pressure (vs. 2.6 kPa) at the expense of reduced fabric coverage percentage (68% vs. 86%).
Figure 4-14: A (top): effect of varying initial fabric length $L_{F0}$ on passive and active fabric stretch percentages, and total circumference fabric coverage, for low profile, maximum active stroke length, flexible tourniquet design. Seeking again to minimize passive stretch and pressure sets the initial fabric length to 27.6 cm, representing a 86% circumferential fabric coverage when activated. B (bottom): predicted passive and active pressures, and activation stroke length, based on a 27.6 cm initial fabric length. Passive pressure is effectively zero (0.01 kPa) and active pressure reaches 2.6 kPa (denoted by red triangle markers), and activation stroke reaches 23.8 cm (denoted by a blue square marker).
Figure 4-15: A (top): effect of varying initial fabric length $L_{F0}$ on passive and active fabric stretch percentages, and total circumference fabric coverage, for alternate low profile tourniquet design. Selecting $L_{F0}$ based on the constraint of 200% active stretch results in $L_{F0}$ to 17.1 cm, representing a 68% circumferential fabric coverage when activated. B (bottom): predicted passive and active pressures, and activation stroke length, based on a 17.1 cm initial fabric length. Passive pressure is 1.85 kPa and active pressure reaches 5.97 kPa (denoted by red triangle markers), and activation stroke reaches 23.5 cm (denoted by a blue square marker).
Table 4.4: Summary of eleven design parameters and predicted performance for each theoretical active thigh tourniquet concept. The first eight design parameters are identical for each of the four tourniquet concepts; the final three design parameters are individually chosen to achieve specific design goals. Four predicted performance parameters are presented for each design.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Design 1: Max-Pressure Cuff</th>
<th>Design 2: Easy Don-Doff, High Pressure</th>
<th>Design 3a: Low Profile High Coverage</th>
<th>Design 3b: Modified Low Profile High Pressure</th>
</tr>
</thead>
<tbody>
<tr>
<td>$C$</td>
<td>3</td>
<td>3</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>$d$</td>
<td>$305 , \mu m$</td>
<td>$305 , \mu m$</td>
<td>$305 , \mu m$</td>
<td>$305 , \mu m$</td>
</tr>
<tr>
<td>$G_A$</td>
<td>25 GPa</td>
<td>25 GPa</td>
<td>25 GPa</td>
<td>25 GPa</td>
</tr>
<tr>
<td>$\eta$</td>
<td>0.9</td>
<td>0.9</td>
<td>0.9</td>
<td>0.9</td>
</tr>
<tr>
<td>$L_{S0}$</td>
<td>8 cm</td>
<td>8 cm</td>
<td>8 cm</td>
<td>8 cm</td>
</tr>
<tr>
<td>$n_a$</td>
<td>8</td>
<td>8</td>
<td>8</td>
<td>8</td>
</tr>
<tr>
<td>$r$</td>
<td>9.5 cm</td>
<td>9.5 cm</td>
<td>9.5 cm</td>
<td>9.5 cm</td>
</tr>
<tr>
<td>$w$</td>
<td>1 cm</td>
<td>1 cm</td>
<td>1 cm</td>
<td>1 cm</td>
</tr>
<tr>
<td>$E$</td>
<td>1.41 MPa</td>
<td>1.41 MPa</td>
<td>0.359 MPa</td>
<td>0.359 MPa</td>
</tr>
<tr>
<td>$t$</td>
<td>4.9 mm</td>
<td>4.9 mm</td>
<td>0.79 mm</td>
<td>0.79 mm</td>
</tr>
<tr>
<td>$L_{F0}$</td>
<td>13.4 cm</td>
<td>27.6 cm</td>
<td>27.6 cm</td>
<td>17.1 cm</td>
</tr>
<tr>
<td>$P_{Passive}$</td>
<td>77.6 kPa</td>
<td>0.25 kPa</td>
<td>0.01 kPa</td>
<td>1.85 kPa</td>
</tr>
<tr>
<td>$P_{Active}$</td>
<td>145.2 kPa</td>
<td>52.5 kPa</td>
<td>2.6 kPa</td>
<td>5.97 kPa</td>
</tr>
<tr>
<td>$\Delta X_{Activation}$</td>
<td>12.5 cm</td>
<td>19.8 cm</td>
<td>23.8 cm</td>
<td>23.5 cm</td>
</tr>
<tr>
<td>% Coverage</td>
<td>43%</td>
<td>79%</td>
<td>86%</td>
<td>68%</td>
</tr>
</tbody>
</table>
4.5.4 Tourniquet Design 4: BioSuit using Spandex

Finally, using the two-spring model we examine the design space of a thigh tourniquet specifically designed to meet the requirements for the BioSuit: active pressures $> 29.6$ kPa, and thickness $< 5$ mm. First, we examine this space using jumbo spandex (0.7 mm thickness) as the core material. For this design exercise, we allow SMA twinned length ($L_{S0}$), unstretched fabric length ($L_{F0}$), and material thickness ($t$, set by the number of fabric layers) to vary, and we fix the remaining eight parameters to the same values as the previous designs, which are summarized in Table 4.4. With these remaining parameters fixed, it is possible to produce counter-pressure contour plots (for both active and passive counter-pressure) for the full span of possible SMA/fabric length and material thickness values. We studied the following ranges for each parameter:

1. SMA twinned length ($L_{S0}$): 2-12 cm. Based on the assumption that the actuators de-twin to $\epsilon_i = 3$, meaning the tourniquet starting length is 4x their twinned length, 12 cm represents the approximate upper limit in terms of twinned length before the actuators either exceeded the thigh circumference (59.7 cm) when de-twinned, or no margin remained for passive fabric within the thigh circumference.

2. Unstretched fabric length ($L_{F0}$): 11.7-51.7 cm. These values are derived from the difference between the thigh circumference and the range of expected de-twinced lengths of the SMA actuators.

3. Thickness $t$: 0.7-4.9 mm. These values represent integer multiples of the known jumbo spandex thickness (from 1-7 layers), up to the design limit of 5 mm.

Active and passive counter-pressure contour plots spanning the complete domain of these three design variables are presented in Figure 4-16, Figure 4-17, and 4-18. Each plot pairing represents the active and passive pressure contours for a fixed number of spandex layers (resulting in seven plot pairs spread across the three figures), with X markers placed at conditions that are calculated to exceed 200% fabric
Figure 4-16: Active (left column) and passive (right column) counter-pressure contour maps for BioSuit thigh tourniquet made of 1-3 layer spandex. SMA twinned length ($L_{S0}$) and unstretched fabric length ($L_{F0}$) affect steady state pressure levels, for a fixed number of spandex layers. Active pressure contours highlight regions that meet or exceed BioSuit target counter-pressure levels. Conditions that violate fabric stretch limits, and designs that minimize passive pressure while meeting active pressure targets, are both identified.
Figure 4-17: Active (left column) and passive (right column) counter-pressure contour maps for BioSuit thigh tourniquet made of 4-6 layer spandex. SMA twinned length ($L_{S0}$) and unstretched fabric length ($L_{F0}$) affect steady state pressure levels, for a fixed number of spandex layers. Active pressure contours highlight regions that meet or exceed BioSuit target counter-pressure levels. Conditions that violate fabric stretch limits, and designs that minimize passive pressure while meeting active pressure targets, are both identified.
Figure 4-18: Active (left) and passive (right) counter-pressure contour maps for Bio-Suit thigh tourniquet made of 7 layers of spandex (the maximum number of layers before the thickness constraint is exceeded).

stretch length\(^1\). The first active pressure contour corresponds to the BioSuit MCP threshold (approximately 30 kPa). SMA and fabric length combinations outside this contour will not produce active pressures that meet BioSuit requirements. The contour representing exactly zero passive pressure is shown in a dashed line on each plot (corresponding to a 45° contour) - this represents the geometry where the sum of the unstretched fabric length and the de-twinned SMA length exactly match the limb circumference (representing the limit of the passive pressure model).

These plots provide a complete assessment of spandex-based MCP active tourniquets designed for the human thigh. We see that it is possible to achieve 30 kPa active pressure at fabric stretch percentages < 200% when using 2-7 spandex layers (no viable architectures meet these criteria for designs with 1 spandex layer). Multiple viable designs exist within this space, so final selection of an MCP architecture requires analysis of secondary criteria. These criteria may include: minimizing passive pressure; minimizing garment thickness; or maximizing fabric length / minimizing SMA length (to more fully cover the wearer). Designs that optimize each of these

\(^1\)These plots are similar to Figures 4-12 through Figures 4-15, with an added dimension representing varying SMA twinned length. A vertical slice of Figures 4-16 through 4-18 would produce a two-dimensional plot like those shown in the previous design exercise.
secondary criteria while maintaining compliance with the primary MCP criteria (active counter-pressure magnitude, and constraints on fabric stretch percentage and total fabric thickness) can be easily determined: for example, designs that meet active MCP requirements while minimizing passive pressures are identified with a blue square on each plot (these designs also correspond to those with the greatest fabric coverage when active). Percentage fabric coverage can then be traded-off against garment thickness, depending on specific application requirements.

4.5.5 Tourniquet Design 5: BioSuit using Neoprene

Finally, we repeated this analysis using identical parameters as the spandex BioSuit tourniquet, only using 0.79 mm neoprene as the core material (estimated modulus = 0.359 MPa). In this case, material thickness was allowed to vary from 0.79-4.74 mm (1-6 layers). Active and passive counter-pressure contour plots spanning the complete design space are presented in Figure 4-19 and Figure 4-20.

We see drastically different results when designing tourniquets with neoprene compared to spandex. First, for designs using \( \leq 4 \) neoprene layers, no designs achieve the minimum BioSuit counter-pressure magnitude without stretching \( > 200\% \) of the initial neoprene length. For systems using 5-6 neoprene layers, it is possible to achieve 30 kPa active pressures without overstrecthing the material, though it does require passive fabrics that are significantly shorter than the limb circumference (e.g., 19.69 cm unstretched length vs. 59.6 cm circumference) which significantly reduces the active fabric coverage. The negative consequences of such a design (e.g., very high passive pressures due to a large initial stretch) are partially mitigated by the significantly lower modulus of neoprene compared to spandex.

Additionally, these designs are considerably thicker than spandex designs, since at least 5 layers are required. Consequently, applications that require thin garments necessarily require higher elastic modulus materials to achieve their goal. Finally, to help visualize the differences in predicted counter-pressure performance between the neoprene and spandex designs, two sets of three-dimensional plots are included: contours of constant active pressure as a function of the three design variables (as
Figure 4-19: Active (left column) and passive (right column) counter-pressure contour maps for BioSuit thigh tourniquet made of 1-3 layer neoprene. SMA twinned length ($L_{S0}$) and unstretched fabric length ($L_{F0}$) affect steady state pressure levels, for a fixed number of neoprene layers. Active pressure contours highlight regions that meet or exceed BioSuit target counter-pressure levels. Conditions that violate fabric stretch limits, and designs that minimize passive pressure while meeting active pressure targets, are both identified.
Figure 4-20: Active (left column) and passive (right column) counter-pressure contour maps for BioSuit thigh tourniquet made of 4-6 layer neoprene. SMA twinned length ($L_{S0}$) and unstretched fabric length ($L_{F0}$) affect steady state pressure levels, for a fixed number of neoprene layers. Active pressure contours highlight regions that meet or exceed BioSuit target counter-pressure levels. Conditions that violate fabric stretch limits, and designs that minimize passive pressure while meeting active pressure targets, are both identified.
Figure 4-21 and Figure 4-22); active pressure as a function of material and SMA length, with contours of constant garment thickness displayed (Figure 4-23 and Figure 4-24).

4.6 Discussion

The two-spring model derived and discussed in this chapter provides active compression garment designers with the ability to tailor a prototype to meet a variety of performance requirements. Of the eleven parameters that determine passive pressure, active pressure, and activation stroke length, seven parameters related to SMA and system characteristics (C, d, η, GA, na, r, and w) are typically known a priori: optimized SMA parameters follow the analysis presented in Chapter 3; limb radius is dependent on the application; and the number of actuators per width is determined by local manufacturing and assembly capabilities (up to the theoretical limit). The remaining four parameters (E, LF0, LS0, and t) represent the design space that engineers can manipulate to set garment performance. A generalized design methodology using the two-spring is as follows:

1. Determine SMA actuator parameters based on individual performance tests (like those presented in Chapter 3).

2. Determine passive material behavior, including modulus E and elastic range (typically using a uniaxial tensile testing machine), to select candidates for design.

3. Determine range of remaining design parameters (t, LS0, and LF0) based on known de-twinning extensional strain ratios, material thickness limits based on design requirements, and targeted limb circumference.

4. Study the effect of varying t, LS0, and LF0 on active and passive counterpressures, as was done in the BioSuit design example in this chapter.
Figure 4-21: Three dimensional plot identifying contours of constant active pressure as a function of spandex length, SMA length, and number of spandex layers, for active thigh spandex tourniquet.
Figure 4-22: Three dimensional plot identifying contours of constant active pressure as a function of neoprene length, SMA length, and number of neoprene layers, for active thigh neoprene tourniquet.
Figure 4-23: Three dimensional plot presenting active pressure as a function of spandex and SMA length, with contours of constant garment thickness identified.
Figure 4-24: Three dimensional plot presenting active pressure as a function of neoprene and SMA length, with contours of constant garment thickness identified.
5. Select designs that meet active counter-pressure, passive counter-pressure, material thickness, and fabric coverage percentage specifications.

It is important to note that this model only considers the design case where the initial system length (i.e., the unstretched fabric added together with the SMA actuator length after detwinning) is less than the circumference of the object on which the garment is donned. It is possible to design systems where this initial system length exceeds the local circumference (resulting in either no initial fabric stretching, and no passive pressure, or incomplete coil actuator detwinning, if the passive fabric stretching produces tensions less than the necessary detwinning force). These systems will not follow the model presented in this chapter (as their geometries violate assumptions used in the derivation of the model), but an analogous model based on appropriate assumptions can be derived using a similar process presented in this chapter.

Further, the equations to predict passive equilibrium position, force, and pressure (equations (4.7), (4.9), and (4.10)), rely upon the assumption that actuators of spring index $C = 3$ and packing density $\eta = 0.9$ are used, resulting in a maximum irrecoverable strain $\epsilon_{\text{Si}} = 3$. This strain value sets the derived passive equilibrium conditions (i.e., it is assumed that $L_{\text{Si}} = 4L_{\text{S0}}$, and the actuator when de-twinned to its limit will provide infinite resistance to additional stretching), which is a simplification of the true physics of the system: the actual passive equilibrium position will vary slightly from this prediction, as the de-twinned SMA actuators act as very stiff (but not infinitely stiff) springs, and will stretch slightly when paired with stretched fabric. If actuators of different spring index or packing density are used, new passive equilibrium equations must be derived (active equilibrium equations are unaffected). Finally, the equations determining passive equilibrium conditions can be further developed to avoid these limitations, by modeling the de-twinned actuator as an additional linear system (rather than a completely inextensible element).

Regarding the passive materials used for tourniquet design and development, the two-spring model assumes the following: repeatable linearity within the prescribed stretch range (which was consistent with the uniaxial tensile tests conducted on both
neoprene and spandex up to 200%, though this value will undoubtedly change as different materials are explored); constant width, thickness, and modulus through this window; fabric creep and/or relaxation are negligible; and the material stretches uniformly (i.e., friction between the material and skin does not induce non-uniform fabric stretching). Finally, the four moduli values we calculated from the uniaxial instron tests are likely underestimates of the true values, due to our assumptions of constant width and thickness during stretching. We expect the model to similarly underestimate passive and active compression characteristics when using these calculated values.

Given this analysis, it is worth discussing what an “ideal” passive fabric would look like for the BioSuit or other MCP applications. We see that both neoprene and spandex are capable of meeting MCP target active counter-pressure levels with zero passive counter-pressure designs and within maximum thickness and fabric stretch constraints. Because of its higher modulus, spandex was able to meet these design requirements with fewer layers than neoprene. This suggests that materials with higher modulus will produce thinner, more streamlined suits that still achieve target counter-pressure requirements. However, thin designs come at the cost of reduced fabric coverage: the tension to produce active counter-pressure comes from extreme stretching of few material layers, instead of marginal stretching of several material layers. Therefore, in order to have sufficient margin to achieve this stretching, the fabric must be (relatively) short and the SMA actuators must be (relatively) long. This will leave the wearer more “exposed” than designs with longer starting fabrics, which in the case of space suit design, is a serious (i.e., life-threatening) consequence.

Based on these tradeoffs, an ideal MCP fabric would contain the following material characteristics:

1. High (i.e., > 1.5 MPa) modulus, and large linear region (i.e., linear to at least 200% stretch)

2. Minimized (i.e., < 0.7 mm) layer thickness, such that stacking layers to improve fabric coverage adds the least bulk
3. Characteristics conducive to wearer comfort (breathability, surface finish, etc.)

4. A plateau in stress-strain behavior once target tension has been achieved (as discussed by [17])

The study presented in this chapter demonstrates the versatility of compression garments using SMA coil actuators. We derived an analytic, predictive two-spring model (inspired by previous dynamic SMA system models [75]) that combines both SMA and fabric force-displacement equations [71,72]. Using this model, we present hybrid active-passive compression garment designs for a variety of applications based on strategically chosen system parameters, and for the first time demonstrate that simple SMA-passive fabric systems are capable of achieving BioSuit MCP target counter-pressures.
Chapter 5

Compression Textile Design using SMA Coil Actuators

Chapters 2-4 focused on the down-selection, development, and characterization of SMA actuators for use in wearable compression garments, and detailed modeling of such garments given a suite of eleven design parameters. In this chapter, the development and testing of several active compression garments is presented, and the results of these tests are analyzed to assess the validity of the hybrid SMA garment model.

