



CRANFIELD UNIVERSITY
CRANFIELD COLLEGE OF AERONAUTICS

**Enhancing The Post-Buckling Response Of Composite
Plate/Panel Structures Utilizing Shape Memory Alloy
Actuators - A *Smart* Structural Concept**

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PhD Thesis
Academic Year 1998-1999

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February 1999

This thesis is submitted as partial requirement for the degree of Doctor of Philosophy

Abstract

The feasibility of enhancing the post-buckling load bearing capability of carbon/epoxy composite plate/panel structures utilizing embedded activated near equiatomic nickel-titanium, Ni-Ti, shape memory alloy, SMA, wire actuators has been investigated.

Enhanced post-buckling is achieved through utilization of the unique shape memory phenomenon inherent within the Ni-Ti material. The unique phenomenon requiring a thermal stimulus. Within this investigation, such a thermal stimulus is provided for by an electrical current. Several host laminates, varying in lay-up architecture, have been considered.

Two control strategies have been employed that utilize the unique SMA response at an elevated temperature. *Control strategy 1* features embedded SMA actuators located within tubes that run along the specimens neutral plane. Here, the SMA's are constrained to external boundaries. *Control strategy 2* also features embedded SMA actuators. For this control strategy, however, the actuators are *partially* constrained to the host laminate.

For each strategy, upon SMA energization, shape memory constraint results with the formation of a recovery force within the SMA material. It is this recovery force that is employed to control the post-buckling response of the selected laminated specimens. A requirement for *control strategy 2* is that the SMA/host interface must be of sufficient quality to sustain an elevated temperature as well as the imposed recovery force.

Pertaining to *control strategy 1*, for the associated specimens, activation of constrained pre-strained SMA wire actuators can result with a significant specimen post-buckled deflection alleviation while under the influence of an external compressive load that is approximately three times the critical buckling value. While not as effective as *control strategy 1*, the concept behind *control strategy 2* has been shown to work. Its efficient, or optimal, utilization, however, has yet to be demonstrated.

For all the specimen configurations, the constrained SMA response act's to pull the specimens back to their flat configuration. This is true even when employing a low SMA volume fraction. Depending on the magnitude of the in-plane compressive load, however, this can result with post-buckled instability.

SMA restoration recovery forces not only reduce the peak displacement amplitude, they also alleviate high stress levels, local to the boundary supports, that are typical to post-buckled plate/panel configurations. The tendency of adaptation is to redistribute the loading back towards the plates central region, such that, a more uniform stressed state exists.

The stability of the adapted shape is dependent upon the laminate stacking sequence. Due to the elevated temperature required for SMA energization, the stacking sequence chosen should be such that temperature effects have minimal influence on the structural performance.

SMA utilization would certainly be of benefit when such components are subjected a thermal environment by a means other than electrical energization. As an example, heating, associated with skin friction, may be sufficient to drive the actuators through their phase transition such that they exert stabilising recovery forces on the skin sections of high speed aircraft.

The performance benefits of the SMA/carbon/epoxy composites materials, however, must carefully be assessed against issues of technical risk, producability, maintainability, reliability, and, of course, cost. The improved performance must be at an affordable price.

Acknowledgements

I would like to express my extreme gratitude to my supervisor, Dr Joseph Loughlan, for his valuable expertise, guidance and support throughout all stages of my PhD programme. Informally speaking, I especially wish to thank *Joe* for his encouragement during the latter stages of my write-up period and for proof reading this thesis in its entirety. It is very much appreciated.

The Structural Materials Centre of the Defence & Evaluation Research Agency provided vital financial support over the three year PhD period, without which, the programme of work would not have commenced. I would like to thank the Defence & Evaluation Research Agency for providing such financial support and also for providing myself with regular personal bursaries, in which, I am truly indebted.

Throughout the undertaken research duration, Dr Conrad Doran, formerly of the Structural Materials Centre, DERA, provided very useful technical input and programme co-ordination, therefore, enabling progress to be made. I thank Conrad for his important contribution.

Many many thanks go to Mr Morris Crook and Mr Barry Walker, staff within the College of Aeronautics, for all the hours of support that they provided regarding the detailed manufacturing and extensive testing issues that has enabled myself to complete this thesis. I also wish to thank Dr Les Oswald, CoA, for providing and maintaining a very efficient *stress free* computing service, without which, progress would have been slow.

In times of happiness and despair, for the support they have always provided me with, my final acknowledgements go to my family; John, Carol, Finton, Stephen, Dianne, Laura, William, Elliott, Daniel, Samantha, Benjamin, *especially* my mother and my deeply missed father.

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Notation

Alphanumeric Letters

a	Plate/panel specimen width
at. %	Atomic percentage
A_{ij}	Laminate extensional stiffness terms
A_f	Austenite finish
$A_{f, stress}$	Austenite finish when influenced by stress
A_s	Austenite start
$A_{s, stress}$	Austenite start when influenced by stress
b	Plate/panel specimen length
B_{ij}	Laminate coupling stiffness terms
c_p	Specific heat at constant pressure
$C_{M,A}$	SMA stress influence coefficient
CFRP	Carbon fibre reinforced plastic
CHA	Cross head adjustment
CLPT	Classical lamination plate theory
CTE	Coefficient of thermal expansion
d	diameter
d_{xy}	Piezoelectric constant
D_{ij}	Laminate bending stiffness terms
DOF	Degree of freedom
DSC	Differential scanning calorimeter
E	Young's modulus
E_c	Piezoelectric coercive field
ER	Electrorheological
$f_{x,y,z}$	Heat flux
F	Force, matrix mechanical property retention ratio
FEA	Finite element analysis
G	Shear modulus
h	Convection heat transfer coefficient
I	Current, second moment of area
l	Length
k	Thermal conductivity
K_d	Differential stiffness
K_e	Elastic stiffness
L_e	Embedded length
LVDT	Linear variable differential transducer
m	mass, number of half-sine waves in x-direction
M_f	Martensite finish
$M_{f, stress}$	Martensite finish when influenced by stress
M_s	Martensite start
$M_{s, stress}$	Martensite start when influenced by stress
MPC	Multi-point constraints
n	number of half-sine waves in y-direction
$N_{x,y,xy}$	Force intensities per unit length
Ni-Ti	Nickel-titanium
OF	Optical fibre

OTDR	Optical time delay reflectometry
P	Compressive load
P_c	Critical buckling value
P_{imp}	Buckling load for an imperfect plate
PD	Potential difference
PMC	Polymeric-matrix composites
PMN	Lead magnesium niobate
PTFE	Polytetrafluoroethylene
PVDF	Polyvinylidene fluoride
PZT	Lead zirconate titanate
q_v	Volumetric heat generation
R	Rhombohedral Martensite, a/b
RBE	Rigid body element
SMA	Shape memory alloy
SME	Shape memory effect
SPC	Single point constraints
t	Time
T	Temperature
T_g	Glass transition temperature
v_f	Fibre volume fraction
V	Voltage
w	Out-of-plane deformation
x,y,z or X,Y,Z	Global rectangular co-ordinate system

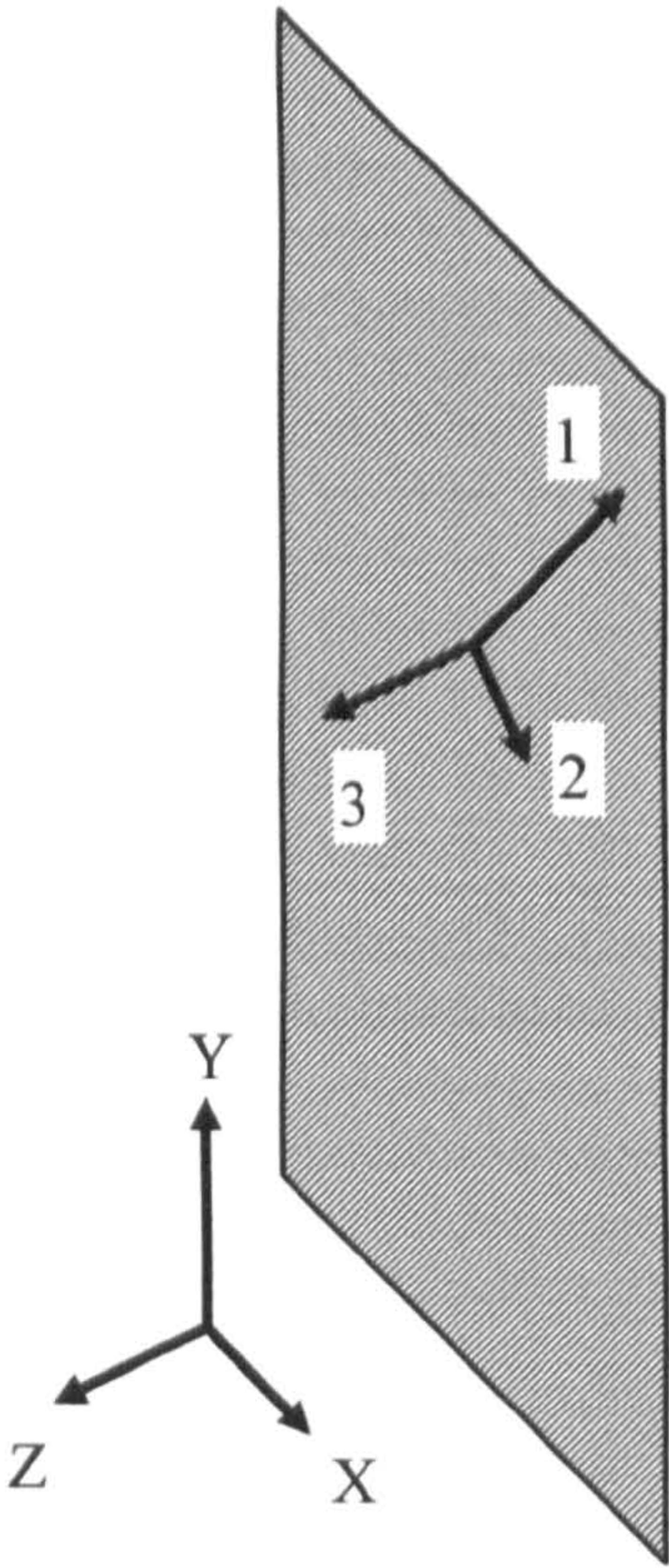
Greek Letters

α	Coefficient of thermal expansion
δ	Displacement
ΔT	Temperature differential
ϵ	Strain
λ	non-dimensional buckling load
ν	Poisons ratio
ρ	Density
σ	Normal stress
τ	Shear stress

Co-ordinate System

The axes 1, 2 & 3 refer to the rectangular co-ordinate components within a unidirectional ply. 1 denotes the fibre direction. 2 denotes the direction transverse to the fibres in the plane of the ply and 3 denotes the direction normal to the plane of the ply and at right angles to 1 & 2.

The X, Y & Z components denotes the global rectangular co-ordinate system. Throughout this thesis, the components X & Y form a plane which is parallel to the constructed plies. Z is in the direction normal to this plane. Unless otherwise stated, within this thesis, the applied compressive load is parallel to the global Y axis.



Introduction

The work presented in this thesis was conducted as part of a **Total-Technology PhD** programme, undertaken at the **Cranfield College of Aeronautics**, and as part of an industrial research contract. The industrial participant being the UK's **Defence & Evaluation Research Agency**.

The objective of this investigation was to assess the feasibility of enhancing the post-buckling load bearing capability of carbon/epoxy composite plate/panel structures utilizing embedded activated shape memory alloy, SMA, wire form actuators. Improved post-buckling performance is realised by removing the post-buckling displacement and alleviating non-linear stress distributions characteristic of a post-buckled specimen. Post-buckling enhancement is achieved through utilization of the unique shape memory phenomenon inherent within a shape memory material. The unique phenomenon requiring a thermal stimulus. Several host laminates, varying in lay-up architecture, have been considered. Initially, it was the intention to enhance the post-buckling performance of composite plates. On completion of this study, stiffened sections were examined.

Glass/epoxy was first assessed for use as a host material within this investigation, however, its non-ideal thermo-mechanical properties precluded its involvement within this programme of study. This led the author to select the much exploited carbon/epoxy as the host composite.

Two control strategies have been employed that utilize the unique SMA response at an elevated temperature. *Control strategy 1* features embedded SMA actuators located within tubes that run along the specimens neutral plane. Here, the SMA's are constrained to external boundaries. See **Figure I.1** for an illustration of *control strategy 1*.

Control strategy 2 also features embedded SMA actuators. For this control strategy, however, the actuators are *partially* constrained to the host laminate and embedded on one side of the specimens neutral plane. An illustration of *control strategy 2* is presented in **Figure I.2**.

For each strategy, upon SMA energization, shape memory constraint results with the formation of a recovery force within the SMA material. It is this recovery force that is employed to control the post-buckling response of the selected laminated specimens.

The concept of adaptation pertaining to *control strategy 1* is quite perceptible. *Control strategy 2*, however, was devised based on initial trial fabrications of SMA actuators embedded within host polymeric-matrix composites. Initially, SMA wires were placed on both sides of laminated plate constructions in a symmetric manner. Upon electrical energization of SMA actuators to one side of the specimen, thermal energy propagation throughout the laminate would thermally energise the opposite SMA actuators. The resulting unintentional SMA thermal stimulation counteracting the desired response. As will be discussed later in this thesis, this would also occur even when placing the actuators in a skew symmetric manner.

Again, as will be discussed later in this thesis, generated internal membrane stresses associated with the activation of completely constrained pre-strained SMA actuators degrade the post-buckling response of the thin plate/panel specimens. Utilization of partially constrained actuators, in order to minimise the distribution of such membrane stresses, was foreseen as a viable means for controlling the post-buckling response of the composite stiffened panels of *control strategy 2*.

The integration of shape memory material within the structural load bearing composite host specimens enables structural adaptation. Efficient adaptation requires some form of

input. To be efficient, the level of input will be dependent upon the operating circumstances. In general, sensory interpretation is employed to ascertain such circumstances. In ensuring the structural adaptation of the SMA/composite specimens, the input required is temperature.

The term *smart* is the commonly referred to terminology that describes the ability of a material, component and/or structure, to sense it's condition and/or environment such that, should it desire, it may tailor its configuration/response in a beneficial manner. To be truly *smart*, a particular product must have sensory and actuating functions linked via closed-loop control. The latter providing appropriate feedback response.

Within this research programme, the author has focused upon the structural adaptation traits of the smart structure discipline. Focusing on the structural adaptation of various test specimens allows for a quantitative assessment to be made on the capability of the chosen enabling *smart* constituent, that being the shape memory alloy.

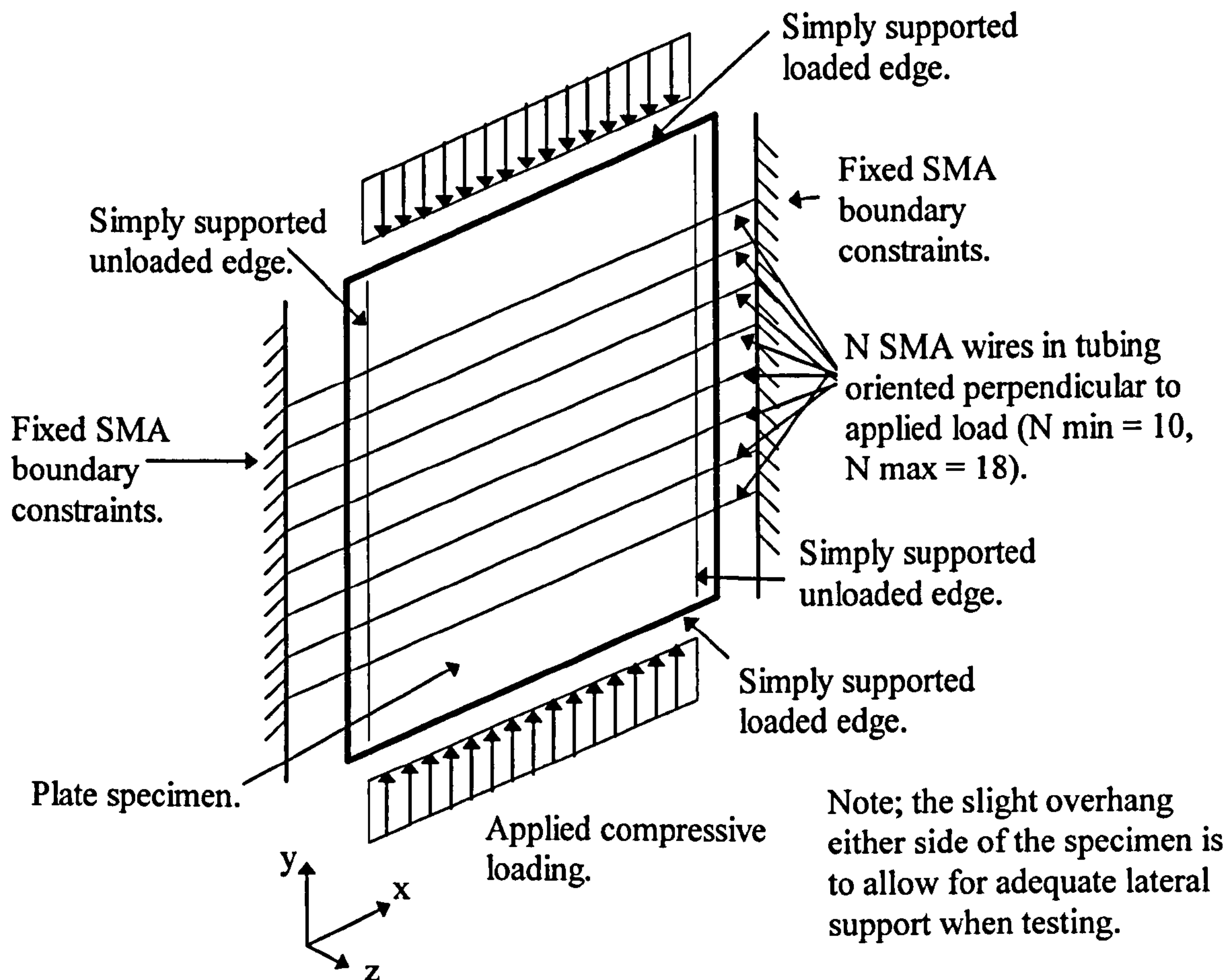


Figure I.1. Schematic of Control Strategy 1.

For each control strategy, shape memory constraint results with the formation of a recovery force within the SMA material. It is this recovery force that is employed to control the post-buckling response of the selected laminated specimens. Adaptive control realising the removal of undesirable out-of-plane deformations and the alleviation of some, if not all, of the non-linearity associated with the in-plane membrane stress distributions. The latter is typical to the post-buckled configurations.

The focus upon smart, adaptive or tailorable composite structures is on enhancing the host's structural function while requiring less mass. It is foreseen that a fully adaptive structure could, in principle, be configured to withstand the *normal* loading conditions in its unmodified form but utilize appropriate actuation systems to cope with abnormal loads.

The subject under study is truly multi-disciplinary. As presented in the forthcoming chapters, it has required knowledge of numerous disciplines.

Chapter 1 first introduces the topic of integrating additional functionality within structural host materials. As previously stated, such functional integration has resulted with the commonly referred to *smart* concept. Additional terminology is presented. This is followed by a broad definition of the concept that may provide the reader with a more plausible perception of such multi-functional structural integration.

Present day technologies are then introduced that will enable synergised smart structures, or smart products, to come to fruition within the marketplace. Here, the favoured technologies of optical fibres, piezoelectric elements and shape memory alloys are highlighted.

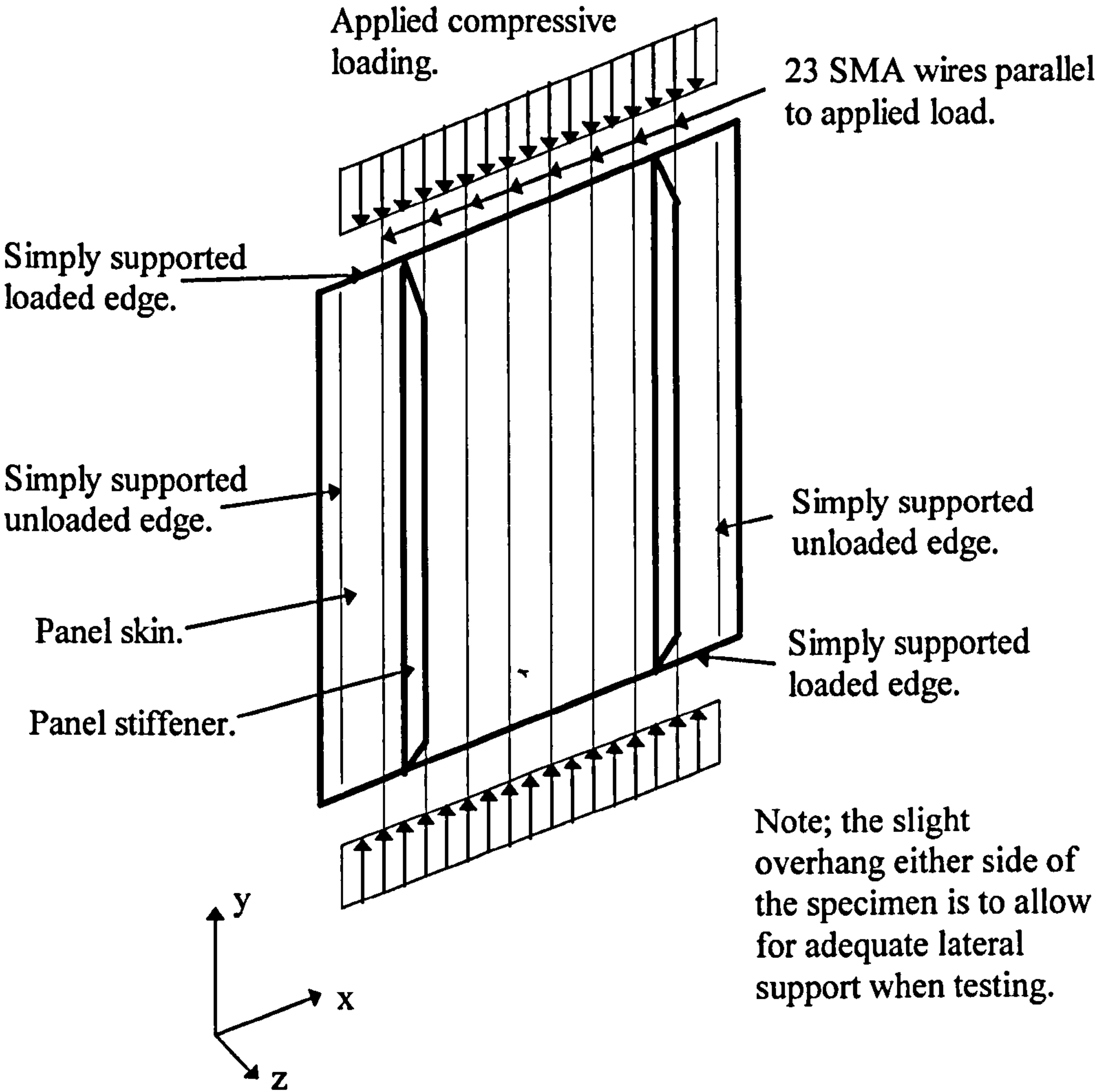


Figure I.2. *Schematic of Control Strategy 2.*

Although in later chapters the author focuses upon actuation, the optical fibre sensing element is introduced as we can conduce an analogy regarding the specimen fabrication implications. Optical fibres having the same geometrical nature as the chosen shape memory alloy actuator. Sensing is finally called upon in **Chapter 7** to aid promotion of the *smart* concept.

With regards the smart products within the marketplace, the author focuses upon aerospace products, in particular, aircraft and spacecraft. Aerospace being the focal point as aerospace offers the greatest potential to exploit the increase in performance, efficiency and reliability, that such smart integration has to offer.

The chapter continues with a review of some of the research work regarding composite specimens with embedded SMA elements. It will be shown that the unique characteristics of such alloys can be exploited to enhance numerous diverse structural parameters such as dynamic or impact response as well as the buckling and post-buckling characteristics.

As found in the open literature, **Chapter 1** concludes with a thorough account of the use of smart technologies in enhancing the buckling and post-buckling of a variety of structural configurations.

It was previously stated that shape memory alloys are the actuating elements to be employed for post-buckling adaptation. **Chapter 2** provides an in depth account of the unique shape memory phenomenon and, in particular, the constrained recovery behaviour at elevated temperature for two different alloys. The latter being exploited in later chapters.

The chapter begins with an account of the unique shape memory transformation process. For the chosen alloys, i.e. alloys having near equiatomic binary Nickel-Titanium compositions, the phase transformation characteristics have been assessed. Such characteristics being the temperature range required for complete solid-solid phase transition.

An accurate perception of the phase transition characteristics enables a sufficient electrical power level to be calculated that can be called upon to energise the pre-treated actuators. Within this document, pre-treatment generally refers to the initial percentage level of pre-strain, or the initial percentage level of detwinned martensite, present within the material prior to embedding and actuation within the host composite. For *control strategy 2*, pre-treatment is also associated with a sufficient interface enhancing process, therefore, preventing SMA/host interface debond that will otherwise render insufficient adaptation/tailorability.

Chapter 2 concludes with an assessment of the integrity of varying pre-treatment processes. This assessment compares the recorded recovery force, at a certain power level, for the chosen Ni-Ti alloys, at selected levels of pre-strain, component processing conditions and/or the interface enhancing technique and compares the results obtained with a baseline recovery force response.

For the plate and panel specimens considered, **Chapter 3** focuses upon the hybrid SMA/composite test specimen fabrication procedures, control strategy methodology and the mechanical implications of embedding SMA actuators within host laminates. The implications being host property degradation.

Within the research conducted, two techniques are presented for embedding SMA actuators within laminated constructions. The first method utilizes pre-strained SMA wire actuators fed through tubes, situated on the specimens neutral plane, after the composite has completed its curing cycle. The SMA actuators are constrained by an external means. Such inclusions manifest a serious geometric and material discontinuity that needs quantification. The extent of local ply disruption is quantified optically and, for the worst case conditions, examined numerically.

The second of the control strategies utilize SMA actuators that are partially constrained to the host matrix. The high recovery force, attributed to the constrained shape memory recovery, poses a serious hazard to the interface region local to the SMA/host. Measures are taken that elevate the magnitude of the interface fracture stress, thereby, preventing interface breakdown within our SMA/host operating circumstances. This interface strengthening mechanism is experimentally quantified at test temperatures ranging from ambient up to 108°C.

Chapter 4 is associated with the buckling and post-buckling behaviour of our selected symmetrically laminated orthotropic and anisotropic plates and panels. Within this chapter, the main emphasis is on the mechanical modelling aspects. The study features the interaction

between internal and external mechanical loads along with the superposition of a non-uniform temperature profile. The influence of the external mechanical loads is initially considered. The thermal aspects are presented in **Chapter 5** and extensive numerical and experimental adaptive post-buckling results are presented in **Chapter 6**.

Within **Chapter 4**, the phenomenon of buckling is first discussed. The chapter continues with a discussion of the specimens to be tested and the employed loading rig. Basic theory pertaining to the modelling aspects will then follow. The finite element method of analysis was predominantly used throughout this research work as it is the most powerful numerical tool available today for predicting the response of composite structures.

The finite element constructions employed to represent the two control strategies are then presented. The geometric representations, SMA actuator representations, geometric boundary conditions and a finite element convergence study complete the finite element modelling discussion.

Numerical and experimental buckling results are then tabulated and discussed. **Chapter 4** ends with presented comparisons of the experimentally obtained and numerically determined post-buckling response for several of the composite specimens. The results presented here are typical of the plate/panel specimens devoid of shape memory adaptation. The results obtained with adaptive control are presented in **Chapter 6**.

Subjecting the SMA wire actuators to an electrical stimulus will result with resistive, or ohmic, heating. As illustrated in **Chapter 5**, such heating imposes a non-uniform temperature profile over the surface of the laminates. This temperature profile needs evaluation so that an account can be made for the resulting macro-scale thermal stresses in the non-linear analysis, **Chapter 6**.

For each control strategy employed, the determination of the temperature distribution inside the representative composite lay-ups has been calculated numerically and verified experimentally. The influencing effects that these temperature profiles will have on the fabricated lay-up's mechanical response is discussed.

Within **Chapter 5**, the governing classical heat flow equations are first presented. Their utilization within the employed thermal numerical software package is then discussed.

Based on the assumption of a uniform temperature distribution in a direction parallel to the SMA wires, a two-dimensional thermal model was used to predict the cross-sectional temperature profile within each of the plate/panel specimens. Experimental and numerical thermal results for each configuration is presented. Thermal stresses, associated with the non-uniform temperature profiles, can then be analysed.

To conclude **Chapter 5**, the subjects of matrix temperature dependency and creep within polymeric-matrix composites are briefly discussed.

Chapter 6 presents the numerical and experimental adaptive post-buckling results for each specimen configuration considered. The experimental set-up is first presented. This will be followed by detailed experimental and numerical results for each specimen. The numerical and experimental results presented will be in the form of applied load and out-of-plane displacement with respect to time. Where appropriate, numerical in-plane force intensity profiles will be provided.

The results presented clearly illustrate the effect that activated embedded SMA inclusions have on the laminates post-buckling response. The discussed results will later be summarised in tabular format within the Conclusions & Further Work section of this document, **Chapter 8**. Envisaged structural applications have been identified that could utilize the findings presented in this chapter. Such applications are presented in **Chapter 7**.

In fulfilling the Total-Technology PhD requirements, in addition to a conventional PhD's technical research content, this thesis contains non-technical aspects associated with the proposed subject. The non-technical discussion is presented in **Chapter 7**.

Chapter 7 focuses on some of the commercial aspects pertaining to the introduction of smartness within polymeric-matrix composite aerospace platforms. This chapter does not limit itself to SMA/PMC smart composites, the constituents predominantly discussed throughout this thesis. It also calls upon the analogous PMC sensory integration, first introduced in **Chapter 1**, therefore, enabling a wider perspective of the marketplace, in which smart PMC materials will enter, to be established.

The chapter first discusses the desired performance benefits anticipated with utilization of PMC materials throughout an aerospace platform and how the performance benefits can be extended with the integration of smartness to the host. The next section discusses new product development. The focal point of new product development being the utilization of passive PMC and smart PMC constituents within aerospace products. This section ends with a discussion pertaining to the marketing of products within the marketplace. Here, the important parameter within marketing is the deliverance of the *marketing mix* to the marketplace.

The chapter continues with a discussion pertaining to the current utilization rate of advanced polymeric-matrix composites within the marketplace. A non-exclusive list of future aerospace products that will significantly extend this current PMC utilization rate is presented. Subsonic civil aircraft, supersonic civil aircraft, military aircraft and space based platforms are all discussed. A more detailed account of the marketplace for future second generation supersonic civil aircraft is provided for as the author feels that such an aircraft offers great potential for incorporating SMA/PMC materials.

The chapter continues with a discussion pertaining to the vitally important issue of *affordability*. Affordability being subdivided into the two governing topics of procurement cost and cost of ownership.

Finally, the author's research conclusions and proposals for further work are presented in **Chapter 8**.

Chapter 1

Smart, Adaptive or Environmentally Responsive Materials & Structures

1.1. Introduction

Within aerospace platforms, ground based high speed transportation systems and large civil engineering infrastructure, the integration of additional functional elements with conventional passive structural design offers the potential for enhanced performance, lowering operating costs and enhanced survivability.

The author believes that future aerospace platforms, such as military aircraft and spacecraft, offer the greatest potential for structurally integrated functionality. It is these end products, therefore, in which the current reported research concentrates upon.

The focus of this chapter is to provide an introduction to the increasingly publicised terminologically interchangeable field of smart, intelligent, active, sensory, adaptive or environmentally responsive materials and structures. The author feels that the *smart* or *intelligent* terminology, commonly referred to in the open literature, is somewhat misleading when utilizing the current state-of-the-art macroscale sensing and actuating techniques. The latter *adaptive* or even *environmentally responsive* terminology provides for a more plausible perception. The correct terminology does, however, depend on the structurally integrated elements.

This subject area is truly multi-disciplinary. Functional structural design requires knowledge of numerous disciplines including, amongst others, sensing, actuating and, of course, conventional structural engineering or design. The interested reader is referred to numerous books, published within the open literature, which provide comprehensive introductory accounts of the smart/adaptive technologies¹⁻³. Such references provided invaluable assistance throughout this research programme.

The chapter begins with a brief definition of the authors perception of smart, adaptive or environmentally responsive materials and structures. The most popular building-blocks of such structures, i.e., the structural host, sensing and actuating elements, are then described. An elaborate account of the structural actuation function is presented as this is the main focus of the research presented in later chapters. The functional element selected for this research programme is introduced and, where appropriate, envisaged *smart* aerospace products are discussed. Such products typically being related to aircraft or spacecraft. The chapter ends with a review of the published work pertaining directly towards the authors research goal.

1.2. A Structurally Integrated Functionality Definition

What do we mean by *smart* or *adaptive* structures? By this, we mean structural components that are able to monitor their internal state and/or their environment such that, should they desire, they can respond to derisory changes in the operating conditions. Such changes being, for example, the realization of an optimal configuration in which the component and, therefore, the global platform can operate.

Such structures, depending on the desired level of functionality, require the incorporation of sensory and/or actuating mechanisms. Optical fibres have received the most attention for sensory applications. Piezoelectric, electrostrictive, magnetostrictive, electrorheological fluids and shape memory materials have frequently been proposed for actuation. The sensing and actuating techniques are described in detail later in this chapter.

Sensory integration requires the means for conveying and interpreting the measured signals, and if necessary, signal conditioning. In general, signal interpretation is met by high speed digital computers. Actuation necessitates the provision of a suitable energy source. An electrical energy source has frequently been applied for energization of the aforementioned actuators. For the overall configuration to be responsive, the system must have closed loop actuator feedback, whereby, the actuator input is dependent on the information received and interpreted.

The so called smart/adaptive systems are considered as an analogue of the human nervous system⁴. Sensors, i.e. optical fibres, act like nerves. The actuators, based on piezoelectricity, electrostriction, magnetostriction, variable viscosity or shape memory, perform like muscles. The role of the brain is carried out by real time processors.

Nickel-Titanium, a well established and commonly exploited shape memory alloy, is the actuating mechanism employed within this investigation. Actuation, as will be described, is met through an inherent solid-solid phase transition. Such phase transition necessitates a temperature rise. Whilst this can be met through the application of an electrical power source, it may also be provided by the operating environment. The use of the latter stimulus, for the energization of such a material, within aerospace components is quite conceivable when considering the operating temperatures of high speed flight vehicles, propulsion systems, or the environment of earth orbiting space systems.

This leads us on to the definition of *environmentally responsive* materials and structures. Such materials and structures can respond to environmental changes, i.e. temperature, and, thus, autonomously adjust themselves toward the optimum condition. The enhancement of the desirable response is met when such actuating mechanisms are integrated with the passive host structure. The system may be completely devoid of any analogue or digital sensing interrogation techniques previously described.

With the integration of any combination of sensing and/or actuation with a passive structural host, then, clearly such structures are different from existing materials and structures. Self-sensing will allow a structure to request for any necessary repair work. Actuating elements allow the structure to control its response to applied mechanical loads, particularly when influenced by unusual loading conditions. The conventionally designed passive structural response has no means of monitoring or coping with unpredictable loads other than through *expensive* over design. Over design of aerospace platforms, enabling the safe operation during the worst case scenario of operating circumstances, results with inferior efficiency during normal operating conditions⁵.

It is the need for future aerospace structures to have increased performance, efficiency and reliability which has boosted research in the field of the aforementioned smart, adaptive or environmentally responsive materials and structures. It is foreseen that such structures will be lighter in weight and, therefore, consume less energy to operate. This will be of tremendous benefit to aerospace platform operators.

Parts of this chapter are devoted to a series of aerospace applications that hold considerable promise for the near term realization of these multi-functional structures. The following section discusses individual constituents of the hybrid components. Emphasis is placed on the constituents employed within this research programme.

1.3. The *Host* Composite Structure

The engineered material, utilized in conventional passive structural design, provides the foundation for the current generation of smart/adaptive or environmentally responsive structures and is referred to as the structural *host*. Such terminology is widely accepted within the published literature. The aim of this section is to introduce the host material employed for the investigation of this thesis. Such a host being the amalgamation of pre-defined constituents forming an advanced composite.

Aircraft and spacecraft have become increasingly weight-sensitive. As a result, recent aeronautical platforms have seen an increased use of advanced composite materials. Advanced composite materials comprise of structural load bearing reinforcement bound by a rigid matrix. The matrix, along with other functions, provides a means for load transfer to the reinforcement. The overall properties of the macroscale composite are far better than those of the individual constituents. The actual properties being influenced by the constituents, volume fraction, their distribution and local interaction.

Advanced composite materials can be separated into four distinct groups; metal-matrix composites, ceramic-matrix composites, carbon-carbon composites and polymeric-matrix composites. The former three groups are currently receiving considerable interest for high temperature applications, i.e. future engine design and high speed civil aircraft demonstrator programmes⁶. It is the latter polymeric-matrix composites, however, that have received the widest attention for procurement within aerospace platforms. While initially employed for secondary structures, they are increasingly replacing their metal counterparts as primary load bearing members, especially within military aircraft where issues of performance are critical.

For an aerospace application, the selection of the polymeric-matrix composite over the conventional metals is based solely on their high specific strength and stiffness ratios. An aircraft comprising a higher specific strength/stiffness airframe will provide for increased payload or lower direct operating costs. Additional advantages include excellent fatigue resistance, corrosion resistance, and ease of tailorability of macroscale mechanical properties.

Helicopter rotor blades have, for a long time, used polymeric-matrix composites, not only for weight reduction, but also for the ability to tailor the dynamic frequencies with variation of mass and fibre orientation along the blade length.

Continuous reinforced carbon based polymeric-matrix laminates can be designed to have zero coefficient of thermal expansion in a desired direction. This has led to their selection for space applications where dimensional stability is required for thermally stable structures.

Polymeric-matrix composites have been extensively used on a variety of civil aircraft. Until recently, they have mainly been limited to flight control surfaces and engine nacelles. However, increased confidence and experience with their operating performance has led to their selection for primary structures. An example of this is the recently certified Boeing 777 having an all composite empennage comprising carbon reinforcement bound by a toughened epoxy resin.

Military applications have seen the widespread use of polymeric-matrix composites, particularly in air vehicles. As an example, the much publicised Eurofighter 2000 has 70% of the outer aircraft skin manufactured from carbon fibre composite solid laminates or honeycomb panels. An additional 12% of the outer surface is comprised of glass fibre reinforced material.

In comparison to the metal alternatives, however, a composite designer should be aware of their disadvantages. These being poor transverse and shear strength, environmental degradation, low fracture toughness, inherent brittleness, thus, propensity to delaminate upon impact, poor erosion resistance and poor fire resistance. These limitations, along with

complex failure mechanisms, have lead, in the past, to significant over design and, therefore, a subsequent reduction in their advantageous use.

The constituents employed within the advanced polymeric-matrix composites are varied. The reinforcement may comprise of unidirectional fibres, or woven weaves of, carbon, glass, kevlar or boron. The direction of the reinforcement is arbitrary. Its orientation is dependent on the required thermo-mechanical properties in a particular direction. A variety of matrix formulations exist. Thermosets, i.e. polyester, epoxy, polyimide, bismaleimide, and thermoplastics, i.e. polyetheretherketone, have each received widespread interest.

The choice of the individual constituents is based on the operating conditions and requirements for a particular part. As an initial guideline, matrix selection is based on the operating temperature of the manufactured component for a particular application. Epoxy is the most common matrix employed for aerospace applications. Generally it has a working temperature of approximately 130°C, though, higher temperature formulations exist that have operating temperatures of approximately 180°C.

Reinforcement of the matrix is met through judicious choice of the aforementioned reinforcing constituents. Boron/epoxy has very high mechanical stiffness and strength. However, it is the most expensive reinforcing constituent. Kevlar/epoxy has excellent impact resistance. It is difficult to machine, however, and has poor compressive properties. Glass/epoxy composites have the lowest raw material costs, therefore, making them ideal for mass production. They have, however, the lowest mechanical strength and stiffness properties as well as a slightly higher density. Carbon/epoxy composites, in-particular, high-strength carbon/epoxy, have many excellent properties which have led to their widespread exploitation within military and space applications.

Carbon/epoxy, in the initial form of unidirectional prepreg, is the material employed for this investigation. Its selection was based on its widespread acceptance within the aerospace community for high performance parts and the anticipated ease of integration and consolidation of the chosen adaptive constituent.

Integration of functionality within the selected advanced composite host is a step towards more efficient tailorable aerospace platform structural design. Direct integration of either sensing or actuating mechanisms within the laminated constructions will result with a fully load bearing multi-functional component. Their incorporation should be such that the global mechanical response is not adversely affected. Such affects being discussed in greater detail in **Chapter 3**.

Numerous texts are devoted to the field of polymeric-matrix composites. The author acknowledges the published texts⁷⁻¹⁰ which have provided valuable insight into the characteristic behaviour of the selected material, therefore, enabling completion of this research programme.

1.4. State Of The Art Sensing

Sensors are an essential ingredient for a smart/adaptive structure as they provide the ability to monitor the external stimuli and even the subsequent behaviour throughout the structural medium. Dependent upon the host material selected for a particular application and the desired measurand, sensors can either be surface attached or directly embedded within the host. The latter can easily be achieved when considering the polymeric-matrix composites.

A variety of approaches are available for sensing the real-time status of a structure. They include the use of piezoelectric materials, optical fibres, foil gauges and LVDT's³. For

internal status monitoring, clearly, the choice of sensor is dependent upon the manufacturing aspects of the host. The sensor should be selected such that the fabrication route will not damage the sensor and, also, for minimal sensor influence on the overall structural response.

Optical fibres have numerous advantages over their alternatives. They are small in size, light weight, low power consuming glass cylindrical fibres that permit distributed measurement². The piezoelectric materials, foil gauges and LVDT's are limited to sampling over finite areas.

Optical fibres have wide bandwidths, immunity to electromagnetic interference and the ability to survive under relatively hostile environments. In addition, if incorporated into aircraft components, for example, their multiplexing ability can allow direct communication links to flight control surfaces⁵.

Such smart sensing has received considerable interest with regards to the monitoring of laminated constructions. Although this type of sensing is not directly related to the author's research programme, there exists an analogy pertaining to the manufacturing and sensor inclusion implications.

Smart sensing is briefly described here as it will be called upon in **Chapter 7** to aid the promotion of the smart, adaptive or environmentally responsive structural components where cost benefits and product marketing issues are discussed.

1.4.1. Fibre Optic Sensors

One of the unique features of fibre optic sensing is that an extremely rich variety of transduction techniques are available. In all cases, light is transmitted through the optical fibre. If a glass fibre is unstrained, an initially unpolarized light signal, propagating along the fibre, remains unpolarized. However, if the glass is strained, the signal becomes birefringent. The path length that the light ray must travel is also altered.

Fibre optic sensing technology is based on observing and analysing such modifications in amplitude, wave length, phase and polarization characteristics of the signals within the sensor and, therefore, the structure being deformed.

Commercially available sensors are based upon one of three different approaches; (i) Fabry-Perot sensing where the light-ray phase changes are monitored, (ii) Fibre Gratings where light-ray reflectivity and transmission are monitored, and (iii) Optical Time Delay Reflectometry (OTDR) where the optical time delay is monitored.

For example, OTDR relies on measuring the intensity of back-scattered light that has been launched into the fibre from an amplitude-modulated pulsed source. The light is Rayleigh scattered from refractive index fluctuations in the core of the fibre, or may be reflected from discontinuities, such as connectors, splices, or fibre breaks.

Generally, optical fibres show very high sensitivity but they do additionally require considerable data processing, especially when dynamic strains are required.

1.4.2. Health Monitoring Concepts

The purpose of sensor integration onto, or within, a structure is to enable the structure to record its loading history and present a record of its condition with a view to alerting some form of corrective action. Such action may include the alleviation of unwanted vibrations on earth observation spacecraft or a warning to maintenance personnel, of large civil or military

aircraft, indicating damage initiation/accumulation associated with impacts. Impacts resulting from bird strikes, runway debris and, for military aircraft, battle damage.

Although the technology is not yet fully mature, aerospace systems with a self-sensing capability will be capable of diagnosing their status prior to takeoff/launch and schedule post-flight repair and maintenance¹¹. This will undoubtedly reduce personnel requirements, therefore, reduce operating costs, while increasing safety. Such systems will be capable of optimizing the components lifetime and, therefore, the global platforms utility.

To ensure maximum flight safety and in-service life, it is essential to keep track of the load spectrum and damage initiation/growth experienced by the aerospace platform. A monitoring procedure may be outlined as shown in **Figure 1.1**¹².

Several novel examples regarding sensory techniques are now presented. Concern regarding *barely visible impact damage* within polymeric-matrix composites has led to research into novel monitoring schemes where the composite contains built-in non-destructive evaluation. The embedding of optical fibre sensors, during the fabrication of polymeric-matrix composites, has received considerable interest. Not only can the *in-service* internal life of the component be monitored, but also the quality of the part can be verified, prior to service entry, by monitoring the fabrication parameters such as temperature, pressure and viscosity. Such a built-in capability will augment current non-destructive evaluation techniques. It is expected that this system will lead to greater confidence in the use of advanced composite materials and weight savings through avoidance of over design.

Several researchers have investigated the potential for built-in damage detection systems^{13,14}. A typical system consists of both sensors and actuators linked to processors¹³. Local actuators dynamically excite the structure while sensors measure its response. The processor compares the response to a baseline previously measured from the undamaged structure. A significantly differing response may indicate damage accumulation.

Such a technique will require an excitation with a wavelength smaller than the characteristic length of the damage itself. Piezoceramic actuation may provide high-frequency excitation. Finding damage at this level provides a warning prior to actual failure¹³.

The detection and location of impacts in graphite epoxy composite laminates has been studied¹⁴. This scheme employs the highly sensitive extrinsic Fabry-Perot interferometric optical fibre based sensing system for recording the differential arrival times of impact generated signals using a set of four sensors. The sensors are completely embedded in the composite specimen. A mathematical model is coded into a computer program to enable real-time, on-line determination of impact locations. It is reported that the precise location of the impact can be deduced to an accuracy better than 5mm.

As an alternative, in-situ monitoring of electrical resistance variation¹⁵ allows the detection and identification of various types of damage mechanisms. Here, carbon fibres are not only the reinforcement but also the sensors for damage detection. Since carbon fibres are electrical conductors, the measurement of the variation of electrical resistance appears to be a valuable technique for in-situ damage detection. Electrical resistance measurements detect fibre breakage in longitudinal unidirectional CFRP laminates. In the case of transverse and through thickness measurement, where the conduction behaviour depends only on fibre to fibre contact, the technique requires a sufficient carbon reinforcement volume fraction.

Having introduced sensory integration within a structural load bearing host, the chapter continues with an introduction to the most promising elements required for structural adaptive tailorability.

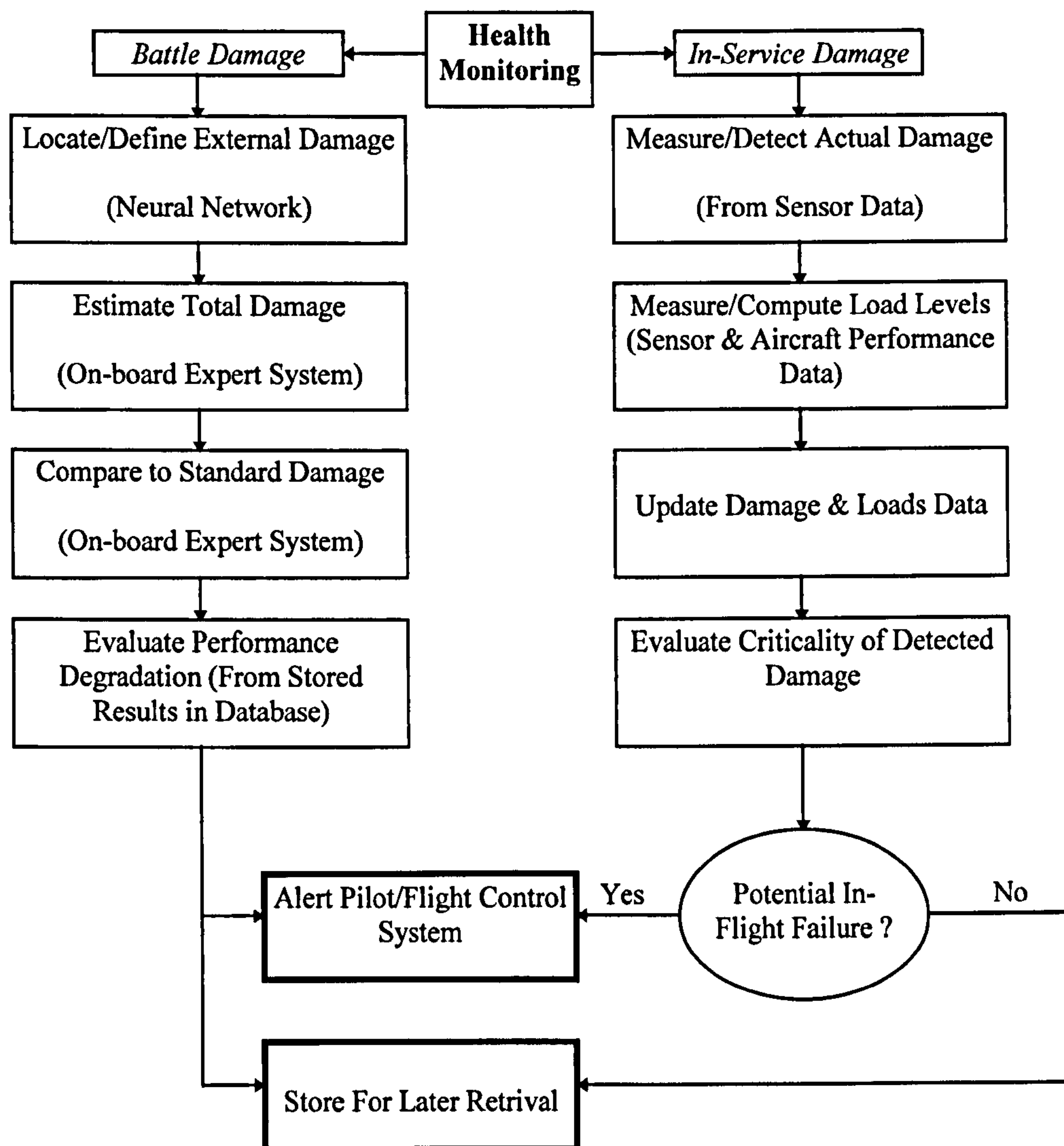


Figure 1.1. *Flowchart of structural health monitoring procedure¹².*

1.5. State Of The Art Actuation

The actuators in smart, adaptive or environmentally responsive structures provide the mechanism for the structure to adapt to its surroundings¹⁶. The actual mechanism behind the most common actuators will be described in the following subsections. Generally, it is met through changes in the actuating materials shape, stiffness or both.

The most important actuator materials for tailorable structures are piezoelectric materials, electrostrictive materials, magnetostrictive materials, electrorheological fluids and shape memory alloys. For aerospace products, the actuators selected should be lightweight, relatively inexpensive and have minimal effect on the host's passive structural response.

For this investigation, it is the general conception that, when integrated into a structure, the actuating mechanism will influence, or induce, localised strains which, in turn, influence the bending response of the structural elements. A desirable parameter, then, is the level of induced strain attainable.

1.5.1. Piezoelectric Materials

Piezoelectric materials exhibit significant material deformations in response to an applied electric field. Alternatively, they produce dielectric polarization when mechanically strained. Such materials, therefore, may be employed for either sensing or actuation. In relation to piezoceramics, such transducers are created by poling an appropriate anisotropic substrate through the application of a large electric field at high temperatures. Poling has the effect of partial alignment of an initial randomly oriented piezoelectric crystal structure. Subsequent exposure to an electric field results with electromechanical coupling. An excellent review of piezoelectric materials, in particular the piezoceramics, is provided for in the open literature¹⁷.

Figure 1.2 shows the co-ordinate system used to represent a poled piezoelectric material. The 3-axis is in the direction of the initial polarization. The 1 and 2-axes are arbitrary in the plane perpendicular to the poling direction. They are arbitrary because a poled piezoelectric is transversely isotropic in the 12-plane.

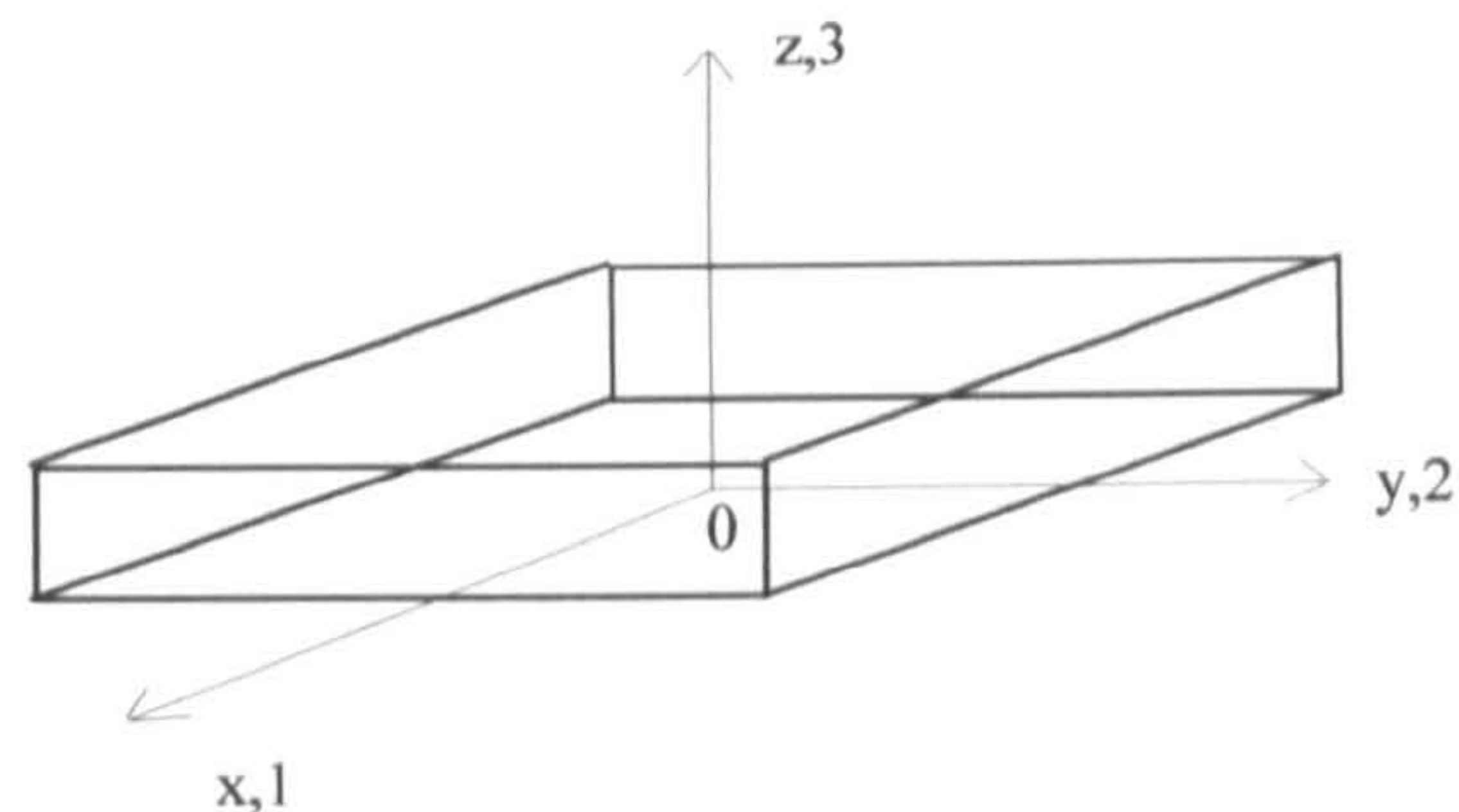


Figure 1.2. Piezoelectric co-ordinate system.