5.1 Modular SMA Cartridge Development

A critical first step in developing active compression garments using SMA actuators is to design a packaging solution for the SMA actuators themselves. While individual actuators have been shown to produce sizable forces when exposed to an applied voltage, the magnitude of force required for MCP applications can only be achieved when several actuators are aligned in parallel. In such a configuration, it is advantageous to minimize the wasted space between actuators (i.e., to pack them as close together as possible) because this maximizes the total force produced per unit width. However, this introduces new design challenges: preventing the actuators from short-circuiting; sufficiently fixing the actuators in place to prevent structural failure (e.g., an actua-
tor breaking free) during activation (especially at high tension/pressure levels); and successfully mating the actuators to the passive fabric.

A cartridge-style SMA structure incorporating a singular, extended and de-twinned SMA coil into 3D-printed end caps and a central spacer, was developed to address this issue (see Figure 5-1 for an example of this design). In this configuration, one SMA spring is laced between two end caps and one central spacing element 12 times, resulting in an actuator with 12 effective parallel springs that are equally spaced. Because the cartridge is comprised of a singular actuator (instead of 12 individual actuators), both electrical conductivity and actuator structural integrity are guaranteed (i.e., the series circuit cannot be compromised unless the actuator wire breaks, and no actuators can individually pull free of the structure, barring failure of the wire or end cap structure itself). Further, given the flexibility in design of the 3D-printed end caps, a variety of designs are possible for mating the cartridge to the adjoining passive fabrics.

Two methods for manufacturing these cartridge actuators were developed based on current 3D printing capability (detailed instructions for each of these manufacturing methods, with pictures, are included in Appendix D):

1. Method 1: Single-plastic, fully embedded SMA cartridge. Using this method, the SMA actuator is fully encased in homogeneous ABS plastic in a single step, part-way through the 3D printing build phase. The 3D printed end caps and spacer are designed with narrow channels (i.e., smaller diameter than that of the SMA spring), and the spring is forcibly laced/embedded in these channels at a pause part-way through the build (requiring a manual tool, such as a flat-head screwdriver, to jam the spring into each channel to lock it in place). Once the actuator is embedded, the print is resumed, and several layers of ABS plastic are deposited over top the actuator, fully encasing the spring in place. This method was designed specifically to be used on a Stratasys Fortus 250mc printer (or functional equivalent).

2. Method 2: Multi-plastic, two piece SMA cartridge. Using this method, three
3D printed structures (two end cap channel insets, and a central spacer) are designed with wide channels (i.e., larger diameter than that of the SMA spring) and are prefabricated using a high temperature plastic (such as ULTEM® 9805\(^1\), using standard procedures on a Stratasys Fortus 400mc printer or functional equivalent). The SMA spring is then loosely laced through these pieces, producing an unfinished structure that resembles the final cartridge dimensions (but has no structural stability). The requirement for the channels to be of larger diameter than the spring is to ensure that it is possible to lace the spring through the finished channels (post-fabrication). With this structure in place, 3D printed ABS end cap superstructures with a strategically designed cavity that matches the shape of the ULTEM end cap insets are fabricated. As is done in the single-plastic method, the 3D print build is paused part-way through, and

\(^1\)http://www.stratasys.com/materials/fdm/ultem-9085
the unfinished SMA-ULTEM structure is inserted (with the ULTEM end cap inserts embedded in the ABS end cap cavities). Once this is completed, the build is resumed, and the ULTEM insets are encased in the ABS end caps, fixing the structure in place.

Examples of finished actuator cartridges using each of these methods are included as Figure 5-2 and Figure 5-3. While both methods produce functional actuators, they differ in terms of their relative structural stability, actuator spacing, and durability. Method 1, which uses homogeneous ABS plastic, produces a structurally superior and simpler actuator, due to several factors: first, the end caps are made in a single build and of a single material; second, the actuators are physically locked in place both by friction and as a result of being encased in plastic during the build; third, the structure is more resilient to cracking and other failures than the multi-plastic design; additionally, because the actuator can be embedded when the structure is incomplete (allowing for much narrower channels than would otherwise be feasible), the actuator spacing approaches the physical limit given the capabilities of modern 3D printers (i.e., in a 1” wide cartridge, assuming a minimum wall width of 0.03” between actuators, 12.65 actuators of our width are physically possible - we effectively match this limit in production). The multi-plastic cartridge, conversely, packs 12 actuators into a 1.485” width (a significantly worse packing density).

However, the single-plastic design was found to fail during SMA activation as a result of the ABS plastic exceeding its glass transition temperature \( T_g = 105 \, ^\circ C \), which is significantly lower than the SMA austenite finish temperature. An example of this type of cartridge failure is included as Figure 5-4. The multi-plastic design is not susceptible to thermally induced failure, as the ULTEM glass transition temperature \( T_g = 186 \, ^\circ C \) is greater than the SMA austenite finish temperature. In effect, the ULTEM end cap insets shield the ABS superstructure from high temperatures during activation, preventing thermally-induced structural failure. However, the thermal stability comes at the expense of certain structural stability: the ABS end cap superstructures in the multi-plastic cartridges were found to crack more easily than their single-plastic counterparts, due to stress concentrations at the ULTEM-ABS
Figure 5-2: Example of finished, single-plastic, fully embedded SMA cartridge actuator.

Figure 5-3: Example of finished, multi-plastic, two-piece SMA cartridge actuator.
Figure 5-4: Examples of thermally-induced failure in the single-plastic SMA cartridge (shown during and post-activation). As the SMAs heat during activation, the ABS plastic end cap enters its glass transition region, resulting in structural failure.

interface during activation.

An ideal cartridge design (i.e., a single-plastic cartridge made from a high temperature material such as ULTEM) would combine the structural stability and high packing density of the single-plastic design with the thermal stability of the multi-plastic design. At the time of this printing, however, it was not possible to implement the single-plastic method on 3D printers capable of printing with ULTEM, because the printing tray cannot be removed during pauses in those printers. As 3D printing technologies mature, this limitation may be resolved. For the prototypes presented in the remainder of this chapter, all SMA cartridges were produced using the multi-plastic method.
5.2 Active Tourniquet Counter-Pressure Testing

A series of active tourniquet prototypes were tested to judge the effectiveness of the SMA actuator cartridge design, and to assess the accuracy of the two-spring model developed in Chapter 4. The following sections describe these tests in detail.

5.2.1 Test Setup, Objectives and Test Matrix

Nine counter-pressure tests were conducted using five different tourniquet prototypes (spanning a variety of design parameters). Each tourniquet was comprised of a passive fabric section mated to a SMA actuator cartridge containing 12 SMA actuators. Each tourniquet was donned on the same PVC pipe used in Chapter 3 ($r = 5.715$ cm), and both passive and active pressure were measured as a function of time and spatial location after the application of a voltage step input. The tests were designed to address the following research questions:

1. What are the total passive and active pressures created by the active tourniquet, and how does this compare to the predictions of the analytic model?

2. Can the analytic model be used to predict performance, and can it be used to back-calculate unknown passive material properties?

3. What is the spatial/circumferential variability of the pressure produced?

4. Does performance change or degrade after repeat usage?

The parameters of each tourniquet test are outlined in Table 5.1. Tourniquets made from two materials (jumbo spandex, and a generic, previously uncharacterized spandex) were tested, with a variety of lengths, layers, and test modifications (and some tourniquets were used in multiple tests). Two SMA actuator cartridges ($L_{S0} = 1.905$ cm, de-twinned length = 7.62 cm cartridge for the generic spandex tests, and $L_{S0} = 2.06$ cm, de-twinned length = 8.26 cm cartridge for the jumbo spandex tests) were used. Consistent power settings ($20.3$ V, $0.61$ A, $12.383$ W, corresponding to
Figure 5-5: Top view schematics of two active tourniquet study test setups. The spandex tourniquet was donned on a rigid PVC pipe with the pressure sensor placed in two locations: exactly opposite the SMA cartridge (which we refer to as the “far-field”), and exactly underneath the SMA cartridge (which we refer to as the “near-field”), resulting in measurements over 84.9% of the total circumference. The pressure sensor contains 16 sensels across its length, numbered 1-16 as shown in the schematic.

the maximum power setting that can be accommodated by the SMA cartridge before structural failure) were used for each test except where otherwise noted.

Passive and active pressure data were collected using the same Novel Pliance pressure system as was described in Chapter 3. Two test configurations were used, and they are depicted in Figure 5-5. Each tourniquet was donned on the rigid pipe, and tests were conducted with the pressure sensor in two possible locations: first, centered exactly opposite the SMA actuator cartridge (defined as the “far-field” location); and second, centered exactly underneath the SMA actuator cartridge (defined as the “near-field” location). This was necessary because the sensor width is approximately half the PVC circumference, requiring two tests with different placements to capture the full circumferential pressure profile. The sensor mat contained 16 sensels across its length, and these sensels were numbered 1-16 and oriented as shown in the schematic. See Figure 5-6 for a depiction of this prototype in both the loose state and donned on the PVC pipe test stand, with the pressure sensor in the “far-field” location.

Additionally, some of the tests were modified to reduce the friction between the tourniquet cuff and the pipe/pressure sensor (e.g., petroleum jelly was liberally applied to the two contact surfaces). Tests where this modification was implemented
Table 5.1: Summary of test parameters for 9 active tourniquet characterization tests

<table>
<thead>
<tr>
<th>Test</th>
<th>Material</th>
<th>$L_F^0$ (cm)</th>
<th>Sample Number</th>
<th>Layers</th>
<th>Sensor Location</th>
<th>Modifications</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Jumbo Spandex</td>
<td>20.3</td>
<td>1</td>
<td>5</td>
<td>Far-field</td>
<td>None</td>
</tr>
<tr>
<td>2</td>
<td>Jumbo Spandex</td>
<td>21.6</td>
<td>1</td>
<td>5</td>
<td>Far-field</td>
<td>Reduced friction</td>
</tr>
<tr>
<td>3</td>
<td>Jumbo Spandex</td>
<td>22.3</td>
<td>2</td>
<td>5</td>
<td>Far-field</td>
<td>Reduced friction</td>
</tr>
<tr>
<td>4</td>
<td>Jumbo Spandex</td>
<td>22.9</td>
<td>3</td>
<td>7</td>
<td>Far-field</td>
<td>Reduced friction</td>
</tr>
<tr>
<td>5</td>
<td>Generic Spandex</td>
<td>21.4</td>
<td>4</td>
<td>5</td>
<td>Far-field</td>
<td>None</td>
</tr>
<tr>
<td>6</td>
<td>Generic Spandex</td>
<td>21.4</td>
<td>4</td>
<td>5</td>
<td>Near-field</td>
<td>None</td>
</tr>
<tr>
<td>7</td>
<td>Generic Spandex</td>
<td>21.4</td>
<td>4</td>
<td>5</td>
<td>Far-field</td>
<td>Reduced friction</td>
</tr>
<tr>
<td>8</td>
<td>Generic Spandex</td>
<td>22.9</td>
<td>5</td>
<td>7</td>
<td>Far-field</td>
<td>Reduced friction</td>
</tr>
<tr>
<td>9</td>
<td>Jumbo Spandex</td>
<td>21.6</td>
<td>1</td>
<td>5</td>
<td>1</td>
<td>Maximum power*</td>
</tr>
</tbody>
</table>

*Altered Cartridge

Figure 5-6: Example of prototype active tourniquet comprised of 5-layers or 7-layers of generic or jumbo spandex and SMA actuator cartridge used for pressure testing (loose tourniquet shown in top left, and front and back views when donned on the PVC pipe test stand shown in bottom left and right, respectively).
are identified in the test matrix, where appropriate.

5.2.2 Test Results

Tests 1-4: Jumbo Spandex Tourniquet Tests

Tests 1-4 were conducted using a single active tourniquet comprised of either 5 or 7 layers of jumbo spandex (of varying length), both with and without low-friction modifications. The design parameters of this tourniquet and the test setup are included in Table 5.2. The results of these tests are presented in two different forms. First, the average tourniquet pressure (i.e., the average of all sensels at a given timestep) is presented as a function of time, with 95% confidence intervals included. Passive and active pressure estimates are also presented, and were calculated based on experimentally estimated fabric moduli values. Second, average pressure vs. sensel location is presented for the steady state passive and active pressure conditions, with 95% confidence intervals included, along with the difference between active and passive pressures at each sensel location.

The data from Test 1 is included as Figure 5-7; the data from Test 2 is included as Figure 5-8; the data from Test 3 is included as Figure 5-9; and the data from Test 4 is included as Figure 5-10. Refer to Table 5.1 for detailed test parameters for each test.

Tests 5-8: Generic Spandex Tourniquet Tests

Tests 5-8 were conducted using a single active tourniquet comprised of either 5 or 7 layers of generic spandex (thickness = 0.7 mm, unknown modulus), of varying length, both with and without low friction modifications. The design parameters of this tourniquet and the test setup are included in Table 5.2.

The data is presented in the same fashion as Tests 1-4. The data from Test 5 is included as Figure 5-11; the data from Test 6 is included only as Figure 5-12 (global average data is not presented); the data from Test 7 is included as Figure 5-13; and the data from Test 8 is included as Figure 5-14. Refer again to Table 5.1 for detailed test
Figure 5-7: Test 1 results. Top: 5-layer jumbo spandex tourniquet average pressure vs. time for a 20.3 V voltage step input, with 95% confidence intervals. Predicted active and passive pressures are included based on estimated fabric modulus. Bottom: average pressure vs. sensel location for both active and passive steady state pressures, with 95% confidence intervals. Also calculated is the difference in values at each sensel location, corresponding to the increase in pressure attributable to SMA activation. An illustration of sensel number and “far-field” location is also provided.
Figure 5-8: Test 2 results. Top: 5-layer low friction jumbo spandex tourniquet average pressure vs. time for a 20.3 V voltage step input, with 95% confidence intervals. Predicted active and passive pressures are included based on estimated fabric modulus. Bottom: average pressure vs. sensel location for both active and passive steady state pressures, with 95% confidence intervals. Also calculated is the difference in values at each sensel location, corresponding to the increase in pressure attributable to SMA activation. An illustration of sensel number and “far-field” location is also provided.
Figure 5-9: Test 3 results. Top: extended length, 5-layer low friction jumbo spandex tourniquet average pressure vs. time for a 20.3 V voltage step input, with 95% confidence intervals. Predicted active and passive pressures are included based on estimated fabric modulus. Bottom: average pressure vs. sensel location for both active and passive steady state pressures, with 95% confidence intervals. Also calculated is the difference in values at each sensel location, corresponding to the increase in pressure attributable to SMA activation. An illustration of sensel number and “far-field” location is also provided.
Figure 5-10: Test 4 results. Top: extended length, 7-layer low friction jumbo spandex tourniquet average pressure vs. time for a 20.3 V voltage step input, with 95% confidence intervals. Predicted active and passive pressures are included based on estimated fabric modulus. Bottom: average pressure vs. sensel location for both active and passive steady state pressures, with 95% confidence intervals. Also calculated is the difference in values at each sensel location, corresponding to the increase in pressure attributable to SMA activation. An illustration of sensel number and “far-field” location is also provided.
Table 5.2: Summary of eleven design parameters for prototype 5-layer and 7-layer jumbo and generic spandex tourniquet prototypes used in Tests 1-8

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Tests 1-4</th>
<th>Tests 5-8</th>
</tr>
</thead>
<tbody>
<tr>
<td>Spring Index $C$</td>
<td>3</td>
<td>3</td>
</tr>
<tr>
<td>SMA Wire Diameter $d$</td>
<td>305 $\mu$m</td>
<td>305 $\mu$m</td>
</tr>
<tr>
<td>Fabric Modulus $E$</td>
<td>1.025 MPa</td>
<td>Unknown</td>
</tr>
<tr>
<td>SMA Shear Modulus $G_A$</td>
<td>7.5 GPa</td>
<td>7.5 GPa</td>
</tr>
<tr>
<td>Packing Density $\eta$</td>
<td>0.9</td>
<td>0.9</td>
</tr>
<tr>
<td>Unstretched Fabric Length $L_{F0}$</td>
<td>20.32-22.86 cm</td>
<td>21.4-22.9 cm</td>
</tr>
<tr>
<td>SMA Twinned Length $L_{S0}$</td>
<td>2.06 cm</td>
<td>1.905 cm</td>
</tr>
<tr>
<td>Number of Actuators $n_a$</td>
<td>12</td>
<td>12</td>
</tr>
<tr>
<td>Local Radius $r$</td>
<td>5.715 cm</td>
<td>5.715 cm</td>
</tr>
<tr>
<td>Fabric Thickness $t$</td>
<td>3.5 mm, 4.9 mm</td>
<td>3.5 mm, 4.9 mm</td>
</tr>
<tr>
<td>Fabric Width $w$</td>
<td>3.81 cm</td>
<td>3.81 cm</td>
</tr>
</tbody>
</table>

parameters for each test. Due to large errors stemming from fabric non-linearities, predicted active and passive pressures are not included for these tests (see section on model accuracy, validation, and limitations for further discussion).

Tests 9: Jumbo Spandex Maximum Power (Test-to-Failure)

Test 9 was conducted with one objective in mind: push the system to mechanical failure to assess both the maximum pressure possible given current manufacturing techniques and to identify failure modes of the system for future improvement. This test was conducted using a single, 5-layer jumbo spandex tourniquet with low friction modifications (this was the same passive tourniquet cuff from Tests 1-2). The same power settings (20.3 V, 0.61 A, 12.383 W) used in previous tests were first applied to the system at $t = 15$s; this power was increased at $t = 90$s to 30.0 V (0.9 A, 27 W). The data is presented in the same fashion as the previous tests in Figure 5-15.