For structural control purposes, the piezoelectric constants, d_{xy} , are of most interest. These constants relate the strain developed in the material to the applied electric field. The d_{33} constant relates the strain in the 3 direction to the field in the 3 direction. Similarly, d_{31} relates the strain in the 1 direction to the electric field in the 3 direction. An important point is that d_{33} is usually a positive number and d_{31} is negative. This means that a positive field, applied in the poling direction, will produce a positive mechanical strain in the 3 direction and a negative strain in the 1 direction.

When subjected to an electric field, the total displacement of the piezoelectric element is simply;

$$\delta = d_{xy} * V \quad (1.1)$$

where V is the potential difference in Volts. The displacement, δ , is of the order of $1\mu\text{m/kV}$. The sign of the displacement is determined by the polarity of the field.

The two most widely used piezoelectric materials are piezoceramics and piezopolymers, of which, lead zirconate titanate, PZT, and polyvinylidene fluoride, PVDF, are respective examples. Typical material properties are presented in **Table 1.1**.

Due to their ceramic nature, PZT patch actuators have rigidities that are comparable to and often exceed those of the underlying host structure. This results with efficient conversion of electrical energy to mechanical energy, therefore, providing efficient actuation. However, they do suffer from inherent brittleness.

The direct application of PVDF films as actuators is limited by the lower piezoelectric constants and the fact that they are much more compliant than most underlying host structures. They, therefore, exhibit significantly weaker electromechanical coupling when compared to those of the PZT's. The dielectric strength of

Property	PZT	PVDF
ρ (kg/m ³)	7.8	1.8
Max. T (°C)	≈ 200	100
E (GPa)	70	2
Max. ϵ ($\mu\epsilon$)	1000	700
d_{31} ($*10^{-12}$ m/V)	-119 → -234	20
d_{33} ($*10^{-12}$ m/V)	268 → 480	-33
E_c (V/ μm)	1	10^4

Table 1.1. Typical piezoelectric properties.

PVDF, however, is larger than the PZT's, hence they can be exposed to higher electric fields.

PVDF films have the consistency of a thin plastic compliant wrap, therefore, they can easily be bonded to virtually any geometry, hence, they are effective sensors. Generally, piezoelectric elements are very thin and, therefore, can easily be integrated with the host component. The very high poling field, of the order of $100\text{V}/\mu\text{m}$, limits the maximum thickness that is realistically available. For a piezoceramic actuator, limiting the drive voltage to readily achievable levels, for example 100V , implies that the maximum usable PZT layer thickness is of the order of $100\text{ }\mu\text{m}$.

A significant advantage of these materials for actuation purposes is their micro-second response times. Piezoceramic actuation, therefore, has received considerable interest for vibration control applications where such actuators can be bonded to host structures without compromising the passive structural response. Structural control through such actuation is as follows.

When the actuator, subjected to an appropriate electric field, is fully constrained, it develops a maximum force, known as a blocking force. When there is no constraint, the actuator expands/contracts freely to its limit and no force is developed. When the actuator is coupled to a system or structure, depending on the actuator/structure stiffness ratio, the actuator develops a certain force. It is this force that is used to control the extension or bending response of somewhat flexible structures.

This underlying principal is illustrated in **Figure 1.3**, where collocated surface bonded actuators are subjected to equal or opposite fields. As depicted, in-plane uniform strains arise within the host substrate when the actuators are subjected to equal electrical field sign intensities, **Figure 1.3(a)**. **Figure 1.3(b)**, however, depicts bending strains which arise due to the application of equal but opposite electrical field intensities.

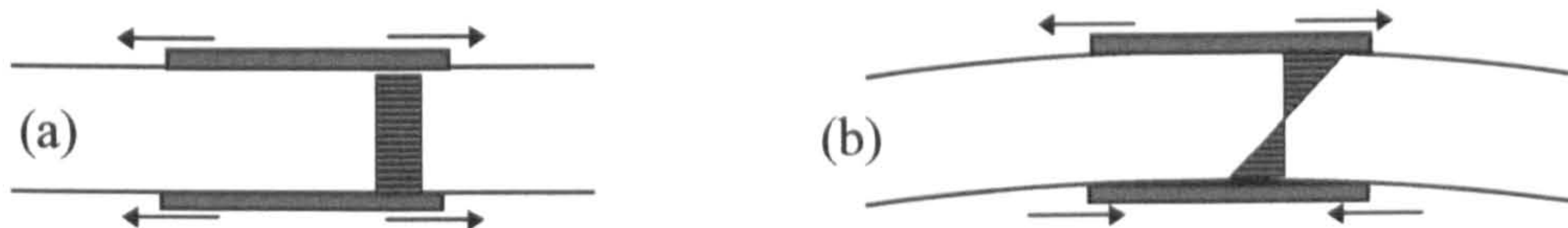


Figure 1.3. Surface mounted piezoceramic actuators; (a) in-phase voltages producing in-plane strains, (b) out-of-phase voltages producing bending strains.

There are, however, numerous disadvantages of such materials that will hinder their employment for certain applications. For the piezoceramics, at high operating frequencies, large losses associated with hysteretic effects, preclude their efficient use. The hysteretic behaviour does depend on the field level, the cycle time and the material of the piezoelectric actuator. The maximum sustainable electric field is limited to approximately 0.75 of the value of the coercive field, E_c , otherwise progressive depoling, or repolarization in the opposite direction, occurs. Either occurrence renders the transducer useless. It is important to note that a high stress level can also cause depoling. This is particularly important when wishing to sense dynamic strains.

Piezoceramics are temperature dependent and exhibit total depolarization above the *curie temperature*. As the *curie temperature* is approached, the value of the piezoelectric constants decrease, again, limiting the actuation authority. The operating temperature, therefore, should generally be much lower than the *curie temperature*.

Although piezoelectric actuators are widely used in smart structure applications, particularly for high frequency applications, they can only generate a sufficiently high force level when employed as a *stack* actuator. Such a configuration does not easily lend itself to

controlling post-buckling deformations in plate/panel structures. This has led the author to seek an alternative actuating mechanism for his PhD research work.

1.5.2. Electrostrictive Materials

Electrostriction is characterized by the mechanical deformation which occurs in a dielectric material when subjected to an electric field. Such a material, capable of useful deformation, comprises of a lead magnesium niobate (PMN) compound. The electrically-coupled strain always lies along the axis of the electric field, or the axis of induced polarization. The maximum strain is about $1000\mu\epsilon$. The stiffness properties are similar to those of the piezoceramics.

Within PMN, the electromechanical coupling is significantly non-linear and, unlike piezoelectric materials, uncharged electrostrictive materials are isotropic and unpoled so that the material exhibits no net polarization and field reversal does not lead to the generation of negative strains. Negligible hysteretic behaviour may be beneficial over the piezoceramics, though, again, their limiting force capability inhibits their employment within this investigation.

1.5.3. Magnetostrictive Materials

The concept of magnetostriction is similar to that of electrostriction in that it refers to the tendency of certain materials to generate strains in response to an applied field, in this case an applied magnetic field. Magnetostrictive actuators convert electrical energy to mechanical energy using the magnetic field induced by an electric current in a wound wire solenoid. Displacement output is varied by controlling the current flow in the coil. A common form of magnetostrictive actuator is composed of terbium, iron and dysprosium.

In the presence of a magnetic field, domains within the material rotate and align with the field. In addition to actuation, sensing may be accomplished through measurement of the magnetic fields which result when mechanical stresses cause rotation of these domains.

Actuators composed of magnetostrictive elements generate very large strains in comparison with those created by piezoelectric and electrostrictive materials. However, current magnetostrictive transducers are large and, consequently, heavy. This weight disadvantage will have implications for their utilization, particularly within aerospace platforms. An additional disadvantage is that they do not easily lend themselves to being embedded in a structure. This is because of the difficulty of delivering a magnetic field within a structure. Such disadvantages led to their deselection from this programme.

1.5.4. Electrorheological Fluids

Electrorheological (ER) fluids experience reversible changes in rheological properties when subjected to electric fields. In the extreme, the field converts a low viscosity fluid to a solid like substance. Such fluids comprise of colloidal dispersions of solid particulates in non-conducting oils. The dispersions are chosen to have a large mismatch between the dielectric properties of the solute material to that of the solvent.

An electric field induces electric dipoles in the solid particulates which then interact to form columnar fibres parallel to the direction of the field. The basic operating mechanism is

very simple. In the neutral state the particles are uniformly distributed throughout the fluid. When the electric field is applied, the relatively high dielectric constant of the particles captures this electric field, redistributes the charge densities within the particles, and causes them to stick together in fibrils. The presence of these fibrils considerably modifies the viscous properties of the fluid. Considerable changes in viscous properties are only achieved at relatively high fields in the order of 1kV/mm.

The change in behaviour in the presence of an electric field renders them suitable to control variable stiffness or damping in a material. It is not thought conceivable to efficiently enhance the load bearing capability of the components considered later in this thesis.

1.5.5. Shape Memory Alloys

Shape memory alloys are a class of metal compounds which have the capability to sustain, and then recover, relatively large strains ($\approx 8\%$) without undergoing true plastic deformation. The most widely used shape memory alloys are the Ni-Ti, Cu-Zn-Al, & Cu-Ni-Al, all fabricated in wire or ribbon form. As an example, an un-constrained nickel-titanium, Ni-Ti, shape memory alloy wire, when strained by 8% below a critical temperature, will reform to its initial length, when subjected to a temperature above another critical temperature. This unique phenomenon is due to the materials ability to undergo internal solid-solid reversible diffusionless crystalline transformations in the presence of a characteristic temperature differential.

Of the actuating materials previously discussed, shape memory alloys may generate the highest level of force for a specific actuator volume. Such a force being generated by the prevention of the shape memory recovery phenomenon. Force levels of approximately 700MPa, for Ni-Ti, were initially witnessed. Such levels were thought to be sufficient for this research programme and, therefore, on this basis, the material was selected.

A full descriptive account of the alloy behaviour is presented in **Chapter 2**. A variety of nickel-titanium shape memory alloys were purchased and tested throughout this research programme. These actuators, being in wire form, permit easy embedding within the laminated polymeric-matrix composites. Two constraining mechanisms have been considered. The constraints induce internal stresses which allow for structural configuration modification by a desired amount.

Due to their unique memory capability, SMA's lend themselves to innovative applications ranging from medical science to space deployment mechanisms. Only recently have they been considered for adaptive control of structural components, some of which are highlighted in **Sections 1.6-1.8**.

There are, however, significant disadvantages associated with the employment of such a material. The properties of the shape memory alloys are very complex and depend critically upon the composition and thermo-mechanical history. The cyclic strain/force capability is equally complex. The greater the magnitude of shape recovery during solid-solid phase change, the shorter the fatigue life of the material.

During operation, the actuators must be forced through the critical temperature, obviously by heating or cooling. Thermally driving the actuators generally necessitates an electrical source and significantly high power requirements. Cooling is generally provided by the environment. Owing to the required duration for one heating - cooling cycle, their application is limited to low frequency or static applications.

The author believes, however, that their advantages outweigh their disadvantages and with careful consideration to critical SMA parameters, then their utilization will lead to the

attainment of our structural modification goal, that being beneficial adaptive post-buckling. Such parameters of importance being described in the following chapters.

1.6. Envisaged Aerospace Applications

Potential areas for fully integrating adaptive structures in aerospace applications have been identified. Future aircraft, spacecraft, launch vehicles, helicopters and even missiles may exploit such technology. Examples where such technology may be utilized within airframes and spacecraft platforms are now presented. Such examples are by no means exhaustive.

1.6.1. Aircraft Applications

Different aircraft have different design objectives. Civil aircraft are required to ferry a large volume of passengers safely and economically to and from their chosen destination. Military aircraft require high performance, agility and stealth characteristics, to mention a few. Both require low maintenance costs and fuel economy. Future generations of both types of aircraft may well include a self-diagnostic capability, **Section 1.4**. While adaptive civil aircraft may be a long way off, it is expected that the adaptive technology will initially be demonstrated on military aircraft where performance improvements are essential in enhancing survivability.

Within aeronautical research establishments, the adaptive response of aircraft structures has been met with great enthusiasm. Such technology could reduce vibration and flutter, alleviate gust response, provide real-time corrective action in response to mechanical failure, attenuate excessive noise levels from vibrating structures and provide an alternative means for variable wing camber during the various stages of flight. Such variable wing sections could replace flaps with potentially significant savings in weight and mechanical complexity.

Past and present aircraft designs incorporate ailerons as standard for flight control during critical stages. These hinged control surfaces are, however, disadvantaged by geometric discontinuities that cause additional aerodynamic disturbances to an otherwise smooth lifting surface. This is especially significant in present day military applications that require low observability characteristics¹⁸. Reshaping smooth aero-structural surfaces in-flight with active-material actuator combinations offers new possibilities for advanced efficient designs.

Such wing camber modifications may be achieved by utilizing actuators within diagonal truss elements, i.e. *active ribs*¹⁹. The diagonal elements are translational actuators that expand or contract a desired amount to deform the wing camber, such a technique being stated to have the potential to improve aerodynamic lift¹⁹.

Alternative methods for optimizing wing shape for various flight conditions have utilized a span-wise twisting concept²⁰. A proof-of-concept has been tested and validated on a 1/10th scale F-18 aircraft. The model was constructed from a composite wing box with built-in SMA torque-tubes to vary the span-wise twist. The essence of the twist adaptive wing concept is to obtain high lift when required without increasing the overall pitch attitude of the aircraft, thereby, enabling short take-off and landing or increased payload capability. An estimate, based on the aforementioned scaled model, shows twists of 2° at the mid-span and 5° at the tip which could produce an increase in take-off lift by 3-5%. This would permit significantly shorter take-off and landings.

Noise cancellation in turboprop aircraft and helicopter fuselages is a major issue. Noise level signals may be monitored, at key locations, then processed for generating anti-noise utilizing piezoelectric actuators within fuselage sections²¹. The creation of anti-noise is based on the generation of a 180° out-of-phase acoustic signal having the same amplitude and frequency that match the noise signal to be alleviated. The systems success will ensure greater passenger comfort.

1.6.2. Spacecraft Applications

Due to the harshness of both the spacecraft launch and the operating environment, future space-based load bearing structural components are anticipated to feature integrated sensing/actuating functionality. While critical primary structures may remain devoid of the adaptive technology, secondary structures would certainly benefit.

On-board structural health monitoring systems could report the spacecraft/launch vehicle status prior to launch, report critical events such as docking or impact and provide information on the dynamic response so that combinations of actuators can cancel detrimental vibrations, therefore, ensuring a high degree of pointing accuracy of critical components²². The latter is paramount for earth orbiting optical and laser based systems.

Vibrations have long been problematical for spacecraft systems, with acoustic and aerodynamic excitations causing failures during launch and periodic disturbances of attitude & orbital control systems degrading performance in orbit. Piezoceramic based adaptive structures are being extensively researched to alleviate such problems within space systems.

Such areas include active payload platforms and adaptive truss structures that suppress unwanted vibrations. As an example, the latter being implemented by piezoelectric active strut members within a truss framework. Flexible solar array panels, robotic arms and other large appendages may also benefit from active damping, where such appendages, in particular the solar arrays, have very low natural frequencies. Actuators may even be employed in antennae, therefore, ensuring high shape precision when subjected to temperature gradients.

Launch vehicle payload fairings, manufactured from advanced lightweight composite materials, impose severe acoustic environments upon the payload. Reducing the acoustic noise that the payload receives during launch is an important design constraint²³. Excessive acoustic noise, transmitted through the fairing, poses a potential threat to the life of the payload. Since future fairings will be constructed of lighter materials that inherently have a lower transmission loss (TL), the acoustic threat will be even more severe. Active control technology is currently being investigated as a potential solution for the fairing acoustic problem. Currently the best candidate actuator to solve the problem is the piezoelectric actuator.

1.7. Shape Memory Alloy Applications

The application areas described in Section 1.6 are desired goals amongst the smart structures community. Although fully integrated smart aircraft or spacecraft have not yet been realized, significant strides have been made with regards to their subassemblies, i.e. plates and beams. The aim of this section is to introduce some of the areas of such subassemblies, and in particular, those featuring embedded nickel-titanium actuators within polymeric matrix composites.

The use of Ni-Ti actuators has been demonstrated in important application areas such as vibration, aeroelasticity and even impact response. Their demonstration and validation, however, on typical aircraft structures has yet to be accomplished.

Although this section is limited to the aforementioned topics, the use of SMA's have been exploited or investigated for a variety of other application areas. These include robotic applications²⁴, aircraft mechanical de-icing systems²⁵, hinge mechanisms for deploying segmented facet-type reflector surfaces on spacecraft antenna structures²⁶, automatic greenhouse window openers²⁷, aircraft hydraulic line pipe couplings²⁷, and even the control of crack growth propagation within polymeric-matrix composites²⁸.

1.7.1. Dynamic Response Characteristics

Investigations into control-structure applications, utilizing the actuating mechanisms outlined in Section 1.5, have largely focused on vibration and noise suppression. For vibration control, the most promising materials are the piezoceramics. The use of shape memory alloys for vibration control, however, has been investigated.

The feasibility of utilizing shape memory alloy actuators in controlling the flexural vibrations of a cantilevered beam has been investigated both theoretically and experimentally²⁹. The results obtained demonstrate the potential of the SMA as a viable means for damping out the vibration of flexible systems. Two actuators were employed to control the first bending mode of a 25mm wide, 0.75mm thick, 250mm long polymethylmethacrylate beam. SMA wires, 0.4mm in diameter, were clamped with the beam at one end and to a region 50mm from the clamped end. Their locations were 0.775mm above and below the beams neutral axis. An external actuator placement ensured enhanced cooling and, thus, enhanced actuator dynamic response characteristics. This is very important because the SMA actuators, even when operating in air, have very limited bandwidth.

Vibration control is met by controlling the spacing between the actuator location points, from their undisturbed location, when subjected to external disturbances. The shape memory effect of the actuator is utilized to memorize the desired undisturbed spacing between the two structural points. The system response is regulated by a control system employing a simple on-off control algorithm for actuator energization.

The former citation employs external actuators for control. When such actuators are integrated within the host structure, in particular, where the host is a polymeric-matrix composite, then vibration or modal response can be accomplished using two alternative techniques³⁰. As cited previously²⁹, vibrations may be suppressed by applying forces such as to dissipate the dynamic response. Alternatively, the global properties of the SMA/hybrid component can be modified by the application of a critical temperature. Providing the volume fraction of the SMA material is sufficient, the application of the critical temperature will ensure an approximate fourfold increase in the SMA Young's modulus. Such an increase in modulus modifying the global properties of the component.

Dynamic characteristics of flexible composite beams have been controlled by heating sets of shape memory alloy fibres embedded along the beams neutral axis³¹. The beam considered, having dimensions of 300mm*25mm*1.56mm, was constructed from randomly oriented glass fibres embedded in low cure temperature polyester resin. The supports having a clamped-clamped arrangement. Activation of 4 constrained 0.55mm diameter SMA actuators significantly enhances the beams stiffness, therefore, the vibration modes of the beam can be tailored and shifted away from a particular excitation frequency, hence, avoiding undesirable

resonance. Only the first three modes of vibration were considered. A natural frequency response increase of 7.9%, 4.1% and 1.5%, respectively, was witnessed.

A similar study indicates a 276% increase in the first natural frequency of a 30in long, 1in wide, 62mil thick graphite epoxy composite reinforced with 25 activated SMA wires, 20mil in diameter and located within sleeves³². Such a configuration necessitates an independent constituent clamping arrangement. Since the geometry of the SMA is restrained by an independent clamping device, a recovery force is generated. This force leads to an increase in the natural vibration frequency of the whole assembly.

The use of SMA's in a transient environment has been limited because of the poor frequency response of the material. As previously stated, SMA utilization requires temperature control for actuator operation. This is usually achieved through resistive heating. Here, however, the response time of the SMA actuators is limited by heat transfer. With regards to dynamic systems, then typically, very rapid heating can be achieved. The time needed for cooling, however, is long compared to the transient period of a particular mechanical component. By using several actuators in parallel and energizing subsets of these during successive cycles of structural motion, an effective bandwidth can be achieved that is greater than the bandwidth possible with a single actuator³³. Such a technique may extend SMA use in structures of the future.

Shape memory alloys also offer the potential to act as a passive damping media. Traditionally, materials used for passive vibration suppression have been viscoelastic solids or viscous fluids. These materials possess an internal structure which facilitates dissipation. Likewise, the highly hysteretic behaviour of SMA's renders them capable of absorbing large amounts of energy under loading³⁴.

1.7.2. Panel Flutter Suppression

The feasibility of using either surface bonded or embedded actuating elements for the suppression of dynamic aeroelastic instabilities of panel structures has been investigated³⁵⁻³⁷. Such instabilities lead to fatigue induced structural failure. Investigations regarding the employment of SMA actuators to increase the *flutter speed* of flat simply-supported aluminium panels in supersonic flow have proved successful³⁶.

Panel flutter is a dynamic instability of thin plate or shell like components, typical of flight vehicles in supersonic flight regimes. Panel flutter is sensitive to diverse factors such as local flow Mach number, flow angle with respect to the panel, edge support conditions and in-plane stresses. It is caused by aerodynamically induced interaction between certain modes of vibration. The aeroelastic interaction tends to draw a pair of panel vibration natural frequencies together so that they coalesce. When this coalescence occurs, the panel becomes unstable and vibration amplitudes build up rapidly.

When a flight vehicle travels at high speeds, then dynamic pressure is not the only form of excitation. The friction from the surrounding air causes increased heating of the panel. This temperature rise may cause large skin aerodynamic thermal deflections, thus affecting flutter response³⁷.

Prevention of panel flutter has generally required additional weight to increase the panel stiffness. The use of so-called smart materials to create an active panel is an alternative to conventional mass stiffening. Depending on the components boundary conditions, a tension field can be created when an active element, such as a shape memory alloy or piezoelectric element, attached to the panel under-surface, attempts to contract. If the actuator is located properly, the panel tensile stress field, created by actuator contraction, separates the critical

panel natural frequencies and increases the flutter speed. At elevated temperature, tensile stresses may autonomously be induced by direct integration of the shape memory alloy actuators.

1.7.3. SMA/Hybrid Composite Impact Resistance

Polymeric-matrix composites are inherently brittle materials and, as a result, have poor impact resistance. Small amounts of embedded fibrous shape memory alloy, 0.3mm in diameter, have successfully been employed to toughen such composite materials³⁸. A 2.8% SMA volume fraction was embedded within graphite/bismaleimide [0₂/90₂/0₂] beams, 1.63mm thick. Low velocity, 2.4-23J (1.8-17 ft-lb), impact test results showed that with the direct inclusion of the SMA fibres, impact resistance of the hybrid composite increased. At impact energies that normally perforate a standard graphite bismaleimide [0₂/90₂/0₂] lay-up, the SMA fibres prevented perforation. Results also showed that the resulting composite ply delamination area was reduced by as much as 25%.

It is hypothesized that the SMA stress induced martensitic phase transformation is responsible for the better impact performance of the hybrid. Such a transformation allows the generation of high strain levels while dissipating large amounts of impact induced strain energy. The high strain levels are generated by the impact induced high localised stresses. As the impact energy is more readily dissipated by the SMA fibres, during the phase transformation, than by the host composite material, then, this impact strain energy is not available to initiate damage to the host composite material.

Composite materials that not only have good structural characteristics, but also good penetration resistance and greater strength after impact may find there way into many aerospace applications.

1.8. Smart Buckling & Post-buckling Literature Review

Throughout this research programme the author has been primarily concerned with the buckling behaviour and post-buckling displacement alleviation of carbon/epoxy plate and panel specimens. Such specimens featuring embedded pre-treated shape memory alloy actuators at pre-defined locations. Their integration, and therefore, research intention, is to demonstrate a sufficiently enhanced adaptive buckling/post-buckling structural capability. This section aims at presenting an introduction to the topic and to reference and summarize the limited investigations previously conducted within this area of study.

Buckling manifests itself by a structure losing its geometrical shape at a critical load level. Active buckling control allows structural members to be loaded beyond their critical buckling load by using sensors to detect the onset of buckling motion and applying actuation forces to restore the member to the undeflected position. Adaptive post-buckling allows for the alleviation of non-linear post-buckled displacements, the principal behind the displacement alleviation being similar to that for buckling control. Once a threshold deflection value is exceeded, actuation forces act in favour of displacement alleviation. A comprehensive literature survey has been conducted. It was found that both piezoceramic and SMA actuators have been employed as a mechanism for enhancing the buckling and post-buckling load bearing capability.

Active buckling control of laminated composite plates utilizing piezoelectric materials has been investigated³⁹. A finite element model was employed to predict the buckling control capabilities of laminated composite plates with integrated piezoelectric sensors and actuators. The finite element model is based on the shear deformation plate theory in conjunction with linear piezoelectric theory. Numerical results presented for the finite element solution show the effectiveness of piezoelectric materials, under applied voltage fields, in enhancing the buckling load. A symmetric cross-ply laminated plate, $[0/90/90/0]_s$, with dimensions 254*254*2.54mm, having all edges clamped and with collocated sensors and actuators surface bonded to the plate centre was subjected to a linearly increasing uniaxial compressive load. Typically, the uncontrolled plate buckles when the axial load reaches a critical value of 32.63kN. Employing active control, full activation is generated after the lateral deflection exceeds a threshold value of 1/10th the plate thickness and remains activated thereafter. It was observed that the plate then buckles at 34.16kN.

The linearized equation of motion of a simply-supported flexible beam, with continuously bonded piezoelectric actuators and subjected to a linearly varying axial load has been derived⁴⁰. This derivation, incorporated into the design of a feedback control system, utilizing thin film piezopolymer actuators and strain gauge sensors, is used to increase the overall bending stiffness of the flexible beam. It was found that buckling of a simply supported beam could be postponed beyond the first critical buckling load.

While the latter two references involve numerical simulations, the active buckling control technique, utilizing the piezoelectric effect, has been experimentally observed^{41,42}. Back-to-back surface bonded piezoceramic actuators were employed to enhance the load bearing capability of carbon/epoxy strips 280mm in length, 35mm in width⁴¹. The pair of actuators were located at the members mid-height. Due to imperfections in the host material, and of a geometrical nature, the composite column strips, with inactivated piezoceramic actuators, would deflect from the onset of loading and reach an ultimate load capability at high deflection levels.

As a result of the presence of imperfections, this ultimate load will be less than the critical buckling load of the ideal structure. By applying a controlled voltage to the actuators, such that one actuator contracts while the other expands, a reactive moment will be induced at the column centre thereby removing the lateral deflections and enforcing the column to behave in a perfectly straight manner. The composite column strips with active control are shown to clearly demonstrate an increase in axial compressive load capacity compared to those without control. For the lay-up configurations considered, the increases in load carrying capability were found to be within 19.8%-37.1%⁴¹.

Experimental results show that, for a steel/piezoceramic column⁴², 320mm in length, 51mm in width and having a thickness of 0.254mm, then a factor of 5.6 increase in the load bearing capability can be achieved through the activation of 5 pairs of collocated back-to-back piezoceramic actuators. Such activation resulted with a stabilization of the first two unactivated uniaxial buckling modes.

Investigations regarding the feasibility of using Nickel-Titanium shape memory alloy actuators for actively controlling the buckling of flexible structures has received more attention. An external SMA helical spring was first employed to enhance the buckling characteristics of long slender beams⁴³. The SMA helical spring was connected to a pneumatic power cylinder which applied a compressive loading to an elastic member. The net effect of actuation was to reduce the compressive loading on this elastic member. Here, the actuator recovery force counterbalances the applied load and prevents the beam from buckling. This work involves sensing the onset of bending and using the SMA actuators to reduce the load on the column, thereby, preventing out-of-plane deformation.

This work⁴³ was extended with an investigation on the buckling characteristics of flexible fibreglass composite beams actively controlled by optimal sets of pre-strained shape memory alloy wires embedded along the beams neutral axis⁴⁴. The actuators, located within vulcanized rubber sleeves, are constrained to an external boundary, thereby, preventing contraction upon activation. A closed-loop computer controlled system was used to control the buckling of the fibreglass polyester resin beam, with dimensions 637.5*25.4*4.5mm, reinforced with 8 Nickel-Titanium wires, 0.55mm in diameter. The results showed that the critical buckling load could be increased three times when compared to that of an uncontrolled beam. The enhanced load bearing capability arising from the increased stability provided for by the tensile stresses within the actuators.

Research towards the active lateral torsional buckling control of a transversely loaded cantilevered beam⁴⁵, with dimensions 406*51*2.5mm, incorporating two 0.55mm diameter SMA wires embedded symmetrically along the beams mid-plane, have shown that the buckled beam can be brought back completely to its unbuckled configuration with appropriate SMA activation. The control strategy employed was similar to that previously presented⁴⁴.

An analytical expression was formulated to predict the behaviour of composite laminated plates integrated with SMA fibres⁴⁶. The formulation allows the prediction of the bending, modal analysis, acoustic transmission and buckling response of SMA reinforced plates. Accounting for the large increase in Young's modulus, when nickel-titanium transforms to its austenite phase, is shown to increase the hybrid composite plates critical buckling value. Utilizing the recovery force generated when the SMA fibres are constrained to the host composite, then the actuators may act to decrease the critical load. The analytical formulation developed, however, does not account for thermal effects.

The utilization of SMA fibre/strips, integrated within graphite epoxy composite panels, demonstrate that the SMA fibres can enhance critical thermal buckling temperatures and attenuate, or even eliminate, thermal post-buckling deflections⁴⁷. Such findings are based on the generation of in-plane tensile forces within the host laminate resulting from the elongation of the activated SMA fibre/strips. The laminate under investigation was a $[0/\pm 45/90]$, with SMA volume fractions between 10 & 30%, initially pre-strained 3-5%. The simply supported critical buckling temperatures are seen to increase drastically to the point where they are unrealistic as they exceed the materials maximum service temperature. Such a high SMA volume fraction may be of benefit, however, to high temperature matrices such as those of carbon-carbon composites. As the exact boundary conditions are not stipulated, the author believes that these results, along with those presented in the following two citations, rely on the shape memory elongation of the inelastically deformed material, rather than the characteristic shortening associated with the fine diameter wires.

An interesting phenomenon has been presented with regards to an SMA/hybrid plates response to thermal loading⁴⁸. SMA/hybrid plates may have two first critical thermal buckling temperatures. The first, or lower critical temperature, may occur at a temperature less than the austenitic start temperature. The second critical temperature corresponds to the case where the temperature is above that sufficient to cause phase transition within the SMA. Here, the temperature must be of the magnitude that it overcomes the strengthening provided by the actuators recovery influence.

For the lay-ups presented, and for SMA volume fractions in the range of 0-30%, the lower critical temperature is shown to decrease with increase in SMA volume fraction, the second is shown to significantly increase with increase in SMA volume fraction. The former is the result of the lower SMA modulus compared to that of the host's.

Finite element analysis was performed to investigate the potential for active buckling control of two different stiffened panels utilizing SMA rods⁴⁹. The stiffened panels were

comprised of a 762*292mm skin, 2.54mm thick, with two blade or T stiffeners running along the longitudinal length 19.1 mm from the free edges. The actuators were located in the stiffeners only. The panels modelled were representative of aluminium. Changes in the predicted buckling load increased with the magnitude of the actuation level for a given structural concept. Increasing the number of actuators yielded greater predicted increases in the buckling load. The practicality of the concept relies on the development of such high authority SMA rods whereby the necessary shape memory elongation can be generated. Energizing finer diameter SMA wires would result with lower critical buckling values, attributed to the shape memory shortening.

Recently, a numerical simulation on the stability of sandwich panels, featuring SMA actuators, subjected to the simultaneous action of a uniform temperature and uniaxial compression has been investigated⁵⁰. The actuators were located within resin sleeves situated on the panels neutral plane and constrained to an external boundary. The sandwich constructions considered had either aluminium or ten layered kevlar epoxy facings. Both types of skin were separated by a core 12.7mm thick. The panel dimensions considered were 1m in length by 0.5m in width. The diameter of the fibres employed is equal to 2.54mm.

For the panels with aluminium facings and a uniform SMA distribution, a 5% improvement in the buckling load at 82°C was observed. This increased to 6.6% when the SMA placement was functionally graded. At 149°C, these values increased to 75.5% and 100.8%, respectively. For the panels with kevlar/epoxy facings, a uniform SMA distribution yielded a 5.7% increase in the buckling load at 82°C. A 7.7% increase can be achieved when functionally grading the SMA distribution. At 149°C, the improvements were seen to be 26.9% and 35.8%, respectively. No account was made for the temperature dependent properties or for degradation in core properties associated with the inclusion of the high volume fraction of SMA material.

Utilizing embedded wire form Nickel-Titanium actuators, buckling control/post-buckling displacement alleviation may be accomplished by several means. Within the framework of this study, two approaches have been studied. Both approaches require actuator treatment prior to their use. The first approach, which, throughout this document, is referred to as *control strategy 1*, utilizes SMA actuators, fed through tubing after specimen fabrication, that are constrained to an external boundary. Actuator treatment is the simple process of pre-straining the actuators, therefore, inducing a detwinned martensitic crystal lattice. The external constraints prevent shape memory recovery, therefore, upon activation, significant stresses accumulate within the actuator that may be employed to achieve our desired structural response. Summarized results for this control strategy can be found in the published literature^{51,52}.

The second approach, referred to as *control strategy 2*, features pre-treated SMA actuators embedded and consolidated to the host structure at a desired location within the host laminate. Its position will be as far off the neutral plane as possible such that, for a given level of SMA activation, recovery forces, equilibrated by the surrounding matrix, invoke the maximum permissible bending moment. The imposed bending moment permits structural tailorability. It should be noted that the level of recovery stress is dependent on the compliance of the surrounding matrix. The lower the compliance, the higher the level of induced force. For this control strategy, the level at which shape memory alloys may operate may be limited by bonding characteristics. This has been investigated and will be discussed in great detail in **Chapter 3**.

The strict requirement for carefully clamped SMA structural boundary conditions may hinder the implementation of *control strategy 1* within conventional airframe design. The internal anchoring of the embedded SMA wires against the natural boundary conditions of the

host structure, *control strategy 2*, is likely to be more acceptable. A detailed account of both control strategies will be presented. One thing is of certainty, our findings, as with the earlier citations of this section, can only highlight the potential of such adaptive structures, particularly within the aeronautics community.

Chapter 2

Shape Memory & Constrained Recovery Behaviour Of Nickel -Titanium

2.1. Introduction

Nickel-Titanium shape memory alloys belong to a class of solid materials that can undergo reversible diffusionless atomic structure transformations between a highly ordered cubic phase called the *austenite*, or the *parent* phase, and a less ordered monoclinic phase called the *martensite*, or the *product* phase. Typically, for a stress free specimen, the former is stable at higher temperatures and the latter at lower temperatures⁵³. Such solid-solid phase transformations can be induced by a change in stress or temperature alone, or by a combination of both⁵⁴.

In general, shape memory alloys, when heated above a certain temperature, have the unique ability to regain apparent plastic deformation. This characteristic, which is unrelated to, and far more dramatic than thermal expansion/contraction, is called the shape memory effect, SME⁵⁵.

Prevention of deformation recovery results with the generation of internal stresses equilibrated by the imposed boundary constraints. For example, when directly embedding in polymeric-matrix composites, providing there is good interface conditions, then, such a constraint is met by the surrounding consolidated matrix. For a particular excitation level, the magnitude of this stress is dependent on the compliance of the boundary constraints.

Throughout this study, the author has concerned himself with the recovery force characteristics associated with complete and compliant constraints imparted on the actuators within *control strategies 1* and *2*, respectively. Such force levels will be presented.

Nickel-Titanium, Ni-Ti, is the alloy selected for this research programme. It is the most common alloy which exhibits the unique mechanical *memory*, or restoration force phenomenon. Such an alloy may have a composition ranging from 52-56% weight nickel, the balance being titanium. Note, the shape memory behaviour is limited to Ni-Ti alloys having near-equiatomic composition⁵³.

The unique shape memory process may be described as follows⁵⁶. The material is first formed into the shape in which it will be called upon to remember, in our case a straight wire of particular length. The alloy is then subjected to an appropriate heat treatment, thereby, imparting the memory capability. Under stress free conditions, the geometric shape of the material, in the austenite phase, is its memory shape. Upon cooling below the alloys characteristic transformation temperature range, **Section 2.5**, martensite forms.

While in the martensite phase, a relatively small stress may be used to deform the material. The deformed alloy will remain martensitic and hold its shape indefinitely as long as the temperature of the material is held below the alloys transition temperature. Apparent plastic wire deformations of 8% may be regained by application of the alloys transformation temperature. Prevention of regain of the *memorized* shape will induce stresses that may exceed 700MPa. The temperature to which the specimen must be heated to return it to the memory configuration, however, depends on the chemical composition of the alloy and thermal history.

The previous paragraphs have introduced the so-called one-way shape memory effect. It is this mechanism that is employed for the structural adaptive intentions of this research. For both of the control strategies employed, boundary constraints provide the mechanism for actuator cyclicability. Under certain conditions, however, when a specimen is heat-treated, cooled to induce martensite, deformed and then heated back to austenite, upon regain of the memorized configuration and subsequent cool down, the specimen may revert back to the deformed shape. This is referred to as the two-way shape memory effect. There are limitations, however, for effective utilization of the two-way shape memory phenomenon, therefore, it is excluded from this study.

Within this chapter, having introduced the shape memory and constrained behaviour of a shape memory alloy, further aims are to elaborate on the unique phenomenon. As previously introduced, an appreciation of the recovery force behaviour is paramount. When employing such an adaptive mechanism for structural control purposes, one should note the need to test the SMA for its properties prior to use. Typical properties, or SMA behaviour, essential for this study are the alloys transformation temperatures and cyclic recovery force capabilities. Both of which, for a variety of specimens, will be presented.

The need to test exists because the thermo-mechanical properties of a typical SMA depend on many variables and a satisfactory constitutive model to account for these variables does not exist at this time. Note, shape memory alloy constitutive modelling was not essential within this study, it is however, discussed in **Appendix A**.

Actual recovery force measurements, for a particular test environment, provide an accurate means of predicting the adaptive structural behaviour, results of which are presented in **Chapter 6**. These recovery force results, when applied to non-linear FEA, may determine the post-buckling behaviour of laminated plate structures with an improved load bearing capability provided for by one of the two SMA control schemes.

To conclude this introduction, the author acknowledges the valuable texts^{57,58}, which provide an in depth review of the behaviour of shape memory alloys, in which the interested reader may wish to refer.

2.2. The Shape Memory Alloy Transformation Phenomenon

In general, there are four important characteristic transition temperatures for an SMA. In the stress-free state, the austenite-to-martensite ($A \rightarrow M$) transformation begins at a temperature denoted by M_s (martensite start) and ends at the lower temperature M_f (martensite finish). The reverse transformation from martensite-to-austenite ($M \rightarrow A$) begins at the temperature A_s (austenite start) and ends at a higher temperature A_f (austenite finish). The two phases can coexist at intermediate temperatures.

The transformation process is illustrated in **Figure 2.1**⁵⁹. If an annealed material is cooled from A_f , *phase 1*, to M_f , *phase 2*, while under no applied stress, a self-accommodating twinned martensitic structure develops. The martensite variants are all crystallographically and energetically equivalent, they differ only in orientation⁵⁴. Generally, the crystal structure of martensite is relatively less symmetric compared to that of the parent phase⁵⁷.

In the martensitic phase, Ni-Ti is very ductile and capable of sustaining large amounts of deformation. With application of stress, martensite twins become aligned. Such alignment, forming detwinned martensite, *phase 3*, is attributed to shear-type displacements within the crystal lattice⁶⁰. It results with the considerable apparent plastic strain in the material. Atoms have shifted without dislocating within the lattice structure. Such dislocations are common to

metals in general at high strain levels. A stress-strain curve, presented in Section 2.4, for a sample of Ni-Ti purchased for this research, illustrates this detwinning process.

When sufficient thermal stimulation is applied to heat the material above the phase transition temperature, and in the absence of an external constraint or an applied load, the lattice shifts to the more rigid, ordered cubic structure, therefore, returning the specimen to its undeformed geometry, *phase 1*. Providing the alloy has had no prior two-way training, re-cooling reproduces the original twinned martensitic shape, *phase 2*.

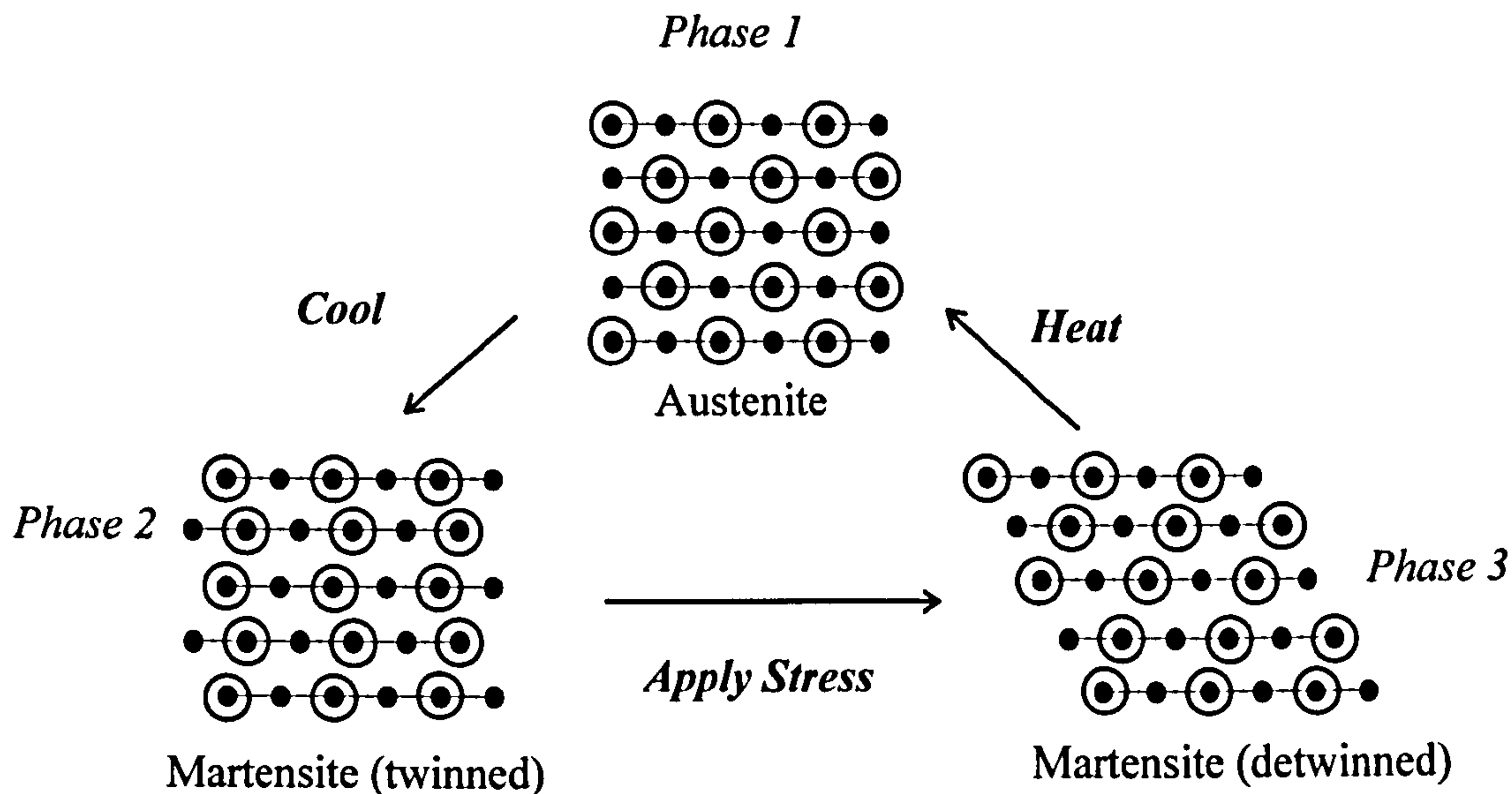


Figure 2.1. Schematic illustration of the phases within an SMA.

2.3. Why Nickel-Titanium ?

The shape memory alloys currently considered for commercial applications are Ni-Ti, variations of the Ni-Ti binary, Cu-Zn-Al and Cu-Al-Ni. The binary Ni-Ti alloys have been selected over the alternative alloying compositions for the following reasons;

- Excellent corrosion resistance. This is of particular importance when embedding in carbon fibre reinforced polymeric-matrix laminates.
- Excellent strength.
- A high recoverable strain capability.
- Under constrained conditions, pre-strained Ni-Ti alloys generate the highest recovery stress levels.
- When compared to the Cu based alloys, Ni-Ti may be processed into fine diametrical wires, owing to their small grain size⁶¹, thereby, imposing minimal disruption when embedded in host composite lay-ups.
- Compared to the alternatives, they have a high electrical resistivity, therefore, for electrical energization, require a lower ohmic heating power consumption.
- Excellent cyclic behaviour at low strain levels.

It is important to note that, however, the processing cost of Ni-Ti is very high. This is due to factors, such as, the required compositional control to be within 0.01-0.1 weight percentage⁶². This makes the Ni-Ti alloy selection to be the most cost ineffective. The requirements of this programme of work are performance driven. The aims are initially to demonstrate a concept. Should the programme concept show significant potential, then, to achieve a desired market realization, issues related to cost effectiveness should be placed on the novel synergistic structural components/constituents.

Typical material properties of the selected alloy are shown in **Table 2.1**. The data was taken from a variety of publications. Nearly all physical properties between austenite and martensite are different and, thus, as the alloy passes through the transformation point, a variety of significant property changes occur.

Property	Value	Units
Density.	6450→6500	Kg/m ³
Melting Point.	1,240→1310	°C
Thermal Conductivity.	18 (austenite)	W/m°C
	8.6→10 (martensite)	" "
CTE.	11 (austenite)	*10 ⁻⁶ /°C
	6.6 (martensite)	" "
Specific Heat.	250→ 490	J/Kg°C
Electrical Resistivity.	0.9 (austenite)	*10 ⁻⁶ Ωm
	0.76 (martensite)	" "
Maximum Recommended Operating Temperature.	<400	°C
Young's Modulus.	75→83 (austenite)	GPa
	20→41 (martensite)	" "
Poisson's Ratio.	0.33	
Ultimate Tensile Strength.	895 (fully annealed)	MPa
	1900 (worked hardened)	" "
Transformation Temperature Range.	-200→110	°C
Latent Heat Of Transformation.	28,000	J/Kg
Transformation Strain;		
One-Way Memory	8 (1 cycle)	%
	6 (100 cycles)	
	4 (100,000 cycles)	
Two-Way Memory	3.2	

Table 2.1. Typical Ni-Ti property data.

Two different SMA diameters have been considered; 0.3mm & 0.4mm. The integration of such actuators into the host composite depends on the control strategy to be employed. The alloys employed^α in this program have nominal compositions of 49.8-50.0 & 50.0-50.2 at.% Ni and were annealed at 500°C for one hour.

^α Purchased from Thomas Bolton Ltd.

2.4. Typical Stress-Strain Behaviour

A typical stress-strain curve, for the 50.0-50.2 at.% Ni alloy, 0.4mm in diameter, is shown in **Figure 2.2**. The specimen was loaded at room temperature. The curve illustrated is typical for Ni-Ti shape memory alloys tested at temperatures below A_s .

It is clearly evident that the curve may be broken down in to a series of paths. During the initial loading stage, *path 1*, the specimen exhibits linear elastic behaviour of the product phase, though a small amount of the parent phase may be present. Such linear elastic behaviour ceases at the first yield point. Note, Ni-Ti, and SMA's of like, exhibit two-stage yielding⁶³. The former yield point depends on the test temperature.

The next loading process is accompanied with a reorientation of the martensite variants, *path 2*. During this phase, stress is nearly constant while the strain increases. Although it appears that there has been permanent plastic deformation, there has been no permanent damage on a microstructural scale. As long as the specimen is not significantly loaded above the recovery strain limit which, for this heat treated alloy composition, is approximately 6%, then, the strain induced is recoverable.

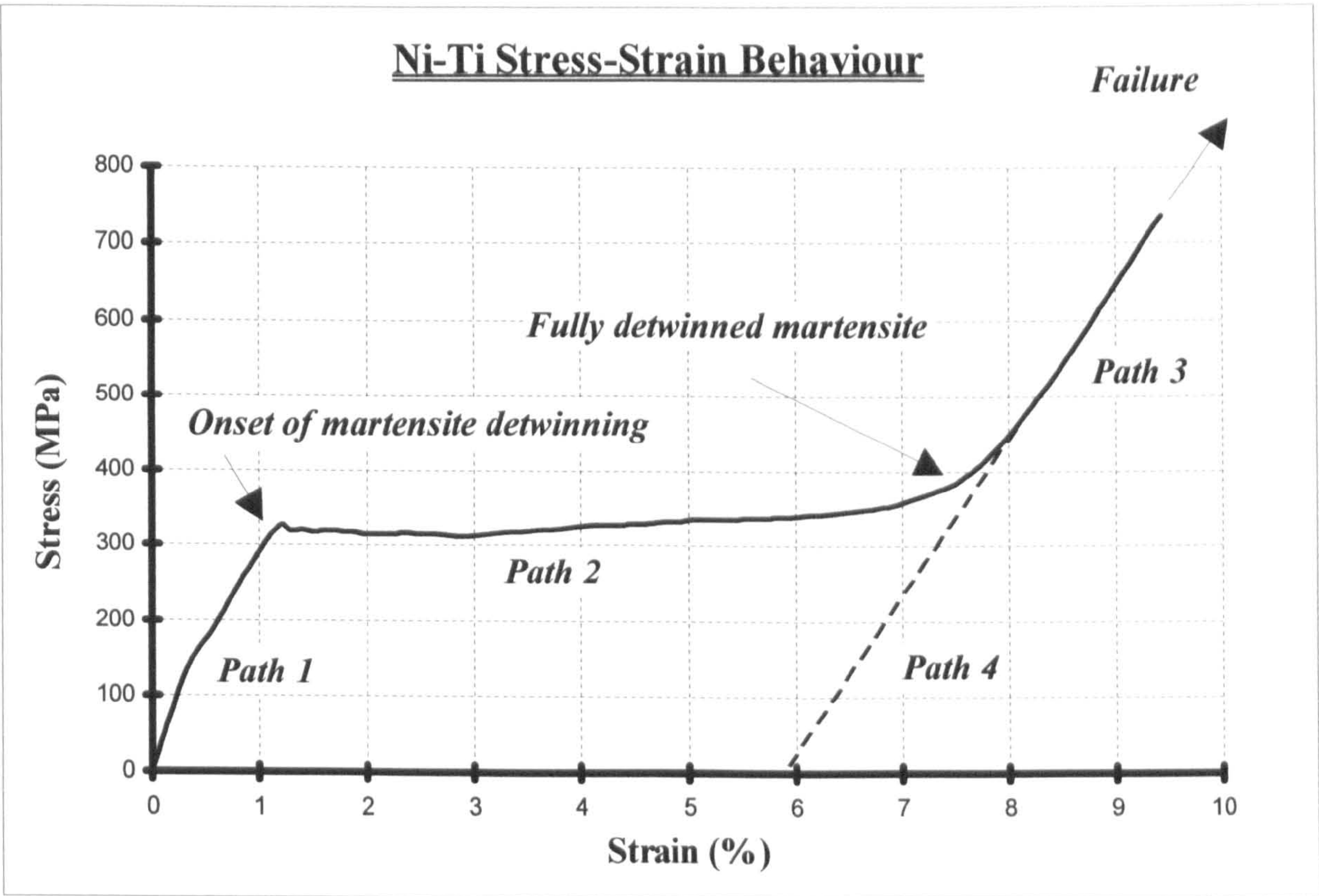


Figure 2.2. Characteristic stress-strain response for the 50.0-50.2 at.% Ni alloy.

Once the martensite has become completely aligned in the preferred orientation, i.e. in the specimen axial direction, the specimen's response to a further increase in load is, once again, elastic, *path 3*. The strain level at which the material becomes completely aligned is called the *domain width* and represents the maximum amount of recoverable strain from the material by heating⁶⁴.

The continued application of load will result with a load path tending towards the second yield point. It is the second yielding which is associated with the true plastic deformation of the martensite. Continued loading will lead to eventual specimen failure.

Upon unloading, *path 4*, prior to arrival at the second yield point, the residual strain can be recovered by heating the material back to its austenitic phase. As introduced earlier in this chapter, this behaviour is termed the shape memory effect.

Depending on whether the Ni-Ti alloy is tested in the austenitic or the martensitic phase, then, the alloy will show a noticeable difference in the mechanical stress-strain response. **Figure 2.2** illustrates the martensitic response. At higher temperatures, such that the austenite phase exists, then, *path 1*, of **Figure 2.2**, is much stiffer and the stress induced martensite occurs at a much higher first yield point. Such behaviour is documented in numerous citations^{57,58,59,65}. Upon unloading, while the temperature remains above A_f , the significant deformation previously witnessed is completely recovered. The load path, following a characteristic hysteretic loop, returns to its origin without requiring the application of extra heat. This behaviour is termed as pseudoelasticity. With the complete removal of the load, the materials lattice has returned, once again, to the cubic austenite. Note, however, the temperature below which martensite can be stress induced from the parent phase is limited to a particular value.