Passive Fabric Modulus Assessment Post-Testing

Following the conclusion of the active pressure testing, passive fabric samples used during the tourniquet tests were re-tested using the same uniaxial tensile test protocol to assess any changes in their elastic modulus values (compared to their initial
Figure 5-11: Test 5 results. Top: 5-layer generic spandex tourniquet average pressure vs. time for a 20.3 V voltage step input, with 95% confidence intervals. Bottom: average pressure vs. sensel location for both active and passive steady state pressures, with 95% confidence intervals. Also calculated is the difference in values at each sensel location, corresponding to the increase in pressure attributable to SMA activation. An illustration of sensel number and “far-field” location is also provided.
Figure 5-12: Test 6 results: average pressure vs. sensel location for both active and passive steady state pressures, with 95% confidence intervals, with the pressure sensor placed directly under the SMA cartridge in the “near-field” location. Also calculated is the difference in values at each sensel location, corresponding to the increase in pressure attributable to SMA activation. An illustration of sensel number and “near-field” location is also provided. We clearly see the locations of the three SMA cartridge structures indicated by large pressure spikes (two end caps at locations 5 and 12, and center spacing structure at location 9), and the subsequently low pressures at the gaps between these structures (where the SMA actuators only slightly contact the surface). Post-activation, we see that the left SMA end cap (previously at location 5) shifted to location 6, resulting in large pressure deltas (i.e., negative delta at location 5, and positive delta at location 6). This is illustrated at the top of the figure.
Figure 5-13: Test 7 results. Top: low friction 5-layer generic spandex tourniquet average pressure vs. time for a 20.3 V voltage step input, with 95% confidence intervals. Bottom: average pressure vs. sensel location for both active and passive steady state pressures, with 95% confidence intervals. Also calculated is the difference in values at each sensel location, corresponding to the increase in pressure attributable to SMA activation. An illustration of sensel number and “far-field” location is also provided.
Figure 5-14: Test 8 results. Top: extended length, low friction 7-layer generic spandex tourniquet average pressure vs. time for a 20.3 V voltage step input, with 95% confidence intervals. Note that in this test, pressure decay is nearly zero after voltage is removed - this is because the force required to de-twin the SMA cartridge is nearly equal to the captured circumferential tension. Bottom: average pressure vs. sensel location for both active and passive steady state pressures, with 95% confidence intervals. Also calculated is the difference in values at each sensel location, corresponding to the increase in pressure attributable to SMA activation. An illustration of sensel number and “far-field” location is also provided.
Figure 5-15: Test 9 results. Top: low friction 5-layer jumbo spandex tourniquet average pressure vs. time for multiple voltage step inputs (20.3 V, 30.0 V), with 95% confidence intervals. The system was pushed to mechanical failure, which occurred at $t = 139\text{s}$, reaching a maximum pressure of 34.3 kPa. Bottom: average pressure vs. sensel location for both maximum (i.e., 30V condition) active and passive steady state pressures, with 95% confidence intervals. Also calculated is the difference in values at each sensel location, corresponding to the increase in pressure attributable to SMA activation. An illustration of sensel number and “far-field” location is also provided.
characterizations). These tests had two objectives:

1. To determine if the passive fabric modulus magnitudes changed as a result of testing, either due to breakdown in the elastic strength of the fiber elements (e.g., due to over-stretching) or due to changes in the effective modulus due to alterations in the fabric structure (e.g., due to stitching the layers together or due to cuts in the fabric needed to conjoin the cuff to the SMA cartridge).

2. To experimentally assess the modulus characteristics of the generic spandex material (which was previously untested).

The results of the jumbo spandex tensile tests are included as Figure 5-16. We see that for both the low- and high-strain regimes, the post-testing modulus data is lower than the initial, pre-testing data (by 21.5% and 16.7%, respectively). The discontinuity in modulus slope at approximately $\epsilon = 0.15$ is still observed. This indicates that the jumbo spandex material characteristics were affected during testing, though it does not provide details regarding the physical mechanism driving these changes - it may stem from losses in elasticity in the material itself, or from the cuts in the fabric that were necessary to attach the material to the SMA cartridge (see Figure 5-17 for an example of this phenomenon). Regardless of the source, we can expect a decrease in system performance commensurate with these measured losses for all tests that employ jumbo spandex as the base material.

The results of the generic spandex tensile tests are included as Figure 5-18. In this case, we observe large non-linearities in behavior for strains $\epsilon > 0.4$ (with modulus values increasing by approximately 800% from 0.197 MPa to 1.776 MPa at $\epsilon = 0.6$, and approximately 3000% to 6.116 MPa at $\epsilon = 0.9$). We also observe little change in modulus between the pre- and post-testing data (0.197 MPa vs. 0.198 MPa at $\epsilon < 0.4$, 1.776 MPa vs. 1.798 MPa at $0.6 < \epsilon < 0.8$, and 6.115 MPa vs. 6.173 MPa at $0.9 < \epsilon < 0.96$). This is validation that the generic spandex material modulus is largely unaffected by stretching (as the pre- and post-stretch data have the same modulus regimes). However, this also suggests that the system performance will change drastically if stretched beyond low extensional strains, leading to poor
Figure 5-16: Comparison of jumbo spandex elastic modulus pre- and post-testing. The previously observed discontinuity in modulus slope at approximately $\epsilon = 0.15$ still exists, though post-testing modulus estimates are consistently lower for both the low- and high-strain regimes (21.5% lower and 16.7% lower, respectively).

Figure 5-17: Changes in stretching behavior of passive fabric samples arising from two cuts in the edge of the fabric required for SMA cartridge donning. A tensioned jumbo spandex sample is shown (clamped in the tensile test machine), but this effect was observed in both the jumbo and generic spandex materials.
In both datasets, we see significant non-linear behavior that does not degrade/change between pre- and post-testing. From $\epsilon = 0$ to $\epsilon = 0.4$, we observe moduli values approximately 0.198 MPa; from $\epsilon = 0.6$ to $\epsilon = 0.8$, we observe moduli values approximately 1.78 MPa (an increase of approximately 800%); and from $\epsilon = 0.9$ to $\epsilon = 0.96$, we observe moduli values approximately 6.15 MPa (an increase of approximately 3000%).

agreement between experimental data and linear model predictions. Additionally, non-uniform stretching may lead to large variances in local pressure production.

### 5.3 Detailed Discussion of Test Results

These results can be analyzed across several dimensions. In general, the system dynamics are consistent with expectations and with the known voltage-force-displacement behavior of SMA actuators and hybrid garment systems (e.g., immediate rise in pressure after the voltage step input, equalizing to a steady state value, followed by a slow decay once power is removed). One test - Test 9 - experienced mechanical failure of the system, which was done intentionally to establish the mechanical limit of the
SMA cartridge design. We summarize the major findings and important implications for future system design in the following subsections.

5.3.1 Spatial Pressure Distribution

The variability in spatial pressure distribution can be assessed in three different ways:

1. The difference in pressure profiles between the “near-field” and “far-field”, relative to the SMA actuator cartridge. Tests 5-6, which vary the pressure sensor location with all else equal, can be used to directly analyze this contrast.

2. The variability in the static, steady-state spatial pressure distribution prior to activation for a given test setup in the “far-field”. Similarly, the variability in the static, steady-state spatial pressure distribution after actuation (and the difference between these two steady-state conditions) in the “far-field”. These contrasts can be analyzed using the pressure vs. sensel number plots for each test.

3. The effect of friction reducing agents (i.e., petroleum jelly between the contact surfaces) on the “far-field” pressure distributions. Comparisons between high- and low-friction tests can be conducted to assess this effect.

Variability in the near-field pressure distribution

The spatial distribution results from Test 6, with the pressure sensor centered directly beneath the SMA cartridge in the “near-field” (shown in Figure 5-12), are markedly different than all other spatial pressure distribution results. The spatial data taken with the pressure sensor opposite the SMA cartridge (in the “far-field”) all show similar characteristics: they are generally smooth distributions, albeit of varying magnitudes and variances, with universally positive pressure deltas between the passive and active states (indicating that counter-pressure universally increases in the “far-field” during activation, as expected). The data from the “near-field” from Test 6, however, differs in several regards.
First, we clearly see discontinuities in the data that correspond to the locations of the three SMA cartridge structures, indicated by large pressure spikes (two end caps at sensel locations 5 and 12, and the center spacing structure at sensel location 9). Not only do these structures produce significant point pressures – 41.9 kPa, 28.3 kPa, and 25.2 kPa passive pressures, and 47.1 kPa, 66.9 kPa, and 50.8 kPa active pressures – they also induce significant counter-pressure voids where the SMA actuators lie by holding the actuators away from the underlying surface. Additionally, we can actually see that the left SMA end cap (initially at sensel location 5) shifts to sensel location 6 during activation (as a result of the SMA cartridge activation stroke), resulting in large pressure deltas at both sensel location 5 (a negative delta as the end cap vacates this location) and location 6 (a positive delta as the cap enters this location).

Outside of the 3D printed structures (i.e., where the fabric mates to the cartridge), we see low (but progressively increasing) passive and active pressure as the distance from the cartridge increases. This relationship is expected, and is attributable to the fabric-cartridge fixture: the fabric is pulled away from the surface slightly near the edges of the cartridge (reducing the effective pressure felt by the sensor in the regions immediately adjacent to the cartridge) and this effect dissipates as the distance from the cartridge increases.

It is clear from this examination that this specific SMA cartridge design introduces significant and unavoidable variabilities in the spatial pressure distribution. Namely, it seriously skews a majority of the region in the “near-field”, creating over-pressures at the locations of its structures, and under-pressures both at the actuator locations and at the fabric locations immediately adjacent to the cartridge end caps. These discontinuities do not exist in the “far-field”, where we observe highly continuous pressure distributions. This implication is significant from a design perspective: it is critical that the footprint of the SMA cartridge be minimized to minimize this “near-field” effect. Additional modifications to the SMA cartridge design may prove helpful in minimizing this effect.
Variability in passive and active spatial distributions

To judge the variability in the passive and active spatial pressure distributions, normalized residuals were calculated at each sensel location for all “far-field” pressure tests (i.e., all tests except Test 6). These residuals (calculated as the difference between the steady-state pressure measurement at a given sensel and the average “far-field” tourniquet pressure for that test, normalized by the average pressure) enable us to visualize global spatial variance trends and to determine whether non-zero correlations exist between sensel number and passive and/or active counter-pressure. The results of this analysis are aggregated for all “far-field” tests, and are presented as Figure 5-19a-b, including best-fit estimates.

We see large spatial variability in the “far-field” passive pressure data (with normalized residuals ranging ±0.5), though this variability is uncorrelated with sensel number (i.e., the best fit line and coefficient are effectively zero). This is consistent with expectations, as the tourniquet in this condition is identical to any passive, homogeneous compression garment, so no spatial correlations should exist. The uncorrelated variability in the residuals likely stems from random variances in either the tourniquet construction or the initial conditions after donning (e.g., slight wrinkles in the fabric, inconsistent stretching, variability in either the tourniquet construction or the passive fabric properties, etc.). This consistent variability directly sets the size of the 95% confidence intervals of the steady-state passive pressure segments shown in each total tourniquet pressure vs. time dataset.

We see markedly different behavior in the “far-field” active pressure data. Significant spatial correlations exist between normalized active pressure residuals and sensel number, with sensels at the edges of the pressure sensor (i.e., sensels 1-3 and 14-16) experiencing higher than average pressures than sensels in the center of the pressure sensor. Further, this effect asymmetrically affects sensels 14-16 (suggesting that the edges of the “far-field” both experience higher pressures than the center of the field, and one edge of this field is more affected than the other).

To resolve the source of this variability, the pressure sensor itself was tested for
Figure 5-19: Aggregate normalized passive pressure residuals (top plot) and active pressure residuals (bottom plot) vs. sensel number for all “far-field” pressure tests, with best-fit estimates and $R^2$ values included.
accuracy and spatial variance (see Appendix G for a detailed discussion of these
tests): the sensor was found to be highly accurate (average measurement errors of
only 1.3% after correcting for errors in calibration) and to be free of large spatial
variability (±2σ < 12.7% for all tests). Eliminating pressure measurement error as
the source of observed spatial variability, clear physical explanations exist for each
of these findings that are best understood by considering the geometry of the test
setup and the relative locations of each numbered sensel. Sensels 1-3 and 14-16 are
relatively closer to the SMA cartridge than the remaining sensels (with sensels 14-16
wrapping around the left side of the PVC pipe when viewed from above, and sensels
1-3 wrapping around the right side - refer to Figure 5-5 for a visual explanation of
this setup). We know from the thin-walled hoop stress equation that pressure is a
function of the local tension, which implies that the tension at sensels 1-3 and 14-16
is higher than the tension at sensels 7-10. In fact, the data suggests a circumferential
tension gradient exists, with the greatest tension located at the junction of the SMA
cartridge and the passive fabric, and this tension progressively decreases along the
tourniquet length (with the minimum occurring exactly opposite the SMA cartridge).

The asymmetry between sensels 1-3 and 14-16 arises due to asymmetrical activa-
tion of the SMA cartridge itself. It was noted repeatedly during testing that the
SMA cartridge, when powered, tended to first contract near the end cap opposite
the voltage leads (i.e., from right to left in Figure 5-6). This phenomenon is shown
in detail in Figure D-3 in Appendix D. Given the relative orientation of the SMA
cartridge during these tests, sensels 14-16 were located nearest the region of active
stretching, resulting in a greater local pressure than sensels 1-3.

The spatial tension gradient, and associated spatial pressure discrepancy, is evi-
dence of non-uniform fabric stretching that we hypothesize stems from high friction
between the tourniquet and the underlying surface (i.e., the pressure mat, in this
instance). The actuator cartridge succeeded in inducing stretching of the passive
fabric during activation (and this was effectively transmitted to the immediate fabric
that attaches to the actuator), but this stretching did not equilibrate throughout the
full tourniquet circumference (resulting in non-uniform tension and pressure that is
highest in the regions immediately adjacent to the actuator cartridge).

Mitigating this spatial pressure variability likely requires a reduction in friction between the tourniquet and the underlying structure. This is a problem that is intrinsic to pressure garments: friction is a function of both normal force and the local coefficient of friction, and pressure garments by definition seek to apply large normal forces to the wearer (in order to impart pressure). Therefore, to mitigate high friction and therefore increase pressure uniformity while still maintaining large normal forces, the coefficient of friction must be reduced. This can be accomplished by either changing the surface finish of the passive fabric, or by introducing a lubricating agent (such as petroleum jelly) to encourage non-stick behavior. Alternatively, fabric tension uniformity may increase by introducing additional SMA cartridges, and evenly spacing these cartridges around the local circumference. However, as documented in previous analysis, this will also introduce significant pressure discontinuities in each cartridge “near-field”, likely leading to a net decrease in pressure spatial uniformity.

**Effect of low-friction modifications on pressure profiles**

Finally, to test the hypothesis that spatial variability in active pressure measurements stems from excessive friction between the tourniquet and the underlying surface, comparisons in spatial variability between high- and low-friction tests were conducted. Normalized residuals from the previous analysis were separated into two groups (i.e., tests with no modifications to the friction characteristics, and tests with petroleum jelly applied to the tourniquet and underlying surfaces). These modified residual plots are included as Figure 5-20a-b, including best-fit estimates for each residual group. Also presented in Figure 5-21 are representative spatial pressure maps demonstrating active pressure evolution for both high and low friction tests.

In these figures, we see evidence that friction modifications affect the tourniquet spatial pressure profile as judged by the aggregate normalized residual values and by the evolution of the spatial pressure map. While a slight effect is observed in the passive high-friction pressure data, its magnitude is low (± 0.1 across the full sensor length) and is likely exaggerated due to the small number of high-friction data
Figure 5-20: Aggregate normalized passive pressure residuals (top plot) and active pressure residuals (bottom plot) vs. sensel number for all “far-field” pressure tests, grouped into low-friction and high-friction categories, with best-fit estimates and $R^2$ values for each group included.
Figure 5-21: Example of “far-field” spatial pressure evolution presented for both high (left) and low (right) friction tourniquet tests (scale is included on the far right). High friction pressure tests show a severe and asymmetric pressure gradient between the edges and center of the sensor that increases as activation progresses. Final steady state active pressures are considerably non-uniform. Low friction pressure tests evolve in a more uniform fashion, with final steady state active pressure more evenly distributed.
Table 5.3: Statistical comparison between sensel groups both with and without friction.

<table>
<thead>
<tr>
<th>Sensel Groups</th>
<th>Data Subset</th>
<th>Significance [P-value]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1-3, 7-10, 14-16</td>
<td>All - Passive</td>
<td>0.897</td>
</tr>
<tr>
<td>1-3, 7-10, 14-16</td>
<td>All - Active</td>
<td>&lt; 0.001</td>
</tr>
<tr>
<td>1-3, 7-10, 14-16</td>
<td>High Friction - Passive</td>
<td>0.196</td>
</tr>
<tr>
<td>1-3, 7-10, 14-16</td>
<td>Low Friction - Passive</td>
<td>0.794</td>
</tr>
<tr>
<td>1-3, 7-10, 14-16</td>
<td>High Friction - Active</td>
<td>&lt; 0.001</td>
</tr>
<tr>
<td>1-3, 7-10, 14-16</td>
<td>Low Friction - Active</td>
<td>&lt; 0.001</td>
</tr>
</tbody>
</table>

points (only two tests were conducted in this group). No effect is seen in the passive low-friction pressure data. A greater effect is observed in the active pressure data: while pressure is still lowest at the center of the field, the asymmetry between sensels 1-3 and 14-16 is largely eliminated in the low-friction group, producing a spatially symmetric (though still variable) pressure field. This is evidence that the addition of petroleum jelly contributes to a more uniform pressure distribution, but is insufficient on its own to mitigate the full non-uniformity in tourniquet tension during activation. This contrast is well-illustrated when comparing the evolution of the high and low friction pressure maps shown in Figure 5-21.