2.5. Ni-Ti Transformation Temperatures

Calculation of the appropriate power level necessary to transform the SMA actuators into complete austenite, therefore, inducing maximum recovery stress, requires the experimental determination of the phase transformation temperatures. The most important transformation temperature being A_f . Such experimentally determined temperatures can then be inserted into shape memory alloy constitutive equations, along with other experimentally determined material properties, to predict the shape recovery and thus structural response, **Appendix A**, or simply enable the prediction of an optimal current level necessary for energization.

The phase transformation temperatures for both the nickel-titanium alloy compositions have been determined.

2.5.1. DSC Results

Both the austenite and the martensite transformation temperatures were determined by differential scanning calorimetry (DSC). This technique measures the rate of heat absorption or emission from a specimen as its temperature is raised or lowered. Any transition will appear as a step change in the heat absorption versus temperature curve. Such a change indicating the difference in the specimens heat capacity.

For all the specimens, DSC measurements were performed on a mass of a few milligrams. The DSC curves were recorded in a temperature range from -30°C to 120°C with a heating and cooling rate of $10^{\circ}\text{C}/\text{min}$. This rate was a common value within published literature and, therefore, was selected for this study. To record the austenite transformation temperature, the sample was first cooled to -30°C , within the DSC apparatus and then heated to 120°C . The sample was then cooled back down to -30°C to record the martensite transformation.

Transformation temperature measurements were recorded for several samples of each alloy, therefore, providing evidence of consistency in the material behaviour.

Typical DSC curves for both the 0.4mm & 0.3mm diameter samples, with alloy composition 49.8-50.0 at.% Ni, are shown in **Figures 2.3 & 2.4** respectively. The upper curves of both figures show the endothermic phase transition to austenite and the lower curves show the exothermic phase transition to martensite. For both alloys, the endothermic phase transition to austenite is characterised by a single peak, whereas the exothermic phase transition to martensite, upon cooling, is characterised by two peaks, the first being attributed to the formation of R-phase (rhombohedral) martensite, the second peak occurs during the reversion to complete martensite.

All peaks initiate at the phase transition start temperature and terminate at the phase transition finish temperature. Such behaviour is characteristic of both alloys. The formation of the intermediate rhombohedral martensite is attributed to the processing conditions. It has no significant effect on this study and was, therefore, with the exception of cyclic operations, not thoroughly investigated.

The phase transitions are characterised as being athermal, the transition proceeds only with a change in temperature. If the temperature is held constant, the transition will not proceed with time.

All measured transition temperatures are listed in **Table 2.2**. Typescripts M, R and A refer to martensite, rhombohedral and austenite respectively. Subscripts s and f refer to the start and finish temperatures respectively.

As seen in the table, stress free nickel-titanium, with a nominal composition of 49.8-50.0 at.% Ni, requires a temperature in excess of 68°C for complete phase transition.

As will be shown, the employment of such constrained actuators within laminated constructions necessitates significant power requirements to utilize their recovery stresses, therefore, enabling an adaptive capability. Such high

power requirements will subject the host laminates to a significant temperature increase which, without careful consideration, may be detrimental in nature.

As seen from the table, a slight increase in the nickel content will substantially lower the characteristic phase transition temperatures. This is important as the lower actuation temperature requires a lower power stimulus and will subject the host laminates to a more acceptable thermal environment.

From discussions with the manufacturer, an increase in the Ni concentration from 50 to 52 at.% will, in effect, change the transformation temperatures from 60°C down to -100°C. Transformation temperatures can also be influenced by additives such as Zr, Au or Pt. Here, the effect is to raise the transformation temperature to approximately 600°C⁵⁶. This is, of course, undesirable for our intention. More importantly, however, as soon to be described, the phase transformation temperatures are affected by stress.

For practical applications, thermal cycling effects in the alloys are an important factor to be explored. Transformation temperatures vary with successive thermal cycles. It was found within the literature⁶⁶ that martensite transformation temperatures initially decrease rather drastically with increase in cycle prior to levelling off. This decrease reached about 25°C after 10⁴ cycles.

Alloy;	49.8-50.0 at.% Ni		50.0-50.2 at.% Ni	
	0.4mm °C	0.3mm °C	0.4mm °C	0.3mm °C
A _s	50	45	21	21
A _f	68	67	32	29
R _s	45	43	17	17
R _f	35	33	11	9
M _s	25	8	-25	-25
M _f	7	-7	<-30	<-30

Table 2.2. Phase transition temperatures.

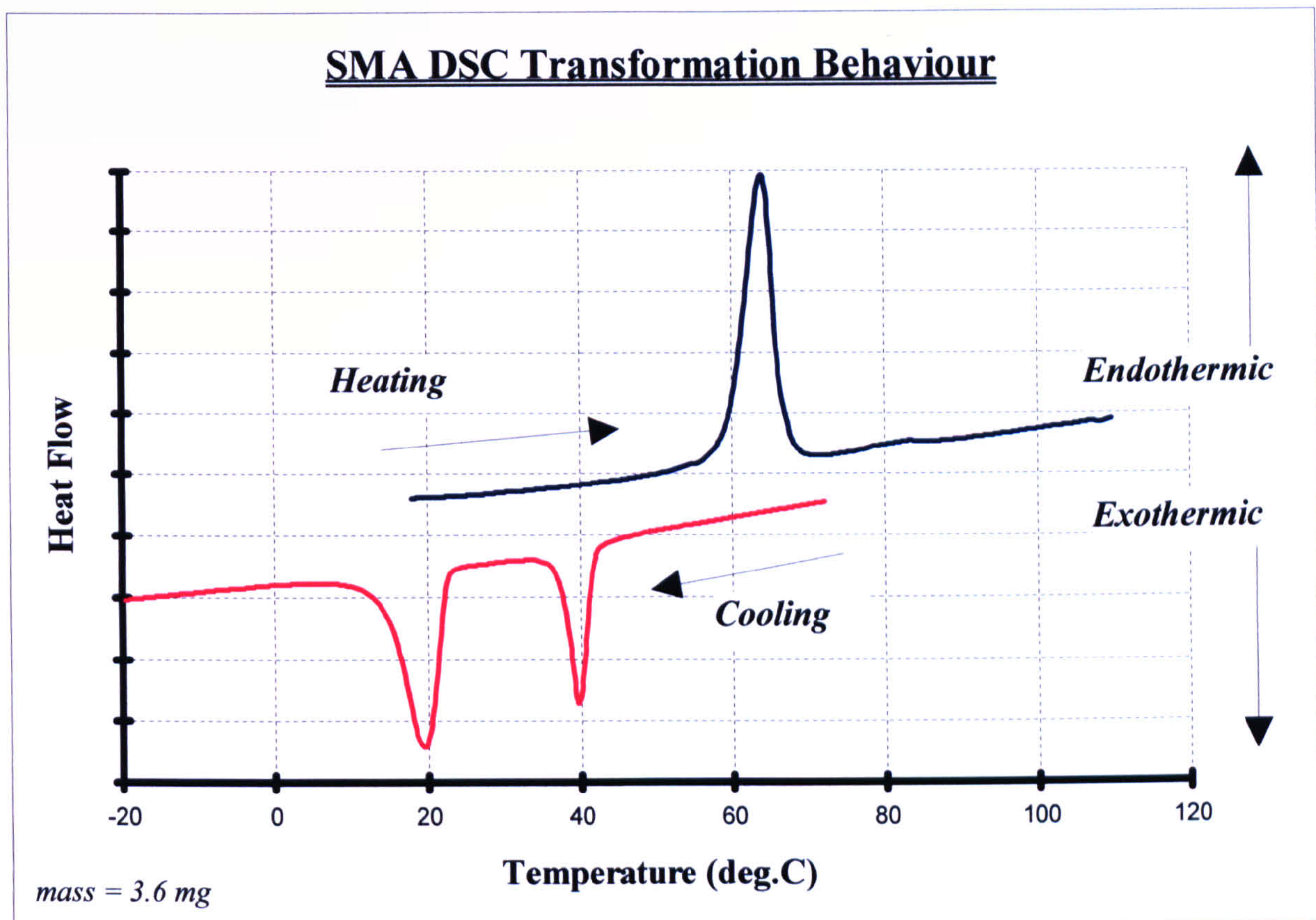


Figure 2.3. Transformation behaviour of a 49.8-50.0 at.% Ni alloy, 0.4mm in diameter.

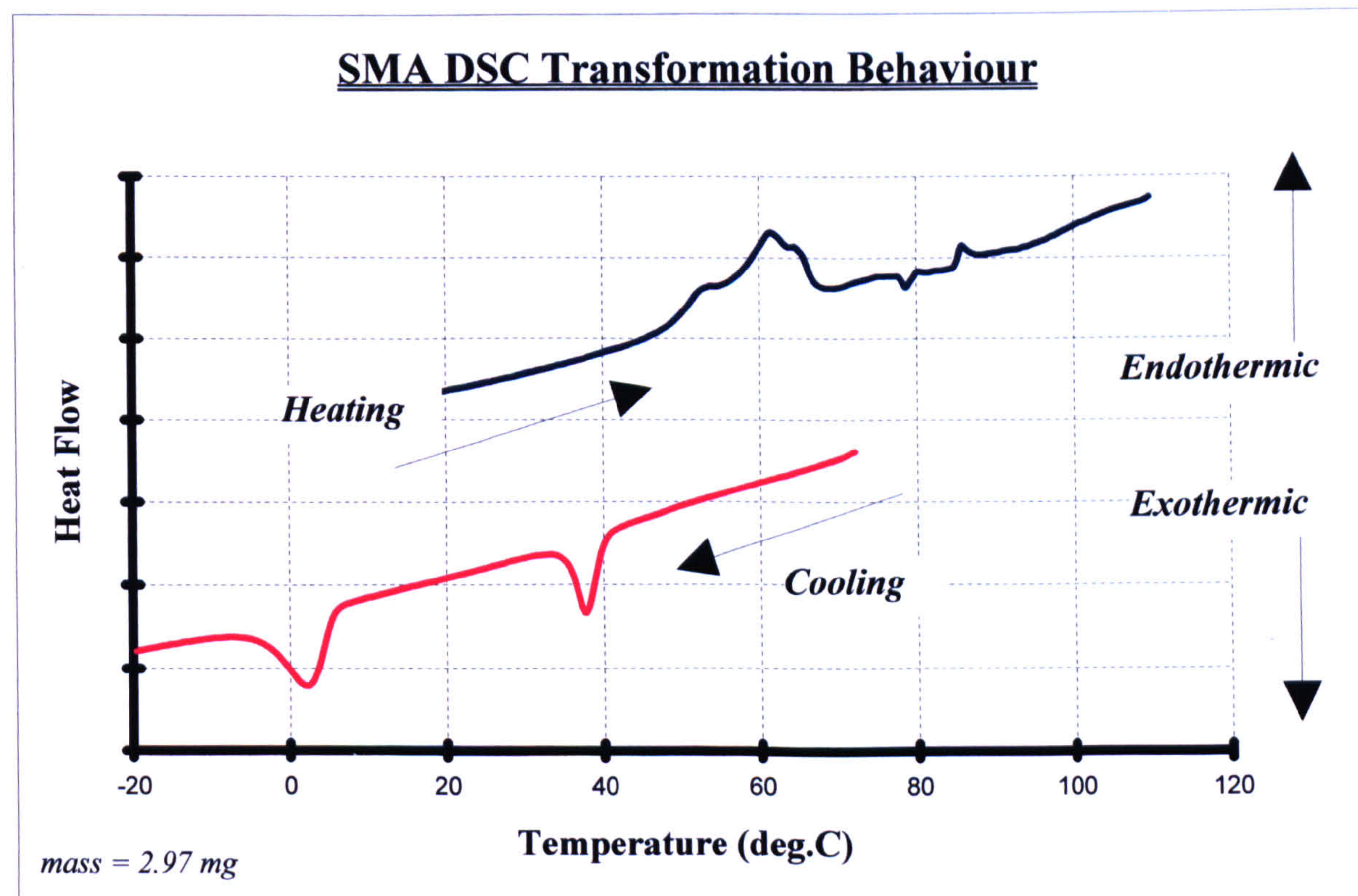


Figure 2.4. Transformation behaviour of a 49.8-50.0 at.% Ni alloy, 0.3mm in diameter.

Austenite transition was also found to decrease when cycling, though the amount of decrease was only about 5°C after 10⁴ cycles.

It was found within reference⁶⁷, where SMA wires were embedded in a thermoplastic composite, that the transformation temperatures are forced higher as a result of the curing cycle. Such cure cycle temperatures are much higher than that of thermoset cycles. It was stated that the effects of the thermoset processing temperatures are not thought to be significant.

2.5.2. Effects Of Stress On The Transformation Temperatures

The transformation temperatures are assumed to be influenced by stress, σ , according to the following linear relationships;

$$\begin{aligned} M_s &= M_s(\sigma=0) + \sigma/C_M & M_f &= M_f(\sigma=0) + \sigma/C_M \\ A_s &= A_s(\sigma=0) + \sigma/C_A & A_f &= A_f(\sigma=0) + \sigma/C_A \end{aligned} \quad (2.1)$$

The constants C_A & C_M are stress influence coefficients⁶⁹. It was seen within the published literature that C_A varies between 4.5→13.8MPa/°C and C_M varies between 7.0→11.3MPa/°C. It is often assumed, however, that C_A & C_M are equal to one another and have a continuously constant value over a temperature range. As an example, employing a stress influence coefficient of 10MPa/°C, then, a particular transformation temperature will be raised by 1°C for each 10MPa of axial stress applied to the specimen.

The affect stress has on the transformation temperatures is an important issue when embedding deformed martensitic SMA within a composite laminate. To what extent the matrix influences the phase transformation of the SMA fibres has been studied by electrical resistivity⁶⁸. Internal stresses may be present within the SMA due to thermo-mechanical mismatch between the SMA fibres and the host composite. This is attributed to the difference between the linear coefficient of thermal expansion of the metallic fibre ($\alpha_{SMA} \approx 6.6 \rightarrow 11 \cdot 10^{-6}/^\circ\text{C}$) and the employed carbon reinforced epoxy matrix ($\alpha_{ply} \approx -0.3 \rightarrow 28 \cdot 10^{-6}/^\circ\text{C}$). For a low SMA fibre volume fraction, a straight SMA fibre is subjected to an axial stress;

$$\sigma_{SMA} = E_{SMA}(\alpha_{comp} - \alpha_{SMA})(T_0 - T) \quad (2.2)$$

A positive value for σ_{SMA} indicates compression. T_0 is the temperature in the stress free state, i.e. at the curing temperature of 120°C. The internal residual stresses within the SMA are dependent upon composite constituent materials, lay-up configuration and orientation of the actuator from the global axis. Equation (2.2) gives a theoretical stress value not accounting for stress relaxation. It was stated in reference⁶⁸ that such internal residual stresses only weakly affect the R-phase and martensite transformation temperatures for an SMA wire embedded in an epoxy ($\alpha \approx 70 \cdot 10^{-6}/^\circ\text{C}$). No information was given regarding the austenite transformation.

Constraining an activated pre-strained SMA wire from returning to its memorised configuration results with the formation of a recovery stress. This stress will alter the transformation temperatures in an upward manner. If the activation temperature was equal to the stress free austenite finish temperature, then, only partial activation will result. The activation power level required to induce the full recovery stress must take into account the

expected recovery stress, **Section 2.6**, and the internal residual stress, equation 2.2. Knowing the total stressed state, the actual austenite finish temperature can be calculated, equation 2.1.

2.6. Ni-Ti Shape Memory Recovery Forces

The ability of the actuating material to operate within the host composite is, amongst others, dependent on the integrity of the SMA. Prior to integrating SMA actuators within composite laminates, the effect of SMA preparation and composite manufacturing, on the shape memory performance, must be determined. In order to determine the true usability of the actuators, the repeatable range of recovery stress that can be generated needs to be assessed. This will be achieved by cycling variable samples through their transformation temperature range and monitoring the associated recovery stress. The results of which will lead to a better understanding of the forces associated with the constrained recovery process. Such forces can then be employed in a detailed FEA simulating the hybrid structure behaviour.

For *control strategy 2*, the embedded SMA wires are subjected to a high temperature during the curing process. The peak recovery stress was found to drop as a result of the thermoplastic processing⁶⁷. It was stated, within this reference, that thermoset processing has a minor effect on the actuators performance and this will be verified.

As thoroughly discussed in **Chapter 3**, providing an abraded surface finish to the actuators, via sandblasting, enhances the mechanical interaction between the SMA and the matrix material and, therefore, hinders interfacial decohesion. A question unanswered within the published literature is *to what effect will such an abrasive action have on the shape memory recovery stress* ? An attempt is made to quantify such effects by the comparison of recovery stress measurements between a sandblasted and plain actuator. The plain actuator refers to SMA material with initial elongation only.

In summary, for both the alloys and alloy diameters considered, the effects of the actuator preparation parameters on the shape memory constrained response is to be evaluated. The requirements are of minimal effect compared to the plain material straight from the manufacturer. Other parameters, particularly related to the host composite material, also need investigation. With regret, due to time constraints, this has not been achieved to the level desired. Such parameters are void growth and distribution surrounding the embedded wires⁶⁹ and on the detrimental effects on the overall mechanical properties of the hybrid system. Such investigations will either qualify or impair the reliability of the envisioned adaptive hybrid systems.

2.6.1. Experimental Set-Up & Test Procedure

To gain a better understanding of the forces associated with the shape memory effect, tensile recovery stress characteristics of SMA specimens, with variable parameters, have been determined. Each specimen tested was first uniaxially elongated a predetermined amount, i.e. 2-6%, by loading it in tension, therefore, producing a stress biased martensite microstructure, **Figure 2.2**.

Testing was conducted in an Instron testing machine. The samples were clamped at both ends, one end bolted to a load cell, the other to a fixed cross head. This configuration would prevent contraction upon heating the deformed specimens. The wire actuators were heated by

a DC power source. Such an electrical power source necessitates suitable insulation from the testing machine. The recovery force, the electrical input and the associated temperature with time were monitored and recorded at a frequency of 10Hz. The results of which are shown in the next section.

Each sample was subjected to a number of operating cycles. A typical operating cycle is defined, via input of a step DC power source, as the heating of the actuator to reach its peak recovery stress. Such a peak recovery stress will be held for a short period of time, and then allowed to cool back to the ambient condition. Cool down is achieved by removal of the DC power source. All tests were conducted at an ambient temperature of approximately 20°C. Each cycle was run at intervals of approximately 60 seconds.

This activation strategy was employed throughout this programme, whereby, we are concerned with a martensite→complete austenite on-off type response. When embedding in polymeric-matrix composites, due to the constraints imposed by the surrounding matrix, only partial austenite may be achieved. This is particularly true when embedding the 49.8-50.0 at.% Ni alloy. This is undesirable as we fail to utilize the alloys full capability. The predominant matrix constraint is associated with the maximum imposed power in stimulating the actuators.

The drive variable for the SMA material is temperature. Therefore, the temperature rise, associated with the applied drive current, was monitored by the use of a fine thermocouple located at the surface of the specimen. For validation purposes, the end result, or steady state temperature, was compared to an energy balance solution.

The problem of isolating the electric heating current from the monitoring thermocouple was solved by creating an electrical resistive barrier in the form of 0.07mm thickness polyimide tape. Although the thermal conductance of the tape is low, the temperature differential across such a fine thickness is assumed to be negligible. To ensure a minimal temperature lag, a small diameter, 0.12mm, K-type Chromel-Alumel thermocouple was employed. The thermocouple was bonded to the insulated surface using redux 420 epoxy.

Ambient air movements may cause fluctuations in the temperature on the surface of the wire. Therefore, during testing the specimen was enclosed within a shroud. A better method of temperature control and measurement would be to test the sample in an environmental chamber, though restrictions are placed on the heating rate.

In general, the power source applied to the actuators resulted in an SMA temperature greater than the austenite finish temperature. Therefore, any fluctuations in temperature, in response to environmental influences, would have no effect on the measured recovery stress - the predominant parameter under investigation. Later on, experiments were conducted at lower power levels to assess the knock down effect.

2.6.2. Recovery Force Test Parameters

Table 2.3 lists the SMA sample variables under consideration and their appropriate results figure of reference. Prior to elongation, all samples were heated in boiling water to remove any plastic deformations present as a consequence of packaging and transportation, then allowed to cool to the ambient. The samples were then subjected to their appropriate variant. Two samples of each variant was tested ensuring consistency in material behaviour.

For the 49.8-50.0 at.% Ni alloy, the power level applied to energize the actuator was 21.5W/m. This would result with a recorded steady-state temperature, depending on the accuracy in positioning the thermocouple, of 120-140°C.

Alloy	Dia. (mm)	ϵ_L (%)	SB	SCC	CHA	Results Figure	
49.8-50.0 at.% Ni	0.4	6				2.6, 2.23	SB - SMA sample subjected to sandblasting.
	0.4	6			✓	2.27	
	0.4	6		✓		2.8, 2.23	
	0.4	6		✓	✓	2.9	SCC - SMA sample first prestrained and then subjected to curing cycle. During the curing cycle, the SMA is fixed at both ends.
	0.3	6				2.10, 2.23	
	0.3	6			✓	2.11, 2.23	
	0.3	6		✓		2.12	
	0.3	6		✓	✓	2.13	
	0.4	6	✓			2.14	CHA - Cross head adjustment removing/releasing slack/tension after heating cycle.
	0.4	6	✓	✓		2.15	
	0.3	6	✓			2.16, 2.23	
	0.3	6	✓*			2.17, 2.23	
	0.4	3				2.18, 2.23	
	0.3	3				2.19, 2.23	* - Actuators are subjected to a partial sandblast, i.e. only abraded over 10mm region either end of the wire.
50.0-50.2 at.% Ni	0.4	6				2.20-1, 2.24	
	0.4	2.5,3.5,4.5				2.24	
	0.3	6				2.22	
	0.3	3.5				2.24	

Table 2.3. *SMA recovery stress variables.*

For the 50.0-50.2 at.% Ni alloy, the power level was varied between 22.1 and 12.1W/m. These power levels were adjusted slightly to account for the variable length of sample with pre-strain. This will ensure the same steady state temperature within a particular alloy.

The recorded transient thermal response for the former alloy is illustrated in **Figure 2.5**. The figure illustrates that, when a voltage, V, and current, I, are applied suddenly to the SMA specimen, its temperature will rise from the ambient temperature in an exponential like manner. Ignoring the effects of radiation, latent heat and assuming convective cooling to be negligible, then, from an energy balance equation, a first approximation for the steady state temperature differential may be calculated from;

$$\Delta T = \frac{IV}{mc_p}$$

(2.3)

For the particular alloy shown, i.e. 0.3mm in diameter, 400mm in length, when subjected to step input of 7.17Volts and 1.2Amps, then, assuming a 300J/Kg°C specific heat, **Table 2.1**, a temperature differential of 156°C can easily be calculated. Accounting for environmental influences should ensure a closer comparison with the recorded steady-state behaviour of **Figure 2.5**.

A note of warning; the fibredux 913 pre-preg system employed, **Chapter 3**, has a maximum operating temperature of 131°C. The SMA activation temperature must, therefore, be lower than this temperature. The effect of the SMA activation temperature on the composite mechanical behaviour is discussed in great detail in **Chapter 5**.

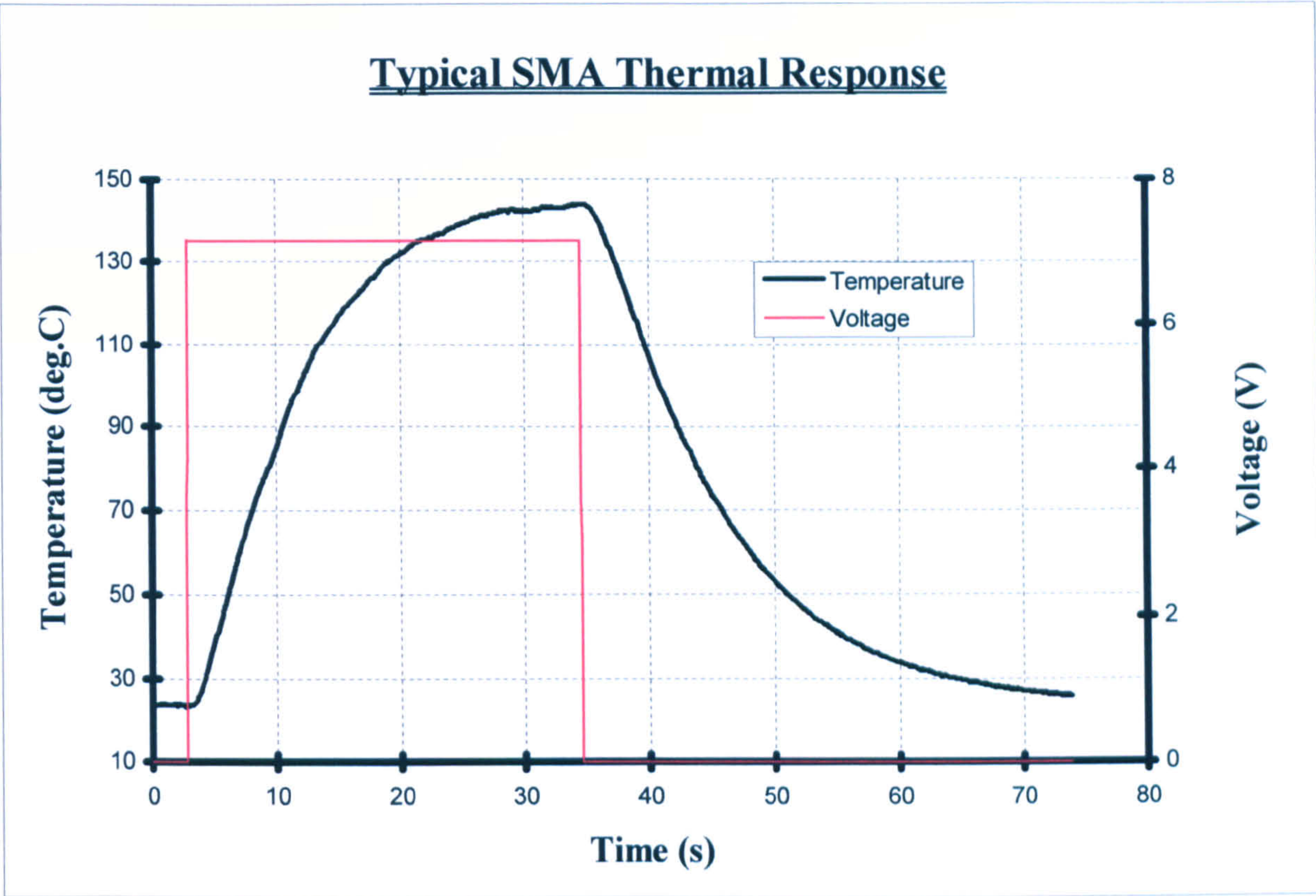


Figure 2.5. Typical shape memory alloy, 49.8-50.0 at.% Ni 0.3mm in diameter, transient thermal response for a step DC input of 21.5W/m.

2.6.3. Constrained Recovery Force Results

Figures 2.6-22 show the recorded SMA tensile recovery force curves generated as a result of the activated wire trying to return to its memorised length. The horizontal axis exhibits time, the vertical axis exhibits the recovery force. We first concern ourselves with the lower atomic percentage nickel based alloy. **Figure 2.6** shows the cyclic recovery force for the 0.4mm diameter alloy initially subjected to a 6% pre-strain, no other variants were applied. It is clearly evident that the maximum recovery force is generated during the first cycle. Subsequent cycles show a decrease in the recorded recovery force. The greatest reduction occurs during the first few cycles. After 20 cycles the recovery force becomes more stable.

The behaviour of the actuator during its operation is characterised by an initial sharp rise in recovery force. This being attributed to the step input power source. Lower power/heating rates would result with a lower recovery force gradient. A peak in recovery force will not be reached until the material stabilizes at a temperature above $A_{s, stress}$ or reaches $A_{f, stress}$. This characteristic is observed for all tests, **Figures 2.6-22**.

An interesting feature occurs during the first cycle. After the force reaches a certain peak value it gradually decreases, even with further increase in temperature. The recovery force then settles down to a constant value, the specimen being maintained at a given constant temperature. Such behaviour was shown to exist in the literature⁷⁰, where it is stated that the peak value of the recovery stress, during this cycle, is influenced by the heating rate. The higher the heating rate, the greater the peak recovery stress. After the peak, no matter what the heating rate, the recovery stress would tend to the same value. It would appear, from **Figure**

2.6, that this phenomena exists only for the first cycle. Cyclic behaviour was not discussed in the cited reference⁷⁰. No definitive explanation for the aforementioned behaviour is put forward. A plausible conjecture, however, may associate the phenomenon with the materials coefficient of thermal expansion and/or the formation of lattice defects such as dislocations. We will return to this phenomenon later in this section.

For this particular specimen, after the first cycle, when allowed to cool to the ambient temperature, the actuator became loose within the fixtures. This slack is a result of the formation of permanent strain, within the actuator, when subjected to the elevated temperature. The formation of a permanent strain in the SMA actuator diminishes the stress in the SMA crystal lattice⁵⁸.

With the addition of the permanent strain, upon subsequent tests, when heated to austenite, then allowed to cool to martensite, while free from any external stress, the specimen will not revert to completely deformed detwinned martensite. In actuality, the actuator becomes one part twinned non-deformed martensite and the other part deformed detwinned martensite.

The specimen must be in the deformed detwinned martensite state in order to forcibly recover during heating⁶², therefore, the level of recovery stress will be lower for each cycle. As long as permanent strain is continuously induced, then, thereafter, the SMA will cool to a smaller percentage deformed detwinned martensite. As the percentage of deformed martensite diminishes with each succeeding cycle, the peak recovery force continuously drops.

To a large extent, the actual mechanism for permanent strain formation is associated with the a uniform internal defect formation such as dislocations. To a limited extent, it may also arise at the constrained boundaries. As the temperature profile of the wire changes with current flow, the material undergoes the transformation process, whereby, internal stresses are generated and are equilibrated by the constrained boundaries. Owing to heat sink effects at the boundaries, a finite region in close proximity to the boundary interface may not undergo transformation⁵⁴. Such a region, remaining martensitic, will be subjected to significant levels of stress that may be detrimental in nature. This, of course, will not pose any threats should the boundary be at the elevated temperature.

From **Figure 2.6**, after 20 cycles, the recovery force level is seen to stabilise at 58N, a drop of 19N compared to cycle 1.

It is shown in **Figure 2.7** that removal of the permanent strain, employing Instron cross head adjustment (CHA), prior to each cycle, results with SMA repeatable cyclic behaviour. Cycle 15, **Figure 2.7**, shows approximately a 20N increase in the recovery force compared to its counter part of **Figure 2.6** where the actuators must first take up the slack before generating any useful recovery force. After 15 cycles, the cross head adjustment required to remove the slack was 5.519mm. Unfortunately, the manifestation of such slack removal could prove difficult for an in service SMA/hybrid structure.

The recovery force curves for the same alloy diameter, though initially pre-strained and subjected to the curing cycle, are shown in **Figures 2.8 & 2.9**, without and with CHA respectively. A direct comparison with **Figures 2.6 & 2.7** indicates that the thermoset cycle has no compromising effects on the actuator recovery force.

Similar behaviour is shown to exist in **Figures 2.10-13**, for the 0.3mm diameter 49.8-50.0 at.% Ni alloy, with one conspicuous exception. **Figures 2.10 & 2.11** show the measured recovery force cycles without and with CHA respectively. With this alloy, when allowed to cool down to martensite, slight tension was recorded. CHA alleviating such tension. Comparing these figures, it can be seen that a 12N difference exists between cycle 15, the favourable scenario having no CHA.

Figure 2.10 shows that the greatest reduction in recovery force occurs during the first few cycles, and to a much lesser extent in later cycles, analogous to the 0.4mm diameter alloy. On the other hand, removing the residual force, upon cool down for each cycle, results in a considerable reduction in recovery force, **Figure 2.11**. It is expected that such a reduction would continue until the displacement of the cross heads reaches the actuators initial pre-strain value. At this point, no deformed martensite will exist and, thus, no recovery force will be generated.

It can be seen, when comparing **Figure 2.10** to **Figure 2.12** and **Figure 2.11** to **Figure 2.13**, that the effect of subjecting the material to the thermoset processing is either indistinguishable or minimal, analogous to the 0.4mm diameter alloy. This is an interesting observation, particularly for the first cycle of **Figures 2.8, 2.9, 2.12 & 2.13**. The thermoset processed actuators have previously been subjected to 120°C for the duration of a cure cycle. Such an environment would place the constrained actuators under significant stress, analogous to the application of a electrical power source. All the results obtained, so far, show detrimental effects in cyclic performance, particularly early on in the cyclic life. The first *measured* cycle of the thermoset processed actuators fail to comply with this trend. Therefore, from a recovery stress point of view, such alloys are deemed suitable for directly embedding in polymeric-matrix thermoset composite constructions.

From the results so far, an expected variation observed is the lower maximum stable recovery force of 50N for the first cycle of the 0.3mm diameter alloy, compared to 78N for the 0.4mm diameter alloy. After 15 cycles, this reduces to a stable 40N, without CHA, for the 0.3mm diameter alloy, compared to 58N for the 0.4mm diameter alloy. The reduction in the recovery force is much larger for the latter. This is thought to be associated with the martensite phase transition temperatures. For the 0.4mm diameter alloy, complete martensite start occurs at 25°C. However, complete martensite start does not occur until 8°C for the 0.3mm diameter alloy, way below the current operating temperature.

When activating such alloys from room temperature and then allowing the alloy to cool, the 0.4mm diameter alloy will undergo an *almost complete phase cycle*, i.e. austenite → R-phase → martensite start. The 0.3mm diameter alloy will only undergo an incomplete phase cycle, i.e. austenite → R-phase.

It was found in the literature⁶⁶ that a complete phase cycle reduces the martensite transformation temperature, rather drastically at first, but then levelling off. Incomplete phase cycling brings about no change in the transformation temperatures. Furthermore, lattice defects, such as dislocations, were found to occur as a result of the complete cycling. Such defects cause a stress relaxation. These defects are not so common when subjecting the material to the incomplete phase cycle. When compared to that of the 0.3mm diameter alloy, it is these lattice defects that are the likely cause for the greater force reductions seen in the 0.4mm diameter alloy undergoing near complete phase transition. The levelling off of the force reduction may be due to the downward trend in martensite transformation temperatures with each cycle. This will be validated during the discussion on the cyclic recovery forces associated with the 50.0-50.2 at.% Ni alloy. For the current alloy, the affect the heating/cooling rate has on the formation of lattice defects has not been reviewed.

By comparing **Figures 2.14 & 2.15** with **Figure 2.6**, for the 0.4mm diameter alloy, the sandblasting variant is seen to be insignificant. **Figure 2.14** depicts the recovery force generated for an abraded SMA. **Figure 2.15** depicts the recovery force generated for an abraded sample subjected to the cure cycle, now known not to have any significant effect. Both figures show consistency in the material behaviour when compared to that shown in **Figure 2.6**.

With regards the effects of sandblasting the 0.3mm diameter alloy, by comparison of **Figure 2.16** with **Figure 2.10**, it would appear that the abrasive action degrades the recovery force. The first cycle of the unabraded sample can generate a recovery force of 50N, whereas the abraded sample can only achieve a force of 37N. After 10 cycles the unabraded sample can generate a recovery force of 42N. However, the abraded sample can only achieve 33N, a significant decrease.

This degradation in recovery force results when the entire SMA periphery is sandblasted. Based on the work published in the open literature where the stress distribution in embedded actuators and the resulting in-plane actuation force on a hybrid composite beam are investigated for various boundary conditions⁷¹, and with reference to various micro-mechanics composites texts⁸, then, the stress transfer between the SMA and the matrix material occurs primarily through interfacial shear, around the periphery of a the actuator, and will be greatest at the actuator ends.

As most of the recovery force is transferred at the actuator ends, then, only the ends require the enhanced mechanical interaction with the matrix. **Figure 2.17** shows the recovery force curves for a 100mm length of SMA wire, pre-strained by 6%, subjected to a sandblast over a 10mm region from both ends. The comparison of **Figure 2.17** with **Figure 2.16** shows a moderate increase in recovery force for all cycles. However, for the alloy diameter currently considered, the recovery force levels fail to reach those of the unabraded sample.

It is likely that the observed lower recovery force levels are due to periphery material loss. Providing the sandblasting prevents interfacial decohesion, then, for the partially sandblasted actuators, these lower stress levels are tolerable.

Figures 2.18 & 2.19 show the recovery forces generated for the 0.4mm and 0.3mm diameter 49.8-50.0 at.% Ni alloys, respectively, with an initial pre-strain of 3%. No other variant was applied. An interesting feature occurs for each cycle. Upon activation, the recovery force rises to a peak value and then decays to a stable value. Previously, this rise and decrease only occurred for the first cycle. When the power level is turned off, the recovery force is seen to rise prior to its exponential like decay. Such prominent behaviour, for the samples with 3% pre-strain, is also observed for cycle 15 of **Figures 2.11 & 2.13**, though to a much lesser extent.

It is thought that the first peak on heating occurs as a result of maximum recovery force being generated when the material reaches its true austenite finish temperature. With reference to our earlier conjecture, the slight reduction in recovery force is thought to be attributed to the material longitudinal thermal expansion effects with further increase in temperature. Likewise, the rise in recovery force to the second peak, occurring at power shut down, is a result of the longitudinal thermal contraction of the actuator, while the temperature remains above the austenite finish temperature. Below A_c , the recovery force decays in an exponential manner. Why such prominent behaviour is evident only for the actuator configuration with an initial pre-strain of 3% is currently unknown.

With regards the 50.0-50.2 at.% Ni alloy, for the 0.4mm diameter specimens, the recovery force was measured for specimens with initial pre-strain levels of 2.5%, 3.5%, 4.5% & 6%, and for the two power levels of 22.1 & 12.1W/m. The recovery force for the 0.3mm diameter specimens was measured at pre-strain levels of 3.5% & 6% and at a power level of 22.1W/m.

No attempt was made to investigate the effects of the curing cycle or the effects of abrasion on this alloys recovery behaviour. Having stated that a thermoset cure cycle has no compromising effects on the magnitude of recovery force generated by the earlier constrained actuators, it is expected that this holds true for the alloy composition currently under investigation. It has also been shown that sandblasting 0.3mm diameter wires can

significantly reduce the level of recovery force. Similar behaviour is expected from the current finer diameter alloy.

Figure 2.20 depicts the cyclic behaviour for the 0.4mm diameter specimen, pre-strained by 6%, and activated by a step input of 22.1W/m. For the first actuation cycle, the recovery force reaches a level of 110N which subsequently degrades to a consistent level of 96N over 20 cycles. The rate of degradation is at its highest during the first few cycles, such behaviour being characteristic of nickel-titanium. These recovery force levels are significantly higher, for a comparable power level per unit length, than those previously documented.

It is clearly evident that, when deactivating the specimen and allowing it to return to the ambient temperature, the specimen retains a partial force of 23N. This is because the imposed operating temperature cycle remains above the stressed martensitic start temperature, therefore, preventing the formation of martensite upon cool down. This remaining residual force was typical for all the 0.4mm diameter specimens tested, regardless of the level of pre-strain.

Similar behaviour is evident in **Figure 2.21** though for a lower applied power level of 12.1W/m. The recovery force traces appear slightly sporadic as the steady state temperature, for such a power level, is approached. It is likely that the input stimulus is insufficient to drive the actuator past the stressed austenite finish temperature. This would account for the lower perceived recovery force of approximately 85N. A prominent observation is the consistency in each illustrated trace, as opposed to the first few cycles of **Figure 2.20**. It is thought that overdriving the actuators leads to the initial recovery force degradation.

Figure 2.22 depicts the recovery force behaviour for a 0.3mm diameter specimen initially pre-strained by 6% and activated by the amount previously specified. Such behaviour is similar to that of the same diametrical specimen documented earlier, i.e. the 0.3mm diameter 49.8-50.0 at.% Ni alloy, with the exception that a slightly higher cycled recovery force level of 47N exists compared to 42N, **Figure 2.10**, and a residual force of 11.2N remains after specimen cool down. For the lower pre-strain levels, for both diameters, similar behaviour to that shown in **Figures 2.20-2.22** was observed. The results of which are Summarized in **Figure 2.24**.

To conclude this chapter, the witnessed Ni-Ti fatigue issues, or degradation in recovery force characteristics, are to be addressed. Such actuators, should they be incorporated within aerospace structural platforms, will generally be expected to function consistently over a great number of cycles and, thus, knowledge of the degradation of the shape memory during cycling is of vital importance. The fatigue properties of SMA elements are usually considered in a different way from conventional structural materials. Here, the actuators primary function is recovery force. Hence, it is reasonable to discuss the fatigue property of the SMA in terms of the recovery force decay with the number of thermal cycles.

Figure 2.23 shows how the shape memory recovery force degrades over 15 cycles for the 49.8-50.0 at.% Ni variants. Both alloy diameters tested require 15-20 cycles prior to generating a pseudo-stable recovery force. The greatest initial recovery drop is seen when the alloys have a 3% pre-strain. For this particular alloy, with the operating constraints imposed for this programme of study, the recovery force is expected to continue to drop. This is especially true for actuators with the higher initial pre-strain levels.

A summary of results for all the tests performed on the 50.0-50.2 at.% Ni alloy is shown in **Figure 2.24**. It is clearly evident, for each curve, that after the first few cycles, the generated recovery forces stabilise to a consistent level.

Two factors affect the fatigue property of the Ni-Ti. These are; (1) the temperature range for a particular alloy composition and (2) the amount of initial imposed pre-strain.

The consistent cyclic behaviour, shown in **Figure 2.24**, is attributed to the operating temperature cycle imposed upon each sample. Activation and deactivation above the materials stressed martensitic start temperature subjects the material to an incomplete phase cycle. The existence of a partial, or even a complete, rhombohedral phase may exist upon cool down but this will not result with the formation of lattice defects which occur when completely cycling between austenite and martensite. It is reported elsewhere⁷² that excellent fatigue performance may be achieved where only an R-phase transformation occurs upon cool down during alloy thermal cycling.

As complete phase cycling degrades the recovery behaviour with each successive activation/deactivation, with knowledge of the alloy operating temperature, then alloy composition and heat treatment can be optimised to improve the cyclic response.

It is evident, from **Figure 2.24** that, for both diametrical samples, a decrease in the initial level of pre-strain, i.e. from 6% to 2.5%, will result with a relatively small drop, compared to the previous alloy, in stabilised recovery force. It is well known that the ultimate recovery force fatigue life of Ni-Ti depends upon the initial level of pre-strain. Therefore, for this particular alloy with the current imposed operating conditions, relatively small amounts of pre-strain can still induce significant recovery force levels. This may be of particular benefit for applications requiring high levels of cyclic operation, i.e. next generation military aircraft, spacecraft or second generation supersonic civil aircraft. The previous undesirable degraded effect, **Figure 2.23**, however, would not be an important issue should such an actuator be utilized in the design of expendable launch vehicles or missiles.

These measured recovery forces have been generated between rigid boundaries, such a condition enforced upon the actuators within *control strategy 1*. When the SMA actuators are embedded and constrained to the host composite, *control strategy 2*, then, owing to the materials stiffness, strain recovery will occur. This will result in lower induced recovery stress. The high rigidity of the laminate constituents employed, however, will ensure that the recovery force loss is minimal.

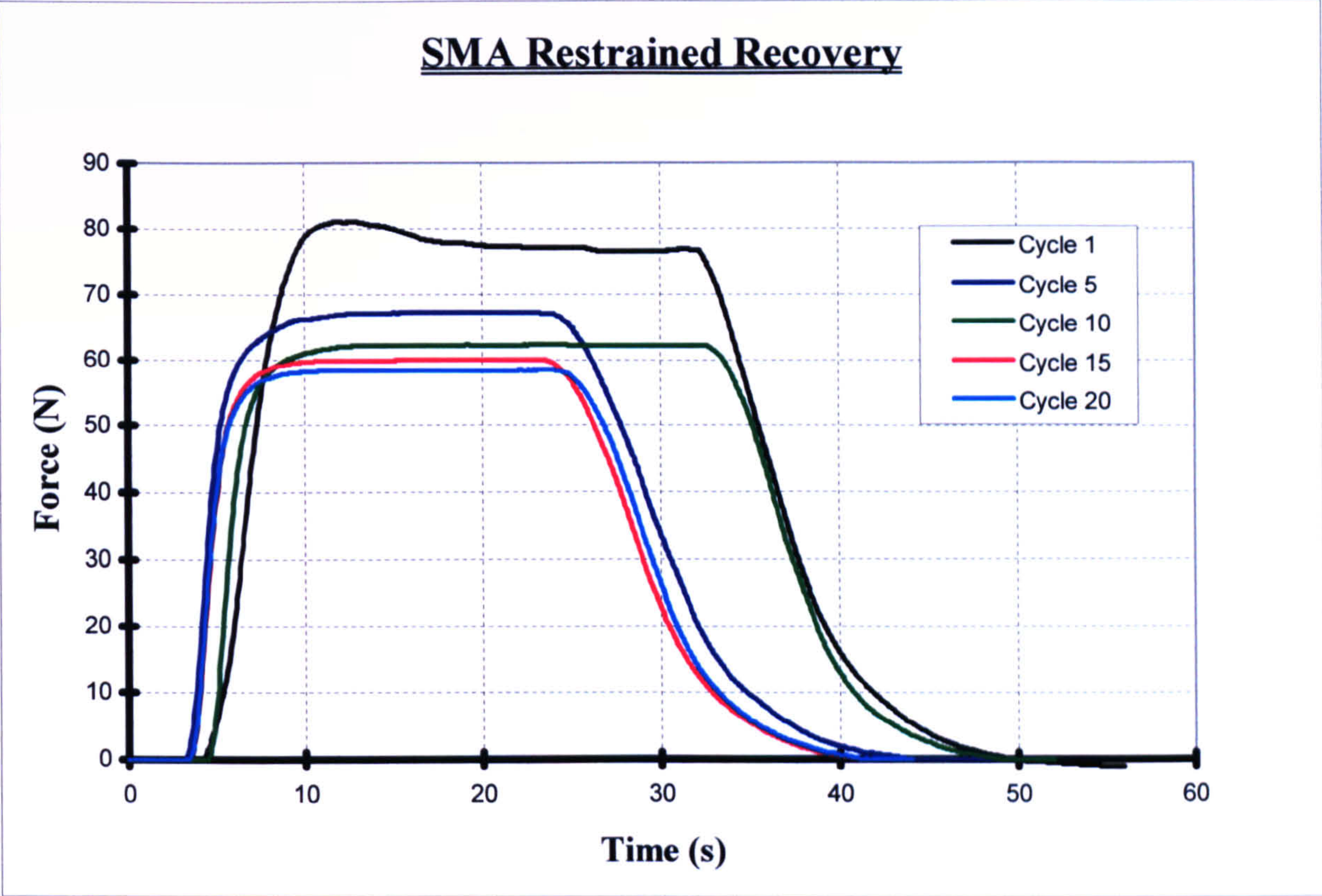


Figure 2.6. Constrained recovery behaviour for the 49.8-50.0 at.% Ni plain alloy, 0.4mm in diameter, initially pre-strained 6%.

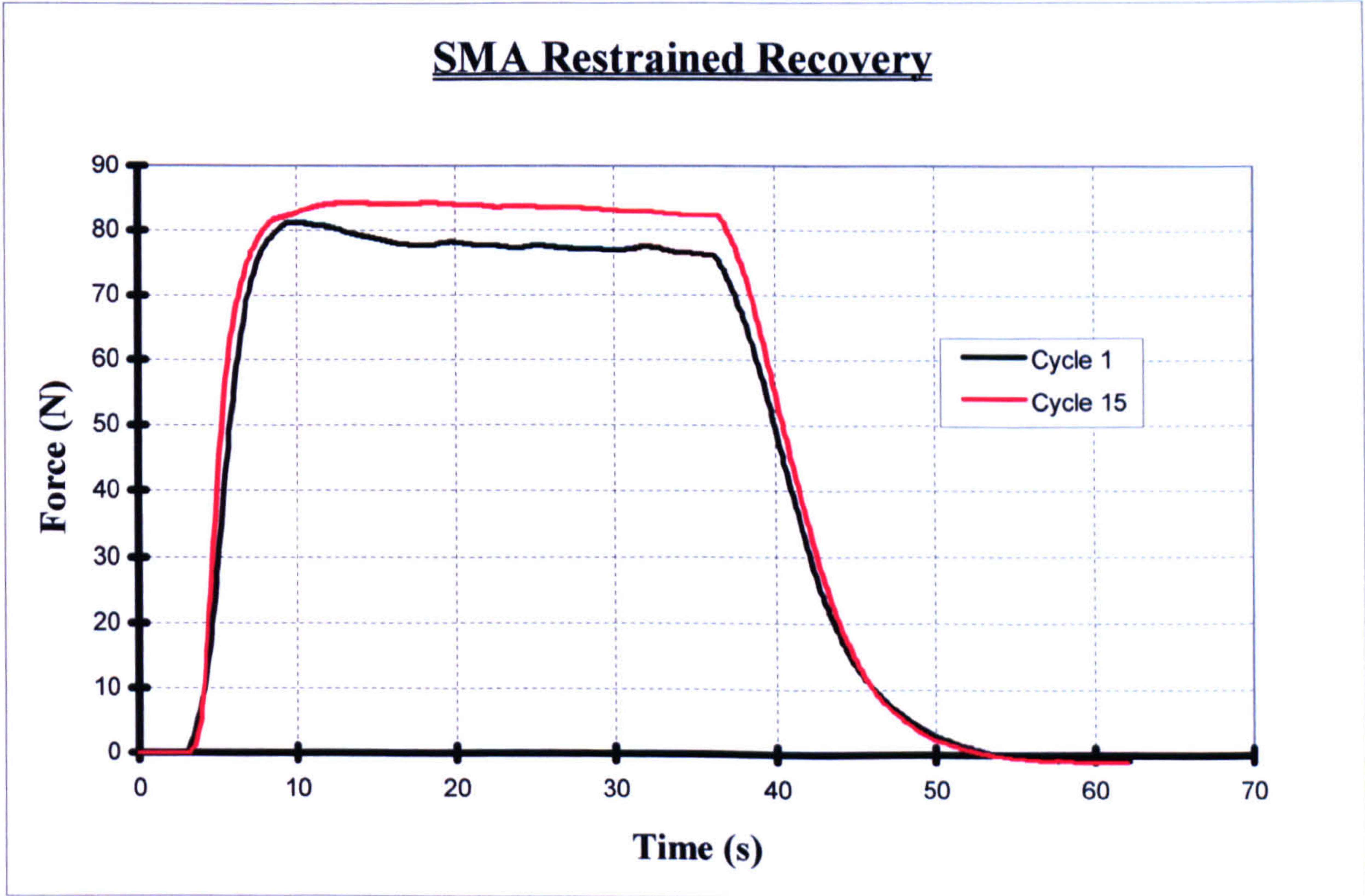


Figure 2.7. Constrained recovery behaviour for the 49.8-50.0 at.% Ni plain alloy, 0.4mm in diameter, initially pre-strained 6% and with CHA .

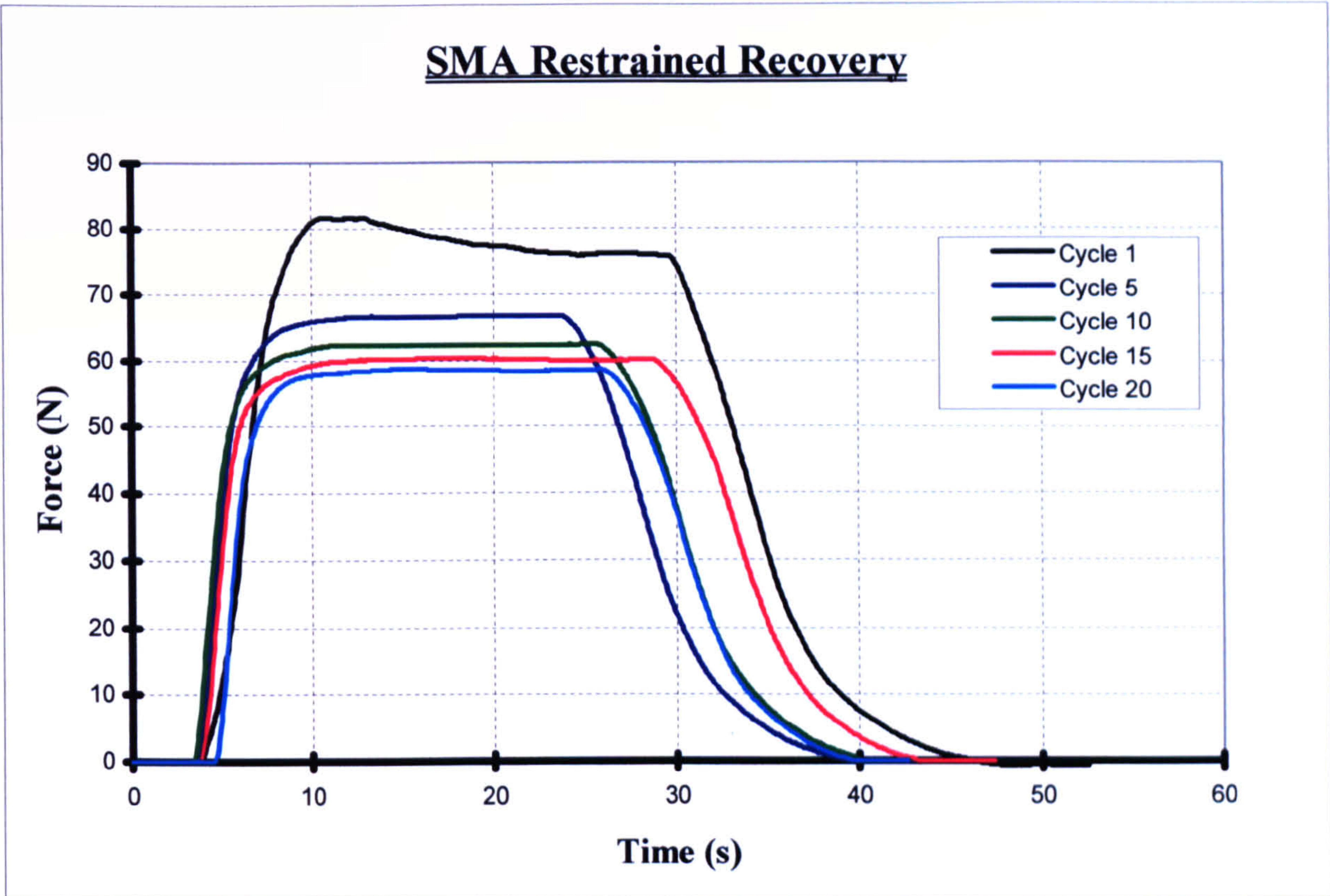


Figure 2.8. Constrained recovery behaviour for the 49.8-50.0 at.% Ni alloy, 0.4mm in diameter, initially pre-strained 6% and subjected to a cure cycle.

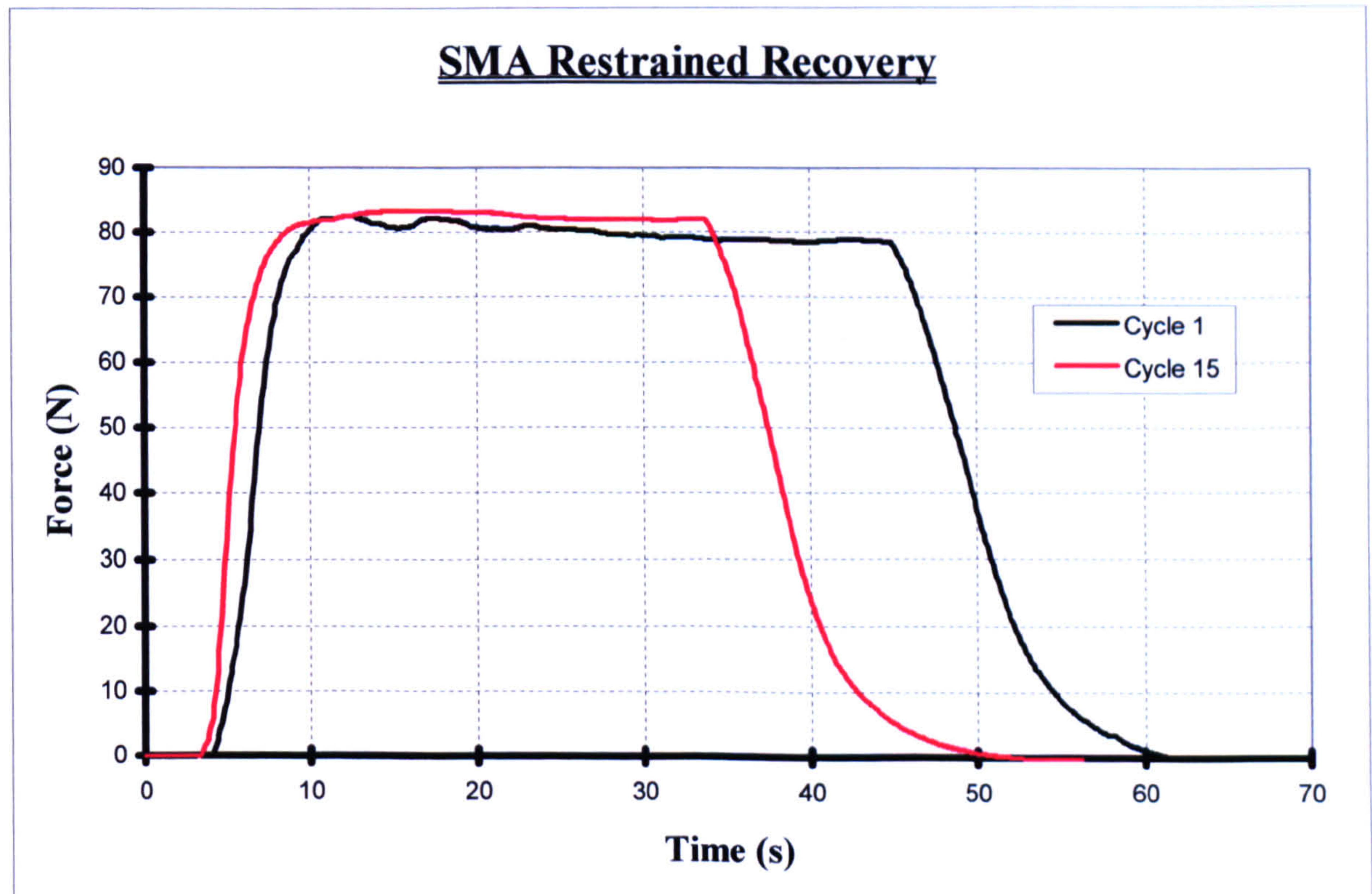


Figure 2.9. Constrained recovery behaviour for the 49.8-50.0 at.% Ni alloy, 0.4mm in diameter, initially pre-strained 6%, subjected to a cure cycle and with CHA.

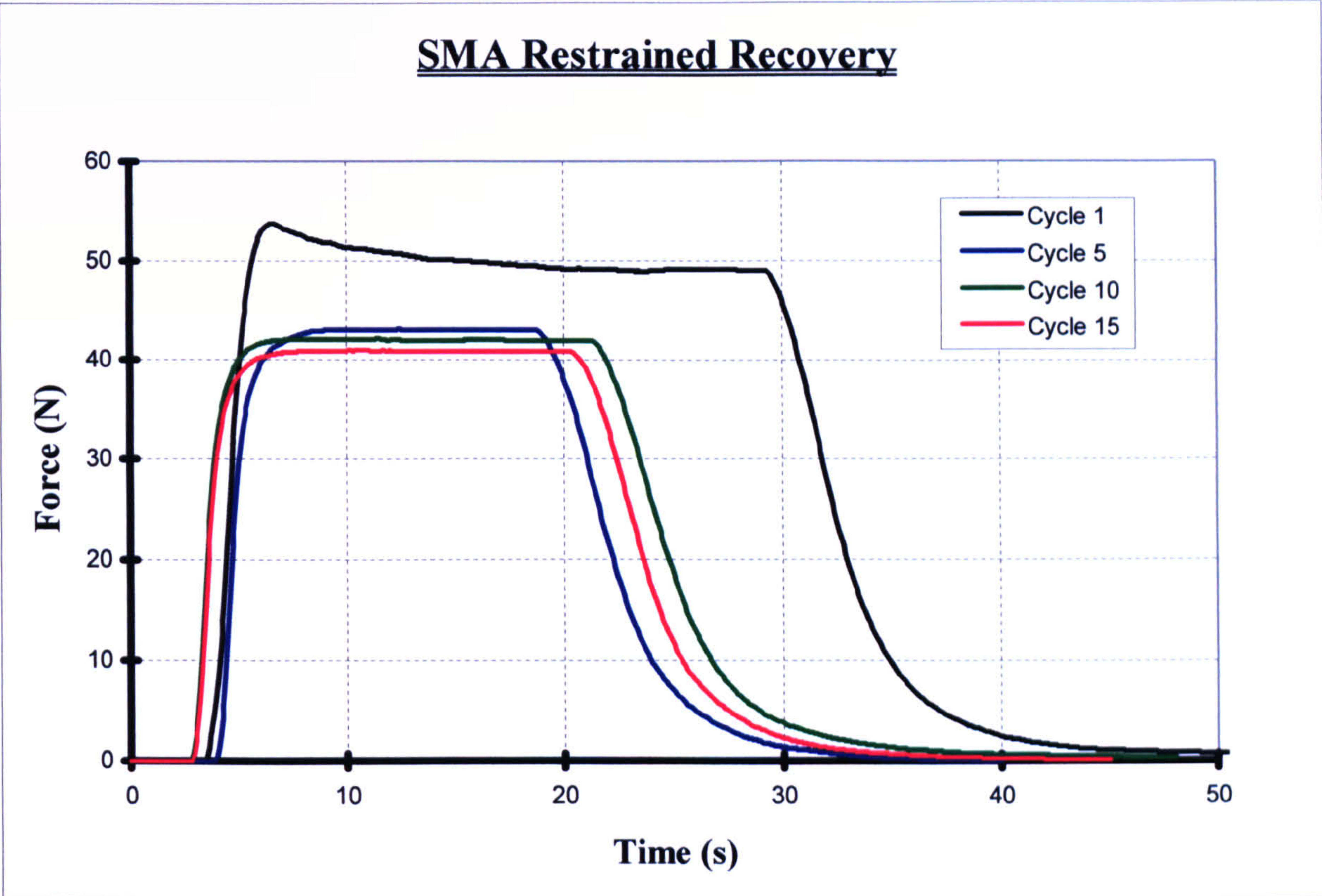


Figure 2.10. *Constrained recovery behaviour for the 49.8-50.0 at.% Ni plain alloy, 0.3mm in diameter, initially pre-strained 6%.*

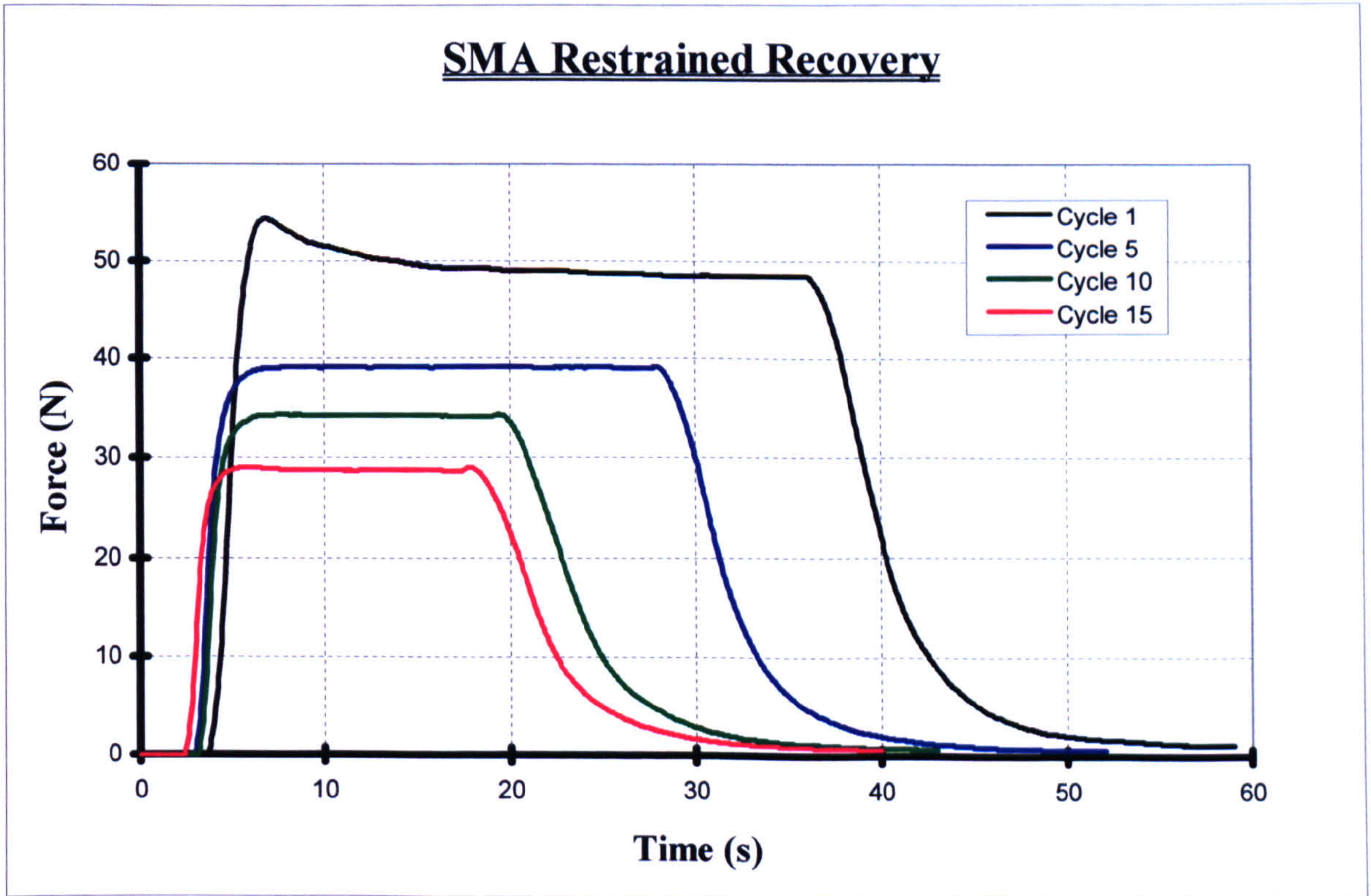


Figure 2.11. *Constrained recovery behaviour for the 49.8-50.0 at.% Ni plain alloy, 0.3mm in diameter, initially pre-strained 6% and with CHA.*

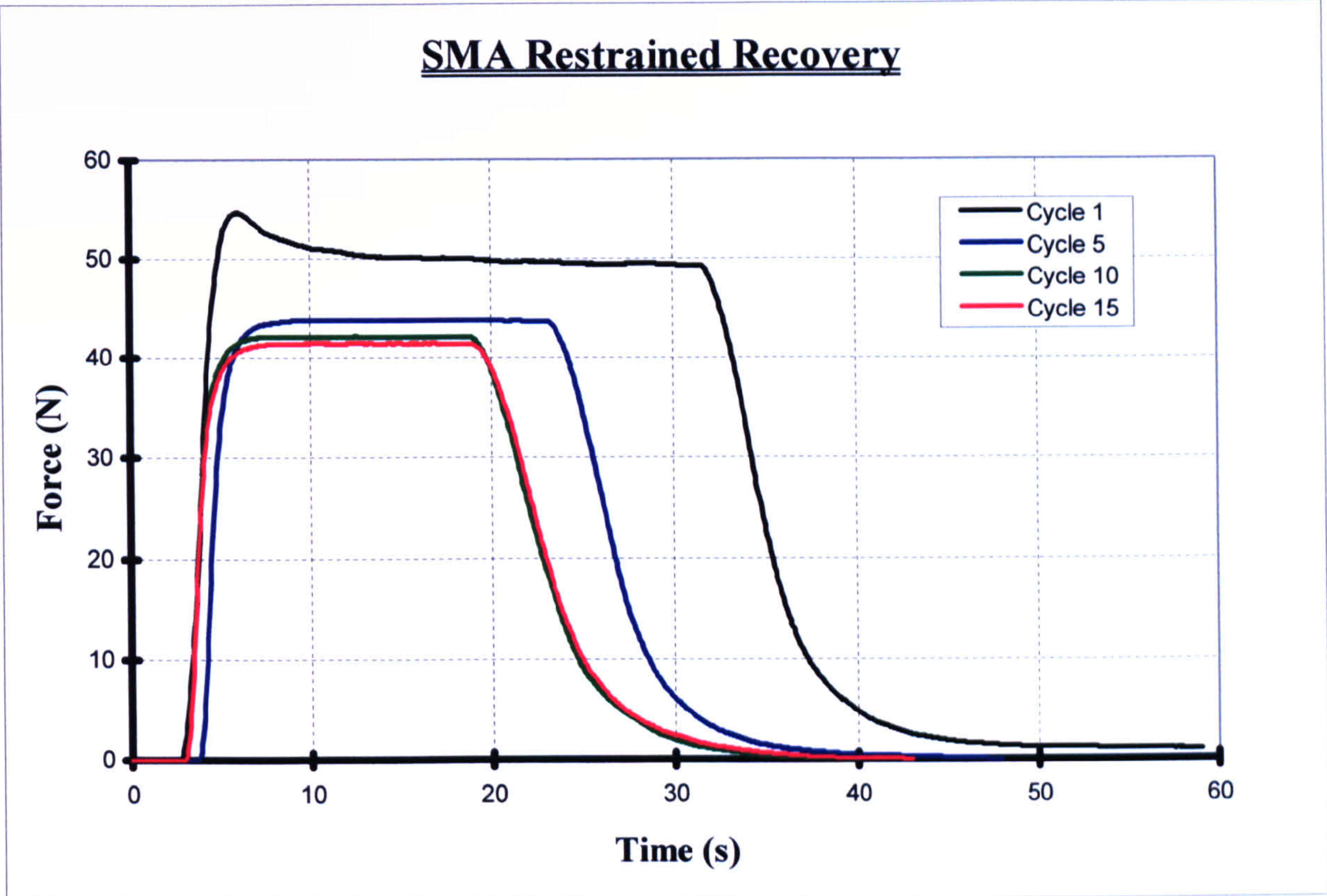


Figure 2.12. *Constrained recovery behaviour for the 49.8-50.0 at.% Ni alloy, 0.3mm in diameter, initially pre-strained 6% and subjected to a cure cycle.*

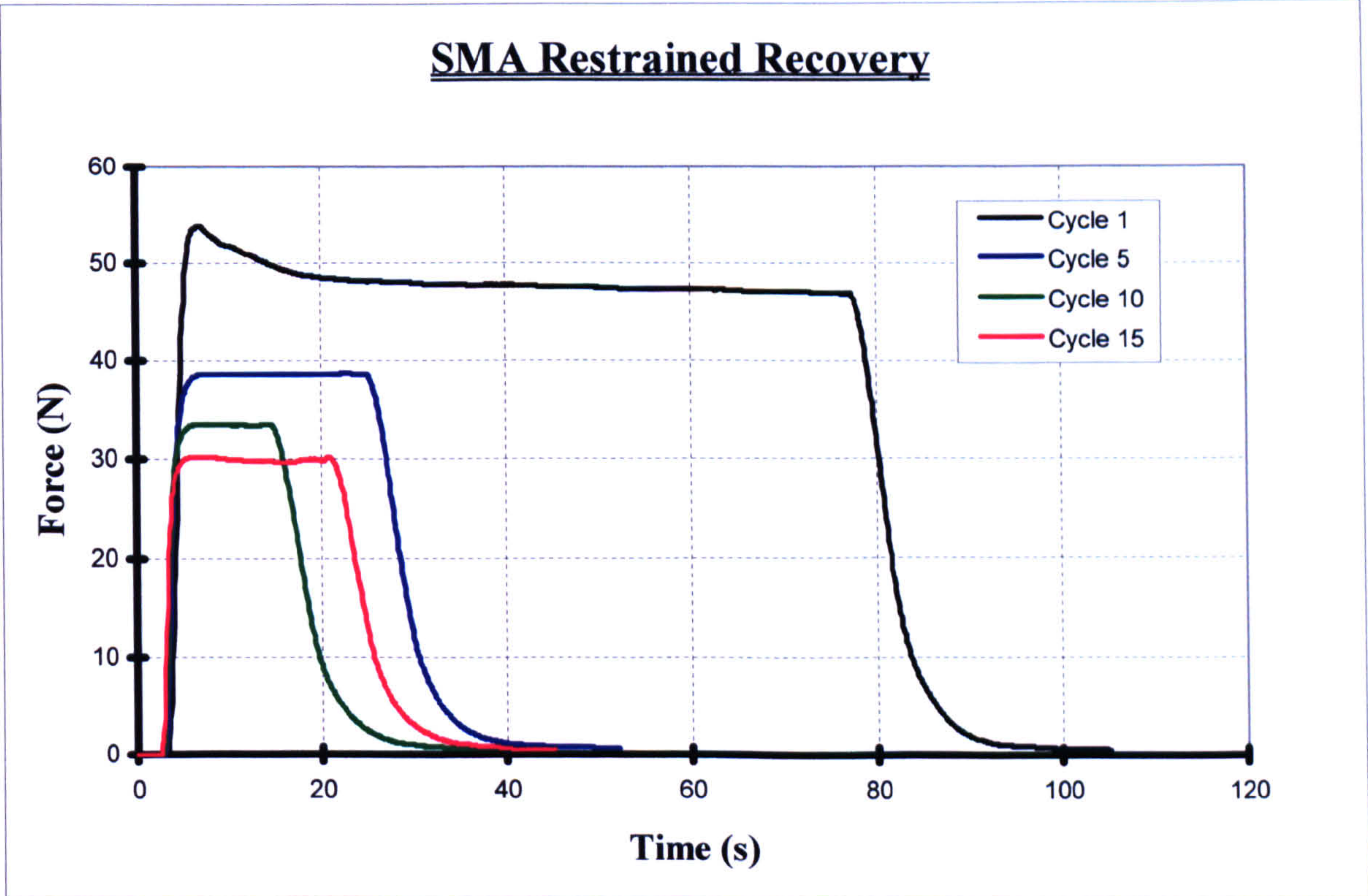


Figure 2.13. *Constrained recovery behaviour for the 49.8-50.0 at.% Ni alloy, 0.3mm in diameter, initially pre-strained 6%, subjected to a cure cycle and with CHA.*

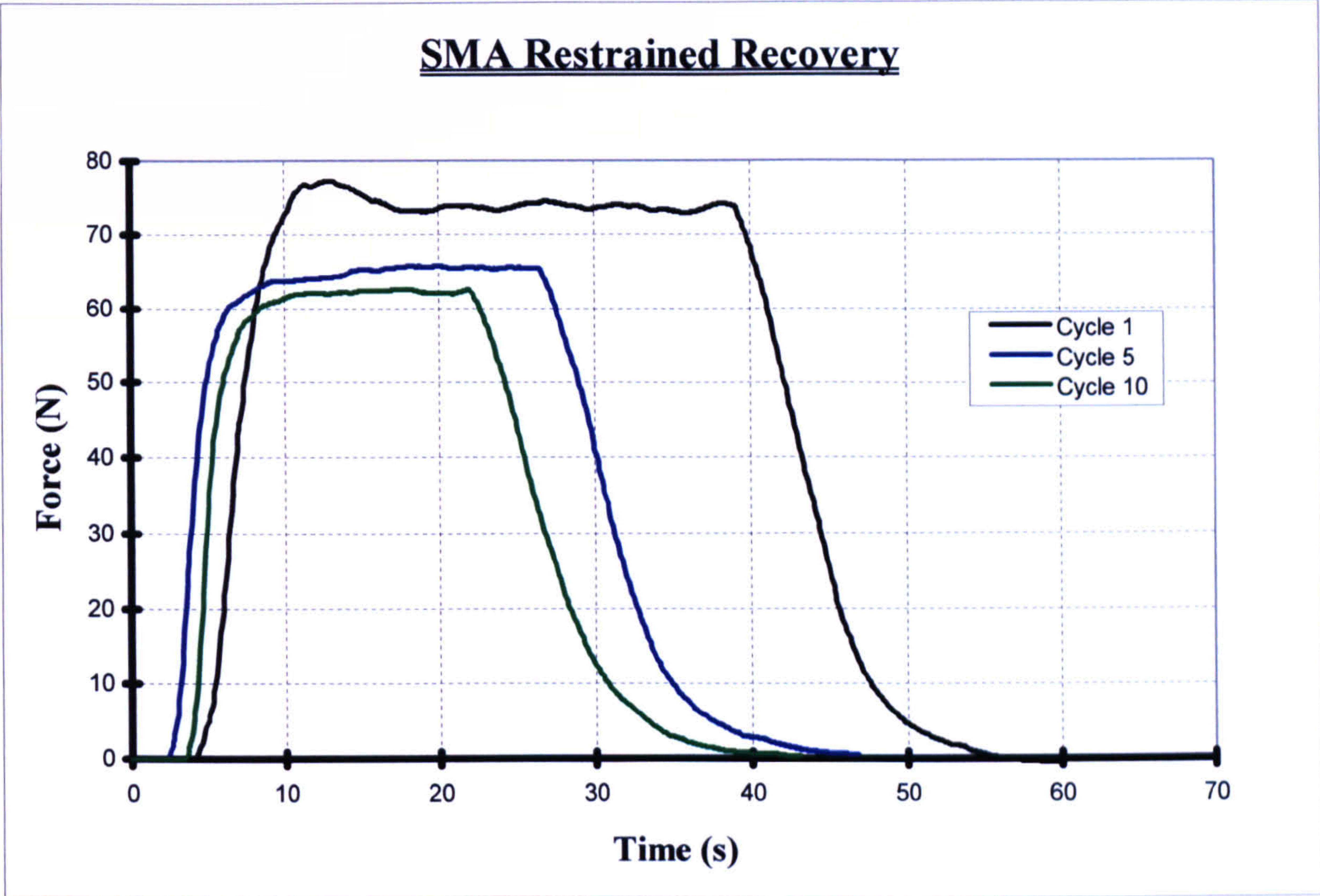


Figure 2.14. *Constrained recovery behaviour for the 49.8-50.0 at.% Ni sanblasted alloy, 0.4mm in diameter, initially pre-strained 6%.*

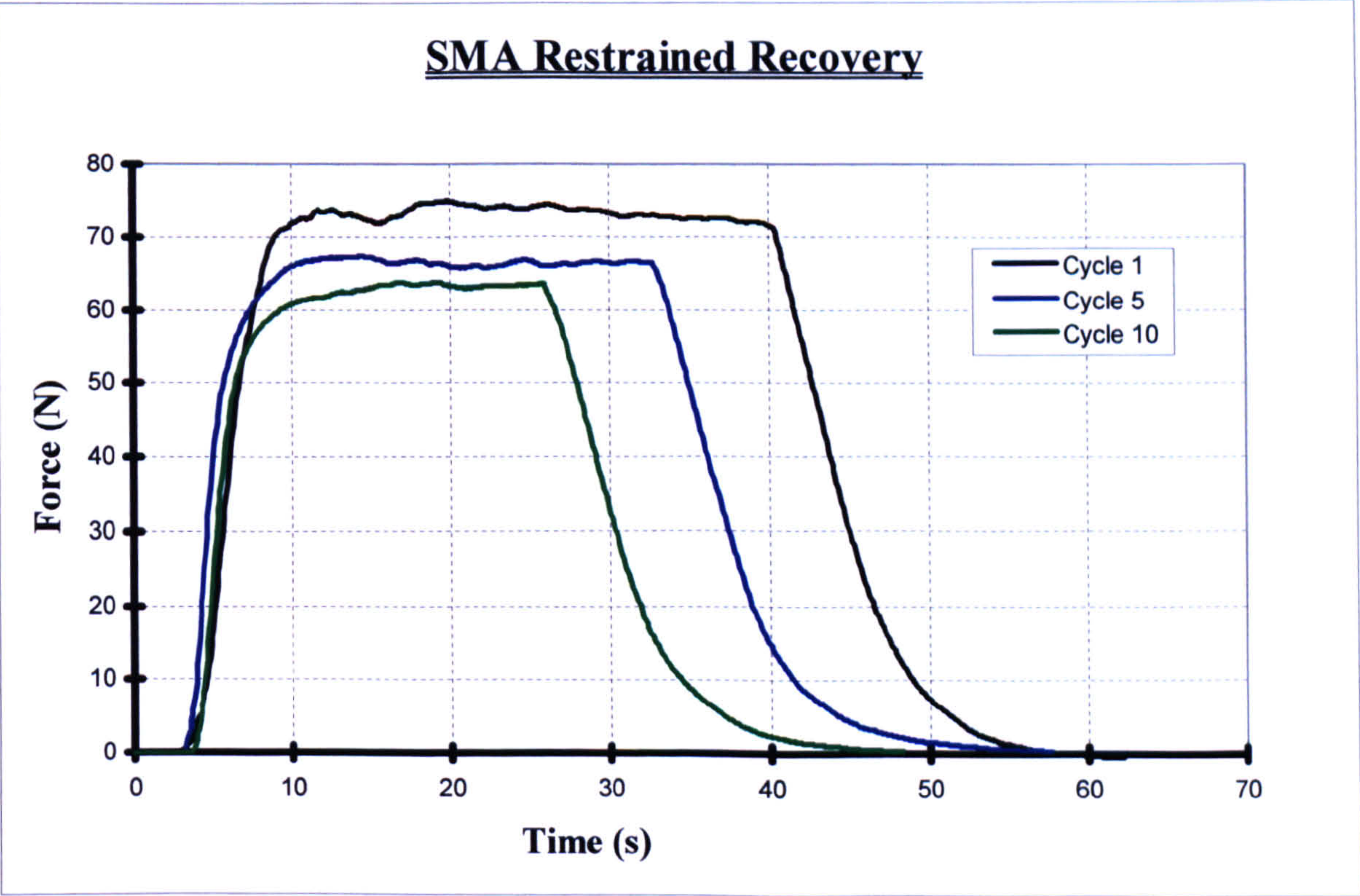


Figure 2.15. *Constrained recovery behaviour for the 49.8-50.0 at.% Ni sandblasted alloy, 0.4mm in diameter, initially pre-strained 6% and subjected to a cure cycle.*

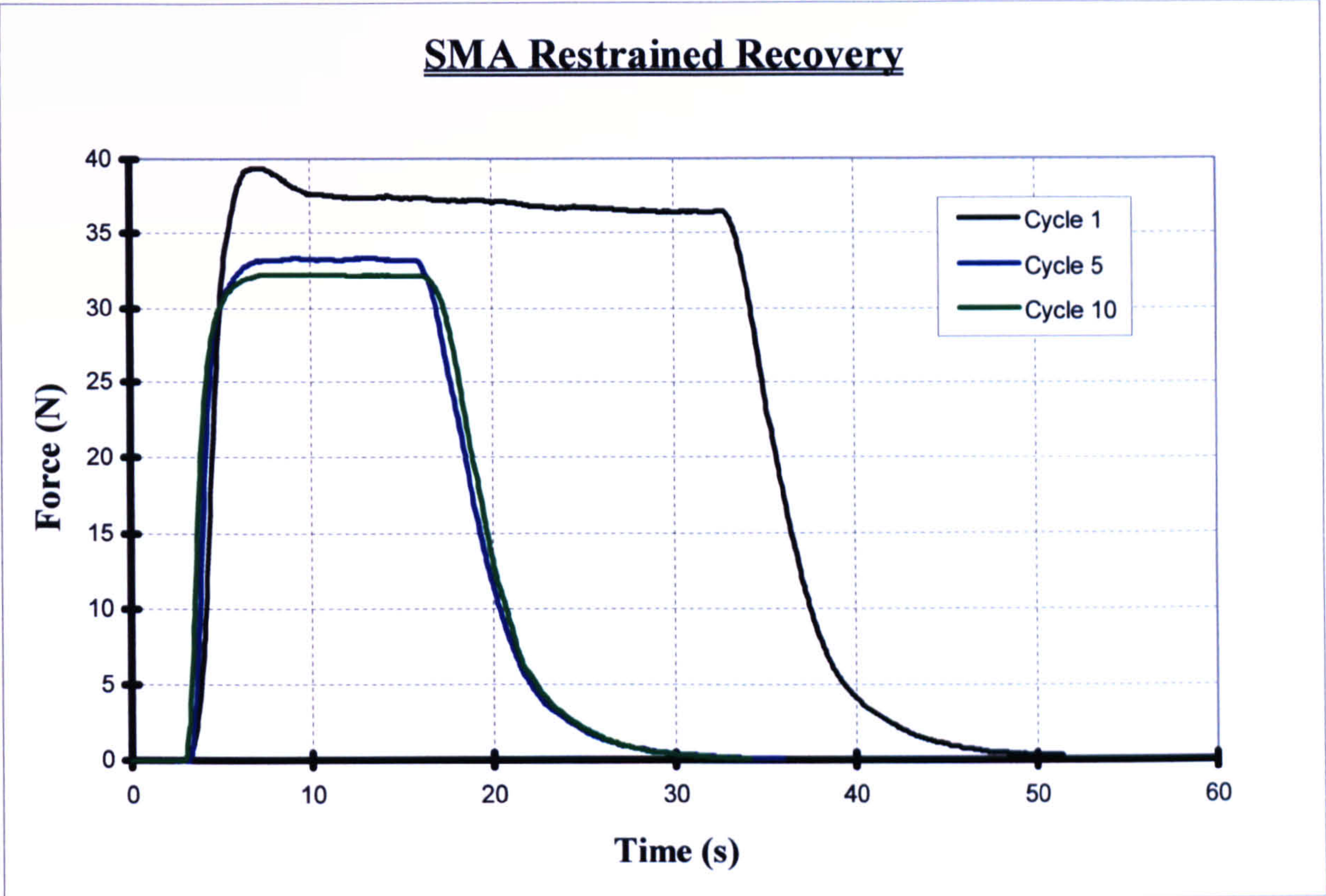


Figure 2.16. *Constrained recovery behaviour for the 49.8-50.0 at.% Ni sanblasted alloy, 0.3mm in diameter, initially pre-strained 6%.*

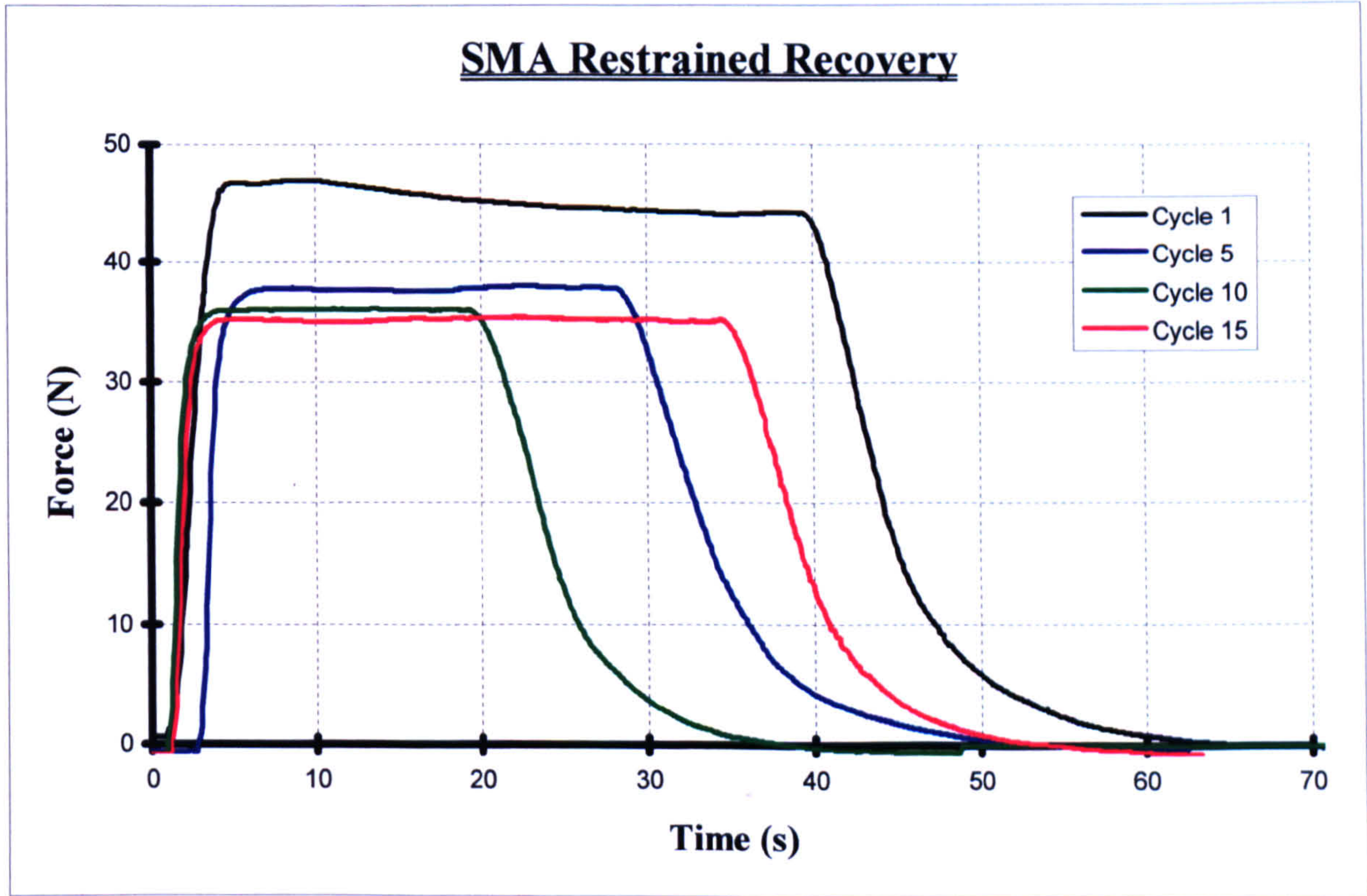


Figure 2.17. *Constrained recovery behaviour for the 49.8-50.0 at.% Ni partially sandblasted alloy, 0.3mm in diameter, initially pre-strained 6%.*

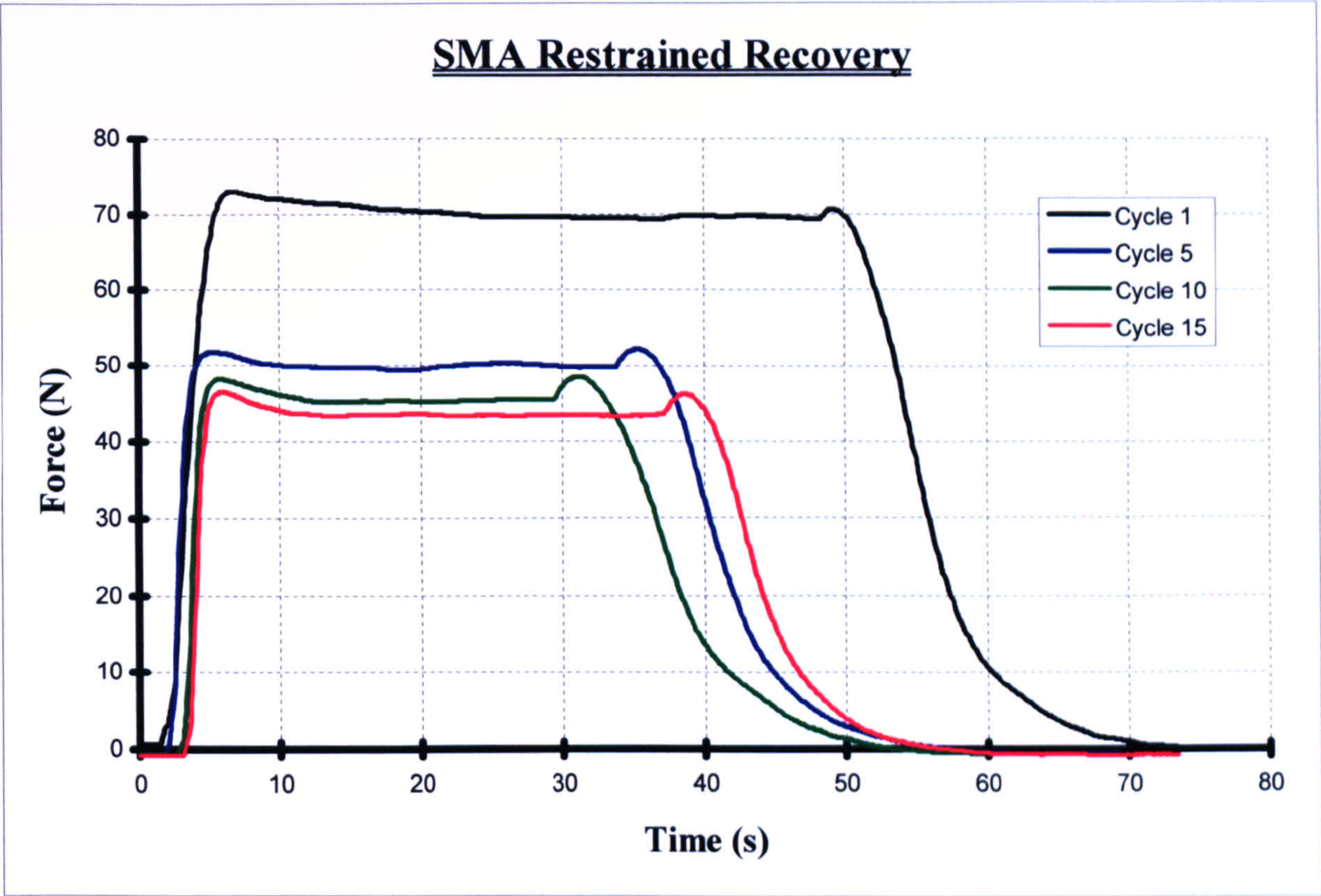


Figure 2.18. *Constrained recovery behaviour for the 49.8-50.0 at.% Ni plain alloy, 0.4mm in diameter, initially pre-strained 3%.*

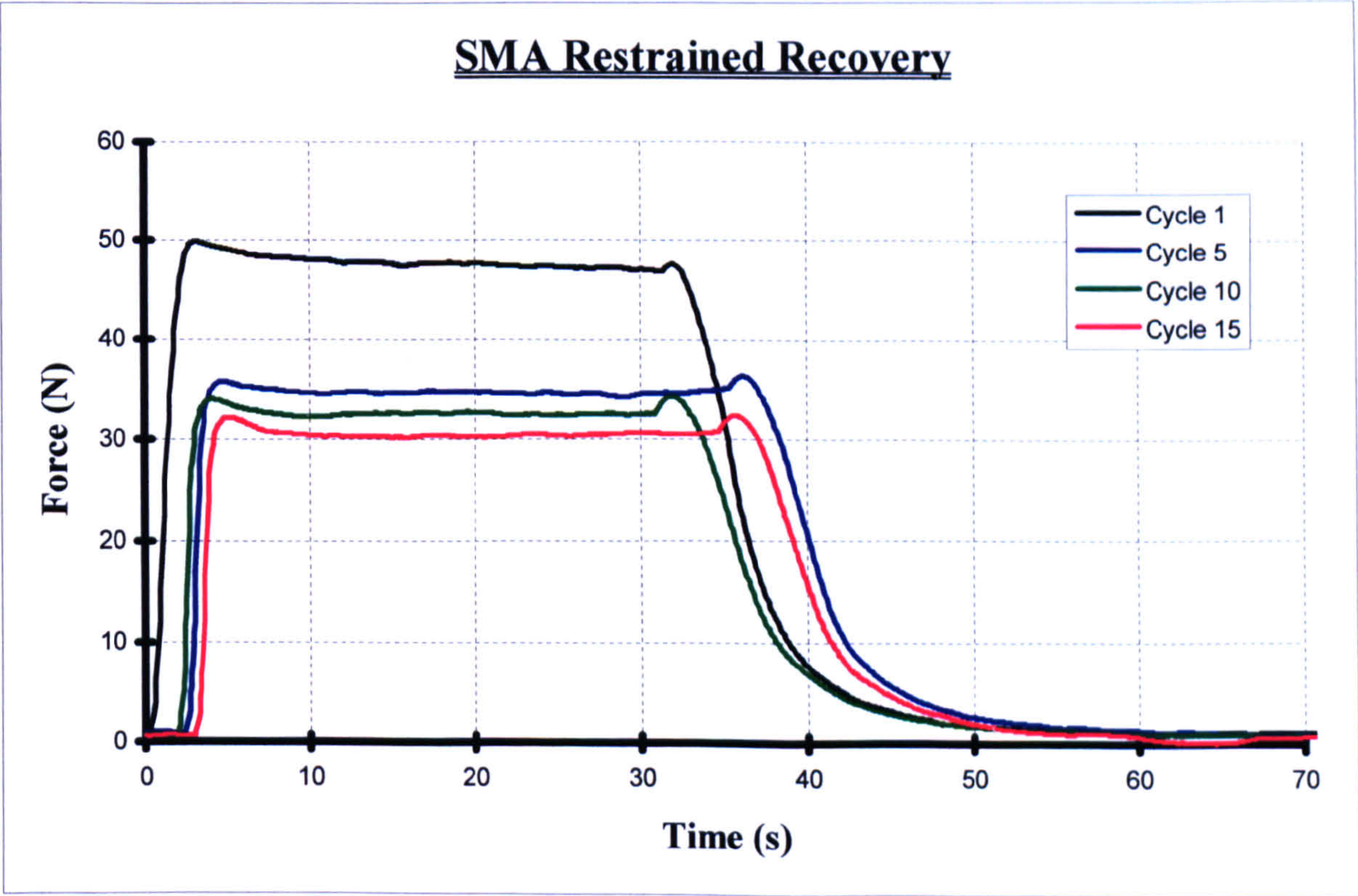


Figure 2.19. *Constrained recovery behaviour for the 49.8-50.0 at.% Ni plain alloy, 0.3mm in diameter, initially pre-strained 3%.*

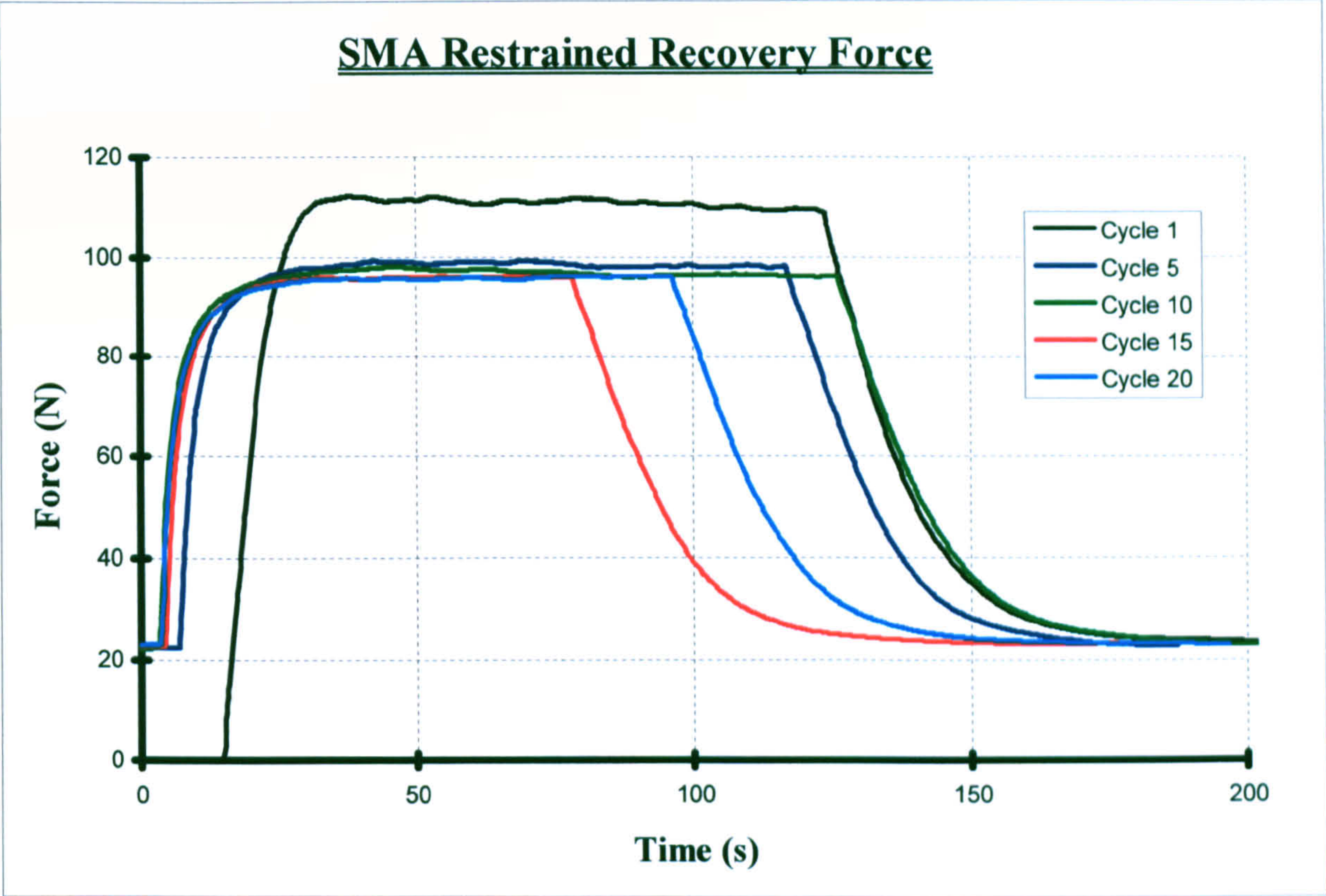


Figure 2.20. Constrained recovery behaviour for the 50.0-50.2 at.% Ni plain alloy, 0.4mm in diameter, initially pre-strained 6%.

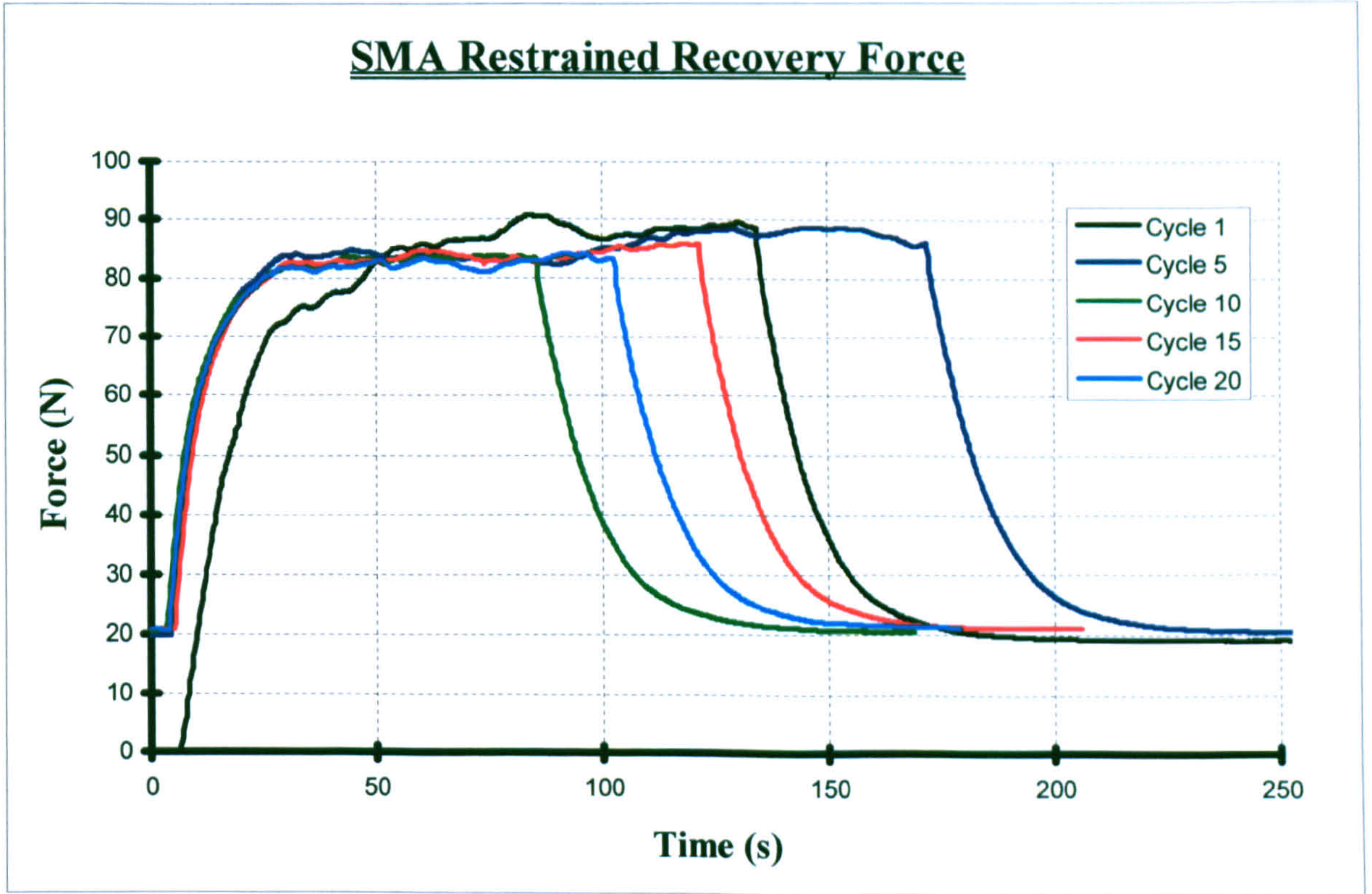


Figure 2.21. Constrained recovery behaviour for the 50.0-50.2 at.% Ni plain alloy, 0.4mm in diameter, initially pre-strained 6% .

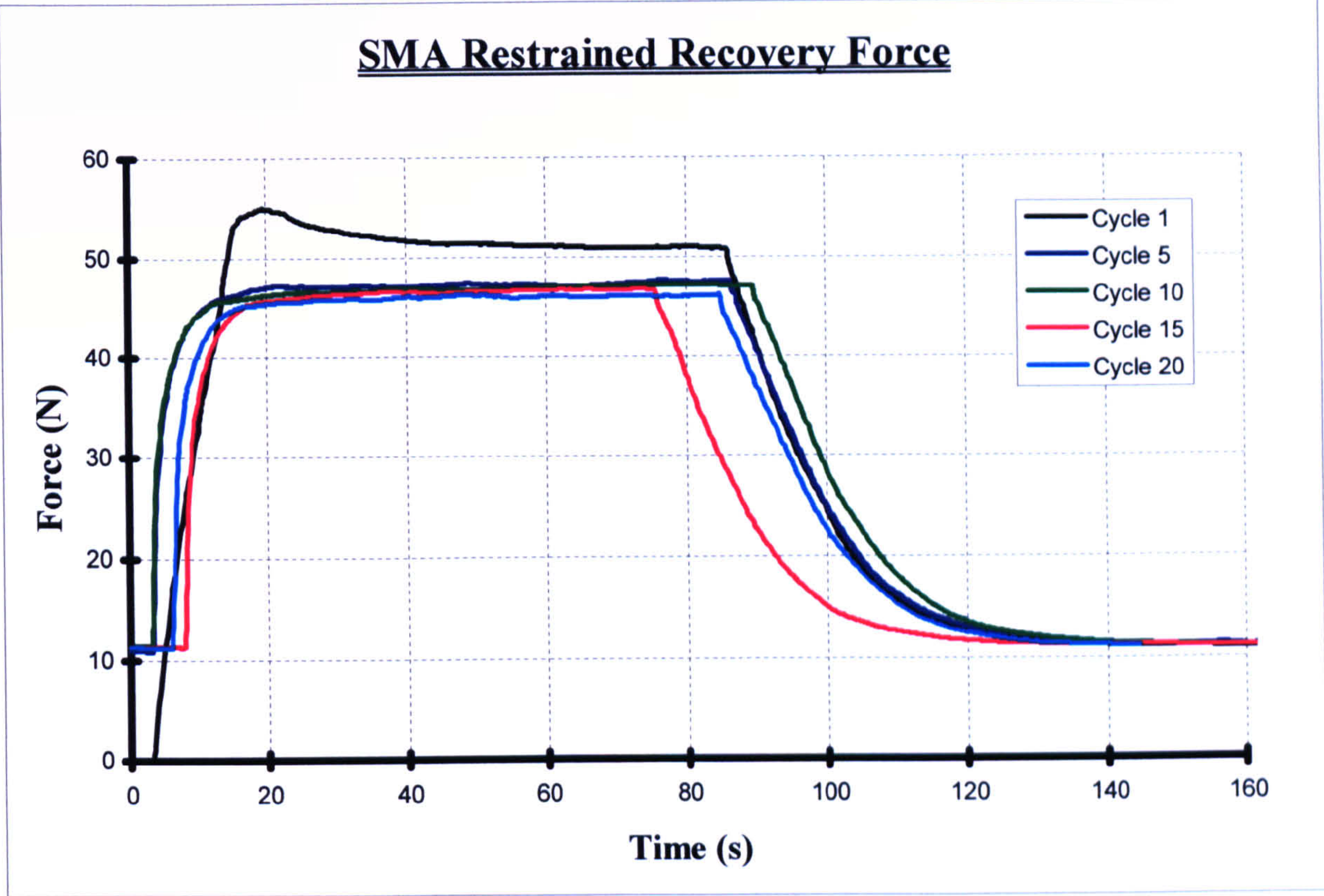


Figure 2.22. Constrained recovery behaviour for the 50.0-50.2 at.% Ni plain alloy, 0.3mm in diameter, initially pre-strained 6%.