Single-factor ANOVA calculations were performed on the normalized residual data to determine if statistically significant differences (\(\alpha = 0.05\)) exist between sensel groups 1-3, 7-10, and 14-16. The results of these analyses are included in Table 5.3. For all active pressure distributions, statistically significant differences were calculated between sensel groups at the edges and center of the sensor. No statistically significant differences were calculated between these groups for any passive pressure condition.

5.3.2 Model Accuracy, Validation, and Limitations

The validity and accuracy of the analytic pressure model derived in Chapter 4 was assessed using the average tourniquet pressure vs. time data that was presented for each test. When testing the spandex tourniquets, all input parameters were theoretically known (i.e., SMA parameters were known from geometry and characterization tests.
In Chapter 3; fabric parameters were known based on geometry and characterization tests in Chapters 4 and 5). With this information, passive and active pressure values were calculated using the analytic pressure model for each tourniquet test using both the pre-test and post-test modulus values, and these estimates were compared against the measured values.

In order to perform these calculations, two important modifications to the model and the dataset were necessary. First, a modification was made to both the final passive pressure equation (4.10) and the final active pressure equation (4.24) to account for the presence of the SMA cartridge structures. The end cap structures of the SMA cartridge occupy part of the total circumferential length, and part of these structures do not overlap with either the fabric or SMA actuators (and therefore should not be included as part of either linear system). This length is hereafter referred to as $L_{Cart}$, and is depicted graphically in Figure 5-22 (for the multi-plastic SMA cartridge design, $L_{Cart}$ was measured to be 0.66 cm for each end cap, totaling 1.32 cm). Analytically, this modification is accomplished by altering (4.10) and (4.24) as follows:

\[ P_{\text{Passive}} - \text{Modified} = \left( \frac{Et}{r} \right) \left( \frac{2\pi r - 4L_s - L_{Cart}}{L_F} \right) \]  
(5.1)

\[ P_{\text{Active}} - \text{Modified} = \left( \Delta X_{\text{System}} - L_{Cart} \right) \frac{G_A L F_0 + Ewt8C^3 \eta L S_0}{r(G_A L F_0 + Ewt8C^3 \eta L S_0)}; \]  
(5.2)

Second, when calculating the active and passive pressures using the analytic model, the lengths of each passive fabric cuff (listed in Table 5.1) were reduced by 1.27 cm (0.5 in) to account for the two sections of fabric (one on each end of the cuff) that were prevented from stretching due to the SMA cartridge donning configuration (again, see Figure 5-22 for a visual depiction of this quantity).

The results of the model validation assessment are included in Table 5.4 (for the jumbo spandex tests) and Table 5.5 (for the generic spandex tests). Included in these tables are the following parameters: initial (modified) fabric length; measured passive and active steady state pressure values; pre-test and post-test moduli estimates; predicted passive and active pressure values based on these moduli; and percentage...
Figure 5-22: Graphical depiction of the SMA cartridge length and unstretched fabric length that must be accounted for in the passive and active pressure analytic models (not to scale). The image depicts one side of the SMA cartridge; equivalent quantities also exist on the opposite side of the cartridge.
error in the model predictions. For both the generic and jumbo spandex tests, the low-strain moduli values were assumed (as initial $\epsilon_{Avg} = 0.28$ for these tests).

We see that for the jumbo spandex tests, the model performs well compared to the experimental values when corrected for the SMA cartridge and unstretched fabric lengths. For Tests 1-4 using the pre-test, low-strain modulus value (1.025 MPa), the model over-predicts the steady-state passive pressure value by an average of 24.7%, and it over-predicts the steady-state active pressure value by an average of 8.2%. Repeating these calculations using the post-test, low strain modulus value (0.805 MPa), we see average passive pressure errors improve to 4.1%, but the model under-predicts active pressure by 10.5%.

These errors are consistent with the experimentally determined modulus data and with expectations. We expect calculations using the post-test, low-strain jumbo spandex modulus (0.805 MPa) to accurately predict passive pressure performance (because the strain at this condition is low). We also expect that calculations using this modulus value will under-predict the active pressure condition, because fabric extensional strain increases as the system actuates, which drives the fabric to the high-strain modulus (1.375 MPa) regime (causing the model to under-predict pressures at high strains if the low-strain modulus value is used).

For Test 9, errors using the post-test modulus value were calculated to be -12.3% and -40.4%, respectively. This is again attributable to the additional stretching that occurred in that test due to the increased power applied to the SMA cartridge, causing the fabric to enter the high-modulus regime. By pure chance, the model performs well using the pre-test modulus value to predict active pressure (producing errors of only 11.8% and -12.3%, respectively); however, this is only because the effective fabric modulus in this instance happens to be similar to the pre-test modulus value (which is a chance occurrence). The magnitude of this effective modulus matches expectations, however, as one would expect the effective modulus for Test 9 to average somewhere between the low modulus (0.805 MPa) and high modulus (1.375 MPa) values.

For the generic spandex tests (Tests 5-8), we observe drastic differences between measured and predicted values. Effectively zero change in fabric modulus was mea-
Table 5.4: Summary of active tourniquet test results and model comparison based on pre- and post-testing low-strain moduli for the jumbo spandex tourniquet tests: measured passive and active tourniquet pressures compared against their predicted values. *Denotes initial length values. **Denotes increased SMA G value (25 GPa) to match increased power setting for Test 9.

<table>
<thead>
<tr>
<th>Test</th>
<th>L (cm)</th>
<th>F0 (%)</th>
<th>Measured Passive (kPa) ± Error</th>
<th>Measured Active (kPa) ± Error</th>
<th>Calculated Modulus (MPa)</th>
<th>Calculated Passive Error (%)</th>
<th>Calculated Active Error (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>19.05</td>
<td>18.6 ± 1.1</td>
<td>29.0 ± 5.7</td>
<td>1.025</td>
<td>24.3%</td>
<td>29.8 ± 2.7%</td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>20.35</td>
<td>14.3 ± 1.3</td>
<td>24.8 ± 3.0</td>
<td>1.025</td>
<td>18.5</td>
<td>2.2%</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>21.0</td>
<td>12.6 ± 0.9</td>
<td>21.9 ± 2.5</td>
<td>1.025</td>
<td>15.9</td>
<td>6.0%</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>21.59</td>
<td>13.0 ± 0.6</td>
<td>21.5 ± 1.7</td>
<td>1.025</td>
<td>19.3</td>
<td>21.8%</td>
<td></td>
</tr>
<tr>
<td><strong>Average:</strong></td>
<td>24.7%</td>
<td>8.2%</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Test</th>
<th>L (cm)</th>
<th>F0 (%)</th>
<th>Measured Passive (kPa) ± Error</th>
<th>Measured Active (kPa) ± Error</th>
<th>Calculated Modulus (MPa)</th>
<th>Calculated Passive Error (%)</th>
<th>Calculated Active Error (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>9**</td>
<td>20.35</td>
<td>16.3 ± 0.7</td>
<td>34.3 ± 4.2</td>
<td>1.025</td>
<td>18.5</td>
<td>11.8%</td>
<td></td>
</tr>
<tr>
<td><strong>Average:</strong></td>
<td>24.7%</td>
<td>8.2%</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**Footnote:** Power setting for Test 9, increased to match increased predicted values. Denotes increased SMA G value (25 GPa) to match increased power setting for the jumbo spandex tourniquet tests.
Table 5.5: Summary of active tourniquet test results and model comparison based on pre- and post-testing low-strain moduli values for the generic spandex tourniquet tests: measured passive and active tourniquet pressures compared against their predicted values (no predictions provided for the “near-field” test).*Denotes modified initial length values.

<table>
<thead>
<tr>
<th>Test</th>
<th>$L_{F0}^*$ (cm)</th>
<th>$P_{Passive}^*$ (kPa)</th>
<th>$P_{Active}^*$ (kPa)</th>
<th>Pre-Test Modulus (MPa)</th>
<th>Calculated $P_{Passive}$ (kPa)</th>
<th>Model Error (%)</th>
<th>Calculated $P_{Active}$ (kPa)</th>
<th>Model Error (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>5</td>
<td>20.13</td>
<td>16.7 ± 1.8</td>
<td>27.1 ± 5.8</td>
<td>0.197</td>
<td>4.1</td>
<td>-307.1%</td>
<td>6.4</td>
<td>-326.8%</td>
</tr>
<tr>
<td>7</td>
<td>20.13</td>
<td>8.3 ± 0.8</td>
<td>14.9 ± 2.7</td>
<td>0.197</td>
<td>4.1</td>
<td>-103.5%</td>
<td>6.4</td>
<td>-134.5%</td>
</tr>
<tr>
<td>8</td>
<td>21.59</td>
<td>4.2 ± 0.7</td>
<td>10.5 ± 1.0</td>
<td>0.197</td>
<td>4.2</td>
<td>0.6%</td>
<td>7.1</td>
<td>-47.8%</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Average:</td>
<td>-136.7%</td>
<td>Average: -169.7%</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Test</th>
<th>$L_{F0}^*$ (cm)</th>
<th>$P_{Passive}^*$ (kPa)</th>
<th>$P_{Active}^*$ (kPa)</th>
<th>Post-Test Modulus (MPa)</th>
<th>Calculated $P_{Passive}$ (kPa)</th>
<th>Model Error (%)</th>
<th>Calculated $P_{Active}$ (kPa)</th>
<th>Model Error (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>5</td>
<td>20.13</td>
<td>16.7 ± 1.8</td>
<td>27.1 ± 5.8</td>
<td>0.198</td>
<td>4.1</td>
<td>-305.1%</td>
<td>6.4</td>
<td>-324.8%</td>
</tr>
<tr>
<td>7</td>
<td>20.13</td>
<td>8.3 ± 0.8</td>
<td>14.9 ± 2.7</td>
<td>0.198</td>
<td>4.1</td>
<td>-102.6%</td>
<td>6.4</td>
<td>-133.4%</td>
</tr>
<tr>
<td>8</td>
<td>21.59</td>
<td>4.2 ± 0.7</td>
<td>10.5 ± 1.0</td>
<td>0.198</td>
<td>4.2</td>
<td>1.1%</td>
<td>7.1</td>
<td>-47.1%</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Average:</td>
<td>-135.5%</td>
<td>Average: -168.4%</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
sured between pre- and post-testing, however errors in passive and active pressure predictions averaged -136.7% and -169.7%, respectively. Further, these errors all under-predict the measured values, suggesting that unaccounted mechanisms are causing unexpected stiffness in the passive fabric (making the low-strain modulus estimate highly unrepresentative of actual system performance). While these discrepancies require further investigation, we speculate that three potential confounding mechanisms (non-linear modulus behavior coupled with localized non-uniform stretching, and restrictive stitching used to join the fabric layers) may contribute to this effect:

- We observe that the measured passive and active pressure drops off drastically between Tests 5 and 7 (which used the same fabric sample), and with Test 8, bringing the model predictions successively closer to the measured value. The test with the greatest predicted errors, Test 5, occurred without friction reducing agents. This test also produced the single-highest “far-field” sensel reading of any pressure test (61.4 kPa, at sensel 16), above both the other high friction test (Test 1, with maximum sensel pressure of 59.4 kPa) and the high-voltage pressure test (Test 9, with maximum sensel pressure of 58.4 kPa). This is especially noteworthy, given the low initial modulus of the generic spandex relative to the jumbo spandex (the latter of which was used for both Test 1 and Test 9).

- The high spatial variance in the Test 5 data suggests the high friction present during that test induced extreme localized non-uniformities in generic spandex stretching. We know that the generic spandex modulus becomes highly non-linear for extensional strains $\epsilon > 0.4$. Friction-induced non-uniform fabric stretching, combined with highly non-linear fabric modulus behavior, would cause a highly non-uniform spatial pressure distribution (with magnitudes greater than predicted by a linear model), exactly like we observed in the data. While this hypothesis needs further investigation, it explains both the unusually high sensel pressure reading observed in the data as well as the large discrepancy in the measured vs. predicted average pressure values.
Additionally, because each tourniquet cuff was comprised of several layers, the layers were loosely stitched together with a central seam to fix the group together (see Figure 5-23 for a detailed view of this configuration). This stitching was a non-issue during the jumbo spandex tests, because extensional strains were generally low in those tests (due to the high material modulus), so the stretching rarely exceeded the margin built into the stitching. The considerably lower initial modulus of the generic spandex, however, led to localized stretching that regularly exceeded the margin in the stitching during testing. Initially, this overstretching would result in increased stretch resistance (which would manifest as increased modulus) as the stitch resisted additional stretching; as stretching increased, however, the stitching was observed to break, which likely reduced the effective modulus for subsequent tests (and is a likely partial explanation for the lower pressure observed in Test 7, in addition to the reduced friction.

In general, the large non-linearity in the generic spandex modulus at relatively low extensional strains is the major hypothesized source of the large errors observed between the experimental and predicted data (which was generated by a model that assumes linear behavior). This reinforces the argument that passive material behavior needs to be well understood prior to designing systems using the linear model, as non-linear fabrics will perform much differently than expected.

This analysis demonstrates that model predictions are highly sensitive to fabric modulus dynamics: the model produces accurate results when describing linear fabric systems with a stable modulus, and produces large errors when non-linear fabrics are used (especially when non-uniform stretching occurs caused by high friction). Additionally, for some fabrics (like the jumbo spandex), modulus values vary over the lifetime of a given sample, and the predictive model increases in accuracy if this variance can be anticipated (and if modulus values can be modified accordingly). For high-performance compression garments, which sustain high circumferential tensions during use, material performance degradation (and subsequent changes in modulus)
Figure 5-23: Image of the stitch pattern used to fix the layers of each tourniquet cuff together. A broken stitch is highlighted, and this occurrence was repeatedly observed during tests using the generic spandex tourniquet.

may lead to losses in garment pressure production with prolonged usage. Even the donning process, which requires initial manual stretching of the material, may irreversibly affect the fabric material properties. Finally, due to the fundamental model assumption that circumferential tension is uniform during activation and that friction is negligible, the model does not capture the emergent spatial variability in the pressure data (neither the “near-field” spatial distortion caused by the SMA cartridge, or the “far-field” pressure gradient and spatial asymmetry).

5.3.3 System Repeatability

We know that both the SMA cartridge and some passive fabric cuffs are vulnerable to performance degradation during use (see Chapter 3 for a detailed discussion of SMA cyclic degradation, and the preceding section for a discussion of fabric modulus degradation). Therefore, we quantify system repeatability both in terms of total system performance degradation as well as judgment of individual changes in SMA and passive fabric characteristics.

Five fabric tourniquet samples were used during testing (see Figure 5.1 for a breakdown of tourniquet sample-test number combinations). Of the nine tests conducted,
Table 5.6: Summary of active tourniquet repeatability assessment

<table>
<thead>
<tr>
<th>Test</th>
<th>Material</th>
<th>$L_{F0}$ (cm)</th>
<th>Sample Number</th>
<th>Layers</th>
<th>$P_{\text{passive}}$ (kPa)</th>
<th>$P_{\text{active}}$ (kPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Jumbo Spandex</td>
<td>20.3</td>
<td>1</td>
<td>5</td>
<td>18.6 ± 1.1</td>
<td>29.0 ± 5.7</td>
</tr>
<tr>
<td>2</td>
<td>Jumbo Spandex</td>
<td>21.6</td>
<td>1</td>
<td>5</td>
<td>14.3 ± 1.3</td>
<td>24.8 ± 3.0</td>
</tr>
<tr>
<td>5</td>
<td>Generic Spandex</td>
<td>21.4</td>
<td>4</td>
<td>5</td>
<td>16.7 ± 1.8</td>
<td>27.1 ± 5.8</td>
</tr>
<tr>
<td>7</td>
<td>Generic Spandex</td>
<td>21.4</td>
<td>4</td>
<td>5</td>
<td>8.3 ± 0.8</td>
<td>14.9 ± 2.7</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>% Change:</th>
<th>% Change:</th>
</tr>
</thead>
<tbody>
<tr>
<td>6.4%</td>
<td>-23.3%</td>
</tr>
<tr>
<td>-14.5%</td>
<td></td>
</tr>
<tr>
<td>0.0%</td>
<td>-50.0%</td>
</tr>
<tr>
<td></td>
<td>-45.1%</td>
</tr>
</tbody>
</table>

Two pairings (Tests 1 and 2 using the first 5-layer jumbo spandex cuff, and Tests 5 and 7 using the first 5-layer generic spandex cuff) are perfect replications, with the exception of the use of petroleum jelly in each follow-up test. Changes in friction should have no effect on total passive or active pressure measurements for the jumbo spandex tests, because the material is linear (so any localized non-uniformities in stretching would simply linearly modify the pressures at those locations); however, these changes in friction may affect the generic spandex tests, due to the non-linear nature of the material. We can calculate proportional changes in system performance between repeat tests using data collected in Table 5.4. These calculations are included in Table 5.6, and identify several points of interest:

1. After a single test, the jumbo spandex sample irreversibly increases in length by 6.4%, and performance decreases by 23.3% (passive pressure) and 14.5% (active pressure), respectively. Variability in pressure measurements decrease from test 1 to test 2 (though this is attributable to the addition of petroleum jelly between tests). Assuming the post-testing modulus value is representative of the jumbo spandex material in these tests (and the small errors in model prediction shown in of Table 5.4 suggest this is reasonable), then the loss in performance can be attributed to the irrecoverable increase in fabric length that was observed between these tests.

2. The generic spandex sample did not change length between tests, but it did
experience an even greater decrease in pressure performance (-50.0% passive pressure loss, and -45.1% active pressure loss). This likely stems entirely from the change in friction between the two tests (as both sample length and modulus were shown to be stable). For a non-linear material, reduction in friction causing a decrease in non-uniform localized stretching would have large effects on active and passive pressures, just as is observed.