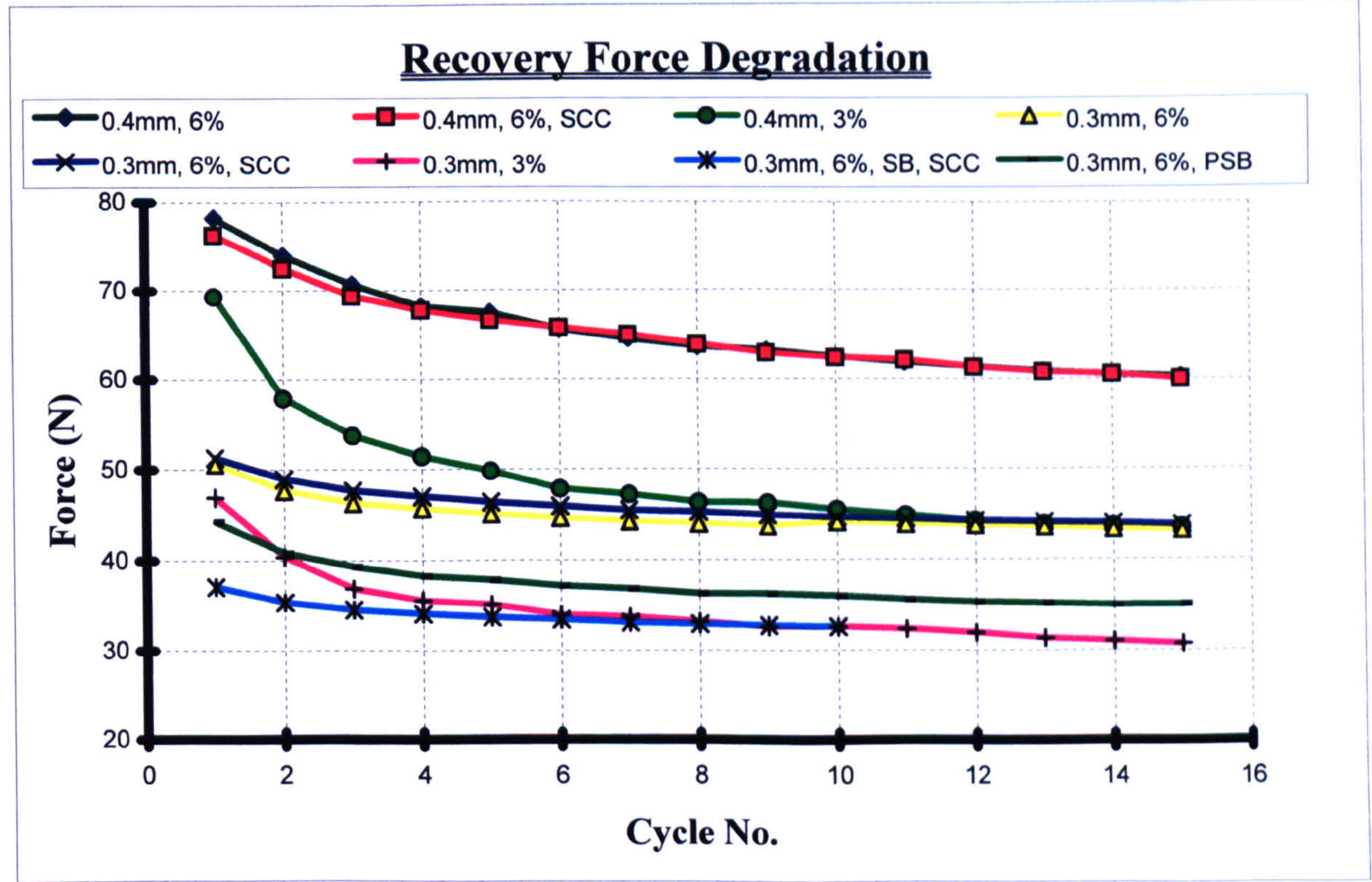


Figure 2.23. Comparison of specimen variant cyclic recovery force degradation for alloy composition 49.8-50.0 at.% Ni.

Cyclic Recovery Force Degradation

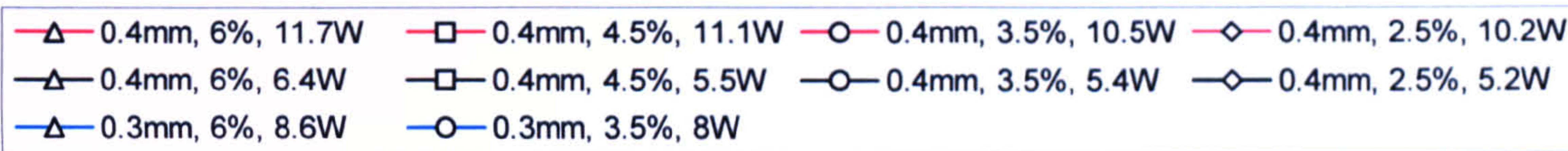
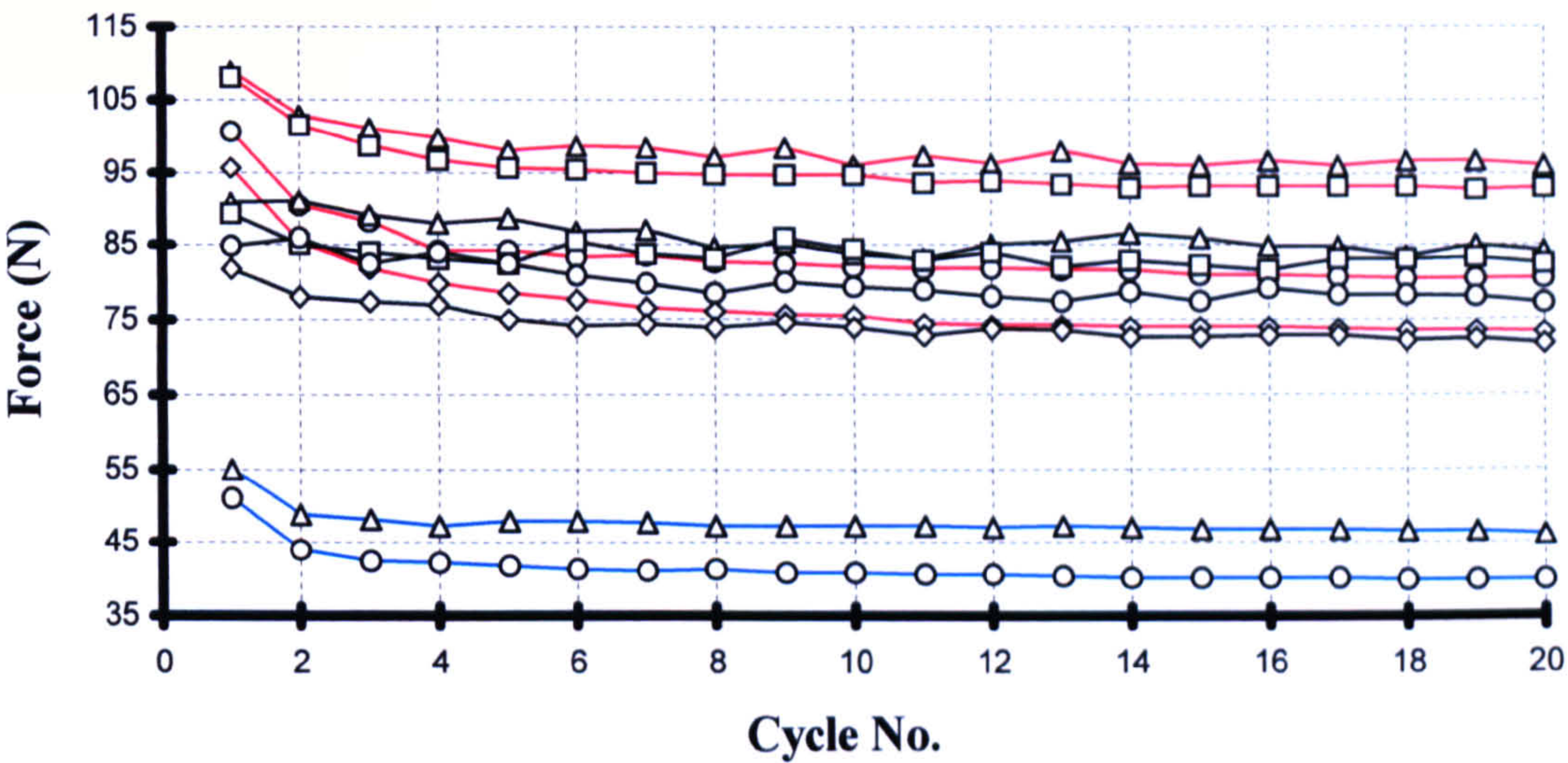


Figure 2.24. Comparison of specimen variant cyclic recovery force degradation for alloy composition 50.0-50.2 at.% Ni.

Chapter 3

SMA Integration, Test Specimen Manufacturing Aspects & Local Inclusion Implications

3.1 Introduction

For polymeric-matrix composites, such materials envisioned as having the greatest potential for integrated sensory or adaptive capabilities, there exists two possible options for placement of the required sensors or actuators. They can either be surface bonded, upon completion of the components manufacturing process, or, prior to curing, they can be directly embedded during the lay-up process. Both of which have advantages and disadvantages.

Surface mounted components offer ease of access and maintenance but would be more easily damaged in service. Aerodynamic considerations of some aerospace components may prohibit such mounting. Sensors or actuators embedded within the structure would provide better protection and more efficient load transfer. They would, however, be more difficult to access, if not impossible. Within the framework of this investigation we are concerned only with the embedding option.

Two control strategies are investigated. The first strategy involves SMA wires that are fed through sleeves which run throughout the laminate at the desired location. It is not a requirement that the SMA wires adhere to the host laminate. Restrained recovery is met through external fixtures. For the second strategy, the SMA wires are required to be constrained by the host composite. Here, the utility of the actuator is limited by the ability of the induced stress to be transferred to the host composite at a desired position. Consideration must be given to the surface preparation that may improve interfacial adhesion and, thus, maintain an adaptive cyclic response.

This chapter first describes the effect that embedded cylindrical inclusions have on polymeric-matrix laminates. Then, and for both control strategies, having introduced the concept behind which structural adaptation is realized, specimen fabrication procedures are discussed. In particular, for the second of our control strategies, the means taken that enhance the potential adaptive capability are presented.

For *control strategy 1* a local SMA/hybrid interface analysis has been conducted. Here, finite element analysis is utilized to obtain an accurate representation of the influence the inclusion has on the local stresses within the host.

3.2 Host Property Degradation Attributed To A Cylindrical Inclusion

Throughout this study, the word *inclusion* is used to represent the long cylindrical SMA actuators present within the laminates. The effects of embedding such diametrical inclusions are synonymous with those of embedding optical fibres, OF's. Compared to alternative actuators and sensors, both SMA's and OF's offer significant potential for integration within laminated constructions. Such potential emanates from their small size, diametrical nature and low weight.

A typical OF, including its coating, or SMA wire diameter, however, can exceed 100 times the diameter of the hosts reinforcement. In fact, as will be shown, the diametrical dimension of an SMA may even exceed 3 times the thickness of a single ply. Such geometrical inclusions may, therefore, result with significant local disruption to the laminate. Its overall affect, however, does depend upon the inclusions volume fraction and the orientation to the neighbouring plies.

There is little doubt that, for the incorporation of hybrid adaptive/sensory laminated constructions into future aeronautical systems, then, any inclusion will have to have minimal effect on the host laminates mechanical behaviour. Several studies have been reported in the open literature regarding the effects of embedded optical fibres, with arbitrary orientation, on the overall mechanical properties of PMC's⁷³.

The majority of the studies are related to the degradation in tensile and compressive strengths and stiffnesses⁷⁴⁻⁷⁶, though, interlaminar shear⁷⁷ and fatigue performance⁷⁸ have also been addressed. While no information was found regarding the adverse effects of SMA inclusions, owing to their geometrical similarity, we can conduce an analogy.

It is well known that, for embedded inclusions placed perpendicular to the reinforcing fibres of the adjacent plies, then, during fabrication, an elliptical resin rich region will form around the inclusion that extends in the direction of the reinforcement². The size of this region is reported to decrease with decrease in the angle between the cylindrical inclusion and the reinforcement of the neighbouring plies. Obviously, the size of the diametrical inclusion and stiffness of the surrounding plies will play a significant role in determining the length of this resin rich region. Placing the inclusions parallel to the fibres of the adjacent plies results with acceptance of the inclusions within the plies and, thus, poses minimal disruption to the reinforcement. Another factor that governs the size of any resin rich region is the thickness of the laminate, i.e. the resin pocket becomes smaller as the thickness of the laminate is reduced².

Studies have been performed to establish the effects of the orientation and location of embedded optical fibres on the uniaxial tensile performance⁷⁴ and uniaxial compressive performance⁷⁵ of graphite/bismaleimide laminates. Experimental strength and stiffness data were obtained and compared for a variety of configurations. [0/90/0], lay-up specimens were fabricated with 250µm diameter optical fibres embedded symmetrically or asymmetrically with respect to the laminate midplane and parallel, or perpendicular, to the applied uniaxial loading and/or adjacent reinforcing fibre directions.

It was shown that the low volume fraction of optical fibres only modestly reduced the tensile properties of the composite laminates. The largest reductions in strength and stiffness were seen to be 9 & 11% respectively. This occurred when the optical fibres were embedded perpendicular to the loading direction and to the adjacent graphite fibres⁷⁴. It was found that the asymmetrically embedded OF failed to induce larger tensile strength or stiffness reductions compared to a symmetric configuration.

The embedded inclusions, however, were shown to severely degrade the compressive performance of composite laminates⁷⁵. Compressive strength and stiffness reductions up to 70% and 20%, respectively, were reported. Optical fibres embedded perpendicular to both the loading direction and the adjacent continuous reinforcement induced the largest reductions in the mechanical properties.

The affect of embedded optical fibres on the compressive strength and stiffness of thick graphite epoxy composites, i.e. lay-ups consisting of 30 plies, has been studied⁷⁶. Variations in the optical fibre diameter, volume fraction, location and orientation with respect to the neighbouring reinforcing fibres were investigated. The optical fibres were always oriented perpendicular to the loading direction. The maximum reduction in compressive strength was

27% and occurred in a specimen where two optical fibres, one at a distance of 3 plies from the surface and the other located on the neutral plane, were placed perpendicular to the surrounding reinforcement. No degradation in compressive strength was reported when the OF's were placed parallel to the adjacent reinforcement. This cited investigation reports no change in modulus due to the presence of the optical fibres for any configuration.

The affect of embedded optical fibres, 250 μ m in diameter, on the static and fatigue interlaminar shear strength of E-glass reinforced polyester laminates has also been investigated. The OF's were embedded perpendicular to the reinforcement on the mid-plane of a $[0]_{16}$ laminate. Seventy specimens with and without embedded optical fibres were tested. In some cases, the tests reached 500,000 cycles. It was stated that the optical fibre does not have a negative influence on the laminate, neither static nor in fatigue⁷⁷. In this case, this is likely due to the highly advantageous lay-up considered.

This is not the case, however, for tensile waveform fatigue testing. Glass/epoxy cross-ply and unidirectional laminates, having quite a high volume fraction of OF's with arbitrary orientation, are shown to have a significantly reduced fatigue life compared to specimens devoid of inclusions⁷⁸. The tests were conducted, however, at 70% of the laminates ultimate strength.

A final comment worthy of mention on the cited behaviour regarding embedding OF's within PMC's is that the interlaminar fracture toughness of the composite material may not be reduced by the presence of embedded optical fibres. In fact they marginally improve the damage resistance of the material¹¹. These results may suggest that the optical fibres act as a global crack arrestor as opposed to an initiator².

From the published literature, a brief summary of the embedding process for long slender cylindrical inclusions within laminated polymeric-matrix materials is presented as follows;

- Significant degradation to the global compressive response of the fabricated specimens will be apparent when locating the inclusions perpendicular to the fibres of both adjacent plies and the loading direction. Such behaviour being attributed to the initial prebuckled reinforcement local to the inclusion. The magnitude of this degradation will be dependent on the specimen geometry, specimen properties, inclusion diameter and inclusion volume fraction.
- In order to decrease the extent of the aforementioned structural degradation, embedded inclusions should be embedded parallel to both the loading direction and adjacent reinforcing fibres. The smaller circular cross-sections of an embedded inclusion parallel to the loading direction should reduce the load bearing ability of the laminate to a lesser extent than the relatively larger rectangular projected cross-section of the cylindrical inclusion embedded perpendicular to the load.

To minimize the property degradation of the host laminates considered within this study, then, when choosing an orientation in which to embed the SMA actuators, it should preferably conform to the latter statement. This may not always be attainable, however, or, from a structural control point of view, be desirable.

The authors main study is concerned with plate/panel post-buckling deflection removal and the associated post-buckled non-uniform stress alleviation, at which the applied stresses are considerably lower than the ultimate. Therefore, at this stage, it seems suitable to orient the embedded inclusion in a manner best suited to achieve the desired structural response.

For the first of the employed control strategies, a 0.4mm diameter SMA wire, plus the sleeving, represents quite a severe geometric inclusion. This is especially true, since these are being embedded in approximately 1mm thick laminates. The extent of the laminate disruption, therefore, does need to be quantified. As will be shown, when embedding the inclusion perpendicular to the local reinforcement, then the local stress-state may be significantly altered. An appreciation of this local stress-state, and its variation from the norm, should be and has been attained.

3.3 Control Strategy 1

With this control strategy, SMA actuators within sleeves, run throughout and along the neutral plane of laminated plates. The strategy necessitates external actuator boundary constraints, thereby, upon activation, preventing free shape memory recovery. Results will be presented, **Chapter 6**, that illustrate the adaptive capability featuring varying sleeves, alloy composition and actuator volume fraction.

The stacking sequences of the laminated plates currently considered are $[0_2/90_2]_s$, $[0/\pm 45/90]_s$, & $[0_2/\pm 45]_s$. The choice of the selected lay-ups is discussed in more detail in **Chapter 4**. The dimensions of the plates under investigation are 300*310mm. A slight overhang in width is allowed for, thereby, providing sufficient lateral support during tests.

3.3.1 Specimen Fabrication

All specimens throughout this programme are constructed from Fiberdux 913C-TS unidirectional high tensile carbon/epoxy prepreg^a. For this particular control strategy, the specimens feature SMA actuators, 0.4mm in diameter, initially pre-strained by 6% and fed through either PTFE^b tubes or polyimide micro-tubes^x. During the fabrication, steel wires are employed to maintain the desired geometrical tolerance within the tubes such that, when removed upon cure completion, the inserted SMA wires have a required slide fit. For each specimen, the SMA orientation is perpendicular to the loading direction.

Ideally, the incorporation of any inclusions must have minimal effect on the manufacturing procedure used in the composite manufacturing process. Fabrication of the plate specimens is as follows.

1. The individual unidirectional carbon/epoxy plies, cut to the desired dimensions, are placed accordingly in their desired stacking sequence. The lay-up being conducted by hand.
2. PTFE or polyimide tubing, containing appropriately sized steel wires, are located on the neutral plane at the desired SMA positions. The inclusions are precisely positioned using a specially devised tooling plate. The embedded inclusions extrude out from the edges of the lay-up a finite distance and held taut. This will ensure their desired alignment.

^a Purchased from HEXCEL Composites.

^b Purchased from ADTECH Polymer Engineering.

^x Purchased from FISHER Industrial Marketing.

3. During the cure cycle, the sleeving is compressed around the steel wires. Upon completion of the consolidation process, the steel wires are then manually withdrawn.
4. Pre-strained SMA wires are then inserted. A tolerance, $\geq 0.05\text{mm}$, allows a slide fit for the 0.4mm diameter SMA. The adaptive capability is provided for by constraining the ends of the SMA wire to an external boundary and applying the necessary temperature for phase transition.

The dimensions of the tubing and steel wire chosen are dependent on the dimension of the alloy to be inserted. To utilize the recovery force of a 0.4mm diameter alloy, 0.45mm diameter steel wires were fed through 0.46mm inner diameter PTFE or 0.51mm inner diameter polyimide tubing. The wall thickness of the PTFE and Polyimide tubing are 0.15mm and 0.025mm respectively. For the former PTFE tubes, reducing this wall thickness to 0.1mm would result with the wall being torn apart when attempting to insert the steel wire.

Regardless of the control strategy employed, the placement of SMA actuators within a carbon fibre reinforced laminate requires suitable electrical insulation between the current carrying actuator and the conducting carbon fibre reinforcement. The properties that make the chosen sleeving suitable for this application are its (1) working temperature beyond 260°C , (2) an extremely high electrical resistance, and in particular to PTFE (3) a very low coefficient of friction.

Throughout this investigation all specimens were fabricated in an autoclave and cured to the manufacturers recommended curing cycle. In general, and in particular to the second of our control strategies, **Section 3.5.1**, the manufacture of these advanced composite components is a fairly complex process that, at the early stages of the programme, resulted in parts of unsatisfactory quality. Primarily, poor compaction was the cause for concern, particularly in regions local to the inclusions.

Prior to curing, in order to facilitate sufficiently compacted laminated surfaces, 1.5mm thick silicone rubber diaphragms were placed between the upper and lower surfaces of the laminates and the caul plate and tooling plate respectively. The silicone diaphragms presence ensures a suitable specimen pressure distribution, therefore, obtaining a compaction of sufficient quality.

Upon completion of the fabrication process, the cured specimens required machining, therefore, obtaining an edge finish suitable for loading. If the inclusion is to exit the loaded composite edge, machining is no longer permitted. Here, the edge must be manufactured such that it's quality permits testing, **Section 3.5.1**, else the inclusion must rise through successive plies of the composite material to exit the part surface. Exiting the composite part in this manner adds manual labour due to the fact that each ply must be individually slit and the inclusion fed through each layer until it finally exits the plate.

For our initial demonstrator programme, the author opted for SMA egress from the unloaded edges, therefore, minimizing the manufacturing lead times and associated costs.

3.3.2 Local Inclusion/Composite Interaction

Optical micrographs for the cured lay-up configurations $[0_2/90_2]_s$, $[0_2/\pm 45]_s$ and $[0/\pm 45/90]_s$ are shown in **Figures 3.1-3** respectively. The sleeving shown within these figures is of the PTFE type. The micrographs, here and throughout this study, were obtained by cutting a small representative section from the global laminate.

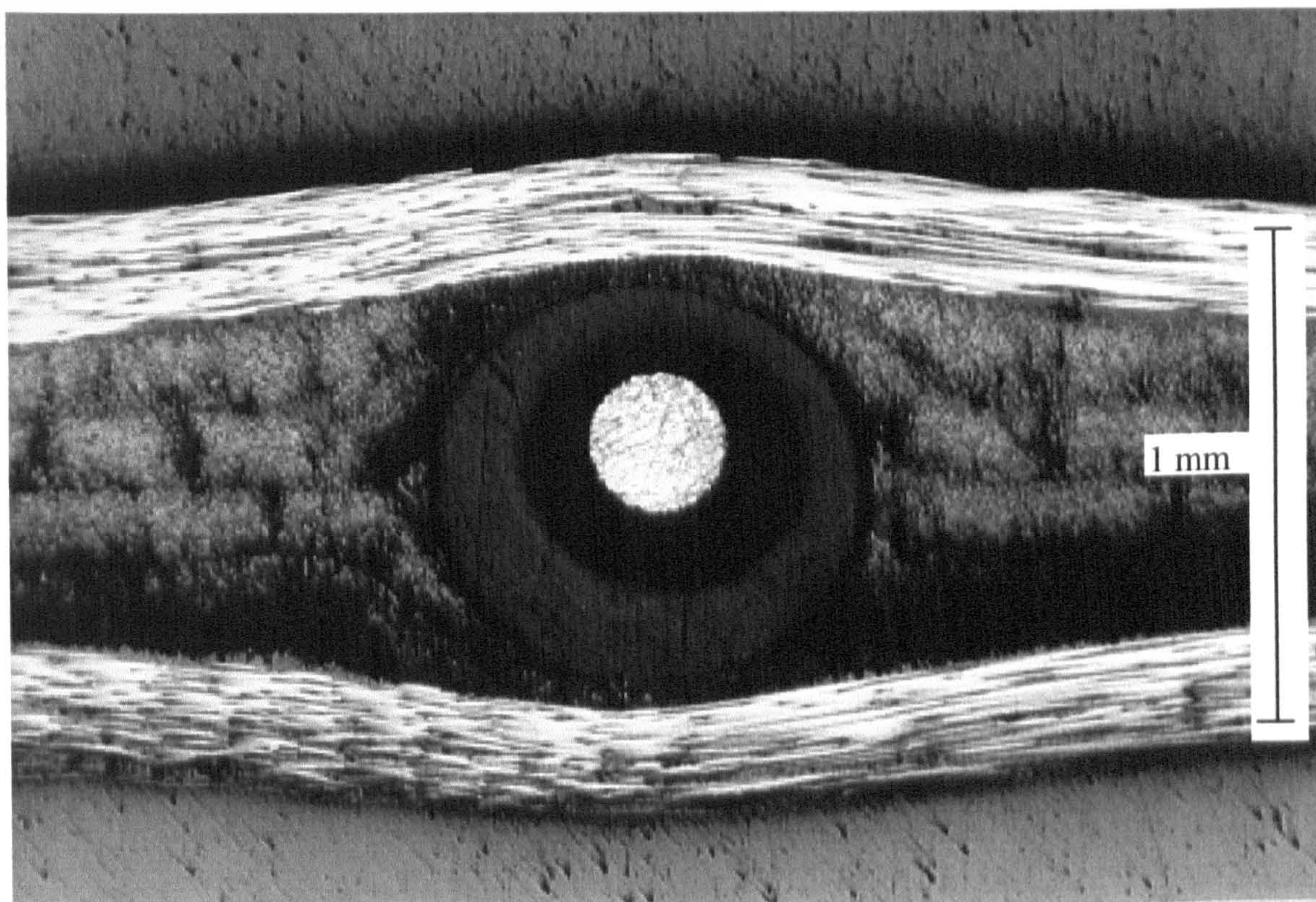


Figure 3.1. $[0_2/90_2]_s$ optical micrograph with embedded SMA within PTFE tubing on neutral plane.

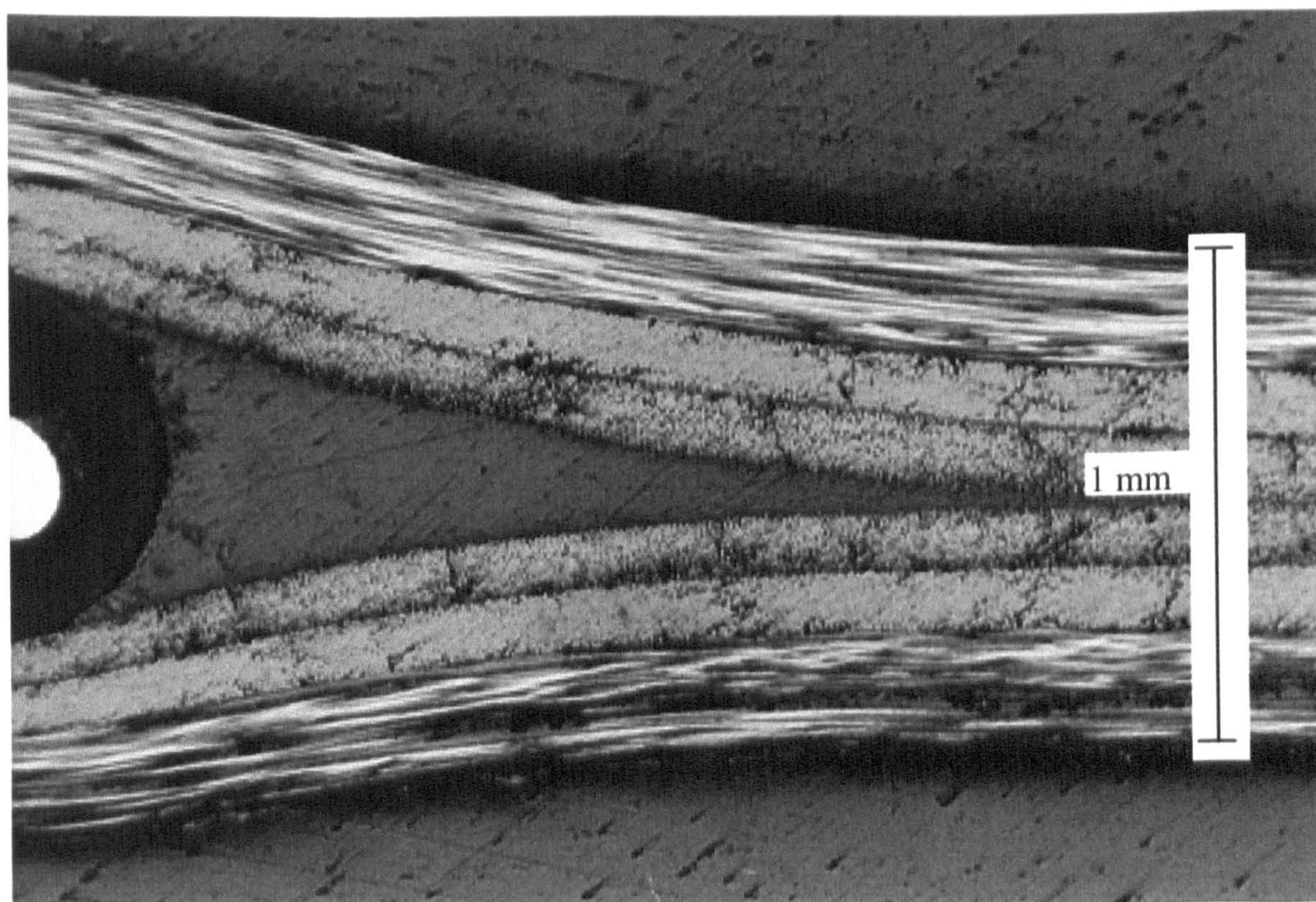


Figure 3.2. $[0_2/\pm 45]_s$ optical micrograph with embedded SMA within PTFE tubing on neutral plane.

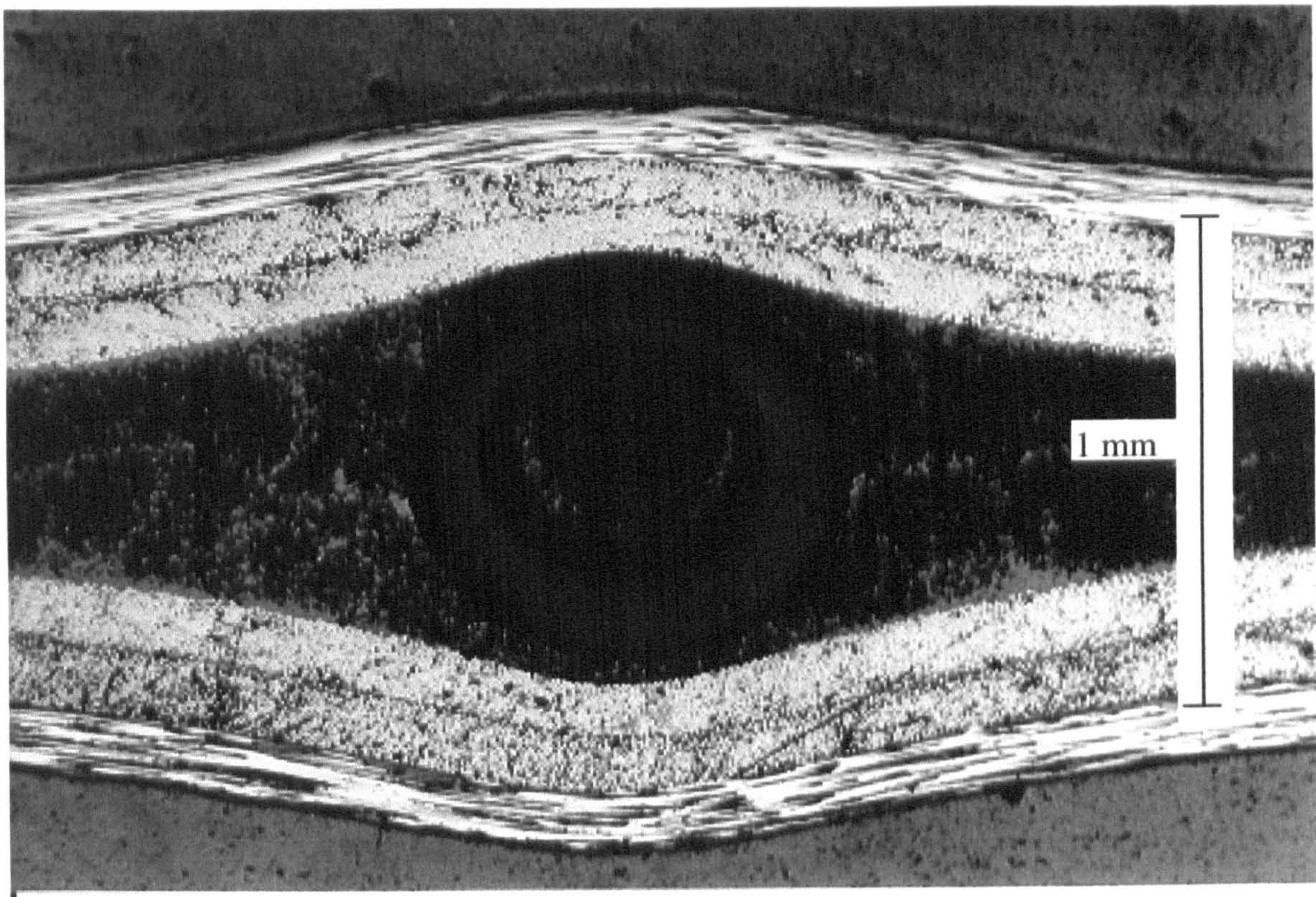


Figure 3.3. $[0/\pm 45/90]_S$ optical micrograph with embedded PTFE tubing on neutral plane.

This section is then potted in an epoxy resin and successively polished with 220 & 1200 SiC grit paper. It is finally polished with 5 μ m diamond paste, then viewed under a microscope.

Each figure clearly shows the affect the embedded inclusion has on the host laminate surface topology and the disruption it can cause to the neighbouring carbon reinforcement.

Placing the inclusion parallel to the neighbouring reinforcement is clearly advantageous, **Figure 3.1**, as such a configuration poses minimal reinforcement disruption. However, placement of the inclusion at an angle to the reinforcement, **Figure 3.2**, results with considerable reinforcement disruption and the formation of a lenticular resin rich region extending approximately 2mm from the inclusion interface.

Although no residual voids are visible in the micrograph of **Figure 3.2**, this may not always be the case. On later micrographs, for such a lay-up, a significant void was present that replaced the pure epoxy. Concern arises due to the presence of such voids or resin pockets as they act as interlaminar discontinuities and, therefore, will pose as a potential reliability hazard to the host structure under either applied static, cyclic or shock loads².

Figure 3.3 illustrates the disruption caused to the outer plies of the laminate when embedding the inclusion parallel to a limited number of adjacent plies.

The extent of reinforcement disruption and the effects on the surface topology may be minimized by placing the inclusions between cut, or discontinuous, plies. For such laminates, however, it is expected that such a technique will impose a greater structural integrity deficiency, therefore, significantly reducing in-service life. While the outer plies local to the inclusion, **Figures 3.1-3**, are somewhat distorted, they will maintain a significant portion of their integrity.

Alternatively, alleviation of this disruption imposed by the embedded inclusion can be met by either increasing the number of plies adjacent and parallel to the inclusion or through the use of the polyimide micro-tubing.

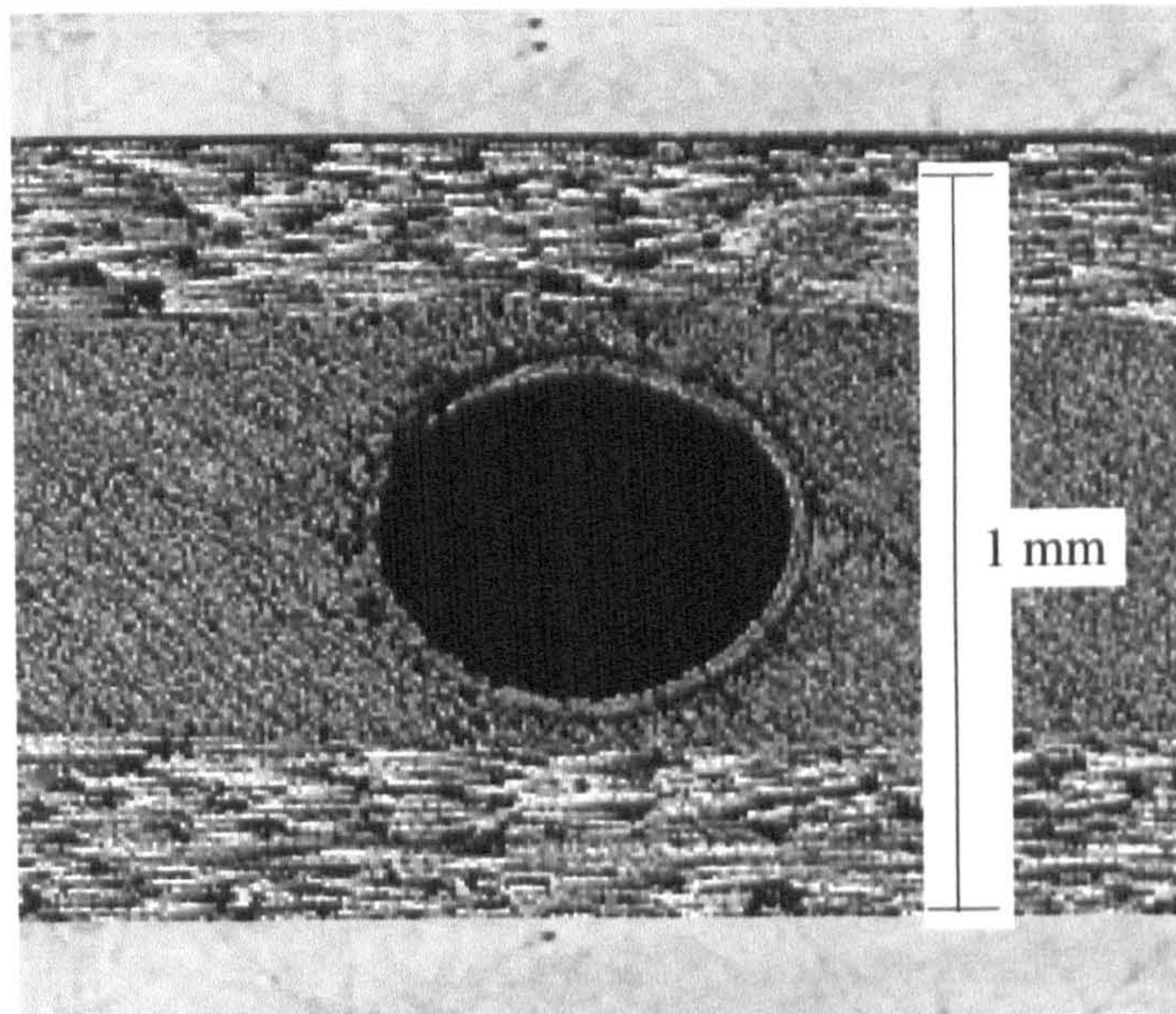


Figure 3.4. $[0_2/90_2]_s$ optical micrograph with embedded micro-tubing on neutral plane.

Figure 3.4 illustrates the nature of the embedded inclusion on the host laminate for a $[0_2/90_2]_s$ lay-up with the neighbouring plies parallel to the inclusion. The micrograph was prepared without the inserted actuator. This lay-up represents a highly advantageous hybrid specimen as the inclusion is least intrusive to the host.

The fabricated specimen orientation, during testing, is such that the SMA's are perpendicular to the loading direction. The investigation has not involved the ultimate strength reduction or stiffness reduction local to the inclusions. This is because of the relatively low volume fraction of SMA employed and the magnitude of the applied compressive load required to cause the desired post-buckling response. This magnitude is not thought to have a significant consequence. Therefore, at this stage, as previously stated, it seems suitable to orient the embedded inclusion in a manner best suited to achieve the desired structural response while ensuring minimal manufacturing implications.

3.4 Investigation on the Local Stress State For Control Strategy 1

An intention of the adaptive/smart capability is to enhance the components load bearing capability, therefore, enhancing its *survivability*. Hence, when embedding either sensors or actuators within laminated constructions, it is important to ascertain the effects so as to ensure that we are not actually degrading the components performance by any significant amount.

The embedded inclusions manifest a geometric and material discontinuity, and hence a stress concentration. The local stress state, in the vicinity of the embedded inclusions, for the lay-up configurations $[0_2/\pm 45]_s$ and $[0/\pm 45/90]_s$ have been investigated. These lay-ups were considered as the optical micrographs exhibited more prominent disruption to the local reinforcement. Due to factors such as complexity of the local geometric shape, the presence of the lenticular resin rich region and the disparity in the properties of the various constituents, no simple closed form solution exists to ascertain the local stress state. Therefore, finite element models were constructed. Such techniques have successfully been employed elsewhere⁷⁹⁻⁸⁰.

3.4.1 Finite Element Model

The local area of interest is a plane section perpendicular to the length of the inclusion, as illustrated in **Figures 3.2 & 3.3**. The local stress state was initially investigated using a 2D plane strain analysis within MSC/NASTRAN. Under certain circumstances, plane strain analysis can be used as, for an applied load $\leq P_c$, the stresses can be assumed not to vary along the length of the inclusion and the strains parallel to the inclusion, induced by the Poisson’s effect, are minimal. For the particular lay-ups chosen and sections considered, the plain strain idealization, that represents the slice out of the body where $\epsilon_x=0^\alpha$, breaks down when the thermal residual stresses are to be accounted for. Therefore, for such load cases, full 3D solid analysis was conducted.

The current investigation focussed over a local region encompassing one of the inclusions that has a pre-selected 12.5mm spacing arrangement, **Chapters 4 & 5**. The local region of interest is located at the centre of the representation. Symmetrical boundary conditions were applied, therefore, reducing the problem to only one quadrant.

Individual geometric models where constructed and meshed based on direct measurements from the optical micrographs. The individual plies were constructed and meshed so that they flow over and around the inclusion.

For the lay-up $[0_2/\pm45]_s$, the finite element model includes the shape of the resin rich area. Separate material

Ply Orientation	0	± 45	90
E_y (N/mm ²)	$140 \cdot 10^3$	$12.64 \cdot 10^3$	$8.8 \cdot 10^3$
E_z (N/mm ²)	$8.8 \cdot 10^3$	$8.8 \cdot 10^3$	$8.8 \cdot 10^3$
G_{yz} (N/mm ²)	$5 \cdot 10^3$	$7.9 \cdot 10^3$	$5 \cdot 10^3$
α_y	$-3.0 \cdot 10^{-7}$	$1.39 \cdot 10^{-5}$	$2.8 \cdot 10^{-5}$
α_z	$2.8 \cdot 10^{-5}$	$2.8 \cdot 10^{-5}$	$2.8 \cdot 10^{-5}$
ν_{yz}	0.3	0.264	-

Table 3.1. Ply properties.

properties of the resin and plies were employed in the analysis. The material properties applied to the appropriate elements are given in **Tables 3.1 & 3.2**. The global y & z Cartesian co-ordinate system, referred to in **Table 3.1**, is shown in **Figure 3.5**. Mechanical tests were performed to validate key properties presented in **Table 3.1** for unidirectional lay-ups only. The results of which are presented in **Appendix B**.

The analysis takes into account the directional dependence of the ply orientation in the global yz-plane, therefore, accounting for the local ply disruption. Due to the excessive compliance of the sleeves, its representation was omitted from the numerical investigation, else numerical instability may prevail.

Plane strain finite element analyses were conducted with either CQUAD4’s or CQUAD8’s, and, where necessary, triangular elements. The material properties were represented on PSHELL data entries that reference the appropriate MAT8 2D orthotropic material property cards.

Epoxy Properties	Value
E (N/mm ²)	$5.0 \cdot 10^3$
G (N/mm ²)	$1.85 \cdot 10^3$
α	$6.0 \cdot 10^{-5}$
ν	0.35

Table 3.2. Epoxy properties.

The 3D solid analysis employs PSOLID element property entries that reference MAT9 3D orthotropic cards for the appropriate solid elements. Special emphasis was placed on conforming the element shape to its desired configuration, though, due to the geometric nature of the problem, this could not always be realised. Regrettably, for both analyses, the use of CTRIA3 or CTETRA elements were required. However, due to their scarcity, the author has no cause for concern.

^α ϵ_x is the strain normal to the plane of the section under investigation.

Approximately 2100 elements were employed for the local structural representations of the 2D analysis. The four noded elements, initially employed within the mesh, were kept fine because any geometric curviform is truly being modelled by straight lines. It is conceivable that the preferred element is the CQUAD8 as its use of higher-order shape functions may result in better modelling. It was found that, for an identical mesh density, using the *p-version* adaptive refinement, the CQUAD4's offer acceptable accuracy. A minimum of 3 elements were located through the thickness of each individual ply, thus adequately determining a first approximation for bending. For the 3D analysis, the 2D mesh was uniformly extruded by the desired amount in the x-direction. The resulting model having in excess of 10,000 elements.

Referring to **Figure 3.2**, no account has been made for the higher percentage volume of fibres in the plies around the geometric discontinuity which has lost resin to the void⁸⁰.

The analysis examines the effects of applied uniaxial compressive load, residual stresses at both ambient and elevated temperature and the combination of these. A temperature of 120°C defines the initial stress free temperature. Uniaxial loads of -0.765kN & -0.958kN were respectively applied to the $[0_2/\pm 45]$, & $[0/\pm 45/90]$, finite element representations. The values represent critical buckling values, **Chapter 4**, at which plane strain analysis is valid throughout the entire laminate. Though, as discussed, this assumption is only valid for a limited number of the load cases considered in this investigation.

Referring to **Figure 3.5**, with the aid of multi-point constraints, the mechanical load is applied such that the loaded edge, NO, is uniformly displaced. As previously stated, symmetric boundary conditions are applied to the appropriate edges, MQ & PO. The geometric representation of the interface between the inclusion and the host laminate is left unconstrained, PQ. The 2D plane

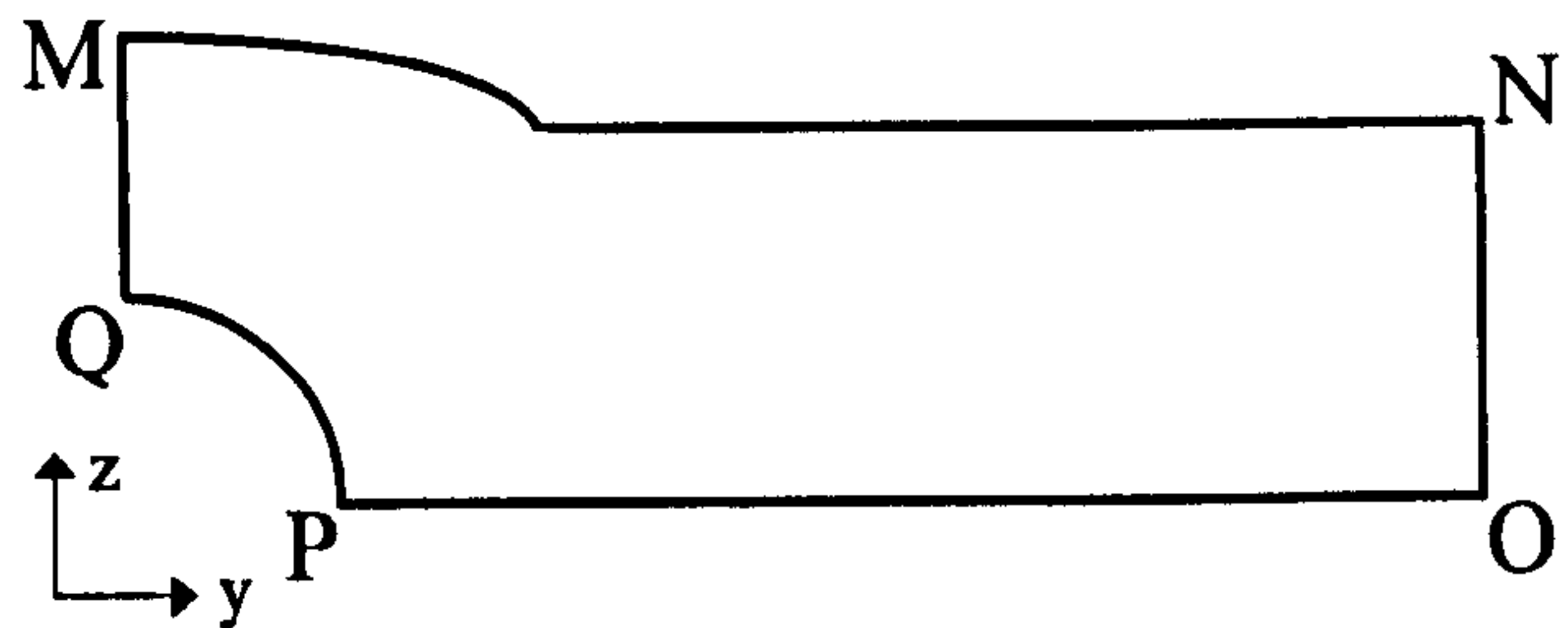


Figure 3.5. Schematic representation of 2D model.

strain elements employed have no resistance to forces normal to their plane or to nodal moments, therefore, the out of plane translation and all rotations are suppressed at all nodes, thus, preventing singularity of the global stiffness matrix. Similar boundary conditions are applied to the 3D model. Here, however, the solid elements employed require only nodal rotations to be constrained.

3.4.2 Finite Element Results & Discussion

Five load cases have been considered. These load cases examine the application of; (i) P_c , (ii) approximately $3P_c$, (iii) the thermal residual stress state at ambient conditions, (iv) P_c + the thermal residual stress state at ambient, and (v) approximately $3P_c$ + the thermal residual stress state at 100°C on the stress distribution in close proximity to the embedded inclusion. The stresses of interest within this study are the YY and ZZ component stresses. The Y and Z orientations are depicted in **Figure 3.5**. The YY stresses are parallel to the applied mechanical loading direction whereas the ZZ stresses are through-the-laminate-thickness stresses.

Considering load case (i) first, then, the YY-component stress distributions are presented in **Figures 3.6a & 3.7a** for the lay-ups $[0_2/\pm 45]$, & $[0/\pm 45/90]$, respectively. The ZZ-component, or through thickness, stress distributions are presented in **Figures 3.6b & 3.7b**. The resulting deformation profiles for this load case, for each respective lay-up, are presented in **Figures 3.6c & 3.7c**.