3. Isolating the source of these overall performance losses (between changes in SMA performance and changes in fabric performance) requires an assessment of the physical mechanisms that affect each performance parameter. The most instructive analysis for resolving this uncertainty is to specifically examine the changes in average passive pressure. We know that passive pressure is not affected by SMA parameters, as governed by (4.10) (except for starting SMA length, which did not change between tests). The fact that significant losses in passive pressure magnitudes were observed between repeat tests explicitly implicates changes in passive fabric properties/response as the source of this variability: either modulus decreased, thickness decreased, or unstretched fabric length increased (or some combination thereof). In the case of the jumbo spandex, the loss in performance is likely due entirely to irreversible stretching of the material. In the case of the generic spandex, the loss in performance is likely due to changes in friction characteristics leading to a non-linear change in response (due to the non-linear nature of the material).

4. It is unlikely that SMA cyclic degradation occurred between each test replication. SMA cyclic performance degradation was quantified in Chapter 3, and the actuators were found to only degrade between 9.1-23.1% over 60 cycles. The marginal change in SMA performance after only one additional cycle is unlikely to be of significant enough magnitude to cause the performance losses seen in this study.

In order to fully resolve and characterize system repeatability, additional testing of both the system and the individual passive fabric samples is necessary. However,
Table 5.7: Summary of percentage increase in pressure and associated settling time for each active tourniquet test.

<table>
<thead>
<tr>
<th>Test</th>
<th>Measured $P_{\text{passive}}$ (kPa)</th>
<th>Measured $P_{\text{active}}$ (kPa)</th>
<th>% Pressure Increase</th>
<th>Settling Time (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>18.6 ± 1.1</td>
<td>29.0 ± 5.7</td>
<td>55.5%</td>
<td>28</td>
</tr>
<tr>
<td>2</td>
<td>14.3 ± 1.3</td>
<td>24.8 ± 3.0</td>
<td>73.2%</td>
<td>26</td>
</tr>
<tr>
<td>3</td>
<td>12.6 ± 0.9</td>
<td>21.9 ± 2.5</td>
<td>73.7%</td>
<td>28</td>
</tr>
<tr>
<td>4</td>
<td>13.0 ± 0.6</td>
<td>21.5 ± 1.7</td>
<td>65.7%</td>
<td>25</td>
</tr>
<tr>
<td>5</td>
<td>16.7 ± 1.8</td>
<td>27.1 ± 5.8</td>
<td>62.7%</td>
<td>18</td>
</tr>
<tr>
<td>6</td>
<td>10.9 ± 5.5</td>
<td>18.0 ± 10.8</td>
<td>65.8%</td>
<td>11</td>
</tr>
<tr>
<td>7</td>
<td>8.3 ± 0.8</td>
<td>14.9 ± 2.7</td>
<td>78.7%</td>
<td>49</td>
</tr>
<tr>
<td>8</td>
<td>4.2 ± 0.7</td>
<td>10.5 ± 1.0</td>
<td>151.1%</td>
<td>19</td>
</tr>
<tr>
<td>9</td>
<td>16.3 ± 1.3</td>
<td>34.3 ± 4.2</td>
<td>110.3%</td>
<td>9</td>
</tr>
<tr>
<td></td>
<td>Average: 81.9% ± 19.8</td>
<td>23.7s ± 7.7</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

despite the limited number of pure system repetitions, analysis still suggests that the system degrades significantly between repeat tests, and that the source of this degradation depends heavily on the nature of the passive fabric being used (stemming from either irrecoverable stretching or changes in friction leading to non-linear changes in fabric modulus).

5.3.4 Analysis of SMA Pressure Augmentation

A final point of analysis is perhaps the most simple, but in many ways the most important: characterization of system performance. Based on each average tourniquet pressure vs. time dataset, we calculated the percentage increase in counter-pressure as a result of activation (i.e., the difference between steady state active and passive pressures, divided by passive pressure), and the associated settling time (defined as the time required for the powered system to achieve 90% of the active steady state value). The results of this analysis are included in Table 5.7.

We see the average percentage increase in pressure is 81.9% (with one test, Test 8, demonstrating a 151% increase in pressure), and the average settling time is 23.7 seconds. We also calculated the percentage increase in pressure during activation vs.
Figure 5-24: Percentage increase in pressure during activation vs. “far-field” sensel number, with 95% confidence intervals included.

“far-field” sensel number, and these results are included as Figure 5-24. Again, we see increases in pressure for all sensel locations, with the edge sensels experiencing the greatest average pressure increase. A single-factor ANOVA comparing sensel groups 1-3, 7-10, and 14-16, calculated statistically significant differences (p = 0.000) between these groups.

We know that these values (both the total average pressure and the average sensel pressure) are inherently dependent on the applied power (assuming the actuators have not fully transformed): if different power magnitudes are provided to the SMA actuator cartridge, these values will change until the actuator transformation limit is reached. For example, Test 9 demonstrates a significantly higher percentage pressure increase than Test 2 (110.3% vs. 73.2%) and a significantly lower settling time (9s vs. 26s) only because of the increased power settings used during that test. This analysis demonstrates the sizable performance increase attributable to the SMA cartridge system, and the relatively short timescale required to achieve full system activation.
5.3.5 Mechanical Limitations of SMA Cartridge

Test 9 demonstrated that a limiting factor in the magnitude of active pressures achievable by these hybrid tourniquet prototypes is the structural stability of the SMA actuator cartridge. For a given test, increased power can be applied to increase pressure response (up until full transformation is achieved), but this comes at the cost of increased mechanical stresses and the associated increase in risk of mechanical failure. Two designs were previously presented for manufacturing SMA cartridges for use in these systems, one with high structural stability but low thermal stability (the single plastic method), and one with high thermal stability but lower structural stability (the multi-plastic method). All tourniquets tested in this chapter employed a multi-plastic SMA cartridge, and with the exception of Test 9, the applied power was intentionally capped to preserve mechanical stability (at the expense of increased activation performance).

The tourniquet tested with maximum power in Test 9 achieved 115.9% of the target MCP counter-pressure prior to structural failure (34.3 kPa average pressure vs. 29.6 kPa target pressure). The multi-plastic cartridge failed because the stresses generated during activation caused one ULTEM inset to buckle mid-way through its length, which in turn caused the inset and actuators to pull away from the ABS end cap. This is shown in detail in Figure 5-25.

Both the SMA analytic model and the results from Test 9 demonstrate that MCP-target active counter-pressures are fully achievable using even simple spandex tourniquets. However, improvements in structural stability of the SMA cartridge are clearly necessary to withstand the high-power and high-circumferential tensions associated with these counter-pressure targets. Refinement of the cartridge design and manufacturing methods to better achieve these goals are recommended for future work.
Figure 5-25: Structural failure of multi-plastic SMA cartridge at high power, high tension/pressure condition. The cartridge failed because the ULTEM inset buckled mid-way through its length, causing the inset and actuators to pull away from the ABS end cap.
5.4 Discussion of Alternative Active Garment Concepts using SMAs

While the majority of this research effort focused on the development of hybrid compression garments that combine SMA actuator cartridges with passive fabric in tourniquet-style architectures, several other active compression garment concepts exist that utilize SMA actuators that are suitable for future research (see Appendix A for a detailed discussion of textile structures and viable textile-active material combinations). In the following subsections we briefly describe these alternative architectures.

5.4.1 Hybrid SMA Biaxial Braid

Biaxial braids, which were briefly discussed in Chapter 2, are cylindrical textile structures that naturally shrink in radius as their length is increased, and vice-versa (see Appendix E for details on biaxial braid dynamics). These structures are used in many devices, ranging from pneumatic muscles (known as McKibben muscles) to finger trap toys to soft robotics [67, 71, 96–98]. Because of their unique length-radius relationship, biaxial braids are uniquely suited for active compression garment design: soft actuators (such as SMA spring actuators) aligned longitudinally can indirectly drive changes in braid radius simply by shrinking/extending the braid length. This means that garments that actively constrict/expand their radius (which would produce an ideal compression garment) can be easily created by pairing SMA actuators with a passive braid structure. This operating principle forms the basis for the meshworm robot developed by Seok et al. [71].

This approach to compression garment design differs from the homogeneous SMA braid prototypes presented in Chapter 2 (i.e., the hybrid braid itself is comprised of passive materials, rather than SMA wire like the homogeneous braid) and actuation is achieved by stimulating SMA coil actuators strategically attached to the structure (rather than by stimulating the braid itself, if it were to be made purely from SMA wire). This ensures that sufficiently large active forces and displacements can be pro-
Figure 5-26: Example of the lines of non-extension (LoNE) as investigated by Iberall in the 1970s (left), and a geometric illustration of the LoNE concept [12,17,99,100].

duced (since coil actuators are driving the activation, instead of basic SMA wire that does not significantly shrink during activation [1]). Future work in this area includes characterizing and generalizing the longitudinal force-pressure relationship of a given braid structure (based on material properties, thread count, resting length/radius, etc.) then designing architectures to meet specific geometry and force/pressure targets.

5.4.2 Lines of Non-Extension (LoNE) Integration

The mobility of full-body compression suits can be improved by strategically designing the suit to exploit the skin’s natural lines of non-extension (LoNE) [12,17,99,100]. These lines represent contours on the human body that do not change length during natural motion (meaning as the skin stretches and deforms during movement, no tension or compression forces act along these specific contours). This concept is illustrated in Figure 5-26 [17,99]. In particular, the BioSuit design calls for integrated elements aligned with LoNE contours to provide wiring and pressure production capabilities that do not interfere with the mobility of the wearer.

SMA coil actuators are particularly well suited for integration in the BioSuit as
part of a design that exploits the LoNE geometry. Two possible architectures are en-
visioned towards this goal. First, the SMA coil actuators themselves could be aligned
along the LoNE contours, providing variable tension based on an applied current that
could be transmitted to the surrounding material to produce pressure (similar to the
prototype sliding tourniquet design presented in Chapter 3). Alternatively, rigid and
conductive cabling aligned along the LoNE contours could be used as the electrical
conduit/structural backbone for circumferentially-aligned SMA actuators designed
to directly tension the surrounding BioSuit material (similar to the tourniquet cuff
designs presented in Chapters 4 and 5).

The goal of developing a full-body MCP suit for future planetary exploration
will require a detailed consideration of jointed compression garment design (which
exceeds the scope of this study). The LoNE contours provide an appealing map for
SMA integration, and these research avenues hold great future potential.

5.4.3 SMA Composite Fibers

An additional concept for future active compression textile development combines low
spring index SMA coil actuators with a passive elastic matrix to produce composite
SMA fibers that are suitable for inclusion in a variety of architectures. This passive
matrix would provide electrical and thermal isolation for the SMA actuators, as well
as provide a bias/restoring force that enables two-way activation and a more stable
textile form factor (i.e., forming the actuators into cylindrical fibers instead of helical
coils). An illustration of this concept is included as Figure 5-27.

Such an architecture has several benefits: the passive matrix would shield each
SMA coil actuator both from the wearer (which enhances user comfort and safety)
and from other actuators (providing better control and reducing thermal or electrical
leakage); the composite fibers could be more easily integrated into traditional textile
structures (because cylindrical-type fibers are better suited for textiles than helical
springs), combining a streamlined textile design with the performance enhancements
that SMA coil actuators offer; and architectures with composite SMA fibers could
produce dynamically controllable garments with two-way activation capabilities.
Figure 5-27: SMA composite fiber concept combining low spring index SMA coil actuators with a passive elastic matrix to produce fibers with favorable textile properties for future active compression garment design.

Two preliminary attempts at prototyping SMA composite fibers were completed, and are outlined in Figure 5-28. The first prototyping method involved casting the SMA actuators in soft resin. A teflon mold matching the outer diameter of the SMA actuators was created, and Shore 10A “Dragon-Skin” silicone resin [Smooth-On, Inc.]² was used to cast a composite actuator, producing a solid composite fiber. The second prototyping method involved sheathing the SMA actuators in Polyolefin heat shrink tubing, then baking the actuators to shrink the coating around the coil structure. This method produces a hollow composite fiber.

Simple and preliminary testing of these fibers was conducted, and these tests are described in Figure 5-29. The resin cast fibers successfully actuated with an applied voltage, though the resin was too weak to restore (i.e., de-twin) the actuator after contraction. The elastomer matrix remained intact through the full activation and manual de-twinning cycle, proving that a composite fiber can structurally survive both contraction and expansion of constituent SMA actuator. The externally coated fibers failed to contract when activated (i.e., they were blocked by the Polyolefin tubing structure, which was overly rigid despite claims of flexibility from the manufacturer). After excessive heating, the tubing melted, which enabled the actuator to contract, but this irreversibly damaged the coating structure.

These tests were not meant to fully assess or validate the composite fiber concept, and further work is necessary to understand and optimize these fiber prototypes (as well as to develop and test any potential woven or braided garments that could be made from the fibers). The resin casting method proved more successful in initial

Figure 5-28: Two manufacturing methods for producing SMA coil composite fibers. The resin casting method (left) produces solid actuators by casting the de-twinned helical coils in soft silicone. The external coating method (right) produces hollow actuators by heat-shrinking tube structures around the de-twinned coil structure.
Figure 5-29: Resin cast fiber (left) and externally coated fiber (right) activation tests. The resin cast fiber successfully actuated without damaging the resin matrix, but did not spring back when de-activated (because the elastic resin was too weak to overcome the de-twinning force). The externally coated fiber was incapable of contracting during activation until it overheated and melted the external coating.
testing than the external coating method, though again further research into the optimal passive matrix materials is necessary.

5.5 Performance Predictions for Soft Surfaces

For ease of testing (and for repeatability purposes), the tourniquet tests conducted in this chapter used a PVC pipe in place of a human limb. This pipe, which is perfectly circular and rigid, differs significantly than an actual human limb (which is neither regularly shaped nor made from a homogeneous, rigid material). Consequently, it is necessary to assess the effect that these differences will have on system performance when donned on an actual human (or otherwise soft) object. It is assumed that these differences will have two effects on the data:

1. If the friction coefficient of human skin differs significantly from the friction coefficient of the PVC pipe, this difference will affect the spatial variability of the passive and active pressure profiles. The friction coefficient of human skin varies, with sources reporting that a typical coefficient of friction for human skin falls in the range of $0.12 < \mu < 0.34$, depending on anatomical region (averaging approximately $\mu = 0.21$) [101]. The coefficient of friction for PVC is reported\(^3\) to be $\mu = 0.2-0.3$. These values are comparable to one another (so the effect is likely minimal), but without a detailed experiment, we cannot quantify the changes in spatial pressure distribution due to differences in coefficients of friction.

2. The difference in rigidity between PVC and human tissue will affect both the total pressure and the spatial pressure distribution. Depending on the bodily location of the active tourniquet, the nature of the underlying tissue may vary wildly: for example, if placed on the front of the shin, where the bone sits just beneath the surface, the system dynamics will differ from a similar test conducted on the upper thigh, where thick muscle tissue exists around the full

circumference. Young’s modulus values for human tissue vary: muscle tissue
Young’s modulus averages between 1-3 kPa [102], which differs greatly from dry
collagen (6 GPa) and bone (5-21 GPa)\(^4\). The Young’s modulus of PVC, on the
other hand, varies between 2-4 GPa\(^5\), which is similar to bone. We therefore
expect a significant change in system dynamics if the tourniquet system is tested
over soft tissue regions (with applied pressure causing a decrease in radius,
causing a decrease in transient tension and subsequent SMA contraction, and
this process continuing until equilibrium is eventually reached). This will likely
require a dynamic control system with closed-loop feedback to monitor forces,
radius, and compression magnitudes during activation. We expect less of an
effect if the region is more bony (like the front of the shin), as pressure will not
affect the underlying radius.

Quantifying this effect was beyond the scope of this study; however, if an effec-
tive modulus value can be determined for each body location (perhaps representing a
weighted average of moduli values reflecting the relative proportion of soft and hard
tissue at that location), then we can calculate the relationships between applied pres-
sure, effective tissue modulus, and effective limb radius (i.e., as pressure is applied to
a soft, circular object, the effective radius will decrease until an equilibrium position
is reached that balances the tissue elastic response and the applied pressure). These
relationships for effective radius could be incorporated into the two-spring analytic
model (treating radius as a variable quantity based on pressure and effective limb
modulus, rather than as a static quantity) to predict passive and active pressures for
soft surfaces. However, this will be further complicated by the fact that human limbs
are not perfect cylinders, and the radius of curvature varies both circumferentially
and longitudinally, posing additional problems for MCP systems.

\(^4\)http://www.doitpoms.ac.uk/tlplib/bones/bone_mechanical.php
\(^5\)http://www.engineeringtoolbox.com/young-modulus-d_417.html
5.6 Summary

The active tourniquet tests conducted in this chapter provide significant insight into future active compression garment design. The hybrid systems developed for these tests exhibited considerable performance increases as a result of the novel SMA actuator cartridge system, and it was demonstrated that this increase in performance can achieve MCP target pressures and can be triggered and completed in less than 60 seconds. To the best of our knowledge, these tests represent the first widespread testing of a controllable, active compression garment that uses integrated SMA coil actuators, and their successful performance provides credibility to the claim that SMA-based active garments are well suited for compression garment applications, including MCP space suit design.

However, several limitations in performance were identified that warrant further research. The analytic model used to predict garment behavior relies upon fabric properties (length, modulus) that were demonstrated to vary significantly over the lifetime of the jumbo spandex tourniquet prototypes. Non-linear fabrics like the generic spandex, especially when tested with high friction, produce pressures that are largely different than predicted values (resulting in large modeling errors). Understanding the dynamics and cyclic performance losses of candidate compression garment fabrics are critical future tasks for system design and optimization. Progress on these topics will improve both garment performance (e.g., leading to greater sustained active pressures) and prototype repeatability (e.g., minimizing losses between repeat tests).

Additionally, significant spatial variability in both passive and active pressure profiles were observed in each test, and this variability improved (but was not eliminated) by greasing the tourniquet and the underlying surface with petroleum jelly. Further work is needed to fully understand the mechanism leading to this spatial variability, and to engineer solutions to better equalize the circumferential tension and pressure. This is especially problematic when working with highly non-linear fabrics, like the generic spandex, as it leads to large deviations between experimental and predicted
performance. Similarly, the SMA cartridge system severely distorts the “near-field” pressure field (resulting in large pressure spikes at the cartridge structure locations, and large pressure voids at the SMA spring locations). Further development of the SMA cartridge design is necessary to address these issues.

Finally, while the SMA cartridges produced in this chapter were fabricated using a novel 3D printed design, additional refinement in cartridge design is necessary to further optimize system performance. Each of the two cartridge designs (the single-plastic ABS design, and the multi-plastic ABS-ULTEM design) suffered failures during testing (thermally-induced failure in the ABS cartridge, and mechanical failure in the ABS-ULTEM cartridge). These problems can be resolved through refinement of the cartridge design and fabrication methods. An additional and important modification to the cartridge design is to introduce a locking mechanism: with such a mechanism, the added tension and pressure that is achieved through cartridge activation can be locked into place after activation is complete, enabling the system to benefit from the cartridge system without requiring it to be continually powered. This type of design is particularly important for MCP space suit applications, both because available power is limited and because it guarantees astronaut survival in the event of power loss.
6.1 Discussion of Key Findings

In Chapters 2-5, we described a series of four related studies that collectively investigated the topic of mechanical counter-pressure (MCP) space suit design using active materials. We summarize and discuss these studies, as well as their key findings, in the following subsections.

6.1.1 Summary of Materials Analysis - Chapter 2

In Chapter 2, we presented a review of the current state of active material technologies. This included nine different materials, ranging from dielectric elastomer actuators (DEAs) to ferroelectric polymers, shape memory alloys (SMAs), and shape memory polymers (SMPs). Baseline MCP requirements were established, and each active material was judged for potential MCP use based on published performance metrics (e.g., active stress, active strain, etc.). Three materials (DEAs, SMPs, and SMAs) met the minimum performance requirements, and initial investigations into each of these materials were conducted.

DEAs were found to suffer from durability issues (both because of the extremely thin geometries required and the nature of the acrylic elastomer backbone); SMPs were found to suffer from viscoelastic effects that severely limit their ability to main-
tain active stresses; SMAs, as homogeneous textiles, were found to be too weak to produce active stresses and strains to achieve MCP targets; and SMAs, in linear actuator form to maximize active forces and displacements, were found to hold the most promise for active compression garment integration.

6.1.2 Summary of SMA Actuator Development - Chapter 3

In Chapter 3, we presented an end-to-end study of low spring index SMA coil actuators optimized for compression garment design, including: actuator concept; design and parameter selection; force, thermal and pressure modeling; manufacturing methods; fundamental characterizations (calorimetry, de-twinning, active blocking force, path dependence, force-voltage, cyclic performance, and linearity); and first-generation active tourniquet prototyping and performance testing.

Beginning with existing models predicting SMA spring force-displacement behavior, three parameters (packing density, \( \eta \), extensional strain, \( \epsilon \), and spring index, \( C \)) were introduced, and a simplistic pressure model was developed to predict SMA compression garment behavior. Minimum spring index (i.e., \( C = 3 \)) actuators were determined to produce the greatest force per unit width (which is ideal for MCP garment design), and such actuators were found to be easily manufacturable using published methods [71, 74]. Both annealing and starting martensite state were shown to affect critical activation temperatures, and actuators were found to be highly linear at extensional strains \( \epsilon \leq 1.77 \). Cyclic effects were found to be most significant when actuators experience significant de-twinning between cycles. The first-generation active tourniquet demonstrated controllable compression capabilities that were consistent with the derived pressure model.

6.1.3 Summary of Analytic Pressure Modeling - Chapter 4

In Chapter 4, a complete analytic model was derived to predict hybrid compression garment performance (i.e., passive fabrics connected in series with SMA coil actuators). This model, which assumes both the SMA actuators and the passive fabrics
can be modeled as linear spring systems, predicts both passive and active pressures based on eleven design parameters (spring index, $C$; SMA wire diameter, $d$; fabric modulus, $E$; packing density, $\eta$; SMA shear modulus, $G_A$; unstretched fabric length, $L_{F0}$; twinned SMA length, $L_{S0}$; number of actuators, $n_a$; limb radius, $r$; fabric thickness, $t$; and fabric width, $w$). Several passive fabric samples were tested to assess the linearity assumption: two samples (spandex and neoprene) were found to be highly linear up to 200% stretch, while two other samples (BioSkin and flat polyester elastics) were found to be largely nonlinear. A series of design exercises was presented using the derived model to demonstrate the range of possible garment performance outcomes based on strategically chosen SMA and material parameters.

In particular, a detailed design study was presented that focused on parameter selection for BioSuit MCP applications. An active thigh compression tourniquet design was analyzed based on combinations of three design variables ($L_{F0}$, $L_{S0}$, and $t$) using both neoprene and spandex as the base material. Material thickness was allowed to vary (by varying the number of material layers used) such that total garment thickness did not exceed MCP design limits ($t \leq 5$ mm). Garment designs producing active pressures $> 30$ kPa with zero passive pressure were identified for spandex designs with 2-7 layers, and for designs with 5-6 neoprene layers. These design studies demonstrated that compression garments comprised of simple materials and SMA low spring index actuators are capable of achieving both MCP space suit target counterpressures and a variety of secondary design goals (depending on parameter selection).

### 6.1.4 Summary of Active Garment Prototyping - Chapter 5

In Chapter 5, two novel methodologies for producing modular SMA actuator cartridges using 3D printed materials were developed and presented. These cartridges approach the physical limit in terms of the number of actuators that can be feasibly packed into a given width in a single layer, and are designed specifically to be mated with passive fabrics for active compression garment prototyping. Using these cartridges, five active tourniquet prototypes were produced (made from either jumbo spandex or generic spandex, and with varying lengths, layers, and friction characteris-
tics), and nine activation tests were conducted to assess their performance (including their average passive and active pressures, and the variability in the spatial pressure profile).

The results of the activation tests demonstrate that the hybrid tourniquet designs are highly effective (i.e., the average “far-field” pressure increased by an average of 81.9% when the SMA cartridge was activated), taking an average of only 23.7 seconds to reach steady-state activation. Limitations in passive fabric performance (non-linear modulus behavior, irrecoverable strain, and modulus creep) were observed during testing, and these limitations affected the accuracy of the analytic model derived in Chapter 4. Further, these limitations in passive fabric performance both limited the total pressure magnitudes that were achieved by the tourniquet prototypes and decreased the repeatability of the prototype systems. In spite of these limitations, though, one high-power tourniquet test produced average active pressures reaching 115.9% of the target MCP design pressure.

No variances in “far-field” pressure were observed when analyzing the passive spatial pressure profile; however significant (and asymmetric) active pressure spatial variances were observed. It is believed that these variances arise due to high friction between the tourniquet and the underlying surface (and to a lesser extent, to the asymmetric activation nature of the SMA cartridge itself). Modifications to the test setup to reduce tourniquet friction successfully reduced (but failed to fully eliminate) this spatial variance. Pressure distributions in the “near-field” were distorted by the influence of the SMA cartridge itself: over-pressures were observed beneath the cartridge structures, and under-pressures were observed beneath the actuators themselves. Opportunities to refine the SMA cartridge design were identified that will further enhance active garment performance.

6.2 Hypotheses Assessment

Given the results of these studies, we assess our initial hypotheses as follows:

Hypothesis 1: Counter-pressure levels of 29.6 kPa (4.3 psi) can be repeatedly and
uniformly achieved (i.e., deviations no greater than ±10%) using a combination of passive elastics and one or more active materials integrated into a wearable garment. 

Assessment: Partially validated. Active SMA compression tourniquets produced average active pressures up to 34.3 kPa (representing 115.9% of the target pressure goal), with pressures greater than 60 kPa measured at individual sensel locations during activation testing (despite significant and unexpected limitations in passive fabric performance). However, large (i.e., >> 10%) spatial variability was observed in all tests, caused both by excessive friction between the garment and the underlying surface and by local distortions caused by the SMA actuator cartridge architecture. Modeling of SMA-based active compression garments suggest that uniform 29.6 kPa counter-pressures are well within reach given improvements in passive material performance, reductions in garment friction, and improvements in SMA cartridge design.

Hypothesis 2: This garment can be donned and doffed in less than 10 minutes.

Assessment: Validated. The SMA actuators produced in this research effort activate on the order of seconds, and settling times across multiple active prototype tests (defined as the time required to achieve 90% the final steady state average pressure) averaged only 23.7 seconds. Further, de-activated hybrid SMA garments are loosely fitting (and modeling suggests that even completely loose garments can achieve target counter-pressure levels), meaning the wearer should be able to don and activate this type of garment in far less than 10 minutes (assuming sufficient available power).

6.3 Limitations

We recognize that this analysis has limitations that warrant discussion. First, the architecture downselection decisions articulated in this thesis are not intended to forever curtail investigation of other active material/textile combinations for active compression garments. The conclusions regarding the viability of SMA coil actuators above all other active materials reflects the current state of active material technologies, and this is a highly dynamic field. For example, new research into active materials
using everyday materials such as fishing line and sewing thread already opens the
door to new and exciting active textile opportunities [45]. While SMA coil actuators
were the superior choice in this research effort given our system requirements and the
current active materials landscape, this tradespace analysis warrants continual study
as active material technologies advance.

SMA actuators themselves have limitations that will affect the systems in which
they are used. The SMA frequency response is low, relative to other active mate-
rials, which limits their performance in highly dynamic systems [103]. The systems
proposed and studied in this thesis are effectively two-state, static systems, and the
actuators are used to transition from one static state (the passive pressure condi-
tion) to another static state (the active pressure condition). Should it be desired
for the compression garment to dynamically function, either as a powered support
device or to morph or otherwise augment the wearer during motion, then the choice
of SMA coil actuators as presented may be sub-optimal. However, SMA frequency
response actually improves as its size decreases (see Appendix F for more information
on the benefits of SMA micro-actuator design), so dynamic systems may benefit by
miniaturizing the SMA actuators described in this thesis (offering additional design
possibilities).

SMA actuators are also notoriously power hungry and hysteresis prone: the single
tourniquet system tested in this study required 27 W of power for 9 s to achieve MCP
pressure targets over a 3.81 cm width. Extrapolating these values over the total limb
length (2 arms and 2 legs, with an estimated total length between 275-380 cm [44]),
we conservatively estimate the power consumption required for a full suit contraction
to be between 1.8-2.5 kW applied for at least 10s. Given this estimate, for any MCP
space suit application (where power consumption is of critical concern) effort must be
made to ensure designs minimize the need for continuous power consumption while
providing fail-safe life support. Designs that can capture the tension created during
SMA activation (through a locking, lacing, or similar architecture), enabling the
actuators to be deactivated once a target counter-pressure has been reached, should
be prioritized and investigated.
The specific SMA material selected for this study – NiTi Flexinol muscle wire, provided by Dynalloy Inc. – was chosen due to its commercial availability, low cost, and prevalence in literature that served to inform this project [71,74]. We did not conduct a comparative study of all available alloys with shape memory properties, nor did we study the comparative benefits of pre-annealed vs. as-drawn alloys (though literature suggests that as-drawn alloys may improve performance [76]). This tradespace should also be investigated further.

The analytic model derived in this thesis relies upon several assumptions that may be violated in certain systems (e.g., that both the SMA actuators and the passive fabric are linear, and their material characteristics are time- and strain-invariant). Further, the passive pressure model was derived based on the known specific maximum extensional strain limits of our specific SMA actuators (with spring index $C = 3$ and $\eta = 0.9$, $\epsilon_{\text{max}} = 3$). Additional analysis is necessary to fully generalize this model, as well as to broaden the active pressure model to accommodate garments that are initially loose fitting (the current model requires garment geometries that at most initially match the limb circumference).

The performance tests conducted in this study used a rigid object (PVC pipe) as the base structure against which pressure was produced. Humans are not rigid objects, and soft tissue will not necessarily respond in the same fashion when exposed to an applied pressure. Subsequent system testing on non-rigid objects (ideally on human subjects) is a critical next step in system validation.

Finally, we observed limitations in system performance that can be attributed simply to limitations in design, manufacturing, or material selection. The passive fabrics were chosen based on commercial availability and not on optimized performance, and breakdowns in their characteristics (i.e., irrecoverable strain, loss of modulus, non-linear behavior, and high friction) affected the test results. Similarly, the 3D printed actuator cartridges, as designed, contributed to the “near-field” pressure variability, and experienced structural failure at high tensions that limited the maximum pressures observed. More detailed consideration of these system aspects is warranted.
6.4 Future Research Directions

Based on the conclusions and limitations of this research effort, many avenues for future work exist, including:

1. Conduct a detailed study of the dynamics of passive fabrics under high tension, to better inform compression garment design. This includes cycle life testing, irrecoverable strain analysis, and characterization of the degradation in elastic modulus as a function of applied tension (and duration of exposure). This assessment should focus on the optimization of linear passive materials to improve the performance and reliability of active compression garments.

2. Investigate, in detail, the mechanisms that affect the performance of SMA actuators in blocking force configurations. A significant discrepancy in active stress was observed depending on whether the SMA actuators were stretched-then-heated vs. heated-then-stretched, and this effect is poorly understood. Blocking force testing of SMA coil actuators, in general, warrants further study, as this paradigm of activation is under-represented in the literature.

3. Expand garment prototyping to full limb systems, including jointed designs informed by LoNE patterning. Refine the SMA cartridge design for additional strength, better “near-field” pressure distribution, and incorporate locking mechanisms to enable sustained active pressure without the need for continuous power. Assess the pressure production and mobility characteristics of these prototypes on human subjects.

4. Investigate differing commercially available SMA materials (e.g., as-drawn nitinol vs. Flexinol, and other alloys), and manufacture actuators using differing annealing parameters (e.g., temperature, exposure time, and quenching properties) to optimize SMA force-extensional strain performance.

5. Investigate additional active compression garment concepts, including active braids and meta-textiles comprised of composite SMA fibers.
6. Refine the two-spring analytic model to accurately capture the dynamics of loose fitting designs, and incorporate methods for predicting both changes in passive material properties over time and system performance for non-linear fabrics.

7. Revisit the active materials landscape (including DEAs and SMPs) as technologies improve to reassess their viability for use in active compression garments.

6.5 Summary of Contributions

The contributions from this thesis to the field of active compression garment design are as follows:

1. A comprehensive assessment of current active material technologies for compression garment applications that selects SMA coil actuators as the most viable actuator architecture given current technology.

2. A complete SMA coil actuator design, including force, thermal and pressure modeling methods, for the purpose of designing high force, large displacement linear actuators for use in wearable garments and advanced space suit design. Minimum spring index actuators are found to combine high force and tension magnitudes with appealing actuator geometries for textile and space suit integration.

3. A comprehensive experimental characterization of these actuators, examining their behavior in blocking-force configurations to assess their controllability, linearity, and performance.

4. The first demonstration of an integrated, controllable active tourniquet compression prototype using low spring index SMA actuators with measurable increases in active counter-pressure.

5. A novel analytic two-spring model that predicts active and passive counterpressures (and a variety of other parameters) for hybrid compression garments
based on eleven design criteria. The first discussion of hypothetical hybrid designs, including multiple designs specifically tailored to meet BioSuit MCP requirements, is presented.

6. A novel SMA cartridge design that combines 3D printed structures with a continuous SMA coil actuator to produce a compact, modular actuator that approaches the physical limit in terms of actuator spacing; and two novel methods for manufacturing these cartridges.

7. The first known experimental assessment of hybrid active tourniquets combining modular SMA cartridges with commercial passive fabrics. These prototypes produced active pressures of sufficient magnitude for MCP space exploration applications, and can be activated in less than one minute.

8. A detailed list of proposed improvements, alternative architectures, and future work to further mature active compression garment design.

6.5.1 List of Associated Publications and Patents


   • 3rd Place, student poster competition


- 1st Place, graduate student poster competition


6.6 Final Comments

These studies, taken in aggregate, support the premise that compression garments can be greatly improved by combining typical passive fabrics with integrated active materials. This design approach has the potential to improve the performance of both high performance MCP suits used by astronauts for future exploration missions, and the performance of everyday compression garments used by millions of people for therapeutic recovery, athletic performance, and other applications. Prototype systems combining SMA actuator cartridges with commercial fabrics are capable of producing great increases in counter-pressure in very short periods of time, providing the wearer with a streamlined, low-mass garment that can provide variable compression magnitudes on demand.

For the first time, soft compression systems combining passive fabrics and active SMA materials have been analytically modeled, and we provided an assessment of the complete design space (including eleven design parameters) that dictate active compression garment performance. Additionally, in this document we have provided a novel end-to-end active compression garment prototyping methodology, starting with raw SMA acquisition, actuator manufacturing, system modeling, and prototype fabrication using a commercial 3D printer. This methodology can be easily implemented at low cost by anyone with access to simple laboratory equipment (e.g., a winding device and furnace) and entry-level 3D printing equipment.

The underlying technology developed in this research effort opens up new research opportunities for dynamic, wearable systems. Beyond simple compression garments, low-profile, soft, wearable actuators have widespread potential for wearable augmentation systems, including soft exoskeletons, dynamic rehabilitation/resistance devices, and improved healthcare treatments. This study represents a significant step forward in the maturation of wearable technologies that may bring these concepts, along with MCP exploration suits, closer to reality.
Critical to this research effort, in addition to active materials selection, is the selection of the garment architecture in which the active material(s) will be embedded. This requires us to consider textile structures for garments with one or more active elements, each with specific capabilities and limitations. SMA and SMP materials offer large recoverable deformation whereas DEA materials offer large recoverable strains. This difference may lead to a divergence in optimal textile design depending on the selected material. Textile fabrics can generally be categorized into three classes: woven fabrics, knitted fabrics, and non-woven fabrics [104, 105]. Each of these classes has unique properties, advantages, and limitations:

1. Woven fabrics use two independent sets of yarn aligned perpendicularly to one another (referred to as the warp, the lengthwise yarn, and the weft or fill, the crosswise yarn), with the weft fibers inserted over and under successive warp lines in a pattern dependent on the desired weave type (see Figure A-1a). Woven fabrics are the most widely produced type of fabric, and contain two bias axes, which lie at 45 degree angles to the warp and weft axes. Bias axes are more
2. Knitted fabrics, unlike woven fabrics, use a single set of yarn that is looped through itself, and the yarn is oriented in the same direction through the entire garment (see Figure A-1b). Knitted fabrics can take either weft- or warp-knitted architectures, depending on whether the yarn moves along the length or the width of the fabric [104,105].