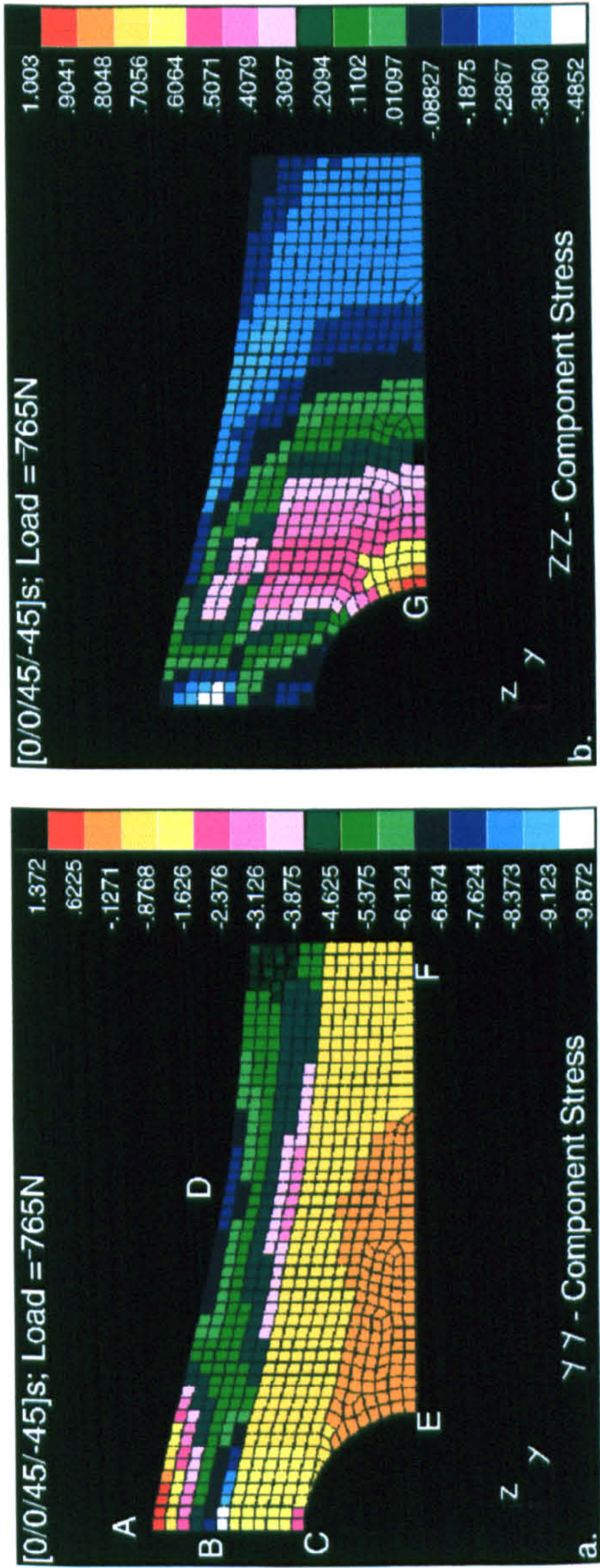
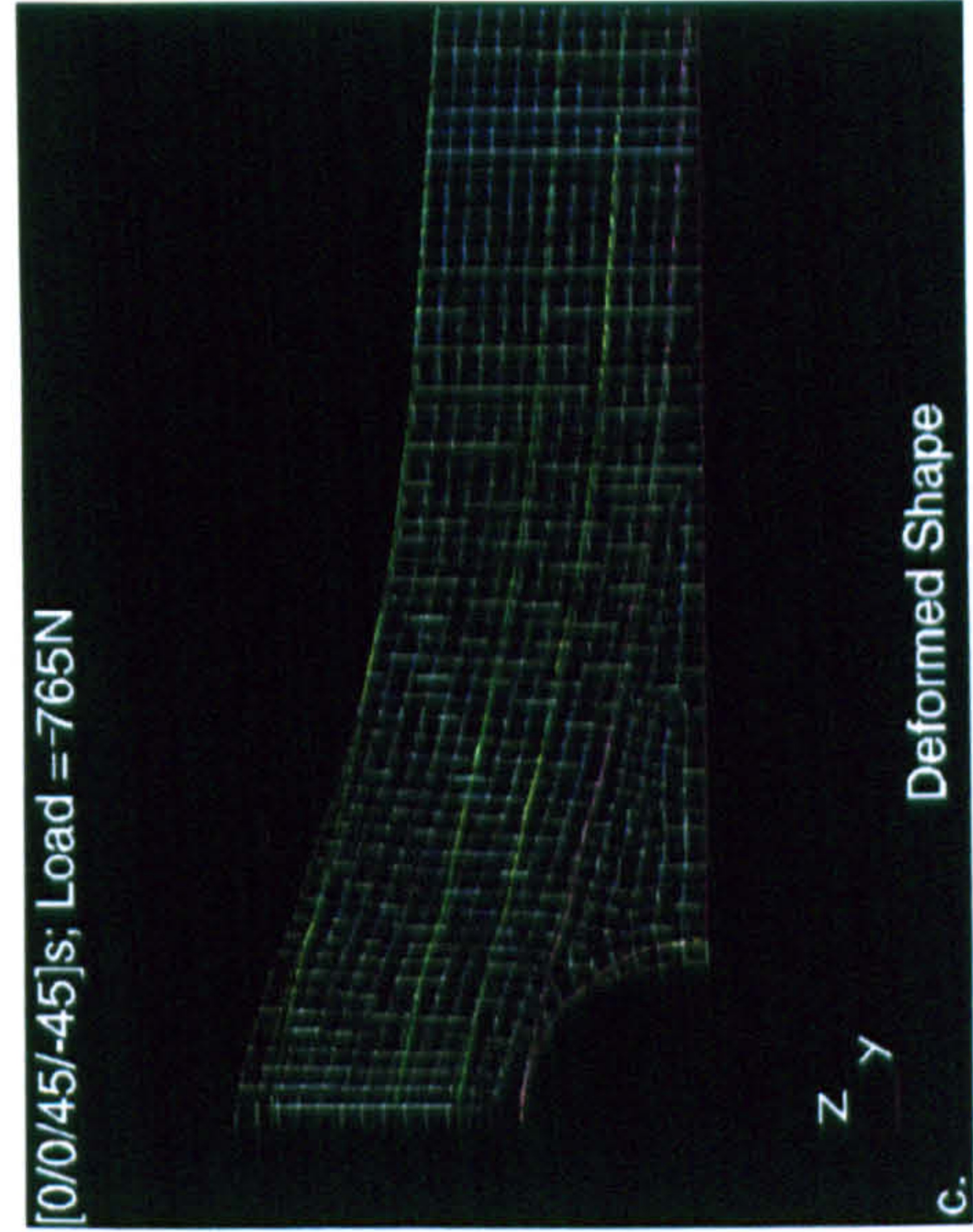


Figure 3.6. Local FE stress analysis for the $[0_2/\pm 45]_s$ lay-up.

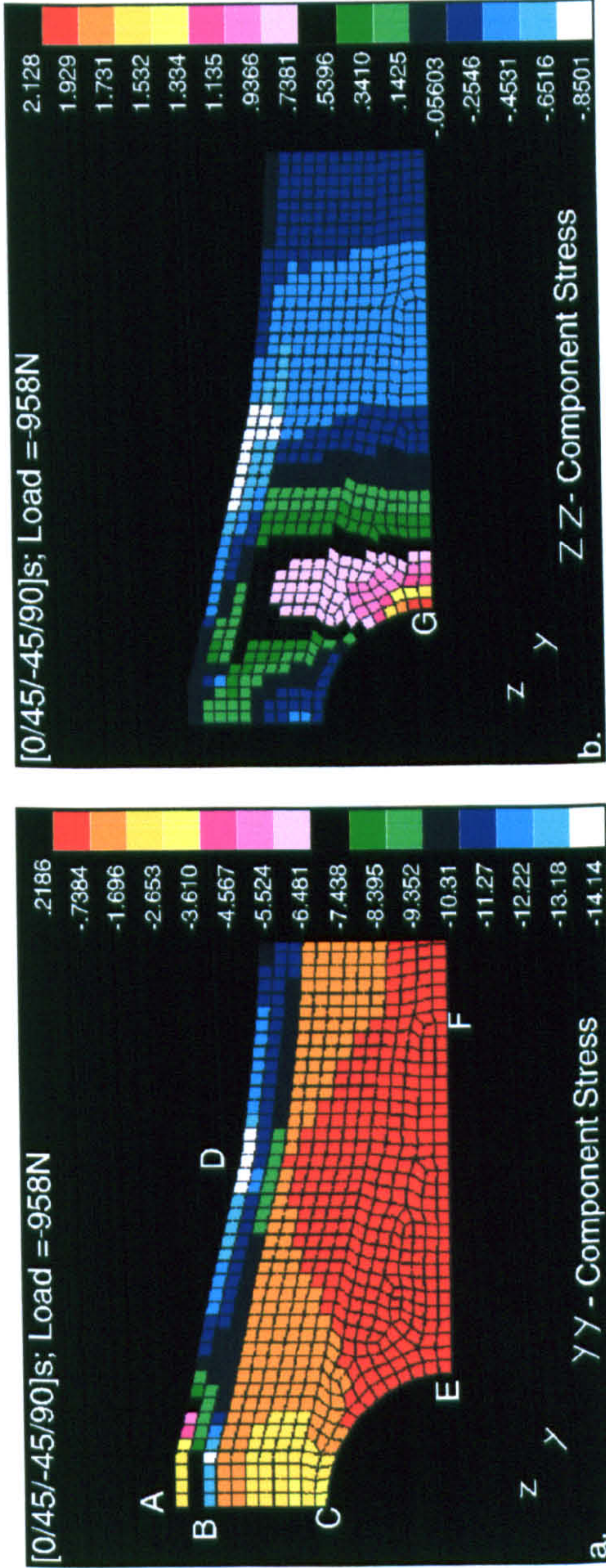
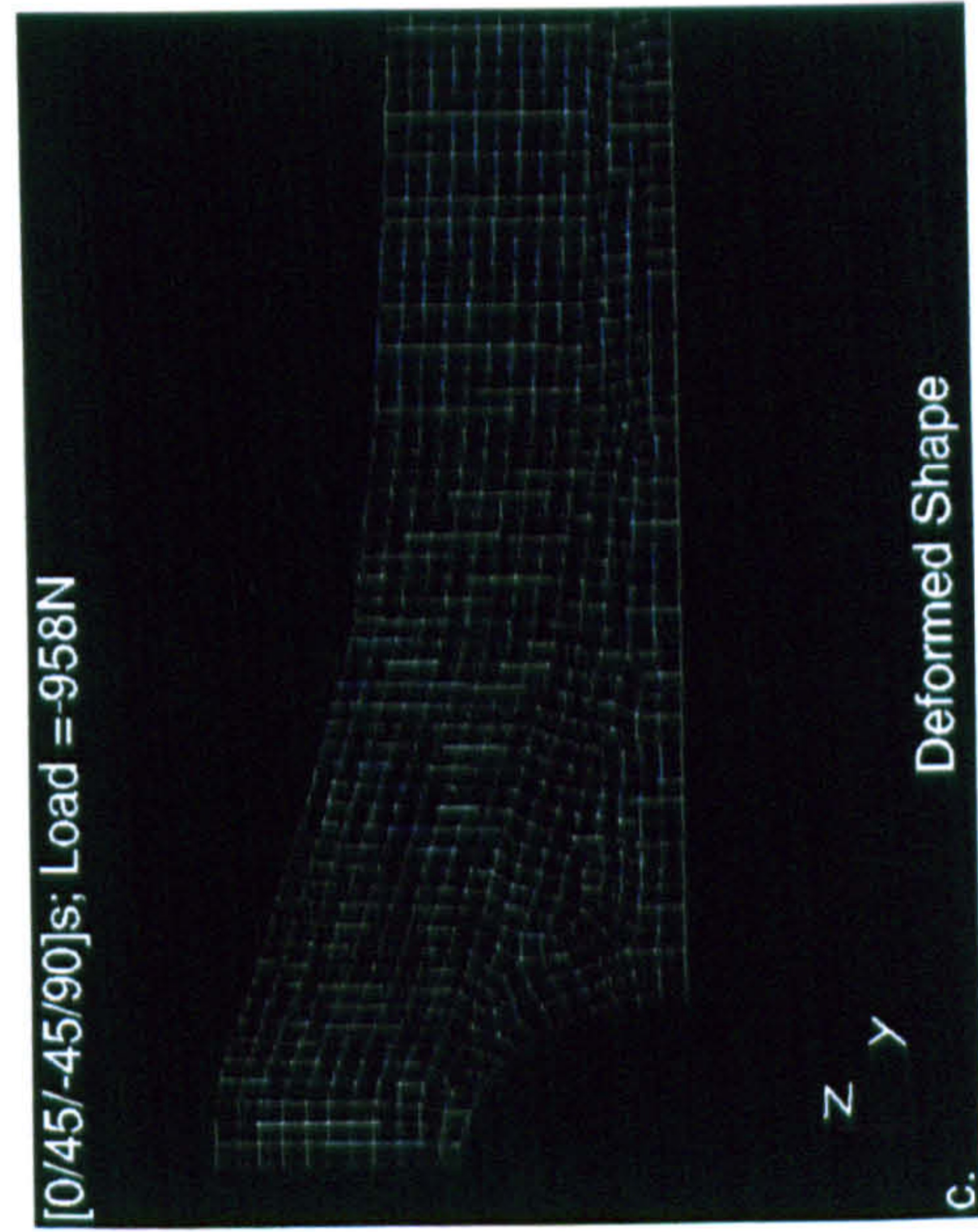


Figure 3.7. Local FE stress analysis for the $[0/\pm 45/90]_s$ lay-up.

From these figures, it is clearly evident that the local inclusion has a profound affect on the local stress distribution. Note, all FEA results presented were computed from the higher order elements, even though, as previously stated, for the mesh density considered, the lower order elements give satisfactory results.

Considering the far field longitudinal YY stress distribution, computed from the 2D plain strain model, for the $[0_2/\pm 45]$, lay-up, **Figure 3.6a**, then, the in-plane compressive load of -0.765kN manifests stress levels of -4.66 N/mm^2 , carried by the outer 0° directional plies, and -0.41 N/mm^2 , carried by the inner $\pm 45^\circ$ directional plies. The negative sign indicating compression. Classical lamination plate theory, CLPT, was used to verify the analysis^{7,8}. Employing identical material properties, ply stacking sequence and ply thickness, CLPT computed results of -4.45 N/mm^2 & -0.48 N/mm^2 for the 0° directional plies and $\pm 45^\circ$ directional plies respectively. Slight discrepancies exist between the FEA and CLPT though this is thought to be associated with differing analysis procedures, i.e., the FEA assuming plane strain and CLPT assuming plane stress. Good agreement, however, is witnessed.

With reference to **Figure 3.6a**, in the vicinity of the geometric discontinuity, where the ply disruption is greatest, bending stresses exist within the plies. Such bending stresses being more prominent in the outer zero directional plies, highlighted by A & B and to a lesser extent at C. For this particular load case, the bending action at the extreme peak of the surface protrusion, A, is such that tensile stresses exist. Compared to the far field stress distribution, high local compressive stresses are observed at the specimen surface, D, though the greatest compressive stress occurs close to the interface between plies 2 & 3, B. The magnitude of such local stresses will depend upon the size of the diametrical inclusion. For such loading, the greater the diameter of the embedded inclusion, then, the greater the bending moment that the outer plies will experience.

Little longitudinal load is carried by the epoxy region as shown at E, **Figure 3.6a**, though examination of the transverse through thickness stress state reveals that a significant part of the epoxy is under a state of mechanical tension, **Figure 3.6b**, highlighted by G.

Considering the $[0/\pm 45/90]$, lay-up, when subjected to a uniaxial compression load of -0.96kN, then, similar behaviour to that previously described is witnessed. **Figures 3.7a & 3.7b** illustrate the nature of the longitudinal and through thickness stresses. FEA far field longitudinal stresses were computed to be -10.28 N/mm^2 , -0.93 N/mm^2 and -0.60 N/mm^2 within the 0° , $\pm 45^\circ$ and 90° plies respectively, **Figure 3.7a**. CLPT computed stress levels of -8.26 N/mm^2 , -2.00 N/mm^2 , and -0.48 N/mm^2 . Reasonable agreement is shown to exist.

As previously stated, bending stresses are incurred in the disrupted plies, A, B & C. Now, though, the highest local compressive stress concentration exists at D. Longitudinal stresses, highlighted by E, are comparable to those of the lay-up $[0_2/\pm 45]$, though, the through thickness stresses are significantly higher and more localised, G, **Figure 3.7b**.

The manufactured plate specimens will be able to carry significantly higher loads than those previously applied. Above P_c , the plate will deform in to a single half wave in both in-plane directions, with the peak deformation amplitude located at the plates geometric centre. Increasing the in-plane uniaxial compressive load, up to several times P_c , will result with a maximum out-of-plane displacement exceeding the plates thickness. At this point, the plates stressed state will be highly non-uniform with the unloaded supported edges carrying most of the load. Under such circumstances, it would be incorrect to apply such load levels to the 2D plane strain analysis to ascertain the local effects.

Activation of the SMA wires at $2-3P_c$ may alleviate all of this out-of-plane displacement. This out-of-plane displacement removal results with the tendency to redistribute the load path back towards the central part of the plate, therefore, the plate will once again exist in a state of

uniform stress. Should such a uniform stressed state exist, then, the assumptions of plane strain become *pseudo*-valid. The 2D analyses were re-run for an applied loading of 1.9kN for both $[0_2/\pm 45]_s$ & $[0/\pm 45/90]_s$ lay-ups. The generated fringe plots were similar to those presented in **Figures 3.6 & 3.7**, though the magnitude of the stress components vary.

Tables 3.3 & 3.4 list the computed stress values, for lay-ups $[0_2/\pm 45]_s$ and $[0/\pm 45/90]_s$ respectively, for this higher applied load case. The results discussed earlier in this section are also summarized along with results computed for the residual stress state at 25°C, the residual stress state plus the application of the lay-ups critical buckling load and the residual stress state at 100°C with an applied mechanical load of 1.9kN. The locations of the stress regions tabulated are those depicted by the previous figures. Fringe plots were seen to be similar to those presented in **Figures 3.6 and 3.7a & b**.

For the 2D plane strain analysis, when comparing the YY-longitudinal far field, FF, finite element analysis with CLPT, good comparison only exists for the first two load cases. Significant disparity between the two solutions was apparent for the later load cases with imposed temperature boundary constraints.

Under such conditions, significant strains in the elements normal direction are neglected by the FEA. For example, considering the $[0_2/\pm 45]_s$ lay-up of load case (iii), such strains, were 67 times greater than those in the YY direction. Quite clearly, the assumptions of 2-D plane strain, have become invalid.

A truly accurate representation, for all load combinations, can only be achieved with expensive 3-dimensional solid finite element analysis. Only then, as shown in the tables, is the comparison between the far field FEA and CLPT sufficiently accurate.

A final note regarding these stresses concerns the materials ultimate failure values. The maximum tensile stress was observed within the $[0_2/\pm 45]_s$ lay-up at location **B** for the third load case. This load is predominantly carried by the outer zero directional plies and amounts to 1.4% of the materials ultimate load carrying capability. Such a stress magnitude, therefore, is no cause for concern. The maximum compressive stress value was witnessed within the $[0/\pm 45/90]_s$ lay-up at location **C** for the fourth load case. At worst, this will be carried by the small amount of 90° directional plies present in close proximity to this region and amounts to 51.7% of the materials compressive load carrying capability. This latter value may have fatigue implications.

Load Case	A, ply 1	B, ply 2	C, ply 4	D, ply 1	E, matrix	F, matrix	G, matrix	FF FEA	CLPT
(i) 0.765 kN	1.37	-9.87	-1.85	-7.11	0.05	-0.25	1.00	0's; -4.66 45's; -0.41	-4.45 -0.48
(ii) 1.9 kN	3.43	-24.68	-4.63	-17.77	0.12	-0.61	2.51	0's; -11.65 45's; -1.03	-11.04 -1.19
(iii) 25°C	-2.96	21.32	-36.23	14.57	0.23	-2.45	-2.05	0's; 10.36 45's; -10.36	9.14 -9.14
(iv) 25°C + 0.765 kN	-1.59	11.45	-38.08	7.46	0.28	-2.70	-1.05	0's; 5.70 45's; -10.77	4.68 -9.78
(v) 100°C + 1.9 kN	2.81	-20.19	-12.26	-14.70	0.16	-1.12	2.08	0's; -9.52 45's; -3.13	-9.12 -3.54

Table 3.3. $[0_2/\pm 45]_s$ localised stress results (N/mm²).

Load Case	A, ply 1	B, ply 1	C, ply 4	D, ply 1	E, ply 4	F, ply 4	G, ply 4	FF FEA	CLPT
(i) 0.958 kN	-2.34	-12.28	-2.84	-14.14	0.11	-0.31	2.13	0; -9.65 45's; -1.45 90; -0.60	-8.26 -2.00 -0.48
(ii) 1.9 kN	-4.64	-24.34	-5.63	-28.03	0.21	-0.63	4.22	0; -18.45 45's; -3.14 90; -1.18	-16.39 -3.96 -0.95
(iii) 25°C	-6.51	-33.82	-126.41	-38.27	-4.65	13.26	5.98	0; -21.45 45's; -0.14 90; 20.68	-22.92 -0.11 21.43
(iv) 25°C + 0.765 kN	-8.85	-46.10	-129.25	-52.41	-4.54	12.95	8.11	0; -31.10 45's; -1.59 90; -20.08	-31.20 -2.11 20.96
(v) 100°C + 1.9 kN	-6.01	-31.46	-32.24	-36.09	-0.77	2.16	5.48	0; -22.97 45's; -3.16 90; -3.17	-21.22 -3.99 3.57

Table 3.4. $[0/\pm45/90]_s$ localised stress results (N/mm²).

3.5 Control Strategy 2

A descriptive account of the conceived adaptive response of composite plates utilizing the restrained recovery of SMA actuators fed through tubing has been presented. Such restrained recovery will be met by constraining the ends of the actuators to an external boundary. An application for the plate components could be aircraft skin panels with spars or webs providing the fixtures necessary for SMA constraint.

An alternative approach is to embed the actuators directly into the host composite during the fabrication process and allowing the epoxy to consolidate and adhere to the actuator. Constraint, ensuring adaptation, is met by the plate component itself.

A descriptive account of how the adaptive response is realized is given. This will be followed by the manufacturing procedure that will enable the realization of such adaptation. The SMA/host interface bond strength will then be assessed, therefore, providing an evaluation on the interface's mechanical capability and reliability.

As stated previously within this thesis, this control strategy requires the adherence of the actuator to the host composite. On activation, restraint is imposed upon SMA strain recovery by the SMA/composite interface. With SMA energization, tensile axial stresses form within the actuator. Such stresses are equilibrated through interfacial shear stresses and result with a compressive load on the composite. This interfacial shear stress acts over a finite distance at the ends of the bonded actuators. The location of these internally generated interfacial shear stresses requires careful consideration so as not to degrade the mechanical performance of the compression loaded panels.

For such a control strategy, the primary concern is with plate/panel instability. Under certain boundary conditions, a possible problem with this adaptive conception is its propensity to initiate buckling, or degrade the post-buckling response, when the host

component is exposed to the aforementioned compressive actuation loading. Therefore, by spacing the recovery forces from the neutral plane, control moments are generated which are then used to control the shape of the structure.

Compressive membrane stresses, however, will remain present. Preliminary calculations showed that such membrane stresses may have a more domineering effect than the bending stresses, **Chapter 4**, and as a result, when energizing the actuators at a value beyond the components critical buckling load, the post-buckling behaviour will degrade. The uniformity and magnitude of such membrane stresses throughout the laminate depend greatly on the number of activated actuators. The worst case scenario is when the entire length of the SMA actuator is bonded to the matrix, more of which will be discussed in **Chapter 4**.

When wishing to alleviate the deflections of a square plate, the actuators must be placed off the neutral plane in a skew symmetric manner. They must be placed on both sides of the neutral plane as, theoretically, we do not know which way the plate will bow. The actuator spacing must be such that, when one side of the plates actuators are energized, the amount of thermal energy within the composite, local to the intended inactivated actuators, is minimal, else partial, or worse, complete unintentional thermal actuation will take place. Such an actuation mechanism will place an additional bending moment upon the plate, though opposite in sense, as well as a considerable in-plane compressive force. Its effect will be to increase the out-of-plane displacement as opposed to its alleviation.

The prevention of unintentional SMA thermal actuation can only be realized with a low volume fraction of SMA material. Such parsimonious use, however, results with high local temperature gradients. As will be shown in **Chapter 5**, such local temperature gradients result with significant in-plane compressive stresses which also act to degrade the performance. It is conjectured that the bending moments induced by the low volume fraction of SMA actuators will fail to alleviate the anticipated mechanically post-buckled deflections and the additional detrimental behaviour associated with the required thermal environment.

Employing this control strategy, the post-buckled deflection alleviation of flat plate like structures is inconceivable. However, this strategy may provide the desired response within skin loaded stiffened panels. Having an understanding of the plates bending-buckling response, a higher volume fraction of activated SMA material placed in an un-symmetric manner may alleviate the out-of-plane deflections.

3.5.1 SMA Adherence/Insulation Implications

This section discusses the manufacturing aspects that may allow the direct integration of an adaptive mechanism within blade stiffened panels. The mechanical response of $[0_2/90_2]$, & $[0_2/\pm 45]$, skin stiffener panels were investigated. The lay-ups in the blade sections of the stiffeners being $[0/90_2/0]$, & $[0/\pm 45/0]$, respectively. Initial trials were based on co-cured panels, though the difficulty in incorporating the embedded inclusions and the poor finished quality of the manufactured parts led us to seek an alternative fabrication procedure. The manufacturing route chosen was such that the skin, featuring the embedded inclusions, was fabricated separately from the stiffeners. The blade stiffeners were fabricated in the form of a T section, the outer 4 plies forming the flange which allows for sufficient bonding to the skin.

As with *control strategy 1*, actuation of these panels may be met either with the passage of an electrical current along the SMA wire, or by exposing the complete component to an elevated temperature environment. The former is proposed for this study and necessitates suitable SMA electrical insulation from the conductive carbon reinforcement. Several insulating methods were investigated, the results were largely unsuccessful, though a reliable,

cheap and efficient technique was found. Firstly, the methods explored to insulate the SMA actuators from the carbon reinforcement, yet ensure the adherence to the laminate, are discussed.

All insulating techniques were performed on both 0.3mm and 0.4mm diameter sandblasted alloys that were oriented both parallel and perpendicular to local ply reinforcement. The actuators were sandblasted as this creates an uneven craterous surface that is desirable for mechanical interaction. Also, this situation represents a worst case scenario as irregular surface protrusions may penetrate the insulating barrier, during compaction and curing, resulting with the formation of a conductive path between the SMA and the carbon. The likelihood of contact between the conductive materials is increased further when embedding the SMA wires perpendicular to the local reinforcement.

The first method explored was to coat the SMA wires with an insulating varnish^β prior to their embedding within specified lay-ups. The coating was applied by dipping clean abraded actuators in a varnish bath. Upon removal of the actuator from the bath, visual inspection revealed a poor surface coverage with a formation of varnish droplets. Such poor *wettability* characteristics of SMA were reported in the literature⁸². The varnish was then allowed to cure in an oven at the required temperature of 40°C for a duration of 15 minutes.

Coated wires were located between plies 1 & 2 or 7 & 8 of a [0₂/90₂], carbon/epoxy laminate. The wires were oriented parallel and perpendicular to the neighbouring reinforcement. For the fabricated test pieces, electrical resistance measurements, made between the egressed SMA actuators and exposed carbon reinforcement, revealed a conductive path between the two constituents. This observation was made for each embedded actuator, and therefore, renders the varnish coating technique obsolete.

A method of insulation, provided for by sandwiching the actuators between N layers of glass tissue, each layer having a thickness in the range of 0.09-0.14mm, or by N layers of filter paper, each layer having a thickness of 0.12mm, albeit *compressible*, prior to lay-up, may yield the desired results in terms of insulation and adherence. Insulation will be met by the non-conductive separation of the SMA from the carbon reinforcement. Adherence to the composite is assured if the sandwiching material is permeable to the epoxy resin. Such a simple process, if successful, has significant economical and manufacturing benefits compared to the former method.

For both SMA orientation angles, adequate insulation was provided for only when the SMA wires, of both diameters, were located between a total of 6 layers of glass tissue. Such a large quantity of insulating material significantly disrupts the internal reinforcement and surface topology. Due to high volume requirements and the associated disruption it causes, this method is not deemed suitable.

Again, the filter paper was placed such that it segregated the current carrying actuator from the carbon reinforcement. Although a minimal amount of filter paper proved successful at insulating 0.3mm diameter abraded SMA actuators within advantageous lay-ups, it soon became apparent that its permeability was not to the required standard. Therefore, we could not guarantee the wetting and, hence, adhesion of the thermoset epoxy to the actuator.

The provision of an insulating permeable barrier segregating the two conductive constituents was met by exchanging the filter paper with woven glass fabric^α. A variety of styles of fabric were assessed, each varying in density and thickness. The fabric chosen was a plain weave (Style Y0001) with a thickness of 0.08mm. Once again, embedding the SMA actuators perpendicular to the neighbouring plies required several layers of the fabric to

^β Purchased from RS Components. Maximum Operating Temperature = 155°C.

^α Purchased from Fothergill Engineered Fabrics Ltd

provide the necessary insulation. This will result with a local low matrix volume fraction which is not considered desirable. The manufactured plate specimens will, therefore, have the embedded inclusions parallel to the neighbouring plies, therefore, necessitating only a single layer of the woven glass fabric, either side of the inclusion.

3.5.2 Specimen Fabrication

Prior to the discussion on the actual manufacturing procedure, a mechanism for composite constrained shape memory recovery at a desired internal position is presented. As previously stated, constrained recovery local to the skin edges will result with the formation of a detrimental in-plane compressive force. This force may easily degrade our desired structural response. A better location for the induced recovery stress is in the vicinity of the bow of the plate, where induced bending stresses have a more advantageous effect on the plate than the detrimental membrane stresses. This presents difficulties in terms of manufacturing. To induce the recovery stress away from the plate edges, the following manufacturing technique was devised. Refer to **Figure 3.8** for a schematic representation.

Pre-strained SMA wires are fed through two lengths of PTFE tubing. The location of the tubing is at the ends of the alloy ❶. This leaves the central region of the SMA wire exposed to the composite, the remainder merely acting in the form of a *current path*. The wires were allowed to protrude outside the specimen for electrical connection. The central region of the alloy, after curing, is constrained to the epoxy. The purpose of the tubing is to provide electrical insulation and allow SMA shape recovery during the first activation cycle after specimen manufacture. The tubing chosen was such that it does not provide a rigid interface to the SMA, therefore, allowing SMA slippage under recovery conditions. This local regain of the initial shape ensures that no local stresses will be transferred to the composite.

Adherence of the SMA to the composite over the central region generates interfacial shear stresses at locations ❷, a pre-defined location where the induced stress is required. The central region of the SMA is sandwiched between 2 layers of the glass weave ❸, thus, providing the necessary electrical insulation for an advantageous lay-up. Prior to integration, the SMA wires were subjected to a partial sandblast. The length of the sandblasted region is equal to 10mm, its location shown in **Figure 3.9**.

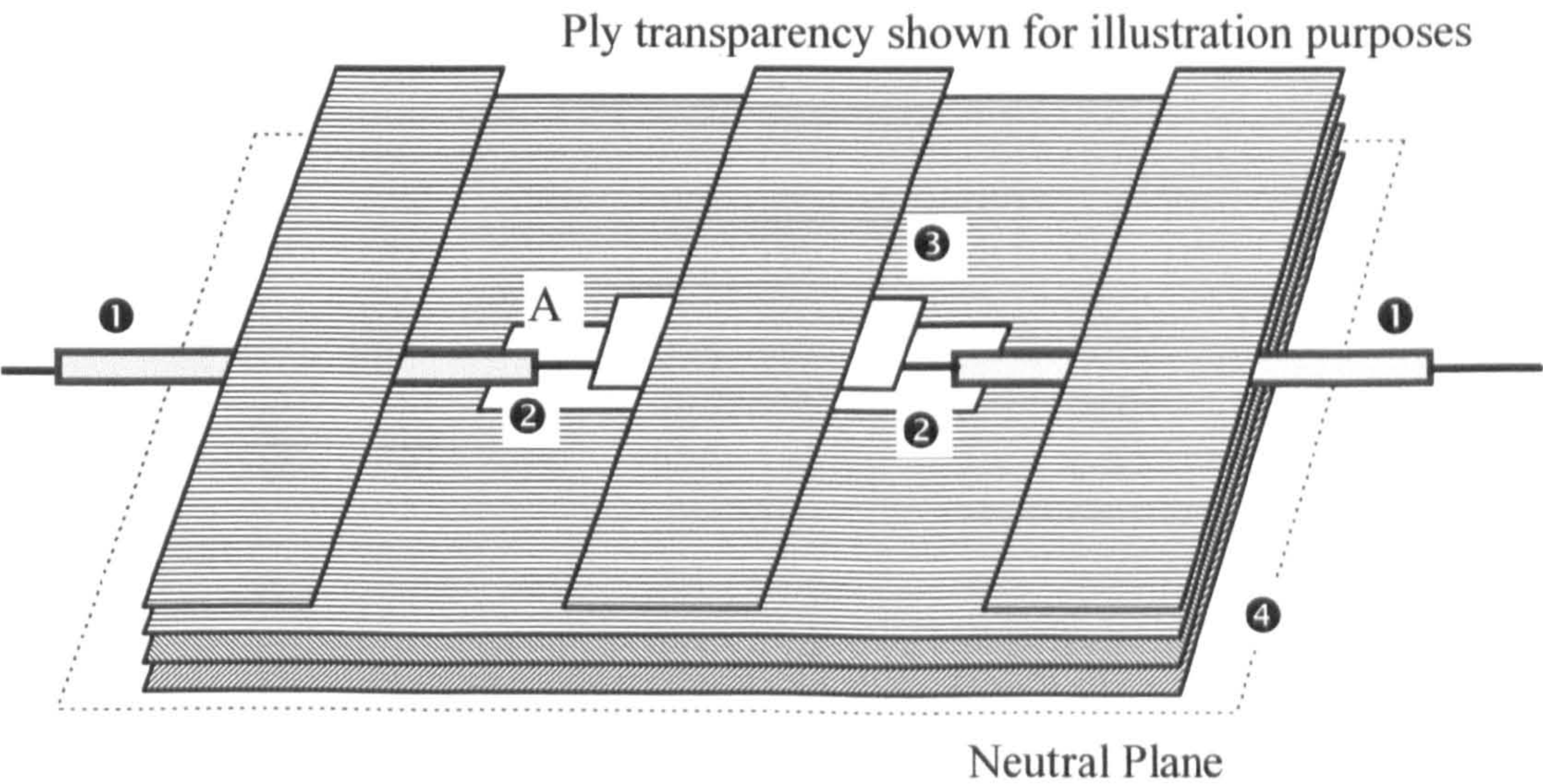


Figure 3.8. A cut through representation of a section of the skin for control strategy 2.

The PTFE employed for this strategy has a 0.4mm internal diameter with a wall thickness of 0.1mm. The SMA wires employed have a diameter of 0.3mm. The tubing, under the curing pressure, compresses to an elliptical shape with the inner minor axis conforming to the SMA

actuator. The lack of conformity of the tubing to the entire SMA surface area results with continuous voids, as seen in **Figure 3.9**. During the curing process, the low viscosity resin flows through the tubing, therefore, adhering to both PTFE and the SMA. Such resin migration within the tubing may be advantageous in that the manufactured laminate has minimal void content, yet may be disadvantageous in that the epoxy may hinder shape memory recovery. This mechanical hindrance is assessed in **Section 3.7**.

A curing rig has been designed and manufactured which will maintain the strain in the shape memory alloy wires during the curing process, therefore, preventing premature actuation.

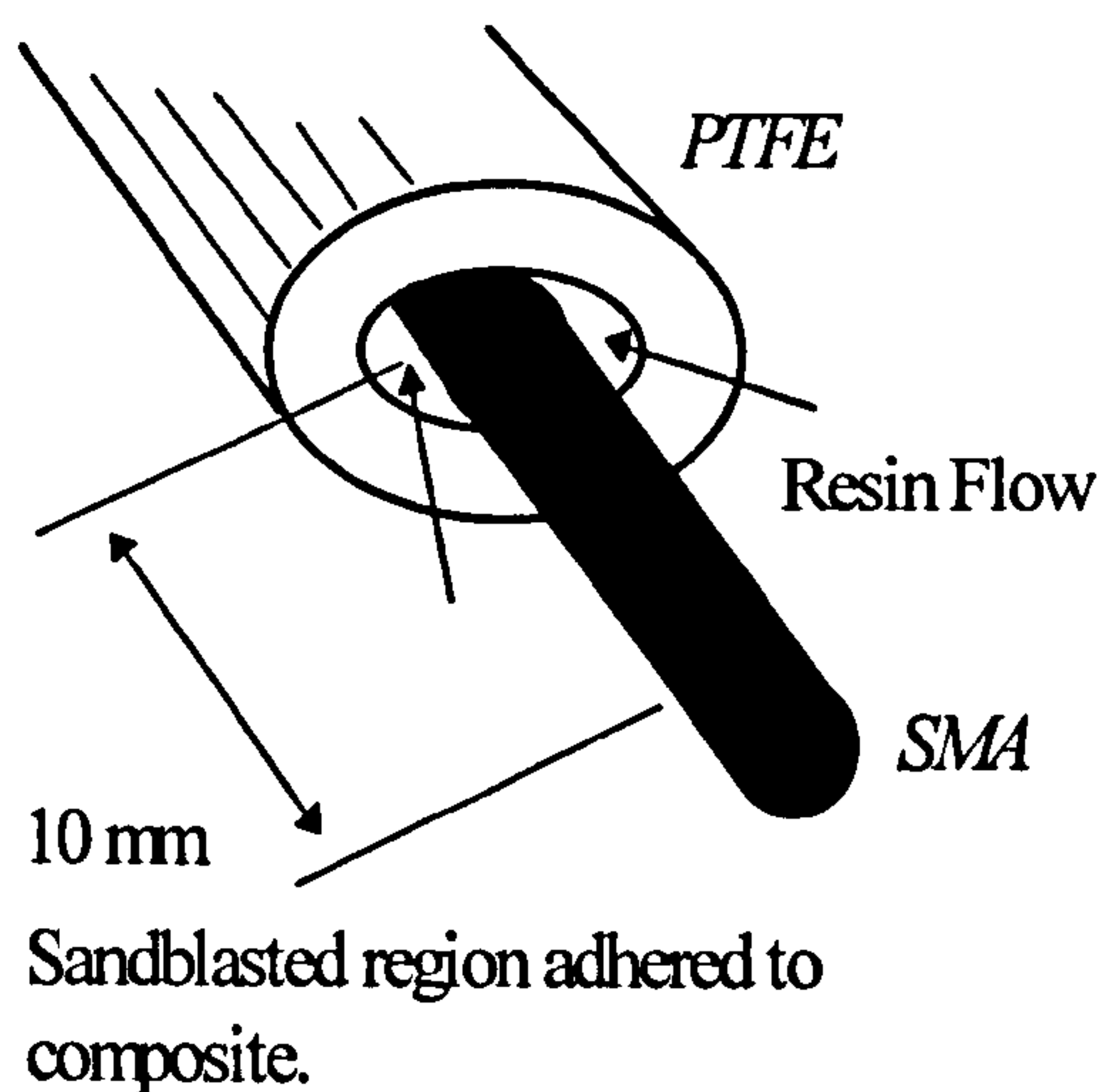


Figure 3.9. *Enlarged view, Section A, Figure 3.8.*

The manufacturing process for the skins featuring the embedded inclusions is summarized as follows;

1. Prior to lay-up, the glass weave insulators, sleeves and SMA's were prepared. The former necessitates cutting to size, while the latter SMA preparation required pre-straining and abrasion of the two 10mm lengths. This operation utilized a manufactured frame with SMA shielding, where desired, thus obtaining the desired sandblasted lengths. Upon completion of the sandblasting process, the actuators were washed in acetone to remove any unwanted debris.
2. The pre-preg was then cut to the required dimensions and orientation.
3. Plies 2-7 were laid up and debaulked within a vacuum bag.
4. Ply 1 was accurately positioned on the moulding\curing plate.
5. The lower glass weave insulating layer was located.
6. PTFE tubing was located on the SMA. One end of the SMA was then clamped at the appropriate position within the curing frame. The sleeves were lined up with the start and finish of the sandblasted region. The other end of the SMA was then clamped.
7. Edge bars, featuring 0.6mm grooves at the appropriate spacing, were pressed down over the sleeves and aligned with the two parallel edges of ply 1. Index pegs ensure the edge bars remain parallel, therefore, producing an edge finish suitable for loading.

At this stage, the lay-up and clamping arrangement is shown in **Figure 3.10**. An internal clamping arrangement is used to avoid associated difficulties with sharp protrusions causing

vacuum bag failure when curing. The positioning of the SMA's is defined by index bars located in the moulding plate frame.

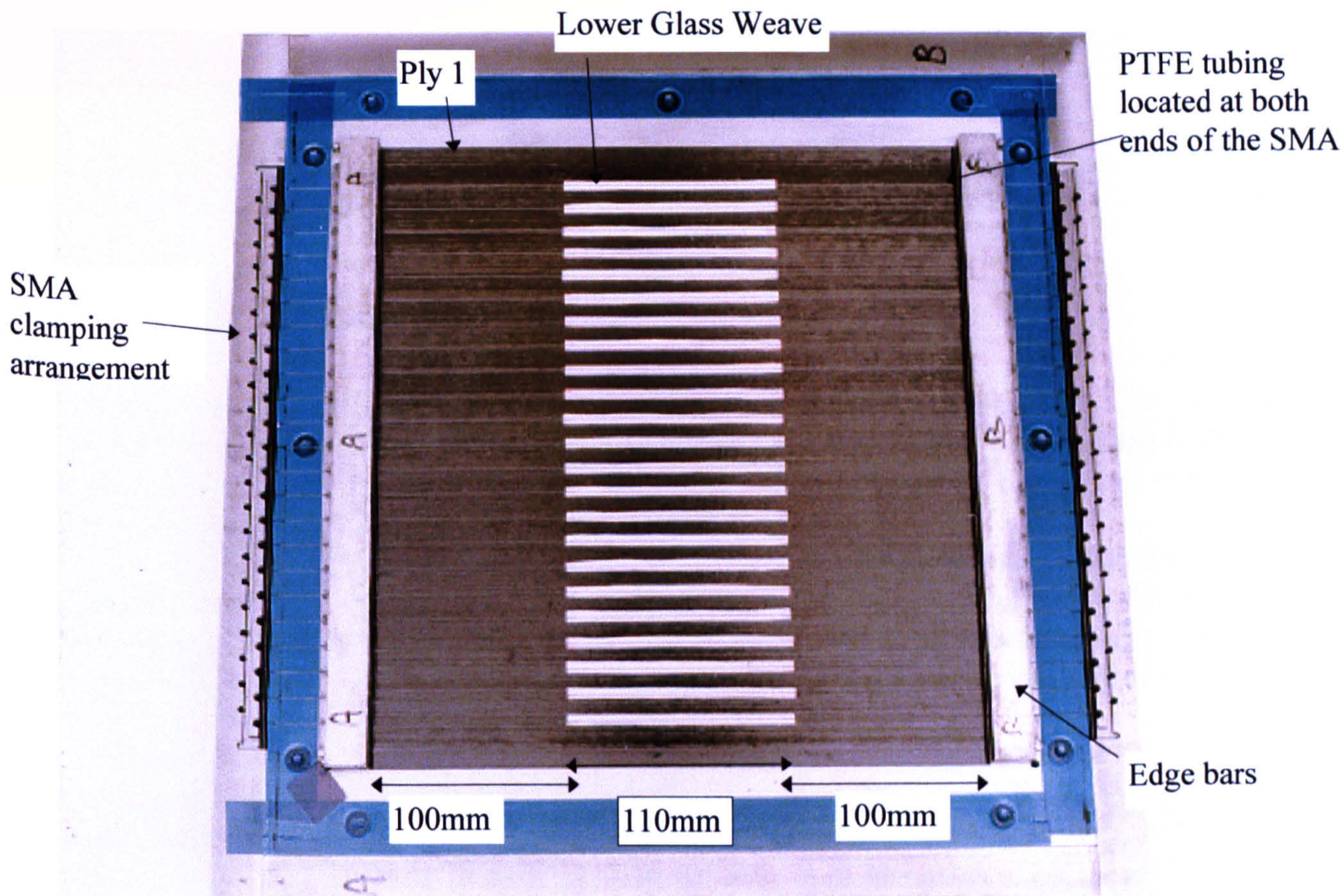


Figure 3.10. *Curing apparatus.*

Continuing with the lay-up process;

8. The top glass weave insulators were then positioned.
9. The centre lay-up, plies 2-7, was then eased down on the built up assembly such that its location was between the parallel edge bars. Width referencing is not required. After curing, this edge is trimmed to the required dimension.
10. Additional identically sized glass weave insulating material is placed between plies 7 & 8 in a skew symmetric manner so as to balance the laminate, else, as will be shown, residual stresses, arising upon cool down from the curing temperature, induce significant skin warpage.
11. Ply 8 is then laid-up prior to the positioning of the top caul plate. Silicone diaphragms between the tooling components and the specimen are not required as the lay-ups considered are highly advantageous, i.e. they lose the inclusions within the parallel reinforcement.

The lay-up of the carbon reinforced stiffeners is summarized as follows;

1. Sets of four plies are wrapped around one right angle of a tooling block and debulked.
2. Two of these sets were then accurately brought together and placed on a base tooling. Further debaulking was conducted prior to curing.
3. Upon fabrication completion, the stiffeners are machined to size and bonded to the skin at their appropriate position.

3.5.3 The Fabricated Specimens

Two completed products, with lay-up $[0_2/90_2]_s$, are shown in **Figure 3.11**. The panel on the right hand side of the figure illustrates the nature of warpage when the specimen is devoid of the skew symmetrically placed glass insulating weave. The panel on the left shows the alleviation of this warpage, by balancing the residual stresses.

An optical micrograph illustrating the nature of the insulation around an abraded 0.3mm diameter SMA wire, between plies 1 & 2 of the $[0_2/90_2]_s$ lay-up, is presented in **Figure 3.12**. The nature of the weave can clearly be seen. Local epoxy resin rich regions are also apparent, however, they are not as prominent as those previously encountered. No significant disruption in ply reinforcement or surface topology is observed.

Figure 3.13 illustrates the nature of the same sized alloy located within the PTFE tubing. The lay-up illustrated is a $[0_2/\pm 45]_s$. It can be seen from **Figure 3.13** that the embedded inclusion will displace the centrally located reinforcing fibres by a small amount. As the load will be applied normal to the plane of the micrograph, such disruption is not thought to be of concern. Such embedded inclusions have minimal effect on the surface topology, as opposed to the disruption caused by the larger diameter inclusions presented in **Figures 3.1-3**.

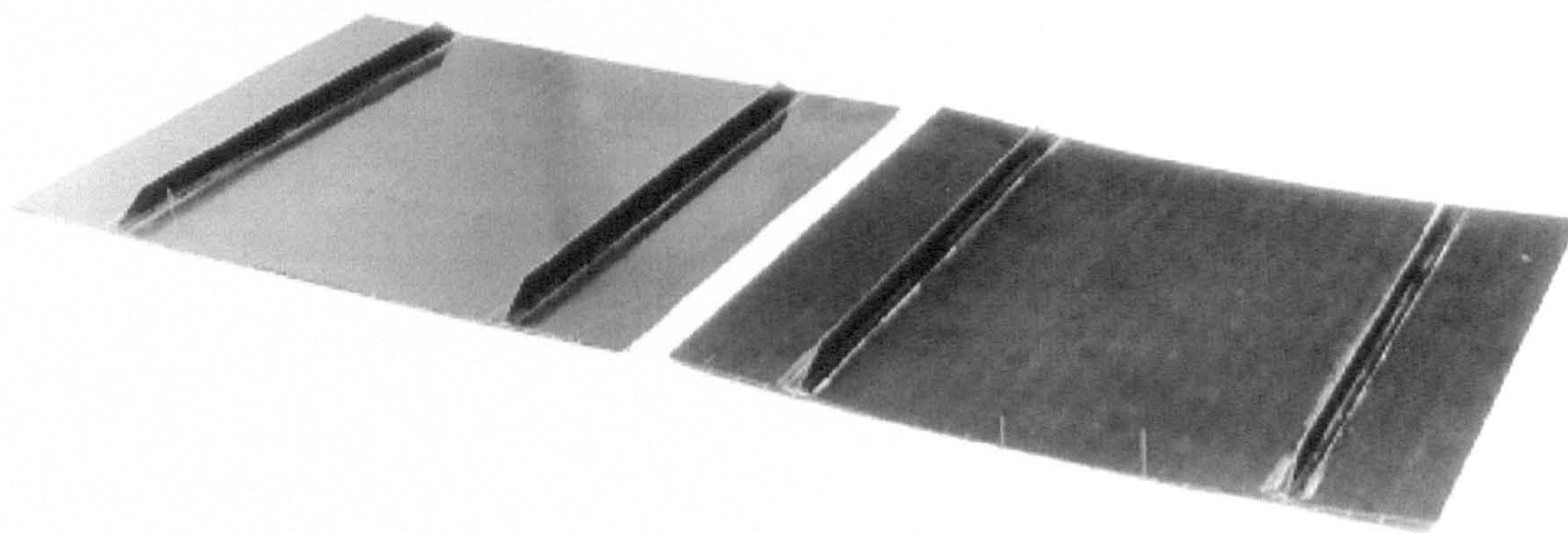


Figure 3.11. *Fabricated stiffened panel specimens.*

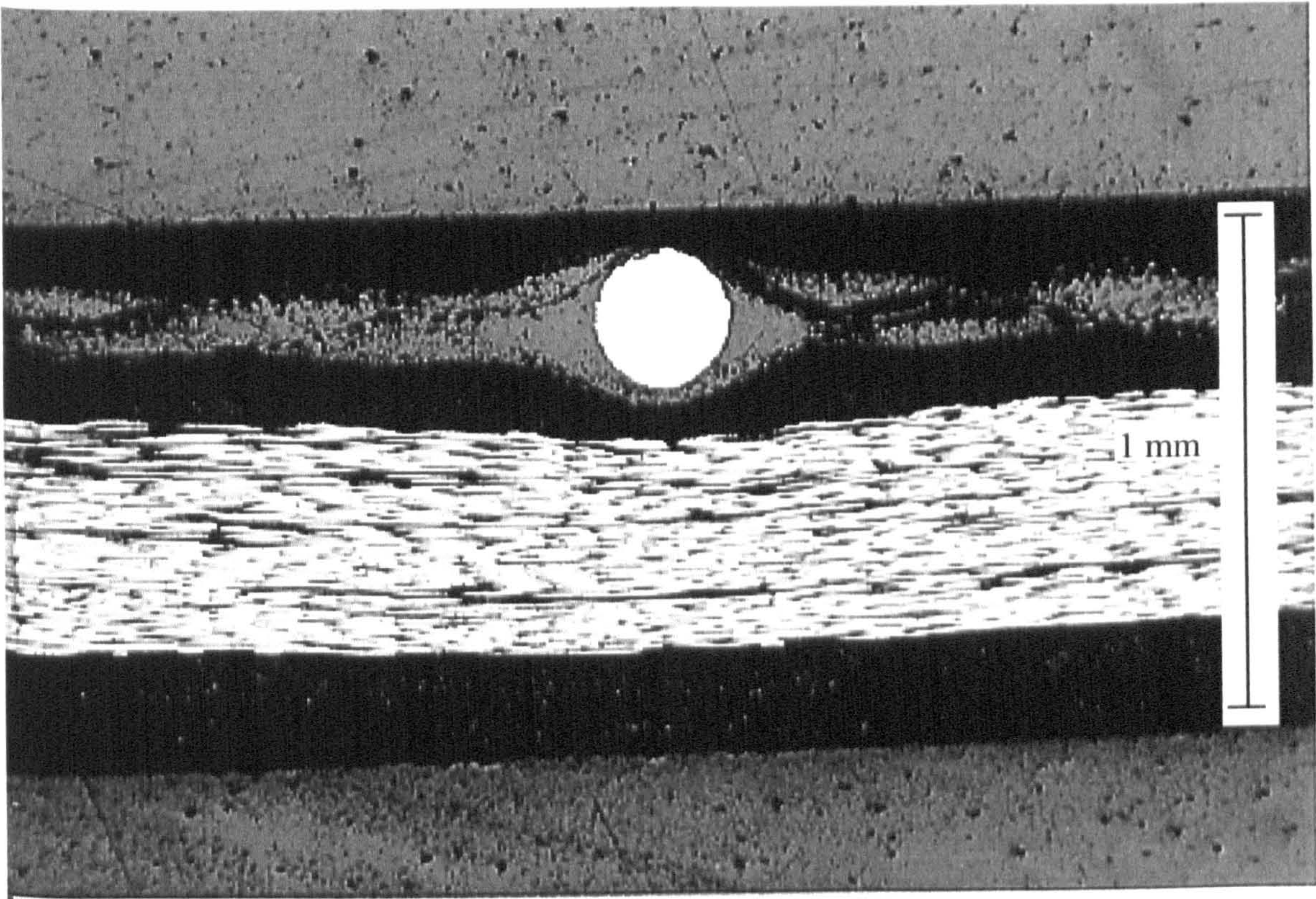


Figure 3.12. $[0_2/90_2]_s$ optical micrograph with embedded SMA within glass insulating weave between plies 1 & 2.

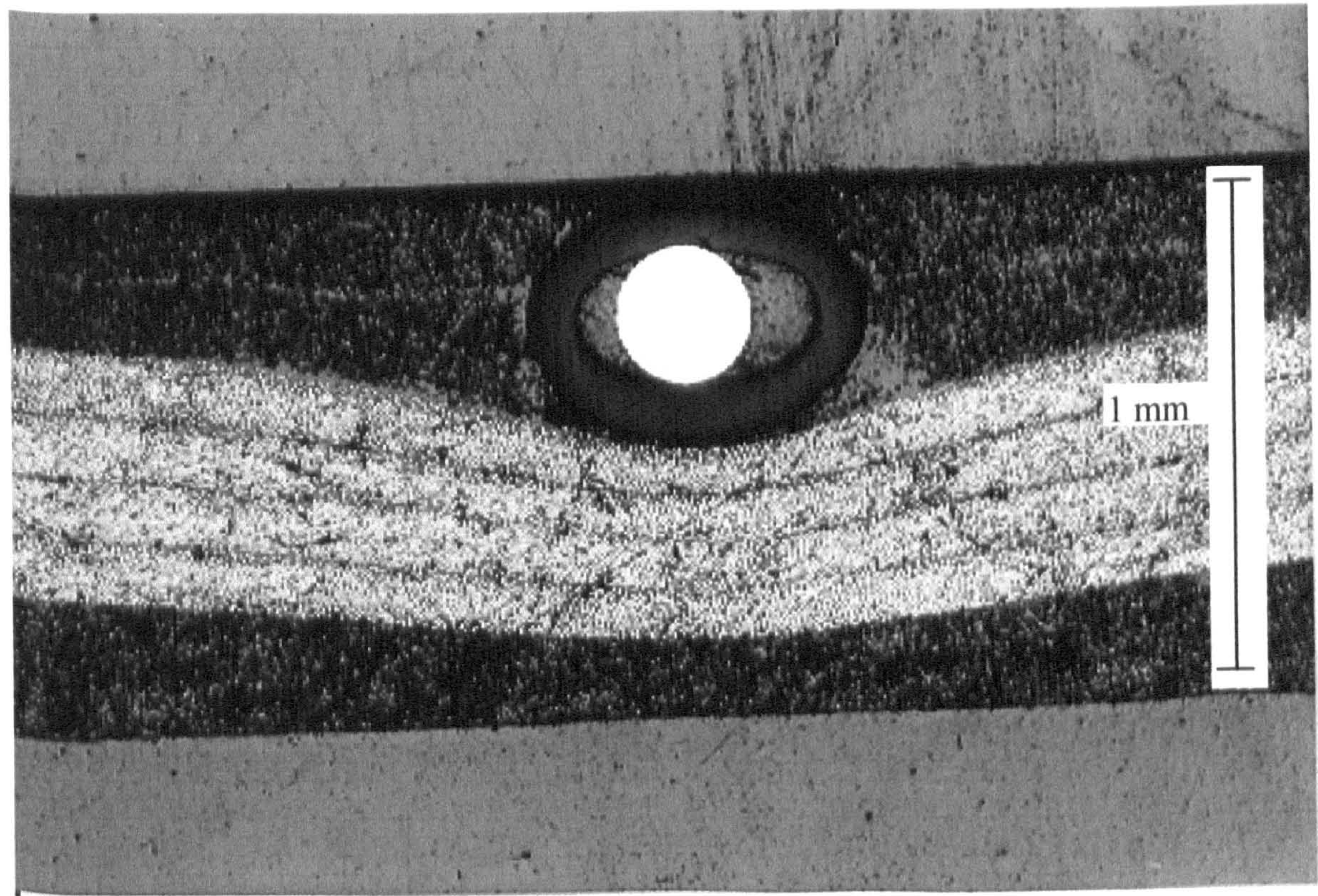


Figure 3.13. $[0_2/\pm 45]_s$ optical micrograph with embedded SMA within PTFE tubing between plies 1 & 2.