3. Non-woven fabrics include any type of fabric not woven or knitted, and consist of interconnected fibers bonded by mechanical, chemical, thermal, or other means (see Figure A-1c). The most common example of non-woven fabrics is felt [104,105].

Beyond these three traditional textile fabric classes, other complex structures and architectures exist that may be relevant to active material compression garment design:

1. Oblique Interlaced, or Braided, textiles are composed of individual elements oriented at oblique angles to the edge of the fabric that pass under and over intersecting elements (also at oblique angles) with a common directional trend [98]. Several different braiding structures (diamond, Hercules), axial configurations (biaxial, triaxial), fiber diameters, porosities, and interlacing angles are possible. A biaxial braid structure is demonstrated in Figure A-2a-b. Braids are commonly used in everything from children’s toys (like the finger trap) to advanced carbon fiber composites [97]. Because of its unique textile architecture,
the fiber elements in a braided cylinder are free to rotate angularly with respect to one another, enabling the cylinder to increase in length when loaded (causing a simultaneous decrease in radius) [98]. For this reason, braided tubes have been utilized in many actuation and morphing engineering structures, including pneumatic artificial muscles (McKibben actuators), expandable tubing sheaths, and in-vitro stents, and hold particular promise for compression garment design [27, 106–108].

2. Looping, Twisting, and Interlinking textiles are structures composed of individual fibers, yarns, or fabric elements that are twisted and/or looped around adjacent elements in an alternating fashion [98]. Several variants and combinations exist (simple linking, link and twisting, spiral interlinking, looping with interlocked stitching, etc.), three of which are shown in Figures A-3a-c. These structures differ from braids significantly, as individual elements are effectively fastened to adjacent elements as a result of the linking/twisting/knotting structure, and each element changes directionality after each interaction (whereas braided elements maintain constant directionality). Looped, twisted, and interlocked structures are ubiquitous in everyday life, as they are used in everything from chain link fences to simple netting.

**Recommended Textile Architectures for Integrated Active Elements**

SMAs, SMPs and DEAs each have different operating mechanisms, strengths, and limitations that may impact their viability in a given textile architecture. DEAs
produce large strains and are nominally produced in ribbon or sheet-type architectures, and are dependent upon planar electric stimulation to produce actuation. Their ability to recover large strains make DEAs attractive for compression garments with wide circumferential elements (such as a coarse weave or non-woven sheath), but are vulnerable to electrical isolation and interaction effects if integrated into a dense, multi-element configuration (eliminating traditional textile architectures from consideration). SMAs and SMPs can be produced in fibrous/wire form, and demonstrate large recoverable deformation capabilities under many different actuation stimuli that scale with fiber diameter. SMAs and SMPs do not generally produce large element-wise recoverable strains. These types of materials thus require garment designs that achieve compression through controllable deformation and interaction effects (such as braids or linked/twisted textiles), and benefit from coarse (rather than fine) fiber structure as the active response scales with fiber diameter.

As a result, three limiting factors exist that set the textile design space for active material compression garments: nature of the active material (controllable deformation vs. controllable strain); form of the active material element (fiber vs. sheet, coarse vs. fine); and textile architecture (circumferential vs. biased elements, weave/knit vs. non-weave, interlaced vs. interlinking). Given the material types and fabric classes/structures available, a matrix of possible material/fabric architectures can be developed to constrain the design space for an active material compression garment with integrated active elements, and each material-textile architecture combination was graded (A, B, C) depending on their estimated performance as
a controllable compression garment, resulting in several downselected architectures recommended for future development. This analysis, including brief justification for each assessment, is included as Figure A-4. Three material-textile architectures were identified as best suited for controllable compression design: DEA non-woven sheath; SMA/SMP coarse braid; and SMA/SMP coarse looping/interlinking mesh. Other options were ranked below these for several reasons, including construction complexity (i.e., DEA braid); potential for stimulus (thermal, electrical) interference (i.e., DEA weave and braid); lack of clear mechanism to produce compression (i.e., SMA/SMP weave and knit); and inefficient material sizing (fine SMA/SMP fiber architectures).
Appendix B

Additional SMA Characterization Tests

In addition to the structured characterization tests outlined in Chapter 3, a battery of initial and exploratory characterizations of prototype SMA actuators was conducted and are outlined in the following subsections.

Force-Extensional Strain Tests

A series of blocking force tests (i.e., tests that fix an actuator at a given extensional strain, then measure the steady state force as a voltage is applied) were conducted at varying extensional strains to assess the force generation properties and linearity of the prototype NiTi actuators. A variable height blocking force test stand was developed that measured activation forces through the use of a Futek LTH350 donut load cell [Futek Inc.]. An actuator was exposed to an 8V load for 60s at 3 different extensional strains \((\epsilon = 0.37, 1.00, \text{ and } 1.73)\), and each test was repeated three times. The results of these tests are shown in Figure B-1a. Each test run produced similar data: force increases quickly after the voltage is applied, and levels to steady state within 5s. Greater variability in the activation force was recorded at higher extensional strains (likely attributable to non-uniform heating and activation as the actuator is stretched to its limit). Force was found to be highly linear \((R^2 > 0.99)\) across the extensional strains tested, validating the force-extensional strain model (equation (3.5)). The
highest average force measured was 8.94 N at $\epsilon = 1.73$, corresponding to a shear modulus $G = 10.80$ GPa, according to Eq. (3.5). This modulus value (which is lower than that reported by An et al.) is likely attributable to both the annealing parameters we selected as well as the alloy selection (i.e., using treated Flexinol wire vs. as-drawn Nitinol) [72,76].

**Force-Voltage Tests**

A second test was conducted to assess the force-voltage behavior of a prototype actuator at a fixed extensional strain. The same isometric test stand was used, and an actuator was held at a fixed extensional strain ($\epsilon = 1.00$) and exposed to increasing voltages (from 0-10 V at 2 V increments, 2 minutes per voltage). The results of this test are shown in Figure B-1b. At low voltages, no active force is measured, as the applied voltage does not generate sufficient Joule heating to cross the critical activation temperature (which, for 305 $\mu$m Flexinol wire, is listed to be 70°C). For all voltages $\geq 4$ V, progressively increasing activation forces are measured as voltage is increased, indicating that the phase transformation of the actuator extends over a wide temperature range due to stress-induced reverse transformation (which follows the Clausius-Clapeyron relation and is consistent with published literature) [73, 76]. At 8 V, the average force profile reaches a maximum, indicating that complete austenite activation has been achieved. For voltages $> 8$ V, we measure a significant loss of force (decaying to nearly zero). This is a result of overheating the actuator to the point that the memory state begins to reset (i.e., the actuator is effectively re-annealed at 10 V due to excessive Joule heating, causing a loss of memory and therefore a loss of active force). This behavior is significant from a design perspective for two reasons: first, force targets can be met either by directly altering the physical characteristics of an actuator to perfectly match the target (then heating the actuator to achieve full austenite transformation), or by over-designing the actuator and controlling the voltage such that only partial activation (and therefore, only partial active forces) are produced; second, system designers must avoid overheating the actuators, as this can lead to actuator shape memory loss and therefore a drastic loss in active force.
Performance of Multiple Actuators in Parallel

Four identical actuators were assembled in parallel to assess the force-voltage and power consumption relationships at a fixed extensional strain ($\epsilon = 1.36$). A circuit was assembled that connected the actuators in two parallel branches (with two actuators in each branch) connected in series with one another. Increasing voltages (from 2-5 V) were applied for 60s each, and both force and power consumption were measured. The results of this test are shown in Figure B-1c. Again, force increases as voltage increases (along with power consumption), with the actuators reaching an average force of 24.75 N at 5 V (4.55 W). This corresponds to an average force of 6.57 N per actuator. Comparing this performance to the extensional strain tests in Figure B-1a, we expect an actuator at 8 V and $\epsilon = 1.36$ to produce 7.90 N (based on interpolating the linear data). At considerably lower power (5 V vs. 8 V, which was conservatively selected based on temperature limitations in the support structure) we see 20.2% lower force, indicating that the parallel actuator setup could produce significantly higher forces than measured if greater voltage were to be applied. This test demonstrates the viability of linking multiple actuators in parallel to produce greater forces (at the cost of greater power requirements) with equivalent activation simplicity.

Actuation Decay Tests

A final test was conducted to assess the decay in activation force as the applied voltage is removed. The same four-actuator setup was tested at 5 V and a fixed extensional strain ($\epsilon = 1.36$), just as was done in the previous test. The test duration was 120s: at 10s, voltage was applied; at 80s the voltage was turned off; and force was measured for the full test duration (i.e., both during activation and deactivation). The results of this test are shown in Figure B-1d. We see consistency in the activation data when compared to the previous test (overlaid on the plot for comparison), and we see significantly slower decay in force as the voltage is removed when compared to the equivalent force profile upon activation (i.e., actuators reach steady state force approximately 10s after activation, but when deactivated the force does not com-
pletely decay even after 30s). This is significant from a design perspective: activation and deactivation occur on different timescales, which needs to be accounted for when designing control systems using SMA actuators; and the time constants for activation and deactivation are large (on the order of 10s of seconds for these actuators, though this depends on actuator geometry, applied power, and wire diameter), meaning that systems requiring fast or dynamic responses may be poorly suited for SMA coil actuators.
Figure B-1: A (upper left): isometric blocking force data for a single actuator at three extensional strains. Force was found to vary linearly with extensional strain ($R^2 > 0.99$), as predicted by the force-displacement model, with the highest average force (8.94 N) measured at $\epsilon = 1.73$. B (upper right): force-voltage relationship for a fixed extensional strain ($\epsilon = 1.00$). Force increases as voltage increases (indicating a progressive austenite transformation), with maximum force occurring at complete austenite transformation (in this test this occurs at 8 V). Once voltage is increased beyond this point, significant losses in force are measured as the actuator is overheated and the memory state is reset. C (lower left): force-voltage-power relationship for four parallel actuators held at a fixed extensional strain ($\epsilon = 1.36$). Force increases with voltage and power, and is consistent with previous tests, demonstrating actuator scalability. D (lower right): Activation and deactivation force comparison. Time constants for activation and deactivation vary (i.e., actuators reach steady state active force in approximately 10s, whereas they do not fully deactivate after 30s of cooling).
Appendix C

Calorimetry Data Analysis

Example

Calorimetry testing is designed to assess activation temperatures of shape change materials by measuring the heat absorbed or rejected during phase change [73]. An example of a calorimetry dataset (both the raw data and the analyzed data) is included as Figure C-1. The data was analyzed using a custom-designed MATLAB script, and the results of this analysis appear in the main body of this document, in Table 3.1.

Raw heatflow vs. temperature data is collected for samples using a Mettler thermogravimetric analyzer/differential scanning calorimetry machine (TGA/DSC 1). Spikes in heat flow are detected when the material changes phase, and are evident in the example plots provided. To calculate critical temperatures (austenite start and stop temperatures, and martensite start and stop temperatures, which are the temperatures at which the spike begins and ends), linear fits are applied to the data in four places: prior to the onset of a temperature spike; on the initial face of the spike; on the trailing face of the spike; and following the completion of the spike. Critical temperatures are calculated at the intersections of these line fits.

This type of analysis is a critical first step in the characterization of any new active material system: we determined that both material composition and annealing parameters affect critical activation temperatures.
Figure C-1: Example of both a raw and analyzed calorimetry dataset. The top two plots show the raw data of a martensite to austenite heating transformation (top left) and an austenite to martensite cooling transformation (top right). Spikes in heat flow represent intrinsic phase changes in the material (as heat is either absorbed or rejected). The critical temperatures (i.e., austenite start and finish temperatures, and martensite start and finish temperatures) are identified as the temperatures that correspond to the start and finish points of a given heat flux spike.
Appendix D

SMA Cartridge Manufacturing
Methods and Drawings

To produce the 3D printed SMA actuator cartridges described in Chapter 5, two different methods (single plastic method, and multi-plastic method) were used. Instructions for each of these methods are included in this appendix.

Method 1: Single-plastic, fully embedded SMA cartridge

The single-plastic, fully embedded SMA cartridge was produced using a Stratasys Fortus 250mc 3D printer. The method described in this section was developed specifically for the Fortus 250mc. However, at the time of printing this machine can only print with ABS plastic, which was found to have glass transition temperatures below the austenite finish temperature, resulting in structural failure of the cartridge during activation. Higher temperature materials, such as FDM Nylon 12 or ULTEM* 9085, are recommended for SMA cartridge prototyping, including this design - however, these materials cannot be used with the Fortus 250mc (so ABS plastic was used). It is recommended this method be adapted for machines that are capable of printing with such materials. Alternatively, the multi-plastic method can be used.

The manufacturing method is as follows:

1. Develop 3D models of desired cartridge structures, similar to those presented
Figure D-1: Dimensioned cartridge 3D models including left and right end caps, and a central spacing unit: full model (top) and cut away model to view SMA channels (bottom).

in Figure D-1, and save the files as .STL files. The models presented in this example, and used in this research effort, were produced using the modeling program Sketchup, and exported as .DAE files to MeshLab, which was used to convert to .STL files. Important considerations for designs using this method: the embedded channels can be smaller than the SMA actuator outer diameter (we used 1.143 mm [0.045"] channels, for actuators with outer diameter = 1.2446 mm [0.049"]) ; the number of channels should be an even number (our designs use 12 actuators) such that the circuit starts and ends on the same side (for ease of actuation); and the minimum wall thickness should be ≥ 0.762 mm [0.03"], given the accuracy of the 250mc printer. Space the parts such that the distance between the left and right end cap is the maximum de-twinned distance desired for your application.

2. Manufacture a single SMA actuator of sufficient length to be fully laced through
the design structure (i.e., at least as long as the desired length of the cartridge structure multiplied by the number of desired channels, with margin).

3. Load the cartridge model into the Stratasys Insight software. Before building the model, change the support structure settings as follows: in the Support drop-down menu, select Setup - Access advanced support settings; in this menu, change “Support Style” to “Basic”, “Supports to Create” to “Base Only”, and “Base Layers” to the minimum (in this instance, the minimum was 5 layers). This change prevents the software from placing support material inside the channel structures, which is necessary for actuator integration.

4. Build the toolpath using the Insight software. Once the toolpath has been built, insert a pause at the layer immediately before the channel ceilings are deposited (i.e., so that the channel walls have been fully formed, but the “lid” of the channel has not yet been deposited). This is done as follows: in the Toolpaths drop-down menu, select Insert Pause; navigate to the layer at which you wish to pause, and select “OK”. Be sure to re-save the job after inserting the toolpath pause.

5. With these changes implemented, send the print build to the printer using the Stratasys Control Center software, and begin the print job. The parts should build until the pause layer is encountered, and the printer should pause indefinitely at this point.

6. With the printer paused, open the printer and remove the tray with the partially completed parts (be careful, as the tray and parts are likely to be very hot) and close the printer door. Manually lace the spring through the structure, applying tension as you go such that the spring is fully de-twinned at each segment. Because the channels are designed to be smaller than the spring outer diameter, it will be necessary to force the spring into each channel (using a flat-head screw driver or other similar tool). This helps to lock the actuator in place prior to the final printing. An example of the actuator laced through the
Figure D-2: SMA lacing step, with end cap lacing magnified. This step occurs at a pause part-way through the 3D print build, once the embedded channels have been printed but prior to the deposition of the final layers. The printed structure at this step is included as Figure D-2.

7. Once the actuator is fully embedded, place the tray back in the printer and resume the build. The printer should apply the final layers, locking the actuators in place.

A common problem encountered during manufacturing is for the actuator segments to break free of the structure during the final build phase. This occurs because the build environment is warm enough to trigger the actuators, causing the tension in the laced actuators to increase until they pull free of their channels. To combat this problem, increase the depth of the plastic channels, and ensure that the actuators are pressed as deeply into the channels as possible before commencing the final build.

A finished version of this actuator is shown in Figure D-3 during activation.

Method 2: Multi-plastic, two-piece SMA cartridge

A second method was developed to produce a two-piece SMA cartridge that combines high temperature ULTEM® 9805 plastic insets (printed on a Fortus 400mc printer).
that are then press-fit and embedded into an ABS superstructure (printed and finished on a Fortus 250mc printer). This method was developed in response to the finding that cartridges made entirely of ABS plastic using the single-plastic method are prone to thermally-induced failure when activated (i.e., the ABS plastic melts). The ULTEM insets serve as a heat shield for the ABS plastic: the actuators are laced through the ULTEM insets after manufacturing, and the insets are then press fit into an ABS superstructure cavity part-way through a secondary build. Subsequent SMA activation leads to heating of the ULTEM parts and not the ABS plastic.

The manufacturing method is as follows:

1. Develop two sets of 3D models of the desired cartridge structures (one set for the ULTEM insets, one set for the ABS superstructure) using the same software as the single-plastic method, similar to those presented in Figure D-4 and Figure D-5, and save the files as .STL files. Important considerations for designs using this method: the channels in the ULTEM parts must be larger than the SMA actuator outer diameter (we used 1.778 mm [0.07”] channels, for actuators with
outer diameter = 1.2446 mm [0.049"'], to ensure that the actuators can be manually laced after these parts are produced; again, the number of channels should be an even number (our designs use 12 actuators) such that the circuit starts and ends on the same side (for ease of actuation); and again, the minimum wall thickness should be $\geq 0.762$ mm [0.03"'], given the accuracy of the 250mc printer. Space the ABS parts such that the distance between end caps is near (but less than) the maximum de-twinned distance desired for your application.

2. Manufacture a single SMA actuator of sufficient length to be fully laced through the design structure (i.e., at least as long as the desired length of the cartridge structure multiplied by the number of desired channels, with margin).

3. Print the ULTEM parts using standard printing procedures (using a Fortus 400mc or equivalent). Examples of these parts are included as Figure D-6a.

4. Manually lace the actuator through the ULTEM end caps and central spacer, ensuring consistent lengths of each actuator leg. This is most easily accomplished by placing the three ULTEM parts in a vice grip at the desired spacing, then lacing the actuator back and forth through the fixed parts. Examples of the SMA laced through the ULTEM parts (from both front and side views) are included as Figure D-6b-c.

5. Load the ABS superstructure model into the Stratasys Insight software. Before building the model, change the support structure settings as follows: in the Support drop-down menu, select Setup - Access advanced support settings; in this menu, change “Support Style” to “Basic”, “Supports to Create” to “Base Only”, and “Base Layers” to the minimum (in this instance, the minimum was 5 layers). This change prevents the software from placing support material inside the superstructure cavity, which is necessary for ULTEM inset integration.

6. Build the toolpath using the Insight software. Once the toolpath has been built, insert a pause at the layer immediately before the superstructure ceilings are deposited (i.e., so that the cavity walls have been fully formed, but the “lid” of
Figure D-4: Dimensioned cartridge 3D models: ULTEM inset ends caps and middle spacer (top view, presented in the upper figure, and bottom view, presented in the lower figure).
Figure D-5: Dimensioned cartridge 3D models: full model of ABS superstructure end caps (top), and cut away model to see the details of the press-fit cavity (bottom).
the cavity has not yet been deposited). This is done as follows: in the Toolpaths drop-down menu, select Insert Pause; navigate to the layer at which you wish to pause, and select “OK”. Be sure to re-save the job after inserting the toolpath pause.

7. With these changes implemented, send the print build to the printer using the Stratasys Control Center software, and begin the print job. The parts should build until the pause layer is encountered, and the printer should pause indefinitely at this point.

8. With the printer paused, open the printer and remove the tray with the partially completed parts (be careful, as the tray and parts are likely to be very hot) and close the printer door. Manually press-fit the ULTEM end pieces into the superstructure cavities, ensuring that the beginning and end of the SMA spring are fed through the openings on the sides of the ABS superstructure. This will lock the ULTEM insets and SMA spring in place prior to the final printing.

9. Once the insets are fully embedded, place the tray back in the printer and resume the build. The printer should apply the final layers, locking the insets,
and the actuators, in place.

A common problem encountered during manufacturing is for the final layers to slump as they are deposited atop the ULTEM inset pieces. This occurs because the surface of the insets is lower than the printer expects (so the deposited plastic slumps until it encounters the inset surface). As long as the surface effectively contains the inset, and is structurally sound, this is not a serious issue. If the slumping is significant, and/or it affects the structural stability of the system, adjust the tolerance/height of the ULTEM inset or the ABS superstructure ceiling to narrow the height difference.

A finished version of this actuator is shown in Figure D-8 during activation.
Figure D-8: Completed multi-plastic, two piece SMA cartridge produced using this method, shown during activation.
Appendix E

Biaxial Braid Dynamics

In order to design compression garments based on complex textile structures like biaxial braids or looping/interlocking meshes, it is important to first model such geometries to understand how the elements in each textile will interact with one another, as these interactions will directly determine how the shape of the textile will change when activated and how it will respond mechanically when loaded. Because the field of active textiles is newly emerging, no standard modeling tools exist to characterize the behavior of morphing textiles. A mathematical model of biaxial braid geometries was derived as follows to predict the relationship between length and radius—a first step in understanding the compression behavior of a biaxial braid comprised of SMA/SMP elements.

One strand of a biaxial braid follows a helical path as shown by Figure E-1. A helix is described by three parametric equations as follows:

\[ x = r \cos(\theta); \quad (E.1) \]

\[ y = r \sin(\theta); \quad (E.2) \]

\[ z = c\theta; \quad (E.3) \]
where \( r \) is the radius, \( \theta \) is the swept angle in the xy-plane in radians, and \( c \) is the height change per turn. The value \( 2\pi c \) is called the pitch or the vertical distance between loops. The arc length \( s \) is:

\[
s = \theta \sqrt{r^2 + c^2}.
\] (E.4)

A helix can also be unwrapped to reveal a triangle where the hypotenuse is the arc length \( s \). Figure 1E-2 illustrates the relationship between height \( z \), braid angle \( \phi \), radius \( r \), and arc length \( s \). The braid angle \( \phi \) is described by:

\[
\phi = \arctan \left( \frac{z}{r\theta} \right); \tag{E.5}
\]

\[
\phi = \arctan \left( \frac{c}{r} \right); \tag{E.6}
\]

As the biaxial braid is stretched, \( r \) decreases and \( c \) increases. To describe this stretching behavior it is assumed that arc length is conserved and that the braid does not twist, which is described by:
\[ s_0 = s_1; \]  
\[ (r_0)^2 + (c_0)^2 = (r_1)^2 + (c_1)^2; \]  
\[ (z_1)^2 - (z_0)^2 = ((r_0)^2 - (r_1)^2)\theta^2; \]

where subscripts represent the initial (subscript 0) and final (subscript 1) state of the braid. Contours of constant arc length are plotted in Figure E-3a where one contour represents a set of possible values for \( r \) and \( c \). Equation (E.8) can be expressed in terms of height \( z \) by:

\[ (z_1)^2 - (z_0)^2 = ((r_0)^2 - (r_1)^2)\theta^2; \]

Physical geometry for donning and doffing determines \( z_0 \) and \( r_0 \). The required pressure and fabric determines \( r_1 \) and the limb size will determine \( z_1 \). These can be used to solve for \( \theta \) using (E.9). Figure E-3b shows this equation applied to model the compressed and stretched state of a biaxial braid.

An ideal finite helix can be stretched until it is a straight strand or compressed

---

Figure E-2: Standard helix 3D view (left), top view (center), and unwound helix schematic (right).
Figure E-3: Biaxial braid length vs. radius contour map for multiple arc lengths (left) and 3D representation of length/radius relationship (right).

until it is a flat circle. However, actual strands have a finite width and biaxial braids are composed of a finite number of strands. This constrains the macro structure (minimum and maximum radius and height) of biaxial braids. Figure E-4 shows a magnified view of packed strands when a biaxial braid is compressed to its maximum radius without straining the material. In Figure E-4, \( w \) is the width of the fiber and \( \epsilon \) is the gap between fibers caused by the thickness of the strands being braided in the opposite orientation. The horizontal projections of \( w \) and \( \epsilon \) are shown as \( w_h \) and \( \epsilon_h \). Because the macrostructure of the braid is circular the following must be true:

\[
 n \left( w_h + \epsilon_h \right) = 2\pi r; \\ (E.10)
\]

\[
 w_h = \frac{w}{\sin \phi}; \\ (E.11)
\]

\[
 \epsilon_h = \frac{\epsilon}{\sin \phi}; \\ (E.12)
\]
Figure E-4: Biaxial braid unit cell at maximum compression.

\[ n \left( \frac{w}{\sin \phi} + \frac{\epsilon}{\sin \phi} \right) = 2\pi r; \quad (E.13) \]

\[ \phi = \arctan \left( \frac{c}{r} \right); \quad (E.14) \]

\[ \sin \phi = \frac{c}{\sqrt{c^2 + r^2}} \quad (E.15) \]

\[ \frac{ns}{\theta c} (w + \epsilon) = 2\pi r; \quad (E.16) \]

where \( n \) is the number of strands. Note that \( c \) is a function of \( r \). When this function is solved for \( r \) the minimum and maximum values of \( r \) are obtained because there is no space between the braid fibers and the braid can no longer move. Solving these equations shows that there are two solutions for \( r \) that are physically possible. For a solution \( r_1 \) and \( r_2 \) the corresponding values of \( c \) are \( c_2 = r_1 \) and \( c_1 = r_2 \), which is expected given the symmetry of the braid structure.

To assess this model, commercially available biaxial braids (TechFlex Flexo Clean Cut 0.375”, 0.75”, 1.5” nominal diameter) were purchased and their minimum/maximum length/radii characteristics were physically measured\(^1\). Predicted vs. measured length/radius limits (extrema) for each braid are plotted in Figure E-5 (along with reference contours of constant arc length). The unique contour enclosed between the extrema represents

\(^1\)http://www.techflex.com/prod_CCP.asp
the operating regime for a biaxial braid of the given arc length. This predictive capability could be used to design braided structures that meet specific morphing requirements for compression and donning/doffing (e.g., a lower leg braid that could both expand to accommodate the heel and contract to produce compression on the ankle).

Discrepancies between the predicted and actual values are likely caused by deviations from the assumption of an ideal helical structure for each braided element. Actual elements in a biaxial braid likely follow a helical quasi-sine wave rather than a simple helix due to the weaving effect stemming from element interactions. Assuming that a strand makes a sinusoidal path as it spirals, the sine portion of the curve has an amplitude and frequency determined by the thickness of the strands $b$ and the number of strands used $n$. The radius is then substituted by the effective
radius (E.17). The phase shift (E.18) is used to represent the number of strands \( n \) in one orientation used to create the overall braid. The mathematical parameterization becomes (E.19)-(E.21):

\[
    r_{eff} = a + b \sin \left( n\theta - \frac{\pi}{2} \right); \quad (E.17)
\]

\[
    \phi = (i - 1) \frac{2\pi}{n}; \quad (E.18)
\]

where \( i = 1, 2, 3, \ldots, n \).

\[
    x = r_{eff} \cos (\theta - \phi) \quad (E.19)
\]

\[
    y = r_{eff} \sin (\theta - \phi) \quad (E.20)
\]

\[
    z = c\theta \quad (E.21)
\]

To represent the surface of each individual strand, open source textile weaving software TexGen was used\(^2\). Each peak and valley in the sine oscillation embedded in (E.17) is treated as a node in a TexGen model, which then interpolates between the nodes with a desired strand cross section to generate a surface. Figure E-6 shows two examples of biaxial braids generated by TexGen using this method. TexGen files can reportedly be exported as geometry files that can be analyzed by Finite Element Analysis (FEA) software to determine the mechanical characteristics of a specific model.

To assess the feasibility of complex braided geometries more similar to those encountered in a conformal garment design, the biaxial braid model was refined to vary the effective radius a function of theta:

\[
    r_{eff} = a(\theta) + b \sin \left( w\theta - \frac{\pi}{2} \right); \quad (E.22)
\]

\(^2\)http://texgen.sourceforge.net/
Figure E-6: Biaxial braid geometries generated using TexGen.

The surface made by the walls of the biaxial braid is directly related to $a(\theta)$. This is possible because the shape is expressed by a parametric equation, and multiple phase shifted strands comprise the biaxial braid. The equations for each x- and y-component are thus phase shifted, but the equation for each z-component remains the same in each strand, which makes the shape radially symmetric. To demonstrate this, let $a(\theta)$ be the following fourth order equation:

$$a(\theta) = -0.0164\theta^4 + 0.267\theta^3 - 1.32\theta^2 + 2.15\theta + 2. \quad (E.23)$$

When the function described by (E.23) is revolved about the $\theta$ axis it forms the sidewalls of the biaxial braid in Figure E-7, demonstrating that complex braided geometries are physically possible and can be effectively modeled using TexGen. This provides the basis for modeling garments catered to specific body geometries that can be used in combination with active elements to create controllable compression technologies.
Figure E-7: Fourth order profile for hypothetical biaxial braid architecture (top). Biaxial braid architecture with fourth order radial profile produced using TexGen (bottom).
Appendix F

Micro-System Applications for SMA Coil Actuators

Micro-electro-mechanical system (MEMS) architectures can also benefit significantly from the use of embedded low spring index SMA coil actuators. Previous studies examining SMAs as miniature linear actuators have concluded that spring-shaped actuators optimize mechanical performance for a given geometrical constraint (which is critical for micro-systems where space is at a premium) [110], and our analysis further identifies low spring index geometries as being the best spring design for maximum force generation. Typical MEMS applications that require significant actuation (e.g., micro-grippers, micro-valves, micro-pumps, tactile displays, and medical devices like micro-endoscopes) could produce larger forces using low spring index SMA coil micro-actuators than with any other traditional MEMS actuator (e.g., electrostatic actuators like Comb drives and Scratch drives, or piezoelectric or magnetic actuators) while exhibiting active displacements up to 3x their starting length [103]. Compared specifically to both electromagnetic and piezoelectric MEMS actuators, micrometer-scale low spring index SMA actuators excel in terms of both active force ($10^{-2}$-$10^{-1}$ N vs. $10^{-7}$-$10^{-4}$ N and $10^{-5}$-$10^{-3}$ N, respectively) and achievable active displacement ($10^{-3}$ m vs $10^{-5}$-$10^{-3}$ m and $10^{-7}$-$10^{-3}$ m, respectively) [103]. See Figure F-1 for further information [103]. Further, NiTi SMAs are biocompatible, which is particularly appealing for medical MEMS development [73,111].
Due to their dependence on thermal activation, SMA coil actuators have low frequency responses, and are poorly suited for systems that require dynamic, high frequency activation [111]. However, their frequency response does improve as SMA wire diameter (and therefore coil outer diameter) decreases, as the ratio of SMA surface area to volume increases, increasing convective heat transfer that results in faster cycling [103]. In fact, thermal-based MEMS actuators are the only category of actuators that demonstrate improvements in frequency response over their macro-scale equivalents [103]. Again, see Figure F-1 for further information [103].

Finally, a challenge often reported when working with SMA actuators is the problem of mechanically mating the alloys to the surrounding structure while maintaining electrical conductivity, all without restricting the ability of the actuator to contract. This may be particularly challenging in MEMS system design, as actuators are miniaturized and assembled in tight proximity to other actuators or sensors. We have found particular success in this regard, on a macro-scale, by press-fitting and epoxying each end of an SMA coil actuator (or multiple actuators) between strategically designed (i.e., snap-together) 3D printed structures embedded with copper tape for electrical conductivity, or by weaving a singular SMA spring through a series of scaffolds (to serve as end cap fixtures). These structures are then easily mated to both a base structure and to the object that is to be manipulated by the actuators. Acrylonitrile butadiene styrene (ABS) thermoplastic parts produced using a Stratasys Fortus 250mc printer [Stratasys, Ltd.] have been used for our macro-scale prototyping, and the system offers a build layer thickness of 0.178 mm (with an achievable planar accuracy of ± 0.241 mm)\(^1\). Even greater accuracy can be achieved using more precise 3D printing equipment (e.g., the Stratasys Fortus 900mc printer offers an accuracy of ± 0.09 mm). Modern manufacturing techniques such as these, with sub-millimeter accuracy, may assist in the development of MEMS systems using low spring index SMA coil actuators.

\(^1\)http://www.stratasys.com/3D-Printers/production-series/fortus-250mc
Figure F-1: Force-displacement (upper plot) and force-frequency (lower plot) relationships for a variety of active materials, on both the macro (green) and micro (black) scale. Among micro actuators, SMAs demonstrate the largest active force, and are among a subset of actuators (thermal-based actuators) that are shown to improve in frequency response when miniaturized [103].
Appendix G

Pressure Sensor Variance Assessment

An assessment of the accuracy and variability of the Novel pressure sensing system was completed, and the results are included as Figure G-1. Using the custom S2075 Pliance pressure sensor and the factory-generated calibration file, a series of pressure tests were conducted as follows:

1. The pressure sensor was placed in the custom pressure calibration device provided by Novel.

2. The calibration device, which contains an inflatable bladder placed between two rigid plates, was then inflated to a known pressure. Data was collected by the pressure sensor as a function of both sensel location (256 sensels arranged in a 16 x 16 square grid) and time.

3. Once the sensor reached steady state and measurements were taken, the inflatable bladder pressure was increased to the next test condition, and the process was repeated until all test conditions were completed. The sensor is rated for pressures from 0-100 kPa, and five pressure magnitudes were tested (20 kPa, 40 kPa, 60 kPa, 80 kPa, and 20 kPa again).
First, the factory-generated calibration file was found to consistently under-estimate applied pressure: average steady-state pressure recorded by the sensor measured only 80.98% of the known pressure, and this ratio was consistent across all pressure levels tested. Consequently, all data for these tests (and all data appearing in this thesis) were post-processed to correct for this error (see Figure G-1b for a comparison of average pressures using the original and corrected calibration files).

Once corrected for the poor calibration file, pressure data closely matches the applied pressure (steady state pressure magnitudes measured for each pressure condition are shown in Figure G-1b, both in column and numeric form). Spatial variance in the pressure data is calculated for each pressure test, and error bars representing $\pm 2\sigma$ are presented (with values reported above each column for both the original (red) and corrected (blue) data). Steady state spatial variance was found to be no greater than $\pm 12.7\%$ across all 256 sensels for any test. No visible hysteresis is observed, as the trailing 20 kPa test is nearly identical to the starting 20 kPa test. These tests demonstrate the high accuracy and low spatial variability of the Novel pressure sensor.
Figure G-1: Novel pressure sensor accuracy and variance assessment. A (above): The custom S2075 sensor was exposed to five known pressure levels, and pressure was measured as a function of sensel location once steady state is achieved (with averages for each condition shown with a dashed line). This data uses the corrected calibration table. B (below): Average steady state sensor measurement for each test, with error bars representing ±2σ demonstrating the magnitude of sensel variability for a given pressure condition, for both the original and corrected calibration files.
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