3.6 SMA Adhesion To Host Composite

This section is primarily concerned with the adhesion of the nickel-titanium actuators to the host epoxy. The adhesion providing the necessary constraint. Behaviour of the SMA/epoxy interface is analogous to the behaviour of embedded sensing devices, i.e. the performance of the embedded sensor is dependent on the transfer of strain from the host to the sensor⁸³. We can also conduct an analogy between the matrix and the host's reinforcement where failure of the interface between the reinforcement and matrix compromises the fabricated components structural integrity⁸⁴.

SMA interface failure results with functional performance degradation. By interface failure we simply mean actuator debond from the host, i.e. where a debond around the circumference of the actuator, parallel to the direction of the SMA, separates the constituents, thus, eliminating the support provided by the matrix.

The literature abounds with studies of fibre matrix adhesion in polymer composites. The open literature does not abound with quantitative data on SMA/composite adhesion.

The adaptability of the fabricated stiffened panel specimens depends on the stress transfer from the embedded SMA actuators to the surrounding composite. With activation of the embedded pre-strained SMA wires, large interfacial shear stresses will be generated at the actuator/composite interface. The shear strength of this interface must be stronger than the SMA recovery stresses, associated with activation, or the interface will fail. It is vital, therefore, that the actuators adhere well to the host composite. The stronger the interface, the greater the transfer of induced stress and, therefore, provision is met for a reliable adaptive response. Should interface failure occur, only frictional resistance will hinder shape memory recovery. A complete breakdown of the interface will lead to the cessation of shape memory adaptation.

The interface region of concern is a two dimensional region between the SMA actuator and the surrounding matrix, though, correctly speaking, there will exist a three dimensional interphase region.

Interface strength is provided for by numerous mechanisms. These include adsorption and wetting, interdiffusion, electrostatic attraction, chemical bonding, mechanical interlocking and thermal residual stresses.

It is reported elsewhere⁸⁵ that significant interfacial decohesion occurs when activating SMA actuators, initially pre-strained by 6%, embedded within a glass/epoxy laminate. It is thought that this interfacial break down is the result of embedding plain wire actuators. The smoothness of the actuator surface may enhance wetting of the polymer resin to the actuator to benefit chemical cohesion. It does not, however, provide for mechanical interaction between the polymer resin and the actuator surface.

By incorporating a suitable surface preparation to the actuators, then, the interfacial bond strength will be increased, therefore, the occurrence of interfacial decohesion may be substantially reduced.

Findings from investigations whereby 0.381mm diameter SMA actuators are embedded within graphite/epoxy thermoset & carbon/PEEK thermoplastic composites⁸², and 0.15mm diameter SMA wires embedded within an epoxy matrix⁸⁶, suggest that the interfacial adhesion between the SMA actuators and the host composite is strongest when the SMA material is sandblasted prior to embedding. Pull-out tests were used to quantify the interface quality.

Within the aforementioned investigations, different actuator surface preparations were considered. These ranged from plain with an oxide coating, i.e. straight from the manufacturer, sandblasted, hand sanded and acid cleaned^{82,86}. A set of samples were also coated with an additional polymer⁸². Pull-out lengths were varied to determine the dependency of the pull-out strength on the embedded length.

It was found that hand sanding and acid cleaning actually reduce the interface strength compared to the plain samples. The reduction in interface strength associated with hand sanding, however, depends on the size of grit paper.

It is reported that sandblasting can increase the interface strength by 217%⁸⁶. Sandblasting not only enhances the interfacial adhesion but also ensures the removal of any potentially weak oxide regions that may exist at the surface of the actuators which originate during the alloy manufacturing stage.

Scanning electron microscope photographs, **Figures 3.14 & 3.15**, show the effects of sandblasting the 0.3mm diameter SMA wires compared to the untreated plain wire. **Figure 3.15** clearly shows a highly irregular rugged surface.

It is reported⁸² that the interface strength between SMA actuators and a PEEK thermoplastic matrix is lower than that of an epoxy thermoset. Throughout this investigation, we are only concerned with thermoset matrices. The curing process will ensure the low viscosity epoxy resin to suitably wet the actuator surface, therefore, enabling good chemical cohesion. The resin will also flow and fill the indented surface forming mechanical projections that will hinder debond of the actuator from the host structure, hence, providing good physical locking. Good wetting of the actuator, by the liquid resin, is of prime importance since poor wetting would produce voids at the interface which would concentrate stresses and initiate cracking.

Sandblasting will also act to increase the contact area with the epoxy when compared to the untreated specimen of **Figure 3.14**. This larger surface area can only improve the interface characteristics.

An interesting observation has been cited⁸⁷. For a 0.2mm diameter Ni-Ti-Cu alloy, utilizing the epoxy resin droplet interface test method, the interfacial shear strength is increased from 9.1MPa to 14.6MPa by pre-straining the *as received* alloy wires 6% prior to matrix crosslinking. It is stated that this increase in interface quality is attributed to surface roughness of the well oriented martensite variant.

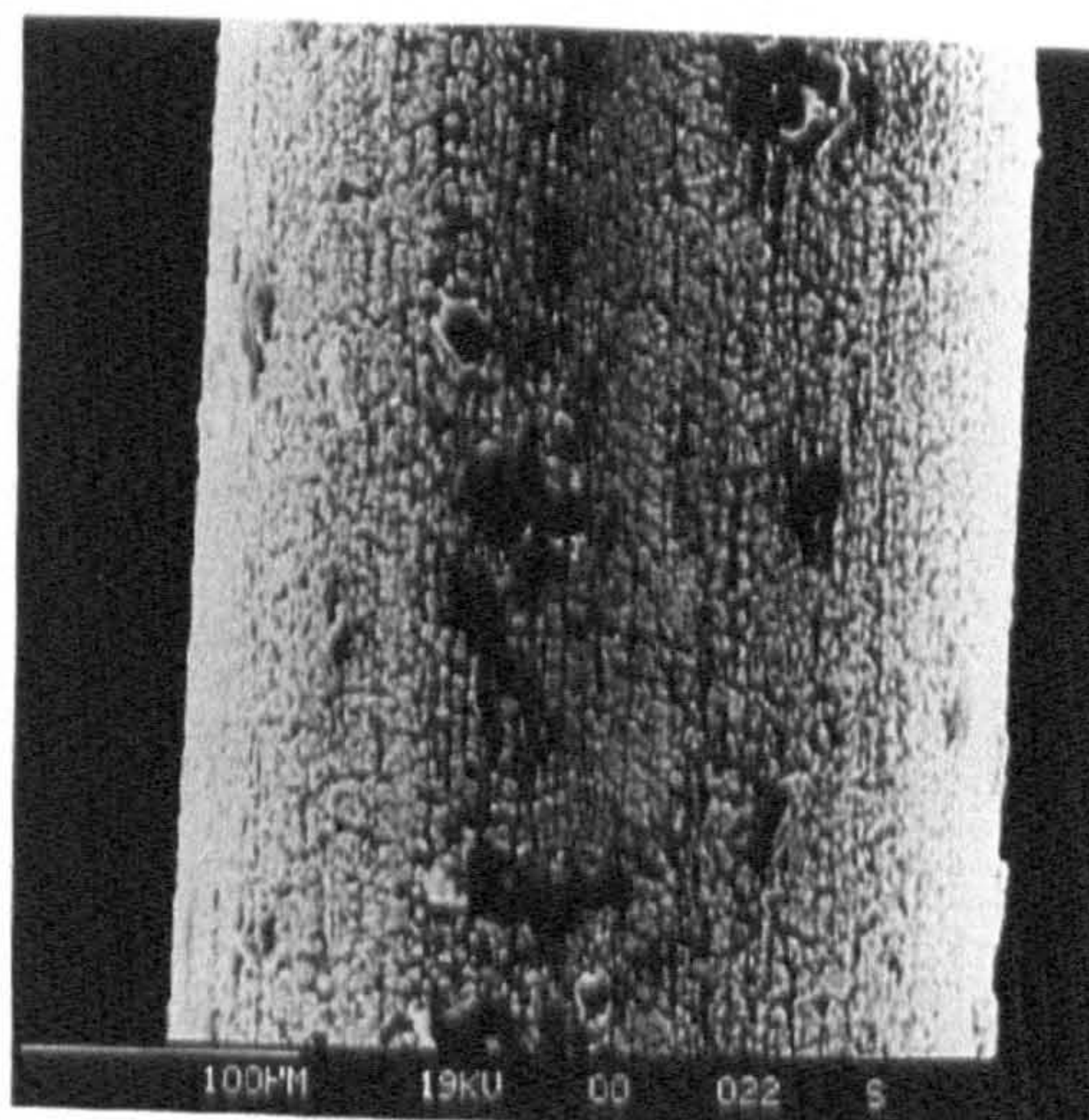


Figure 3.14. *Untreated plain SMA wire.*

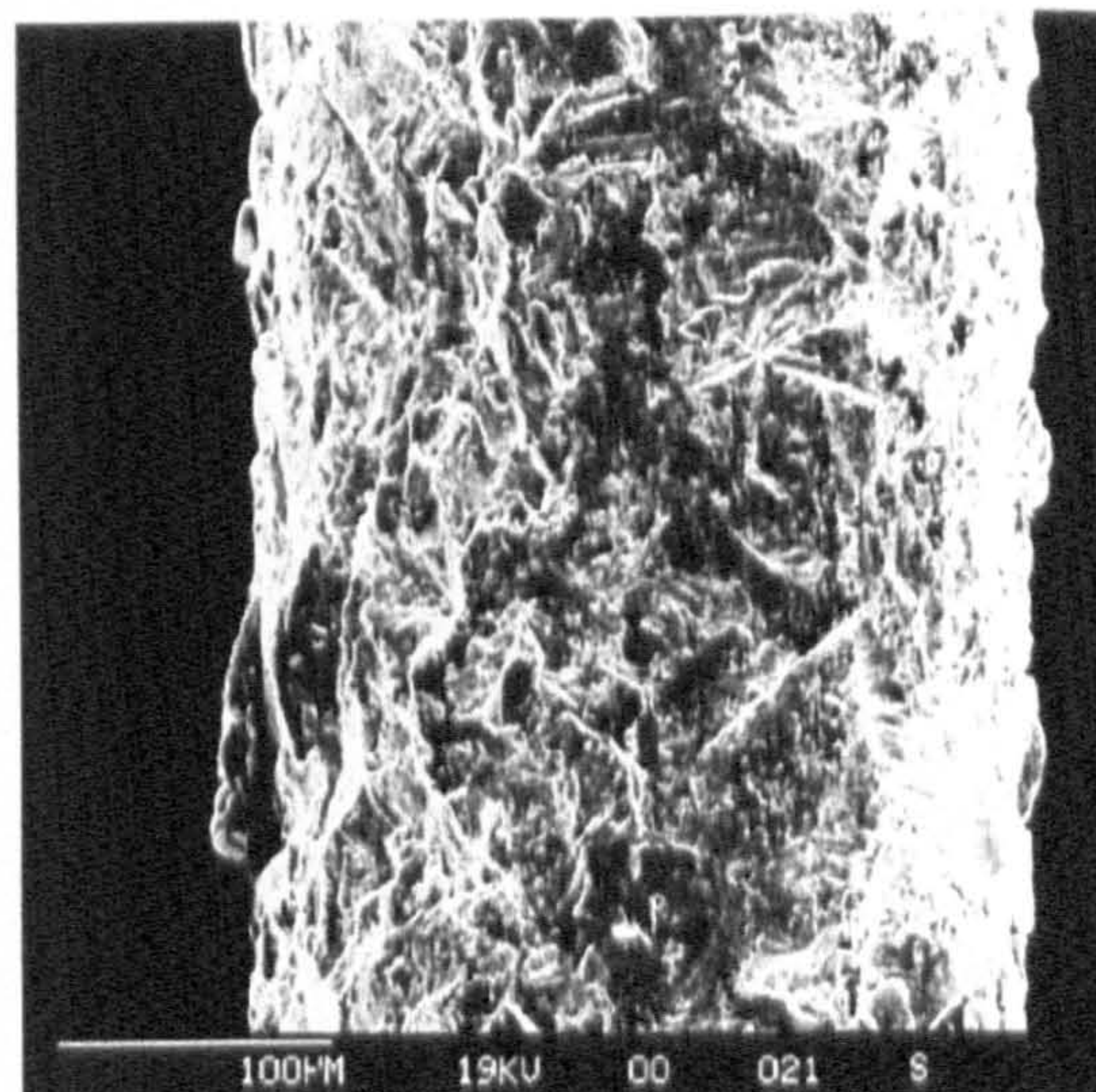


Figure 3.15. *Sandblasted SMA wire.*

3.6.1 Test Procedure For Experimental Interface Qualification

A requirement for adaptation is that the SMA/host interface must be of sufficient quality to sustain an elevated temperature and the applied SMA constrained recovery force. No published results were found that give an account of the SMA/host interface characteristics at an elevated temperature. This section aims to quantify the interface quality of 0.3mm diameter sandblasted SMA wires embedded within two carbon epoxy lay-ups and at temperatures ranging from ambient up to 108°C.

Among the various mechanical interface testing techniques, the fibre pull-out test has become one of the most popular⁸⁸. It is quoted as being the easiest test to use⁸⁴ and is reported as being a satisfactory test method⁸⁹. It is the technique employed to ascertain the interfacial bond strength of the SMA wires embedded within thermoset composite coupons.

The major drawback with the pull-out test is that it only produces a value for the pull-out force, from which, a mean uniformly distributed interfacial shear stress, τ_i , for that length of embedded fibre, may be calculated using the following formula;

$$\tau_i = \frac{F}{\pi d l} \quad (3.1)$$

where F is the pull out force, d is the diameter and l is the embedded length. In reality the behaviour is more complicated. The shear stress has long been recognised to be non-uniform, thus, diverging from the classical constant shear stress condition for a material assumed to deform plastically⁹⁰.

The test employed extracts a single SMA actuator embedded within the host utilizing a tensile load. Two lay-ups and two embedded lengths have been considered. The lay-ups considered are $[0_2/90_2]$, and $[0_2/\pm 45]$, with embedded lengths of 12.5mm and 25mm. The chosen embedded lengths are representative of those published elsewhere⁸². The embedded length does not affect the actual interfacial shear strength. A longer length, however, will require higher pull-out forces because failure, in the form of a circumferential crack, must travel along the length of the SMA wire before the interface becomes short enough to completely fail.

Each embedded length of material was located between plies 1 & 2. This location being representative of the specimens required for the adaptive control purposes of this research. Five coupons of each length were tested at temperatures of 22, 55, 85 and 108°C.

The manufactured composite coupons were cut such that there was sufficient distance between the clamped composite end and the SMA embedded length, therefore, alleviating clamped boundary interactions with the region of interest. A fixture held one end of the coupon rigid. An additional fixture was clamped to

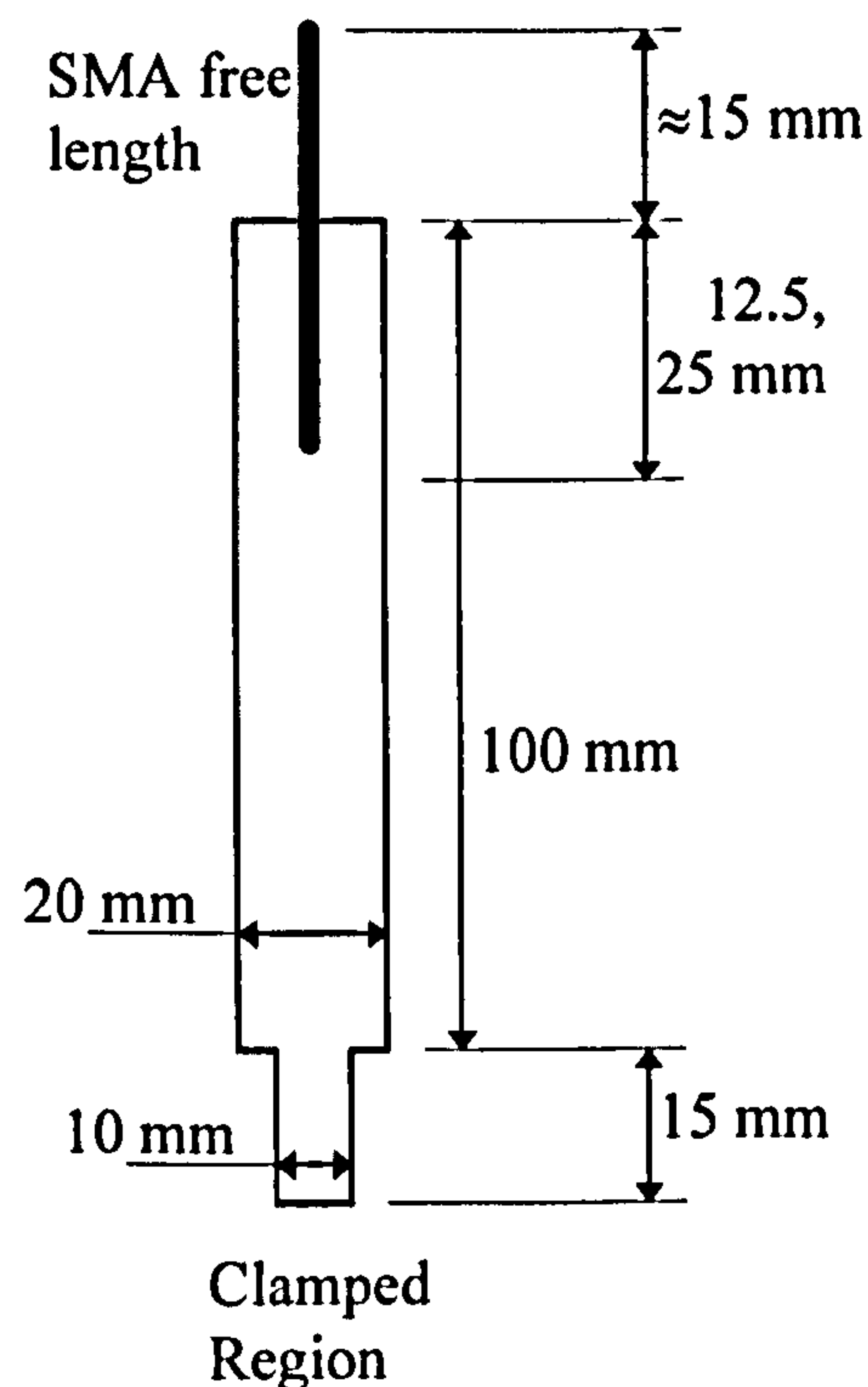


Figure 3.16. *SMA/Composite pull-out test coupons.*

the end of the free length of the egressed SMA wire. Approximately 15mm of free length of SMA material was allowed to egress the laminate. The free length is important only for determining the initial slope of the load versus displacement curve. It does not affect the force required to fracture the interface, therefore, it was not closely controlled. A schematic representation of the manufactured coupon for pull-out testing is shown in **Figure 3.16**.

A heat gun was initially employed to provide the required elevated temperature, though, as described in the next section, repeated experiments were conducted in an environmental chamber. Locally placed thermocouples ensured that the test proceeded at the correct temperature.

Each test, employing an Instron testing machine, involved the application of an increasing axial force to the free end of the SMA wire. Such a condition is met by applying a pre-defined cross head displacement to the free end of the SMA wire, while constraining the base of the coupon. Self aligning clamps ensured the accurate positioning of the specimen within the testing machine. Both load and displacement are monitored until complete pull-out occurs. Initial pull-out tests were conducted at cross-head displacement speeds of 0.5mm/s, though later tests were conducted at speeds of 0.1mm/s.

3.6.2 Test Results For Experimental Interface Qualification

Force versus time curves, illustrating the pull-out process for the lay-up configuration $[0_2/90_2]_s$, with an embedded length of 12.5mm and at the aforementioned temperatures, are presented in **Figures 3.17-20**. The alternative embedded specimen length and lay-up configuration exhibits similar behaviour.

The shape of each curve is characterized by three phases. A highly non-linear pre-fracture phase, *Phase 1*, begins with initial tensioning of the wire. With regards to nickel-titanium, the initial load-extension behaviour of the curve is dependent upon temperature. **Figures 3.17 & 3.18** display the initial SMA wire load-extension behaviour that is characteristic of martensite and martensite detwinning, **Section 2.4**. **Figures 3.19 & 3.20** display the load-extension behaviour that is characteristic of austenite. The recorded force builds up until fracture of the interface occurs.

Phase 2 is characterized by the peak in each trace where the tensile load in the SMA wire, and shear stresses at the interface, reach a critical value⁹¹. Beyond this point, the interface will completely fail, hence, a rapid drop in load is seen. The interface fails in a typical brittle process characteristic of the host. Complete interfacial debond has occurred followed by a short period of slippage. Such a debond initiates at the loaded SMA wire end and propagates along the SMA wire towards the embedded end. Shear stresses at the point of SMA wire entry initiate the debond crack. It should be pointed out that Poisson's effects on the SMA wire, due to the applied tensile force, results with a reduction of the true debond force⁹².

The latter prolonged post-fracture stage of the pull-out traces, identified as *phase 3*, depicts a gradually diminishing force that is required to overcome frictional resistance. Such friction is a result of any normal stresses that have arisen during the manufacturing stage and also, mechanical hindrance associated with the slippage between the two uneven surfaces. Again, Poisson's effects on the SMA due to the applied tensile load will produce a reduction of the fibre cross-sectional area relative to the matrix, resulting in a reduction of both interfacial frictional normal and shear stresses.

A low level of post-fracture pull-out force demonstrates that the mechanical friction on the actuator is not as important to the overall adhesion as the chemical/mechanical

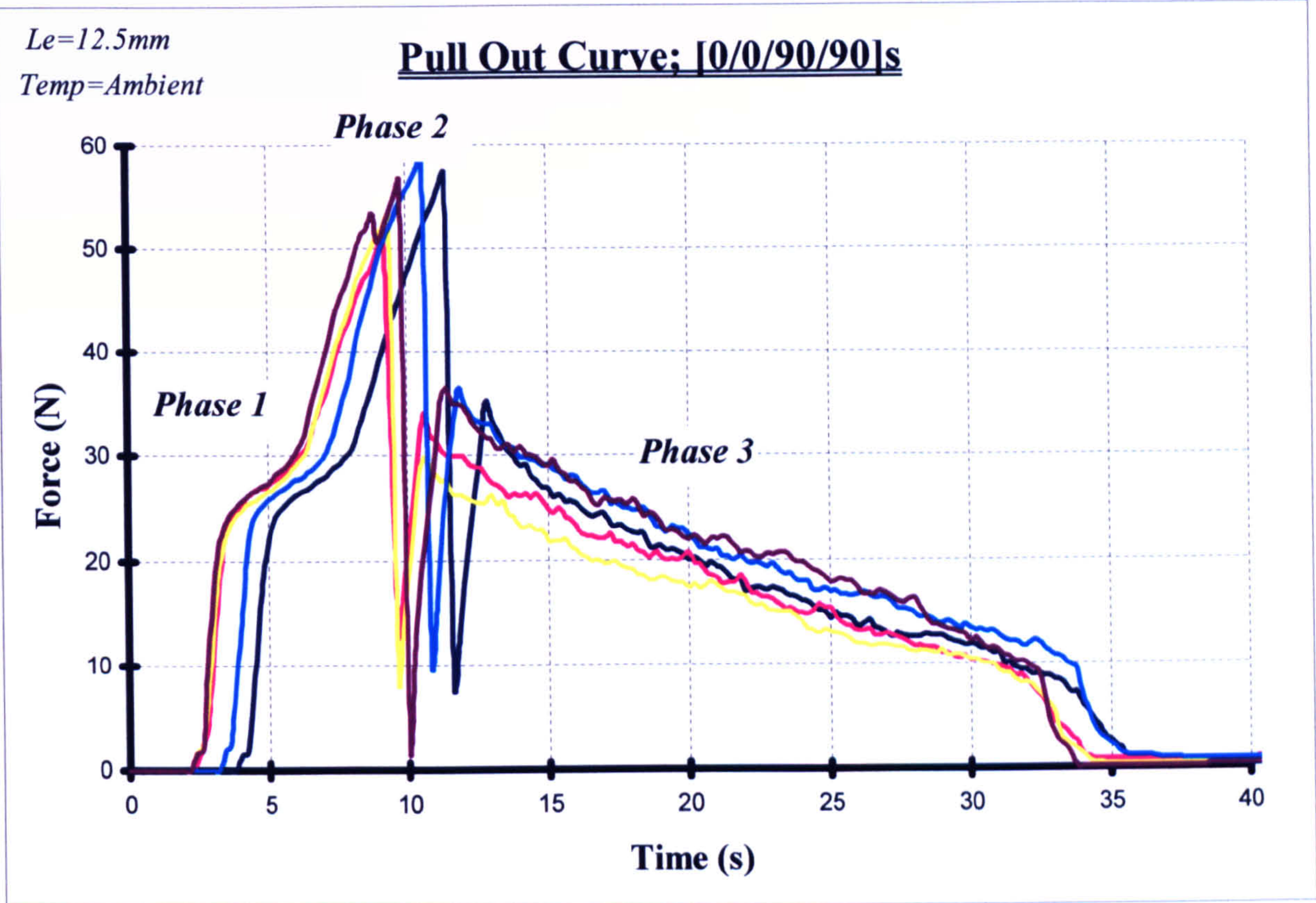


Figure 3.17. SMA pull-out behaviour within a $[0_2/90_2]_s$ lay-up at room temperature.

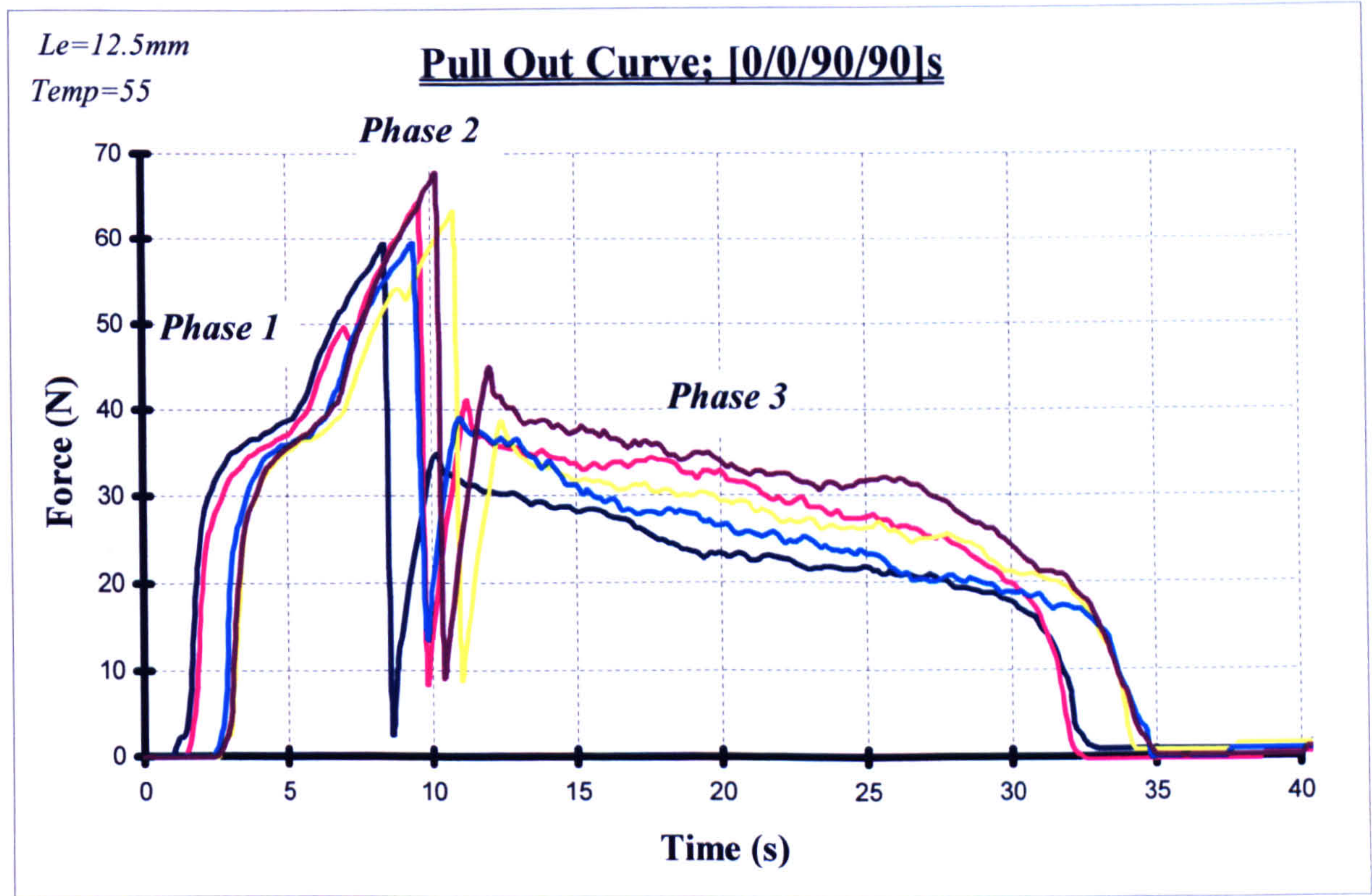


Figure 3.18. SMA pull-out behaviour within a $[0_2/90_2]_s$ lay-up at 55 °C.

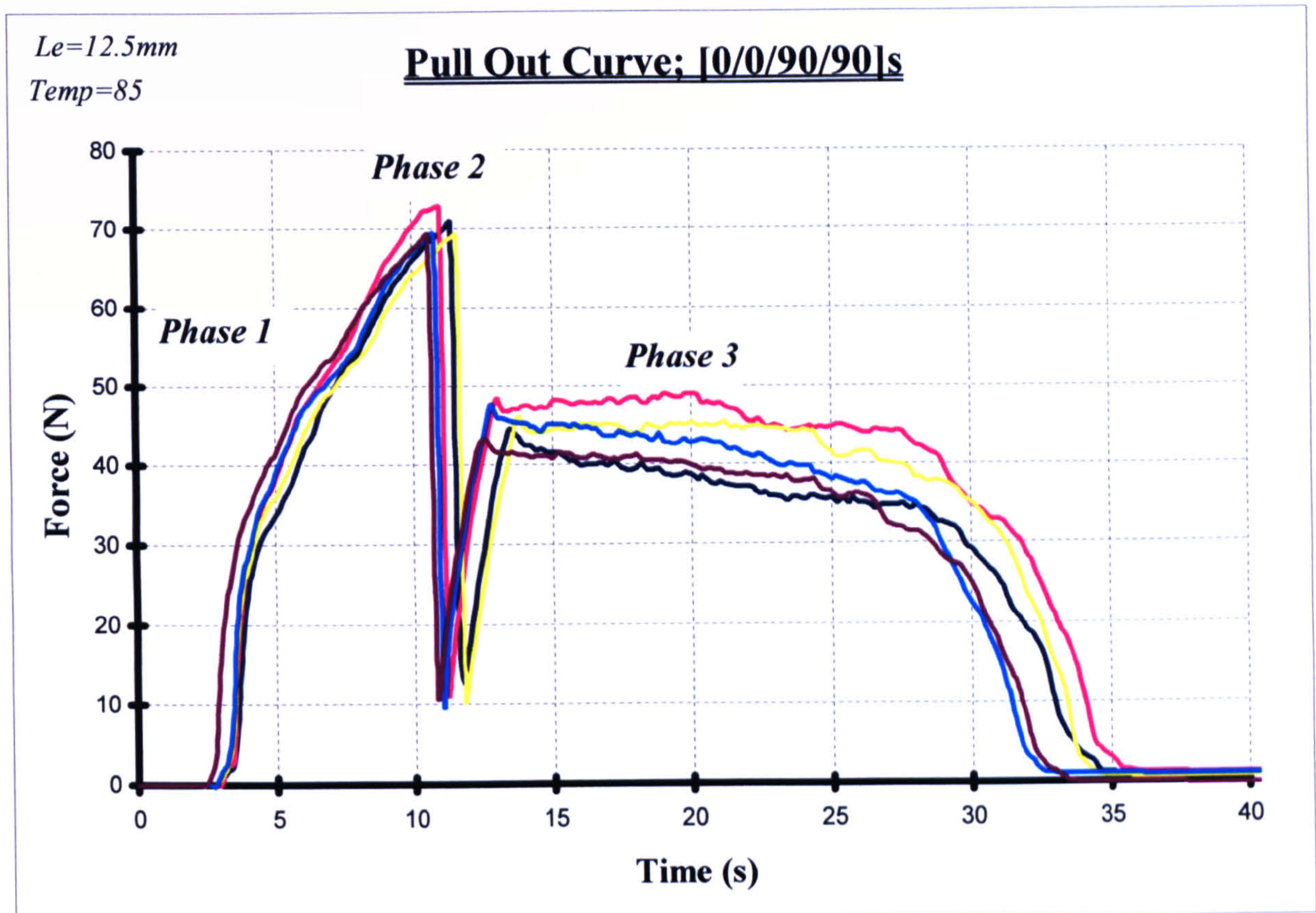


Figure 3.19. SMA pull-out behaviour within a $[0_2/90_2]_s$ lay-up at 85 °C.

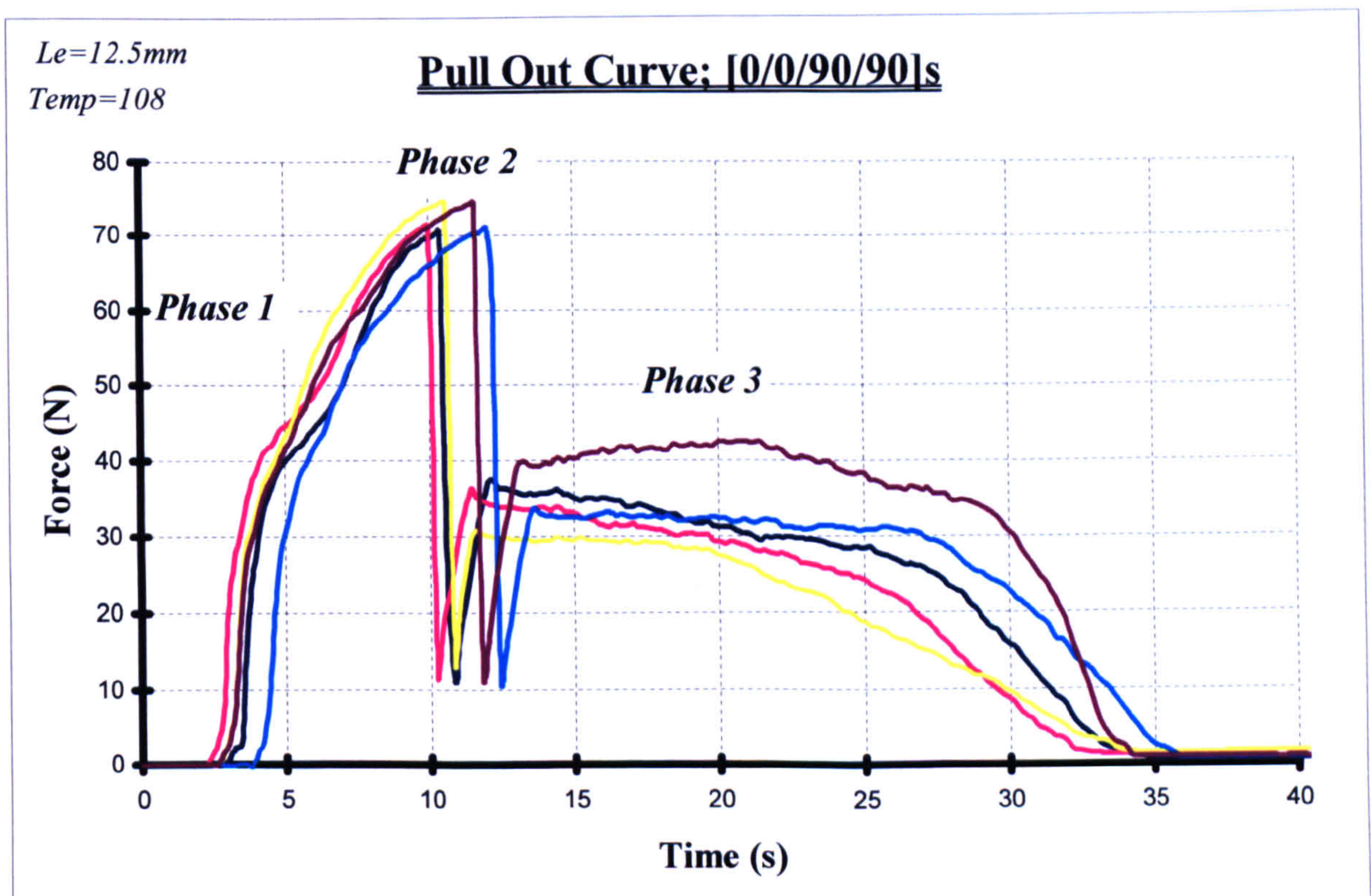


Figure 3.20. SMA pull-out behaviour within a $[0_2/90_2]_s$ lay-up at 108 °C.

cohesion/adhesion. Likewise, a high post-fracture pull-out force indicates an important contribution the mechanical interaction or friction has on the overall interlocking⁸².

For the initial tests that utilize a heat gun, the average peak fibre pull-out force required for complete debond, for each set of parameters, is presented in **Table 3.5**. The associated minimum and maximum values are also presented.

By inspection of the data presented in **Table 3.5**, we can see the trend of an increased pull-out force required when exposing the specimen to an elevated temperature. The trend is shown regardless of embedded length. Owing to the nature of the matrix properties at elevated temperature, this trend was not expected.

Temp. °C;		22			55			85			108		
Lay-up;	L _e ;	min	av	max	min	av	max	min	av	max	min	av	max
[0 ₂ /90 ₂] _s	12.5	51.1	55.4	58.6	59.1	62.6	67.6	69.1	70.2	72.7	70.8	72.5	74.5
	25	65.6	68.9	71.5	69.0	72.8	78.4	60.4	70.3^B	73.9	74.2	75.3	76.0
[0 ₂ /±45] _s	12.5	54.7	60.6	68.2	60.9	63.9	70.9	68.3	69.3	71.7	68.7	70.4	71.5
	25	65.2	70.3^A	73.4	71.7	73.3	75.4	71.4	73.9	76.4	72.2	74.3	76.7

Table 3.5. Peak SMA fibre pull-out force in Newtons (Heat gun results).

^A The average was taken over 4 values.

^B The average value is lower than expected owing to one pull-force equalling 60.4N. Examination of its pull-out trace revealed a shallow smooth frictional path. This may indicate an insufficiently abraded actuator periphery, and therefore, a lower pull-out force. Ignoring this value, the average was calculated to be 72.75 N.

These initial results were thought to be inaccurate owing to the method of heating and the nature of the holder. Additional tests were conducted at elevated temperatures using an Instron environmental chamber. For all the new tests conducted, after mounting the specimen and prior to the test, the chamber was allowed to equilibrate for a few minutes. The following results, for the [0₂/90₂]_s lay-up, **Table 3.6**, verify our initial observations.

Temp. °C	55			85			108		
chs mm/s	min	av	max	min	av	max	min	av	max
0.5	65.2	67.8	69.3	68.3	71.1	76.2	71.9	73.2	75.3
0.1	66.1	68.5	70.5	66.7	71.2	74.0	68.5	70.9	73.1

Table 3.6. Peak SMA fibre pull-out force in Newtons (Environmental chamber results).

Among the numerous parameters affecting the interfacial adhesion in a polymer matrix composite, the temperature plays a major role. Within the published literature, experimental work has been conducted towards understanding the effect of temperature on the interfacial behaviour⁹³. For a PMC, an elevated temperature is said to decrease the interfacial shear strength. This is shown to be true for a single filament of carbon, only, embedded in an epoxy cylinder⁹³. As will be shown, however, this is not always the case.

It is also reported that, as the ductility of an epoxy increases, a decrease in interfacial shear strength between a carbon fibre and the epoxy occurs⁹⁴. It is stated that as the matrix material becomes more compliant, then, the transfer of stress between it and the fibre is reduced. For host matrices with a high glass transition, a higher modulus would be manifested with better stress transfer across the interface, therefore, leading to an increase in the interfacial shear strength. For single filaments in matrices, materials with higher glass transition temperatures would, in general, also lead to higher thermal stresses in the system

which would cause shrinkage of the matrix around the fibre and consequently increase the interfacial shear strength.

The shear strength of an epoxy resin undoubtedly decreases with increase in temperature. Matrix shear strength, τ_m , degradation for an epoxy, in the temperature range 20-110°C, has been reported⁹⁵. The citation also reports the interesting finding that, for a single carbon fibre embedded in an epoxy matrix, the interfacial shear strength, τ_i , as measured by the pull out method, is relatively insensitive to temperature within the aforementioned limits. This citation states that the properties of the interface appear to be independent of the polymer. Their results are presented in **Table 3.7**.

Temperature °C	τ_m MPa	τ_i MPa
20	47±4	56±3
50	40±2	47±4
80	28±2	44±2
110	9±2	60±4

Table 3.7. *Shear properties of a matrix and interface at elevated temperature.*

3.6.3 Investigation on the Local Stress State For Control Strategy 2

Not wishing to contravene the norm for PMC interface behaviour at elevated temperatures, for the coupons tested, an interface strengthening mechanism is proposed.

During fabrication of a polymeric-matrix composite, the presence of internal residual stresses arise through three mechanisms; (i) on a micromechanical scale, upon cool down from the stress free cure temperature, they occur through differential thermal contraction and expansion of the constituent materials. The thermal expansion behaviour of graphite fibres is highly anisotropic, with negative CTE in the longitudinal direction, and positive CTE in the radial direction. The thermal expansion behaviour of the matrix is isotropic, with CTE's of epoxies typically between four and eight times higher than the transverse CTE's of graphite fibres. During cool down from the cure temperature, for a uni-directional lay-up, the difference between the negative longitudinal CTE of graphite fibres and the positive CTE of polymer resins, causes compressive thermal residual stresses to develop in the fibres, along their length, and tensile thermal residual stresses to develop in the resin.

The second mechanism, (ii), owes itself to matrix shrinkage during curing itself. Owing to the random fibre spacing, such stresses will either be tensile or compressive.

The third mechanism, (iii), is dependent on the laminate stacking sequence. On a macromechanical scale, upon cool down from the stress free cure temperature, stresses may result between the individual plies of the laminate. This is attributed to the individual ply's larger coefficient of thermal expansion in the transverse direction than in the axial direction. As an example of this, heating a cross-ply laminate will lead to the transverse expansion of each ply being strongly inhibited by the presence of the other ply.

The aforementioned residual stresses are a function of many parameters such as cure temperature, ply orientation, stacking sequence, constituents employed and constituent volume fraction. Residual stresses are also a function of the cure cool down path. An optimal cool down path minimising such stresses.

Residual stresses, arising from the manufacturing process, affect the tensile or compressive nature of the final stress state after loading and are extremely important for damage analysis. Due to the transition of the material from viscous to viscoplastic to viscoelastic, and finally, to the fully elastic state they are difficult to evaluate exactly. Approximate analysis, however, can be performed by assuming the lay-up is a fully load

bearing elastic material at temperatures below the glass transition temperature, **Chapter 5**. Such an approximation will lead to a conservative evaluation for the stressed state.

It is predominantly the third mechanism which increases the interface characteristics for the lay-ups chosen. To understand the state of stress in the laminates as a result of fabrication, classical lamination plate theory was employed.

The longitudinal, f_x , and transverse, f_y , residual stresses were computed for both the $[0_2/90_2]_s$ and $[0_2/\pm 45]_s$ lay-up configurations. The results are presented in **Table 3.7**. The results presented are the stresses in the outer zero-directional plies only. We are predominantly concerned with these outer ply stresses as these are the plies that host the embedded SMA wires. The latter running parallel to the carbon reinforcement, i.e. in the x-direction.

Clearly, as expected, for the tabulated outer ply stresses, we see a decrease in the thermal residual stress magnitude, in both directions, with increase in temperature. For both lay-ups, a significant transverse tensile residual stress exists at room temperature. For the $[0_2/90_2]_s$ lay-up, a compressive longitudinal residual stress exists at room temperature, whereas, for the $[0_2/\pm 45]_s$ lay-up, the longitudinal residual stress is tensile. While the compressive stresses will act in favour of adhesion, the low temperature tensile stresses are believed to be detrimental in nature. This is attributed to the following.

Application of a tensile load to the SMA wire results with a local tensile loading within the neighbouring host. Therefore, as any tensile thermal residual stress is of greatest magnitude during the 22°C tests, such stresses, when incorporated with those caused by the pull-out force, necessitate lower applied loads to fracture the interface. Alleviation of the tensile thermal residual stresses, during the higher temperature tests, necessitates higher pull-out forces to cause the same interface fracture.

The effect of thermal residual stresses on the fibre/matrix bond strength of graphite/polyimide composites has been published in the open literature⁶. A microdebonding test was used to investigate the interface characteristics for a cross-ply and unidirectional laminate. The results indicate that the measured interfacial bond strength of the unidirectional composite was 24% greater than that of the cross-ply. This increase is solely due to the thermal residual stresses present within the cross-ply. For the cross-ply laminate, the neighbouring cross-ply impose a transverse constraint upon each other when cooled below the stress free cure temperature. This constraint is such that transverse tensile stresses exist in all plies. It is this transverse tensile stress that promotes interface failure. This published result quantifies our own findings.

Continuing the discussion of the present results, it is expected that the interface shear strength would decrease sharply as the test temperature approaches the glass transition temperature of the constituent surrounding the SMA inclusion. Owing to the small deviation in pull-out force between the 85°C and 108°C test temperatures, it may be assumed that, with further increase in temperature, the interface quality may soon be on a significant downward trend.

Lay-up	Temperature °C	f_x N/mm ²	f_y N/mm ²
$[0_2/90_2]_s$	22	-22.18	22.18
	55	-14.71	14.71
	85	-7.92	7.92
	108	-2.72	2.72
$[0_2/\pm 45]_s$	22	9.43	17.63
	55	6.25	11.69
	85	3.34	6.26
	108	1.15	2.16

Table 3.7. *Stresses within plies 1 & 2 determined from CLPT.*

To generate any useful recovery forces from the embedded 0.3mm diameter actuators, **Figures 2.23 & 2.24**, they will have to be subjected to temperatures between 80°C & 100°C. From **Tables 3.5 & 3.6**, for these particular lay-ups, it may be conceivable to say that such an operating temperature provides an optimal interface strength.

Another interesting feature can be seen from the traces presented in **Figures 3.17-20**. For **Figures 3.17-19**, the force required to overcome the frictional effects increases with increase in temperature, though such a force decreases at the higher temperature of 108°C, **Figure 3.20**. Such a decrease, at this elevated temperature, is the likely cause of matrix softening. The initial increase, with increase in temperature, is probably the result of radial thermal expansion of the SMA material and the resulting radial compressive stresses actually clamping the wire. It may also result from the alleviation of any tensile stresses which occur, within the matrix, through resin shrinkage during the manufacture of the coupons. This clamping stress will also have an effect on the pull-out data.

Within this research programme, it is not expected that the constrained recovery force of an embedded abraded 0.3mm diameter SMA actuator will degrade the interface, though, this may only hold true for a limited number of operating cycles. Therefore, the quality of the interface needs to be assessed for a large number of operating cycles.

At the high operating temperatures proposed, viscoelastic effects around the interface region may become important. Future work should examine the importance of such viscoelasticity with respect to proposed operating time scales. Ultimately, a sacrifice in actuator recoverable strain may be necessary to assure that the wires remain well bonded to the matrix during the martensite→austenite transformation. Interfacial failure may not jeopardise the global structural integrity of the fabricated stiffened panels, from a strength or stiffness point of view, provided the volume fraction remains small.

3.7 Desired SMA Strain Recovery Within The Host Composite

For the SMA to regain its shape, it must overcome the constraints imposed by the surrounding PTFE, **Section 3.5.2**. Pull-out tests were used to assess the mechanical force required to breakdown this constraining interface. The lay-up configurations considered were $[0_2/\pm 45]_s$ and $[(\pm 45)_2]_s$. For the $[0_2/\pm 45]_s$ lay-up, the SMA wires, within the tubing, were placed between plies 1 & 2 and parallel to the neighbouring reinforcement. For the $[(\pm 45)_2]_s$ lay-up, the SMA wires were again placed between plies 1 & 2 but at an angle of 45° to the neighbouring reinforcement.

Eight pull-out tests were conducted for each lay-up. A schematic of the coupons tested was presented earlier, **Figure 3.16**. The length of the embedded inclusion is 100mm of PTFE and 100mm of 0.3mm diameter SMA wire located within the PTFE. A further 20mm of SMA was allowed to egress the composite enabling clamping to the test fixtures. It was not the intention to allow the SMA to adhere to the laminate other than the wires embedded end face.

Pull-out curves for the lay-ups $[0_2/\pm 45]_s$ & $[(\pm 45)_2]_s$ are shown in **Figures 3.21 & 3.22** respectively. The pull-out data was obtained for a cross-head displacement of 0.5mm/s. Four pull-out curves are shown for each lay-up, the results of these curves indicate consistency in behaviour. A significant variation in the required pull-out force is seen between the two different lay-ups. Each curve is characterised by an initial build-up of force as the actuator free length is strained, as previously described. The force increases until the SMA overcomes any constraining factors within the PTFE. The force drops as the SMA slips within the PTFE.

Figure 3.21 indicates that the peak pull-out force, required to overcome any mechanical hindrance, is approximately 25N. When placing the embedded inclusions at an angle to the ply reinforcement, this force rises to approximately 70N. The first cycle recovery force generated by a 0.3mm diameter alloy has a value approximately equal to 50N.

In theory, when placing the inclusion parallel to the neighbouring ply reinforcement, the frictional forces, or mechanical hindrance, should succumb to the shape memory recovery force. This will result with the required shape recovery and the transference of recovery stress at the desired location.

These results merely provide insight into the frictional behaviour of the SMA within the PTFE. They are not thought to be an exact representation of shape recovery/hindrance during SMA activation. During these tests, the SMA is placed under a state of mechanical tension. This tensile load will not only generate axial shear stresses within the tubing but also tensile radial stresses that act in favour of the pull-out. An additional force is required to fracture the mechanical interaction between the epoxy coated SMA and composite at the embedded end. Such a fracture is not required for the true manufactured specimens.

The tests were conducted at room temperature. A more realistic scenario would have been at elevated temperature. It is conceivable, however, that shape recovery will occur for advantageous lay-ups, **Figure 3.21**, and a strong possibility for constraint will be imposed by the disadvantageous lay-ups, **Figure 3.22**.

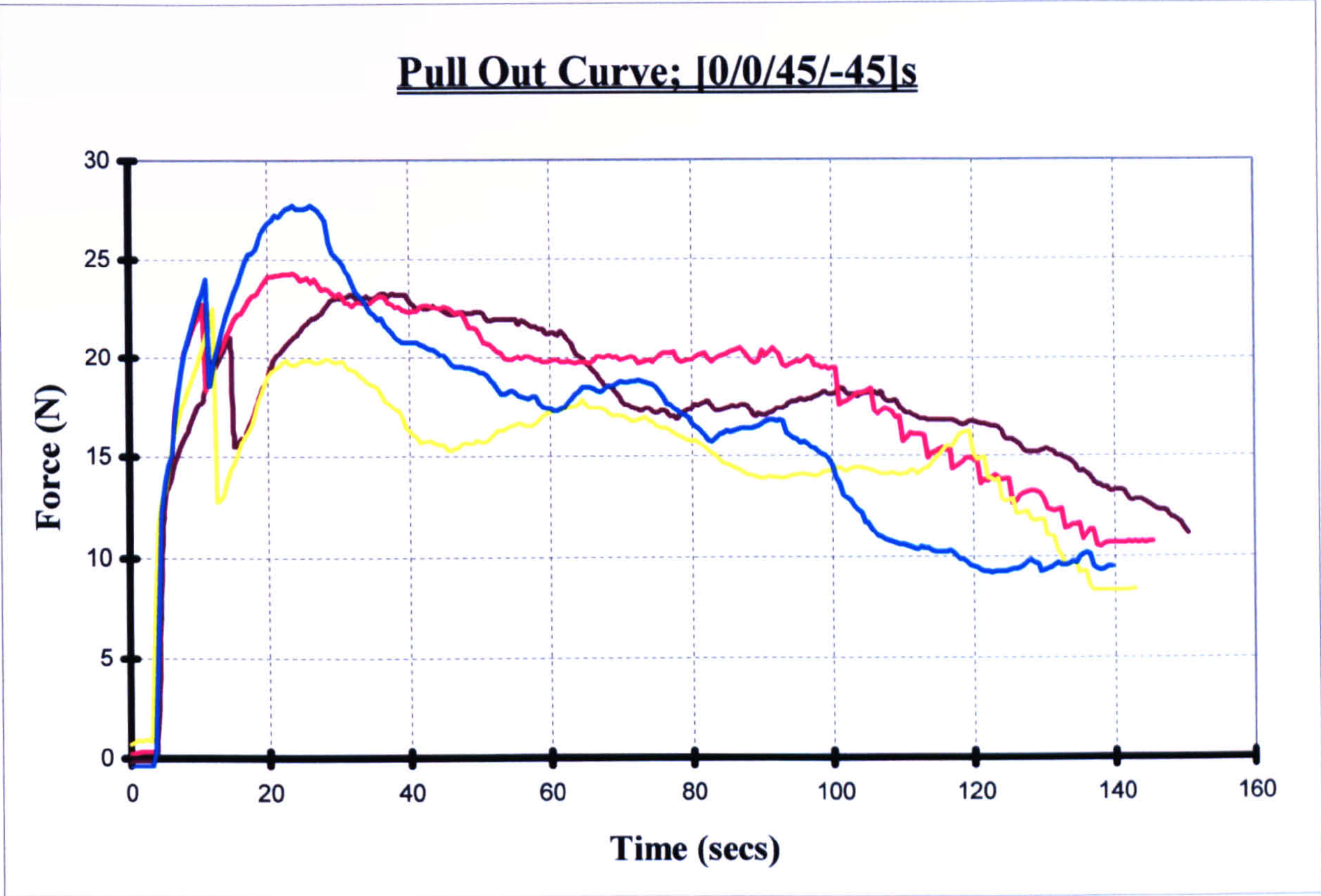


Figure 3.21. SMA/tubing pull-out behaviour within a $[0_2/\pm 45]_s$ lay-up at room temperature.

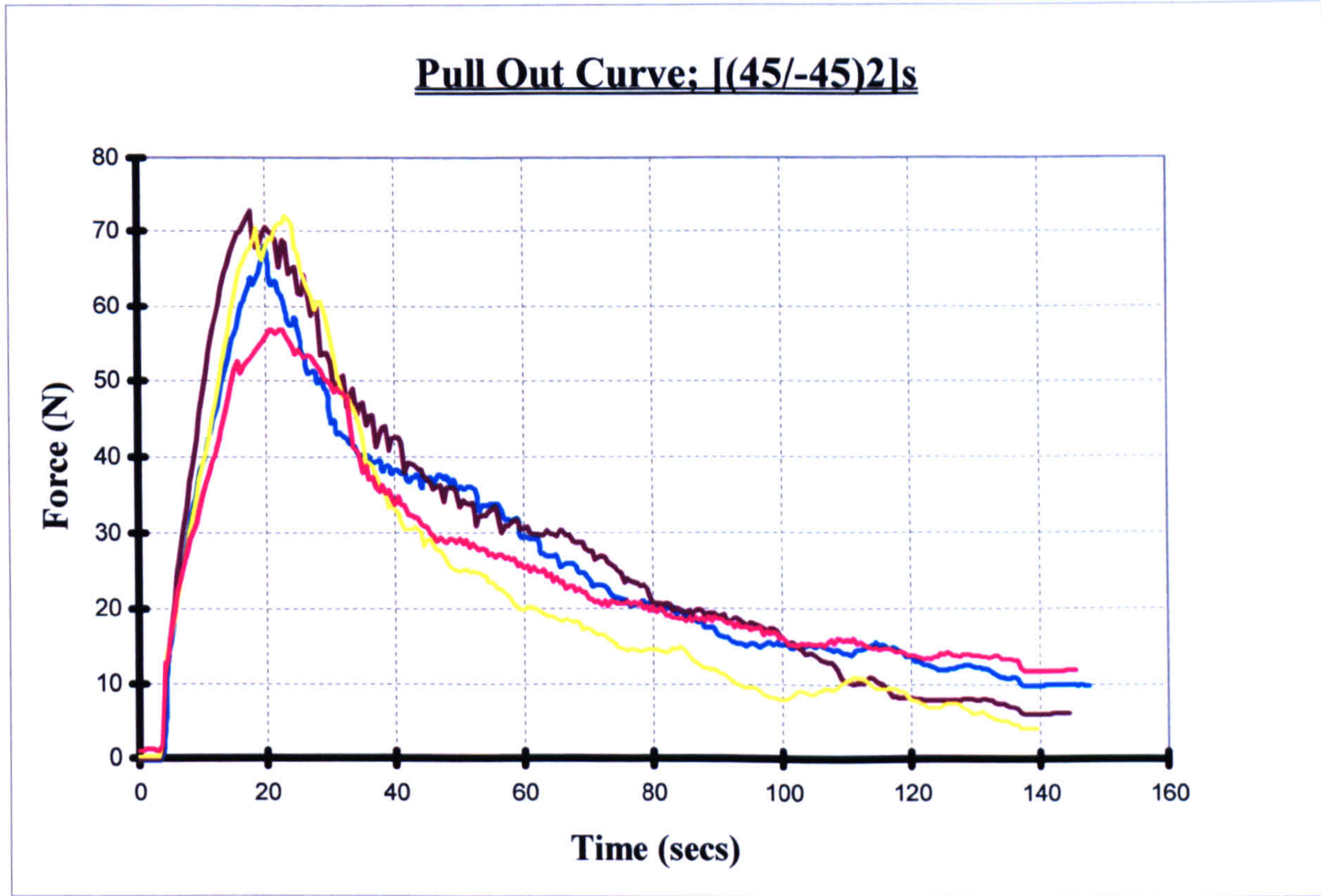


Figure 3.22. SMA/tubing pull-out behaviour within a $[(\pm 45)_2]_s$ lay-up at room temperature.

Chapter 4

Mechanical Buckling & Post-Buckling Modelling Issues

4.1 Introduction

This chapter is associated with the buckling and post-buckling behaviour of the symmetrically laminated orthotropic and anisotropic plates and panels selected for study in this research programme. Within this chapter, the main emphasis is on the mechanical modelling aspects. The study features the interaction between internal and external mechanical loads along with the superposition of a non-uniform temperature profile. The influence of the external mechanical loads is initially considered. The thermal aspects are presented in **Chapter 5** and extensive numerical and experimental adaptive post-buckling results are presented in **Chapter 6**.

Both unstiffened and stiffened polymeric-matrix composite plates are important structural components that are utilized in aerospace construction. They are frequently used in situations where they are subjected to in-plane loading. Examples of components that are of practical importance are wing skin structures, semimonocoque shells of aircraft fuselage sections, launch vehicle booster tankages and launch vehicle booster interstage structures.

Polymeric-matrix composite materials offer very attractive thermo-mechanical properties and, therefore, will play a significant role in the construction of future generations of aeronautical vehicles and space structures⁹⁷.

To realize weight savings, the aforementioned aerospace components are assembled from thin parts. As an example, a stiffened panel generally consists of a skin whose thickness is small compared to its length, width and stiffener spacing. Thin constructions, however, are susceptible to the buckling phenomena when subjected to compressive and/or shear loads⁹⁸. The buckling and post-buckling behaviour is, therefore, of great importance and has been studied by many investigators. No attempt is made to review the full range of work conducted in this area, however, the excellent buckling and post-buckling text⁹⁹ warrants citation.

For certain geometry's and boundary conditions, the post-buckling behaviour of a plate/panel section is stable. Utilization of stable post-buckling in design leads to more efficient material utilization. It is the intention to load the test specimens in this research into the post-buckling regime.

The constraints to be imposed are simply-supported conditions on all edges. Within the post-buckled regime, simply-supported plates typically fail as a consequence of excessive in-plane stresses that are located at the laterally supported edges, or, fail along nodal lines where interlaminar shear stresses are large^{100,101}. The major factor governing initiation of failure is the shifting of the load path towards the simply-supported edges. Load redistribution arises due to the plates out-of-plane deflection.

Two control strategies have been investigated that enable pre-strained embedded shape memory alloy wires to exert recovery forces that act to increase the specimens bending stiffness and remove the lateral deformation, typical within post-buckled laminated plates, while maintaining the influence of the external compressive load. Such strategies may be employed to improve the specimens anticipated life by alleviating critical stresses through the enforcing of a more uniform load path. Such load uniformity is characteristic of the pre-

buckled configuration. Post-buckling alleviation may also be advantageous for wing skin sections where buckling may not be permissible for aerodynamic performance reasons.

For each strategy, finite element modelling will be compared with experimental results. Finite element analysis was employed as no closed form analytical solution exists that will enable the accurate prediction of the specimens non-linear anisotropic response.

Thermal, linear buckling and structural non-linear numerical procedures were employed to predict the improved post-buckling behaviour of the SMA/carbon/epoxy laminated plate/panel structures. The FEA package MSC/NASTRAN ver 69.8-70.0, utilizing Solution Sequences 105 and 106, were used to perform the linear buckling and geometric non-linear analyses respectively. The non-linear capabilities of this version of NASTRAN being far superior to its predecessors¹⁰².

The linear buckling analysis, employing the Lanczos method, solves an eigensolution to predict the eigenvalues and eigenvectors, whereby, buckling loads and their respective mode shapes are obtained. The results of which are to be compared with classical buckling analysis and experiment. To predict the post-buckling behaviour, geometric non-linear analysis, using the modified Newton-Raphson method, was performed. The models were constructed using the pre- and post-processor P3/PATRAN.

A loading rig has been designed and manufactured that will enable the experimental investigation into the buckling and post-buckling behaviour of the composite plate specimens featuring the embedded SMA wire actuators. The details of this loading rig are to be presented.

4.2 The Instability Phenomenon

In a linear static analysis, a structural element, while under the influence of an applied load, is assumed to be in a state of stable equilibrium. When the applied load is removed, the element will return to its original position. For a variety of structural elements, however, there exists a load, or load combination, that causes the structural element to become unstable. Such instability manifests itself in the form of a large lateral deformation which, depending on the structures boundary conditions, may increase in magnitude without an increase in the applied load. The structural element is said to have buckled.

Two modes of buckling exist in an elastic element. They are *bifurcation point buckling* and *extreme point buckling*. This investigation is concerned with *bifurcation point buckling*.

Considering thin plate components, with reference to Figure 4.1, if the plates were perfectly flat, supported on all edges and subjected to an in-plane compressive load, P , out-of-plane deformation will commence when the applied load exceeds the bifurcation point⁹. An increase in the applied load above this critical value will cause a substantial increase in the plates out-of-plane central deflection. As the load is increased further, the deflection continues to grow, but the rate of growth continually reduces. This is due to stabilizing membrane stresses set-up within the central region of the plate as the deformation progresses. Obviously, out-of-plane deformations are predominant local to the centre of the plate, the edges being constrained from lateral movement.

Due to the presence of imperfections, i.e. an initial deviation from flatness, the out-of-plane deflection will grow from the start of loading and remove the sharp branching point from the perfect load-deflection curve⁹. The initial level of growth will depend on the imperfection magnitude. The imperfection, present within the unloaded state, may have arisen from the manufacturing stage or from a previous loading cycle. Typically, manufactured

deviations from flatness are small, being in the order of $1/10^{\text{th}}$ the plate thickness. Loading imperfections may also arise and are generally in the form of transverse loading or edge moments.

When loading the imperfect plate into the post-buckling regime, the deflection path will approach that of the perfect structures path. For the same level of applied load, however, the imperfect plates tend to slightly deflect out-of-plane more than their perfect counterparts¹⁰³.

Typically, for plate structures laterally supported along all edges, the post-buckling response is characterized by a reduction in stiffness without a loss of load carrying capability. An internal stress redistribution commences with post-buckling with the supported edges carrying the larger portion of the load.

The stability of laminated plates, i.e. the magnitude of P_c , depends on numerous parameters. Such parameters being; (i) the location of plies with different orientations, (ii) plate geometry, (iii) boundary conditions, and, of course, (iv) the constituent properties.

As will be shown, variation of the fibre orientation arrangement can greatly influence the magnitude of the critical buckling load, P_c . It is common knowledge that the buckling strength of a clamped plate is higher than that of a simply-supported plate. The boundary conditions also have an important influence on the post-buckling response, i.e. specimens simply-supported deflect more than plates with clamped supports. In addition, the support conditions can influence modal interaction at high load levels¹⁰³, more of which will be discussed later.

4.3 Specimen Configurations Under Investigation

The plate/panel specimens considered within this investigation are presented in **Table 4.1**. Configuration numbers 1-9 are the flat plate specimens. Configuration numbers 10 & 11 are the stiffened panel specimens, the stiffening member providing the benefit of added load carrying capability for a relatively small increase in weight. As seen, only symmetrically laminated specimens are considered. Theoretically, this ensures the elimination of membrane-bending coupling characteristic to unsymmetric lay-ups. All specimens are fabricated from the unidirectional carbon/epoxy prepreg previously introduced, **Section 3.3.1**.

As seen from the table, orthotropic, anisotropic and quasi-isotropic lay-ups have been considered. The lay-ups selected utilize four primary ply orientations common within aeronautical configurations. The fabricated specimens being representative of lightly loaded aircraft skin constructions.

The matrix constituent of the selected composite has highly viscoelastic effects at high levels of load, therefore, it is important to align a percentage of the reinforcement in the direction of any applied load. The lay-ups chosen ensure viscoelastic effects are minimal,

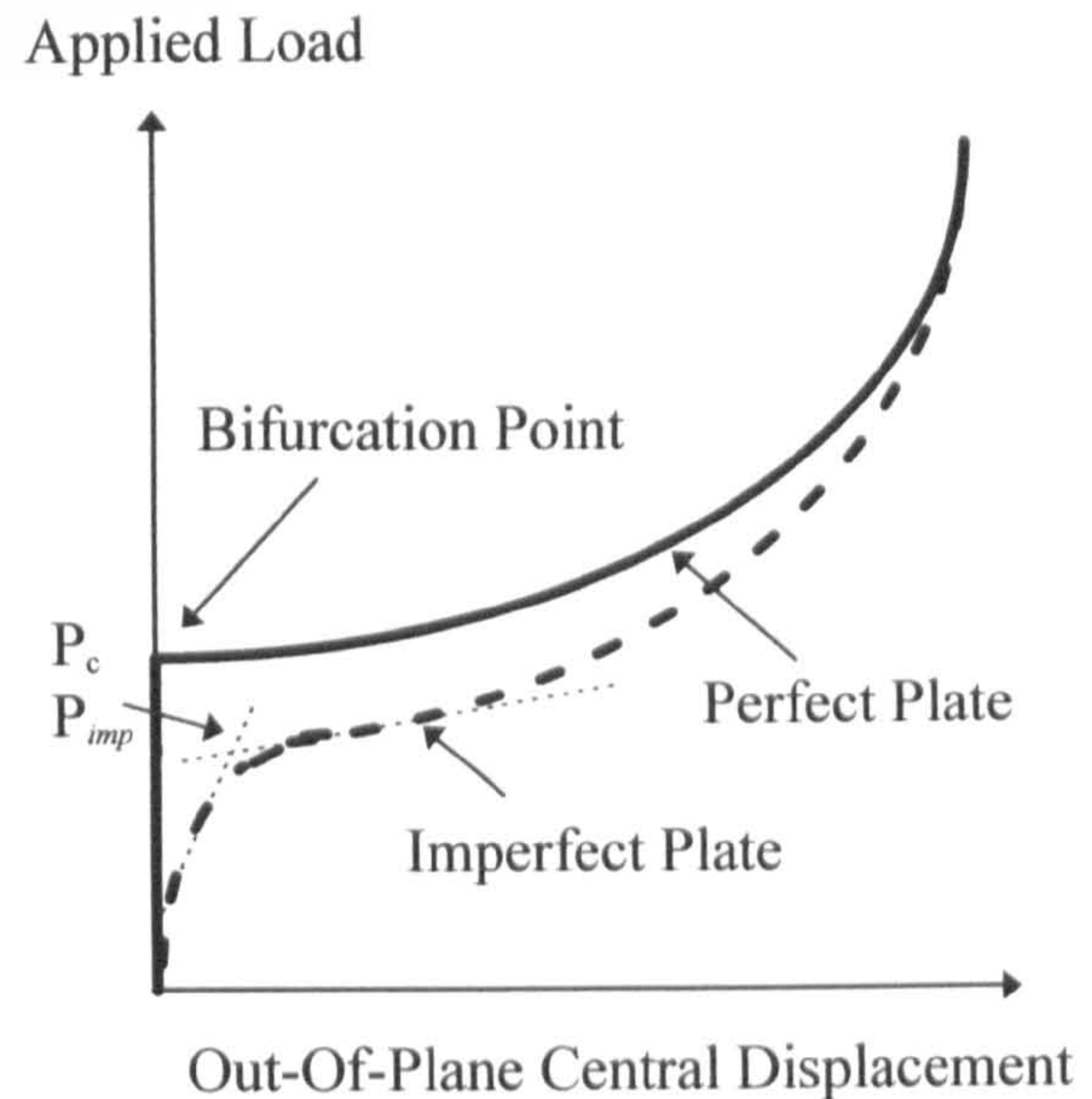


Figure 4.1. Schematic of plate behaviour under compression.

even under the influence of the thermal loads, **Chapter 5**, and, therefore, preserve the linear dependence of strain with stress.

All plate specimen dimensions are 300*310mm. The slight increase in width allows for suitable knife edge support within the testing rig. Likewise, the skin dimensions for the panel specimens are 300*310mm. The dimensions of the stiffeners are 12.5mm deep with a flange width of 10mm. A pre-specified thickness of 1mm was selected for all the components except the stiffener flange, the latter being 0.5mm. Two stiffeners are bonded to each skin section, **Figure 3.11**. They have a spacing of 200mm.

Post-buckling of stiffened panels has traditionally been studied in panels where the skin and stiffeners are uniformly loaded. Often, in aircraft design, the skin/stiffener assembly is such that the stiffeners need to be terminated, thus, avoiding interference with the adjacent structure. A common method of overcoming this problem is to taper the stiffeners height where necessary¹⁰⁴. Tapered stiffeners were employed for the panel specimens under consideration. The angle of the stiffener run-out was selected to be 20 degrees.

While this enables all specimen configurations to use the same loading rig boundary constraints, a consequence of selecting such a configuration is that only the skin can be directly loaded. Due to the eccentricity of the load from the components neutral plane, out-of-plane deformations will commence with the onset of a compressive load. More of which will be discussed in **Sections 4.5.1 & 4.10.2**.

The actual positioning of the SMA wires will be shown on the results figures presented in **Chapter 5**.

Config. No.	Lay-up Config.	No. Activated SMA Actuators	Manufactured Thickness (mm)
1	[0 ₂ /90 ₂] _s	10	1.10
2	[0 ₂ /±45] _s	10	1.10
3	[0/±45/90] _s	12	1.12
4	[0 ₂ /90 ₂] _s	12	1.08
5	[0 ₂ /±45] _s	12	1.08
6	[0/±45/90] _s	14	1.08
7	[0 ₂ /90 ₂] _s	16	1.05
8	[0 ₂ /90 ₂] _s	18	1.08
9	[0/±45/90] _s	17	1.10
10	[0 ₂ /90 ₂] _s	23	1.10
11	[0 ₂ /±45] _s	23	1.10

Table 4.1. Specimen configurations under consideration.

4.4 The Specimen Loading Rig

A loading rig was designed and manufactured, that will allow the application of a distributed in-plane uniaxial compressive load, or enforce a uniform edge displacement, to the plate/panel specimens. An overall view of the plate/panel loading rig is shown in **Figure 4.2**.

The rig has been designed to allow for variation in plate/panel dimensions. The rig can accommodate plate/panel specimens with maximum dimensions of 400mm in width and 500mm in length. However, due to time constraints, only the specimens having dimensions of 300*310mm were tested.

The loading rig supports allow for simply-supported conditions on all edges, though this can easily be modified to allow for fixed boundary conditions, should such conditions be desired. It should be pointed out that simply-supported boundary conditions are quite difficult to realize in practice.

The main components of the loading rig are; (1) Base Plate [1-off], (2) Vertical Support Equal Angles [4-off], (3) Vertical Knife Edges & Supports [4-off], (4) Vertical Knife Edge Support Plates [2-off], (5) Loading Plate [1-off], (6) Linear Ball Bushes [4-off], (7) Loading Plate Guides [4-off], (8) Stiffening Rods [2-off] and (9) Loaded Edge Supports [2-off].

Four structural steel vertical support equal angles are secured to an aluminium base plate at each corner. To give rigidity to the rig, two structural steel rods are attached to the vertical equal angles along the length of the rig.

The unloaded edges of the plate/panel specimens are to be supported by aluminium knife edges. Provision of the un-loaded knife edge supports prevent the specimens from buckling like a wide column.

Knife edge supports are fixed to aluminium supporting plates which, in turn, are fixed to a pair of vertical equal angles. The knife edge positions can be adjusted horizontally if required. This

adjustment capability allows the testing of plate/panel specimens with varied thickness. At the point of contact with the plate/panel specimens, the knife edges were rounded with a radius of 0.2mm. Knife edge supports prevent out-of-plane and allow for in-plane translations. They also allow the plate/panel to rotate about an axis parallel to the knife edges.

The loaded edges of the specimens are to be placed within locating grooves along the aluminium loaded edge supports, **Figure 4.3**, which are bolted to the base plate and to the

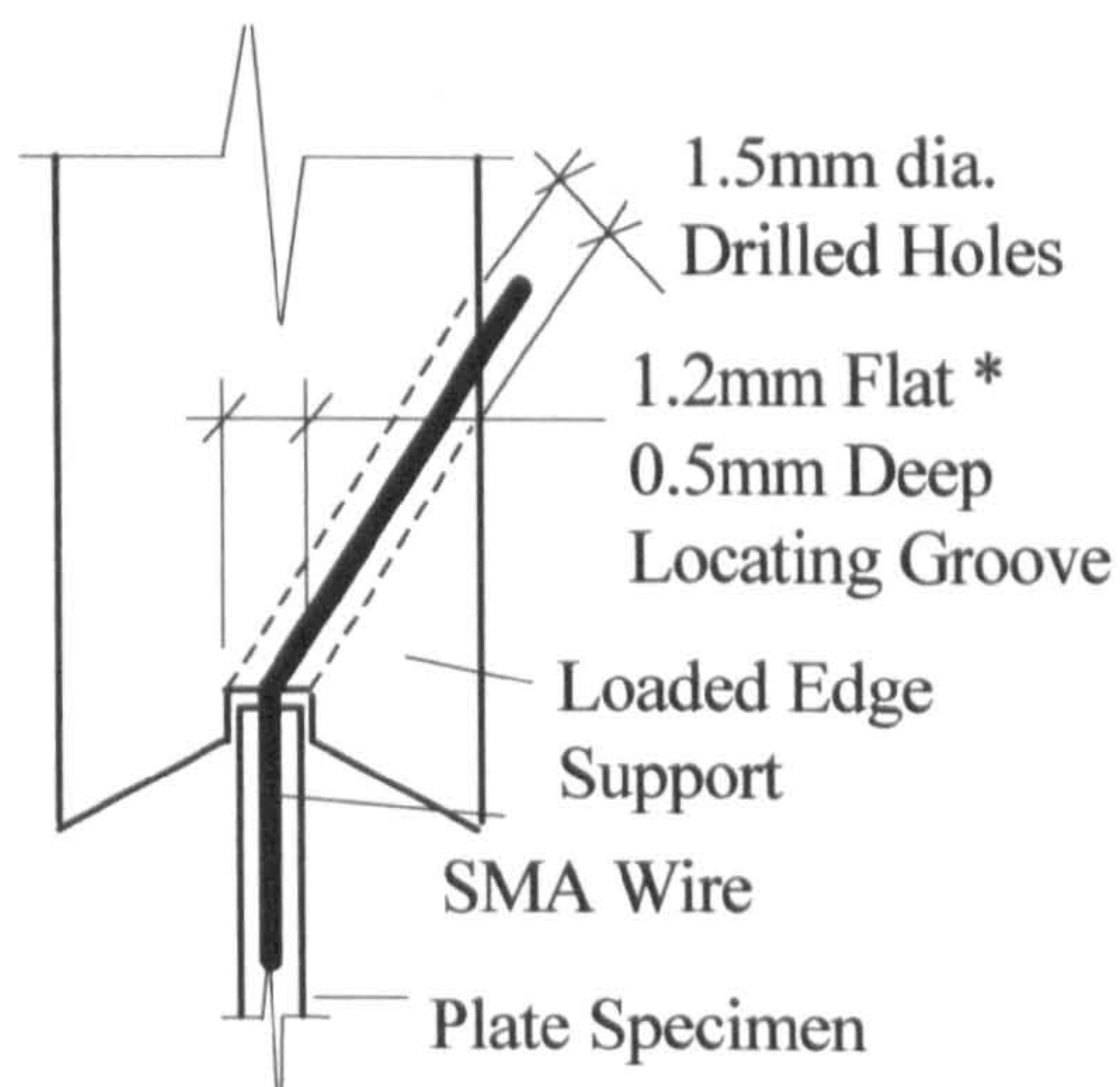


Figure 4.3. *Exaggerated schematic of a loaded edge support.*

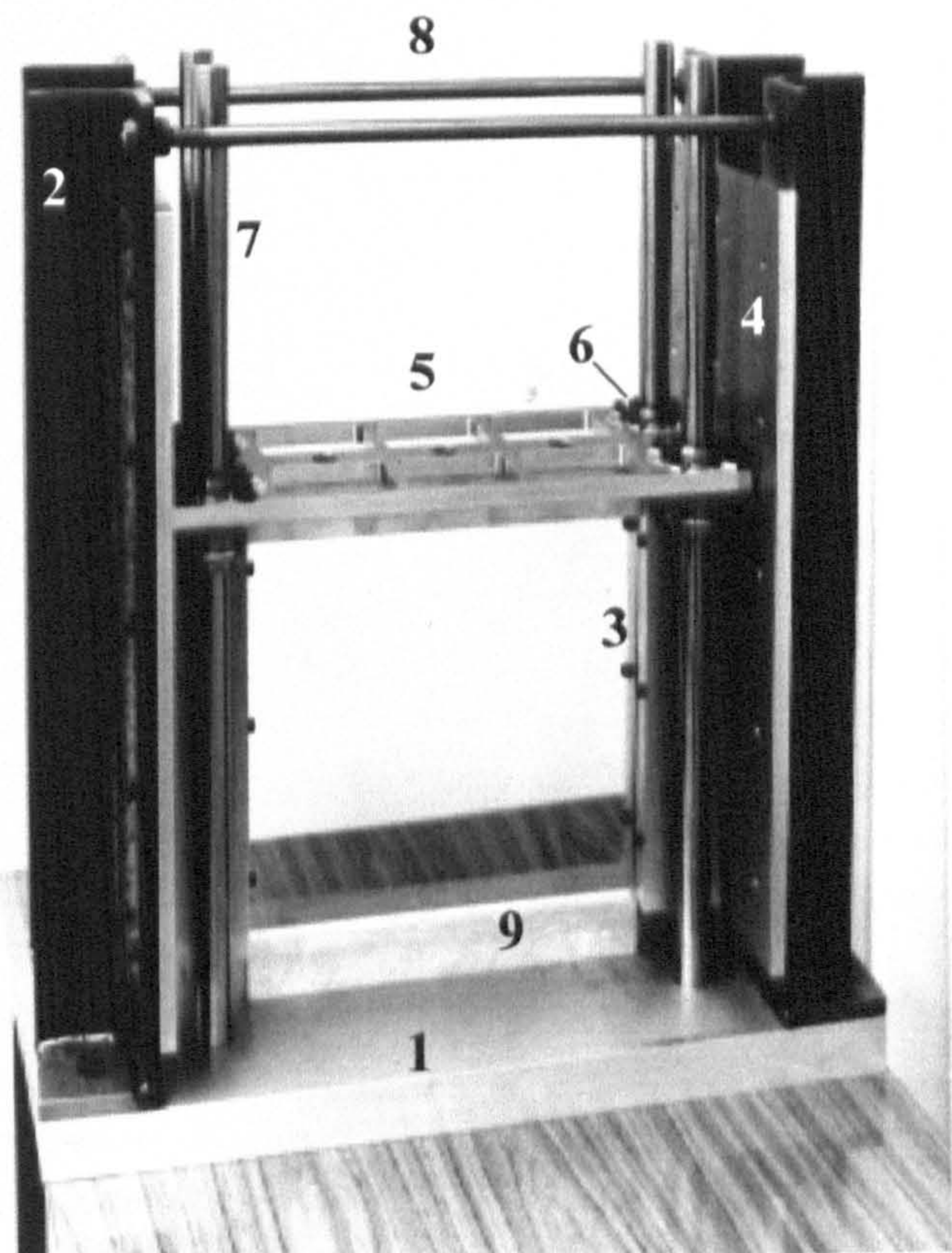


Figure 4.2. *Loading rig.*

underside of the loading plate. These supports prevent out-of-plane and allow for in-plane translations. They will also allow the specimen edge to rotate about an axis parallel to the loaded edge. The locating grooves are 0.5mm deep, which allow for precise vertical alignment, and are 1.2mm in width, therefore, allowing a tolerance of 0.08-0.15mm for the composite specimens manufacture.

The loaded edge supports have holes, 1.5mm in diameter, drilled every 12.5mm at an angle of 30 degrees from the vertical. For the case where embedded SMA wires are located parallel to the loading direction, i.e. *control strategy 2*, these holes allow for egress of the wires from the composite, through the loaded edge supports and, thus, enable connection to the necessary control system.

The aluminium loading plate transfers the applied load, or enforces the applied displacement, to the specimens. Four loading plate guides, in the form of 25mm diameter case hardened steel pillars, located at each corner of the loading plate, ensure the correct alignment of the longitudinal compressive load. The guides pass through linear ball bushes mounted within the moveable loading plate, therefore, enabling frictionless motion in the vertical direction.

4.5 Linear Buckling Analysis

Within the finite element non-linear analysis for specimen configurations 1-9, in order to promote the initiation of the post-buckled deflection behaviour, an initial geometric imperfection must be applied to the model. Such a geometric imperfection will approximate the first linear buckling mode. The geometric imperfection is in the form of a 0.01N force applied laterally to the specimens at their geometrical centre. For any of the specimens, this will result with an initial peak lateral deformation of less than 1% of the total laminate thickness. The resulting effect allows efficient solution progress past the first bifurcation buckling point, while not noticeably affecting the results in the post-buckling range¹⁰⁵.

As will be stated, Section 4.6.1, the non-linear analysis requires a sequence of loading steps for the attainment of the pre- & post-buckling response. In regions of exceptional non-linear behaviour, the loading intervals require sufficient refinement. This is true in close proximity to the theoretical bifurcation point. In order to attain this value and, therefore, specify a sufficient load case set for each non-linear analysis, linear buckling analysis was first performed.

4.5.1 Finite Element Buckling Analysis

A linear buckling finite element analysis, MSC/NASTRAN Solution Sequence 105, has been employed for the determination of the first two critical buckling loads and their associated mode shapes for each configuration. The buckling calculations were performed so that accurate modelling detail, required to predict the post-buckling response, is obtained. Such finite element analysis accounts for the anisotropic nature, i.e. the bend-twist coupling effects, of the $[0_2/\pm 45]$, and $[0/\pm 45/90]$, lay-ups. Solutions obtained by neglecting the anisotropic terms, i.e. the specially orthotropic solution, will overestimate the stiffness of the structure and, consequently, overestimate the buckling loads, Section 4.9.

NASTRAN FE critical buckling loads are determined from;

$$P_{ci} = \lambda_i * P \quad (4.1)$$

where P is an arbitrarily applied load, P_{ci} is the i 'th critical buckling load and λ_i is the i 'th eigenvalue, or non-dimensional buckling load. λ_i is determined from;

$$|K_e + \lambda_i K_d| = 0 \quad (4.2)$$

Within the NASTRAN finite element linear buckling analysis, the finite element stiffness matrix consists of two components, the conventional elastic stiffness and the differential or

geometric stiffness, the latter accounting for element geometry and pre-loads. In equation (4.2), the elastic and differential stiffness matrices are denoted as K_e and K_d respectively. Equation (4.2) is in the form of an eigenvalue problem. It is derived from consideration of the minimum potential energy of the structure¹⁰⁶.

Numerous eigenvalue extraction methods may be employed for the determination of equation (4.2), the method chosen within this investigation is the Lanczos method. Owing to its efficiency and accuracy, when compared to the alternatives, it is the recommended method by the software manufacturer and was seen to be the most popular choice within the published literature^{98,107-109}.

As with the classical plate buckling solution, Section 4.5.2, eigenvalue buckling assumes that the structure is perfect. In reality, however, there are always imperfections. The real structure with imperfections does not display a bifurcation point. Pertaining to the load versus out-of-plane deflection response, Figure 4.1, the sharp branching point is replaced by a gradual curvature. The nature of this curvature is dependent on the of the plates imperfections. Within this study, for imperfect plates, buckling is denoted by P_{imp} . Beyond P_{imp} , the gradient of the out-of-plane deflection becomes excessive, much like that of their perfect counterpart. This linear buckling analysis ignores the effect of structural imperfections.

For the skin loaded stiffened panels of this investigation, bifurcation buckling does not occur. Linear buckling analysis techniques, therefore, are not truly representative of this type of construction. The out-of-plane deflection will steadily increase from the start of loading due to the existence of a moment associated with the offset neutral plane. The slope of the out-of-plane deflection versus load is initially steep and then shallows out beyond a certain load, much like the aforementioned imperfect plates, Figure 4.1. Pseudo-buckling has occurred. Linear buckling analysis has been employed in an attempt to predict the pseudo-buckling of our stiffened plates, thus, aiding the load case refinement within the non-linear analysis.

4.5.2 Classical Buckling Analysis

Validation of the numerically predicted buckling values, associated with configurations 1-9, can be realized with classical buckling analysis. For thin symmetrically laminated anisotropic composite plates made of an elastic material and behaving kinematically according to the Kirchhoff hypothesis, then, the stability of thin rectangular anisotropic plates is governed by the following differential equation^{7,99,106,110,111};

$$D_{11} \frac{\partial^4 w}{\partial x^4} + 2(D_{12} + 2D_{33}) \frac{\partial^4 w}{\partial x^2 \partial y^2} + D_{22} \frac{\partial^4 w}{\partial y^4} + 4D_{13} \frac{\partial^4 w}{\partial x^3 \partial y} + 4D_{23} \frac{\partial^4 w}{\partial x \partial y^3} = N_x \frac{\partial^2 w}{\partial x^2} + N_y \frac{\partial^2 w}{\partial y^2} + 2N_{xy} \frac{\partial^2 w}{\partial x \partial y} \quad (4.3)$$

where w denotes the out-of-plane deflection, N_x , N_y and N_{xy} are the in-plane distributed forces and D_{ij} are the bending-twisting stiffness terms. This equation is for a symmetric laminate, where the membrane-bending stiffness terms, B_{ij} , equal zero.

For an orthotropic laminated composite plate, then, $D_{13}=D_{23}=0$, therefore, equation (4.3) reduces to;

$$D_{11}\frac{\partial^4 w}{\partial x^4} + 2(D_{12} + 2D_{33})\frac{\partial^4 w}{\partial x^2 \partial y^2} + D_{22}\frac{\partial^4 w}{\partial y^4} = N_x\frac{\partial^2 w}{\partial x^2} + N_y\frac{\partial^2 w}{\partial y^2} + 2N_{xy}\frac{\partial^2 w}{\partial x \partial y} \quad (4.4)$$

For a simply-supported laminated orthotropic plate subjected to an in-plane normal load, an exact solution for equation (4.4) can be obtained. The exact closed form solution is obtained by employing the following deflection function to describe the buckled mode shape;

$$w(x,y) = w_{mn}\sin\frac{m\pi x}{a}\sin\frac{n\pi y}{b} \quad (4.5)$$

The mode shape for a particular buckling mode is described by the subscripts m and n where m is the number of half waves in the specimens x -direction and n is the number of half waves in the specimens y -direction. Substitution of equation (4.5) into (4.4) when $N_y=N_{xy}=0$ leads to;

$$w_{mn}\pi^2[(D_{11}m^4+2(D_{12}+2D_{66})(mnR)^2+D_{22}(nR)^4)] = w_{mn}N_a^2m^2 \quad (4.6)$$

where $R=a/b$. a and b being dimensions in the x and y directions respectively. For a non-trivial solution, the critical buckling load is determined from;

$$N_c = \frac{\pi^2}{a^2 m^2} [(D_{11}m^4+2(D_{12}+2D_{66})(mnR)^2+D_{22}(nR)^4)] \quad (4.7)$$

Exact solutions for composite plate buckling problems are available only for a few cases of edge conditions. For unsymmetric or anisotropic laminates, clamped boundary conditions or complex loading scenarios, we resort to numerical approximations, i.e, FEA, FDM, Finite Strip or Rayleigh-Ritz.

4.6 Non-Linear Static Analysis

When the in-plane compressive load on the laminated specimens reaches the associated critical loading condition, while being able to support this load, the specimens will deflect into a shape having finite amplitude. This post-buckling configuration will correspond to another equilibrium state. The specimens will be able to sustain larger loads while suffering larger out-of-plane displacements¹¹⁰. The resulting in-plane load versus lateral displacement curve is non-linear and, for modelling purposes, requires a non-linear, large displacement type analysis.

The capabilities of the finite element code MSC/NASTRAN in predicting the post-buckling behaviour of the unstiffened and blade stiffened laminated composite panels considered in this study have been investigated. Finite element representations of the laminated constructions are presented in Sections 4.7 & 4.8.

4.6.1 Finite Element Non-Linear Analysis

Within the loaded specimens, prior to buckling, there is an almost linear relation between applied load and the resulting lateral deflection. This is true until the peak central deflection is approximately half the specimens thickness. For the thin plates examined in this research, this amount of lateral deflection is achieved with modest levels of applied load. To accurately model the large central deflection behaviour of the plate specimens, elements are employed which account for both membrane and bending strains within the non-linear analysis procedure.

MSC/NASTRAN, solution sequence 106, was used to investigate the structural response. The equation solver employed was the modified Newton-Raphson method. A description of the non-linear algorithms can be found in the software manufacturers text¹¹². A summary of the numerical analysis is as follows.

The non-linear formulation accounts for the actual stiffening of the specimens as they deflect laterally. The procedure also allows for follower forces within the SMA actuators and allows for the opening or closure of gaps that may arise owing to the activation strategy chosen and the boundary conditions employed, none of which can be accounted for in a linear static analysis.

Within Solution Sequence 106, there is the option of choosing between three non-linear iteration methods; AUTO, SEMI, and ITER. In addition, a number of convergence acceleration techniques, i.e modified Newton-Raphson, line search or bisection, exist to enhance the non-linear solution convergence.

The AUTO method is a good starting point and is the default method in a non-linear static solution. This method examines the solution convergence rate and uses one of the aforementioned acceleration techniques to calculate the solution as efficiently as possible. Highly non-linear behaviour may not be handled effectively using the AUTO method.

The SEMI method is similar to the AUTO method though the stiffness matrix is continually updated after each iteration. Unlike the AUTO method, the stiffness matrix update is performed irrespective of the convergence status of the solution. It is effective in many highly non-linear problems where regular stiffness matrix updates help the solution to converge.

The ITER method is similar to the SEMI method, however, the stiffness matrix is updated after every, to be specified, KSTEP iterations.

A disadvantage of the latter two methods is the resulting increase in the cpu time. After an initial extensive review of each method, the author found the AUTO method to be quite adequate in predicting the adaptive non-linear behaviour of the specimens while minimizing the analysis run time.

The AUTO method, with a modified Newton-Raphson iteration procedure, determines the solution of the non-linear equilibrium equations at, to be defined, increments along the structural loading history. Within MSC/NASTRAN, these load increments are referred to as subcases. The loading history is divided into arbitrary subcases as this provides the user with control during the solution. A high concentration of subcases is required in the vicinity close to points of significant behavioural change, i.e. the theoretical bifurcation point of the plate specimens and the pseudo-buckling point of the panel specimens.

Between each successive subcase, the total change of loading applied is further subdivided into smaller parts to allow the solution to converge. Subdivisions within a subcase are termed loading increments. Too large an increment can prevent the solution from converging. This is because the equilibrium state being sought is too far from the initial state.

Should the solution start to diverge between selected increments, the automatic stiffness update method, AUTO, will automatically reiterate with smaller increment¹³.

The mechanical loading stage, up to approximately $3P_c$, was divided into 18 subcases. The division between each successive subcase was approximately equal except in the vicinity of the predetermined buckling load. Here, as required, a higher concentration of subcases were employed. Additional subcases were employed to represent the SMA activation loads and the resulting projected temperature distributions at appropriate time intervals. 10 loading increments were employed between each subcase. For this analysis, the selected subcase intervals and loading increments proved sufficient, in terms of numerical convergence.

Numerically determined deformation and stress profiles were output for post-processing at the end of each converged subcase.

4.7 Finite Element Representation of Control Strategy 1

The purpose of this section is to provide details of the finite element representation employed to predict the buckling, post-buckling and improved post-buckling behaviour of the carbon/epoxy laminated plate structures having arbitrary stacking sequences. The improvement being attributed to the actuation of optimal sets of shape memory alloy actuators. The specimens feature the SMA actuators pre-strained 6% at the desired location, thereby, upon activation, wishing to contain a particular mode shape at high load levels. For this actuation strategy, the SMA actuators are always located on the laminate mid-plane and oriented perpendicular to the loading direction.

The finite element representation discussed here is applicable to the plate specimens of *control strategy 1*. Section 4.8 gives an account of the finite element representation employed for the panel specimens of *control strategy 2*.

4.7.1 General Specimen Representation

One geometrical plate configuration has been considered, its dimensions being 300*310mm. The slight overhang of the unloaded edge must be accounted for in the analysis. The laminated plates were modelled using two-dimensional CQUAD4 shell elements. As with the analysis presented in Section 3.4.1, the carbon/epoxy lay-up was represented by the PCOMP and MAT8 data entries. The whole specimen of each configuration was analysed as certain lay-ups are anisotropic. Symmetry could be exploited for the orthotropic lay-ups, though, this would require an additional geometric finite element construction.

The use of the two-dimensional elements is warranted as, owing to the high ratio of the in-plane plate dimensions to specimen thickness, through thickness transverse shear deformation is of no concern. The specimens could be modelled using solid elements. Such analysis, however, would require many thousands of elements and, particularly for the non-linear analysis, providing ill conditioning does not prevail, would require many days of computing time to solve.

4.7.2 Boundary Conditions

The importance of the boundary conditions in determining the numerical structural response is paramount. The numerical representation and the test article must represent the same support conditions.

Figure 4.4 schematically illustrates the applied boundary conditions. The boundary conditions applied to each edge of the plate specimen representation simulate simply-supported conditions. Knife edge supports were represented by single point constraints, SPC's. The SPC's enforced fixed lateral constraints only and were located 5mm from each of the two opposing unloaded edges. The specimens loaded edges were also constrained laterally with SPC's. With these boundary conditions, should any of the coincident nodes wish to rotate about an axis parallel to their adjacent edge, they are free to do so.

As will be shown in **Chapter 5**, a non-uniform thermal displacement profile will exist within the fabricated specimens. This will result with thermally induced non-uniform edge deformation, particularly along the unloaded edges and, to a much lesser extent, along the loaded edges. For a +ve CTE, the specimens loaded edges will push against the loading platens. For a -ve CTE the laminate may contract to an extent that gaps form.

To allow for this behaviour within the analysis, the loading platens were represented and connected to the plate specimens with one-dimensional gap elements, CGAP's. The loading platens were represented by 2-dimensional CQUAD4 elements that call upon the appropriate material property card containing a high stiffness material. Stresses within the platen representation are not of interest. A high stiffness value was imposed to minimise any platen bending deformations.

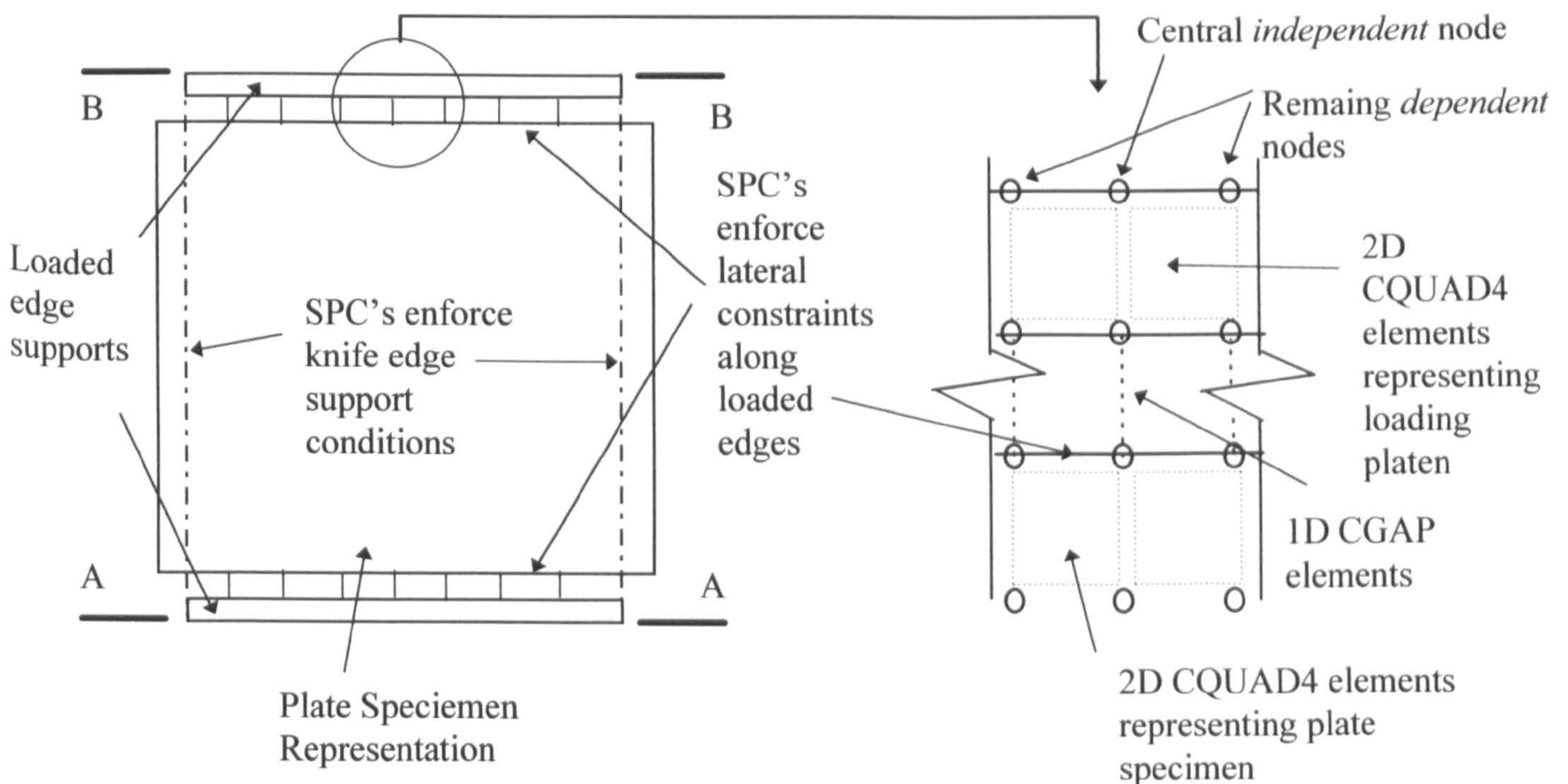


Figure 4.4. Schematic illustration of the FE representation of control strategy 1.

The gap elements simulate contact without penetration or material overlap. As the surface of the plate and loading platen are initially flat, then, contact exists along the whole edge.

Should a portion of the edge wish to expand (+ve CTE) then contact algorithms continually adjust the motion of the expanding plate elements, along the loaded edges, such that they are in accordance with the loading platen. However, if the plate non-uniformly contracts (-ve CTE), a portion of the loaded edge, if it so desires, will be free to separate from the loading platen. These gap elements require bilinear stiffness terms. A high closure stiffness and a very small opening stiffness were employed.

For the applied in-plane compressive loading, the use of gap elements prevent any fixed edge constraints which will arise with the use of SPC boundary conditions or the translational dependency terms of the following RBE2 elements. Caution should be exercised in specifying PGAP properties. In particular, the closed gap stiffness should not exceed the stiffness of the adjacent nodal degree of freedom by 1000^{112} .

Figure 4.4 visually shows the existence of a gap between the loading platen and the compression loaded plate specimen. It is there for presentation purposes only. It did not exist in the actual analysis.

Specimen edge shortening was imposed by vertically constraining one edge of the lower platen representation, AA, and enforcing the displacements at the opposite platens upper edge, BB, to apply compression. Multi-point constraints, MPC's, were employed such that uniform vertical displacement of the entire upper loading platen imposed the desired compressive load/displacement to the laminated specimen. While SPC's constrain the motion by specifying a known displacement value, MPC's enforce the motion of a single degree of freedom to other degrees of freedom that are of *dependency*. A rigid body element, RBE2, was the selected MPC. A complete description of all the types of MPC's is presented in the software's manual¹¹⁴. MPC's are only required to represent the *load control* testing scenario, **Chapter 6**.

To ensure a moveable uniform vertical compressive edge displacement along the loaded edge, an RBE2 element¹¹⁴ was introduced by inserting an additional node (known as the *independent node*), at the mid-point along the top edge of the loading platens FE representation. This is the location of an applied compressive point load. The point load's magnitude varying for each subcase. An SPC, placed on the independent node, enforced a fixed constraint in the out-of-plane translation direction and all rotations.

All the additional nodes, along the top edge of the platen, (known as *dependent nodes*) are connected to the independent node and their vertical and lateral translational motion are of dependency. In this manner, a uniform displacement is imposed along the top edge of the panel, which will not provide constraint against either shape memory adaptation or thermal expansion/contraction. This condition is identical to that enforced by the loading platen of the testing machine.

4.7.3 SMA Actuator Representation

One dimensional CBEAM elements are employed to represent the 0.4mm diameter SMA wires. The element and material properties are represented by the PBEAM and MAT1 data entries respectively. Each beam element has the same global edge length as the adjacent CQUAD4 elements. The topological nature of the beam elements is such that they are congruent with a CQUAD4 shell elements edge and the associated nodes for each element are coincident, **Figure 4.5**.

RBE2 elements are applied to each coincident node set constraining the beam elements to move vertically or out-of-plane in accordance with the motion of the CQUAD4 nodes. No constraint is imposed in the elements longitudinal direction. Should the beam elements wish

to translate along their longitudinal direction, they are free to do so. Such constraints are identical to those within the manufactured specimens.

Depending on the orientation of the embedded inclusion to the adjacent plies, significant local perturbations in the surface topology of the manufactured plates exist, **Figures 3.1-3**. The greatest disruption to the ply reinforcement was when the inclusions were placed on the neutral plane of the $[0_2/\pm 45]$, and at an angle of 45° to the neighbouring plies. This resulted in a local specimen thickness of approximately 1.6mm. An effort was made to account for the increased plate thickness, in the vicinity of the embedded inclusion, on the structural response. This was achieved by adding a fictitious bending stiffness to the 1-D elements representing the SMA actuators, **Section 4.9, Table 4.3**.

The adaptive capability of the test specimens is provided for by applying fixed constraints to either end of the pre-strained SMA wires. Upon the application of the transformation temperature, high recovery stresses are set up which act to remove any out-of-plane deformation. Within the FEA, this behaviour can be simulated by one of two ways, the final outcome being the same.

The first method involves completely fixing one end of the line of one-dimensional elements, the other end having one degree of freedom (DOF) in the direction of the recovery stress. At the required time of activation, a point load can be applied to the 1 DOF node placing the wire in the desired tensile stress.

The second method involves completely fixing both ends of the line of elements and applying the transformation temperature. With careful manipulation of the coefficient of thermal expansion, the desired tensile recovery stress will result.

For simplicity, the former method was applied within this analysis, results of which are to be presented in **Chapter 6**. The magnitude of the point loads applied were based on the experimental recorded SMA output presented in **Chapter 2 & Chapter 6**.

The analysis involves loading the laminated structures to approximately three times the magnitude of the first critical buckling value, or to a value slightly lower than P_{c2} , and then activating optimal sets of actuators. SMA activation results with a non-uniform temperature profile throughout the laminate. This predetermined temperature profile can be superimposed on the structural mesh, thus, accounting for thermal stresses and strains.

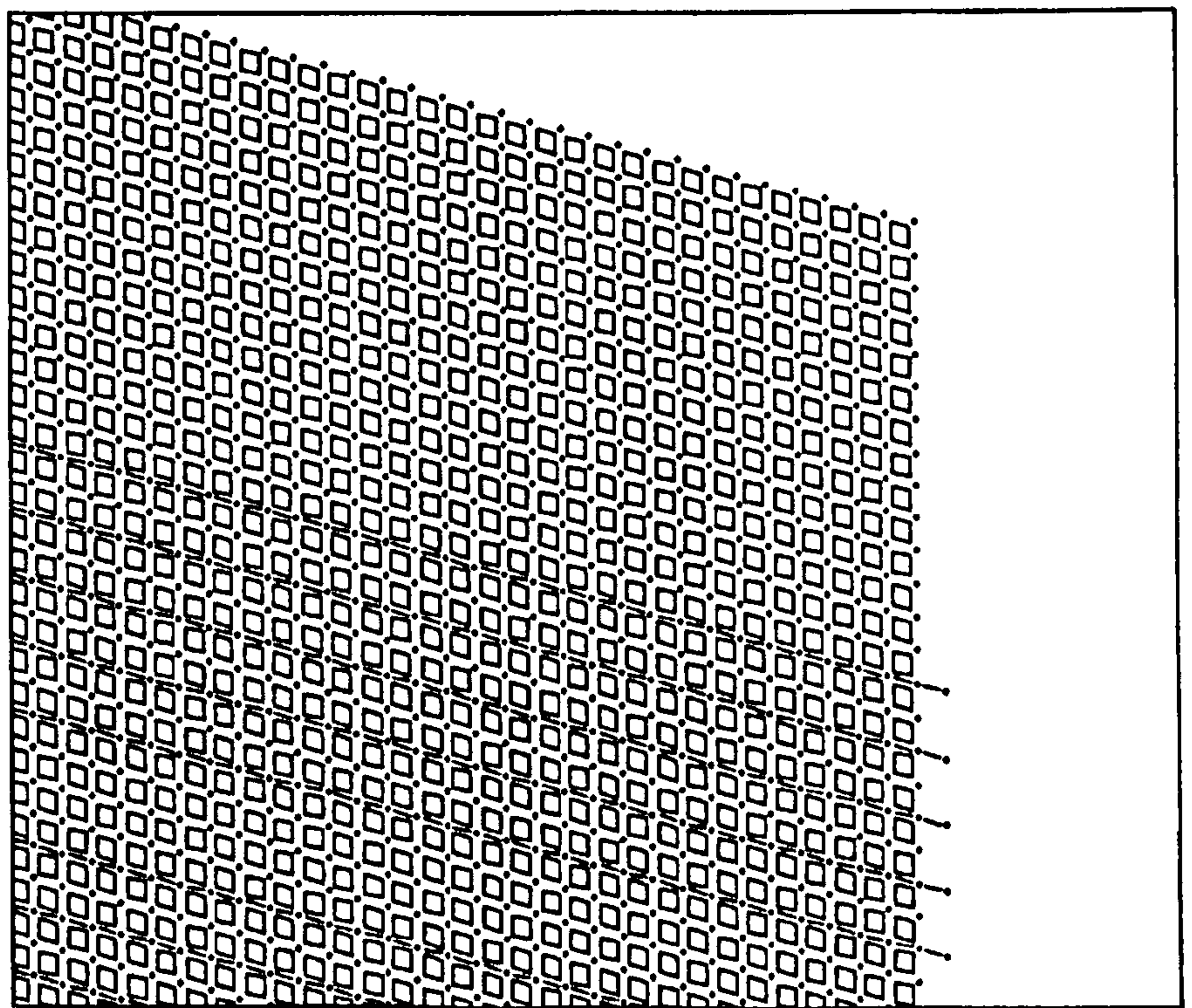


Figure 4.5. *FE mesh shrink plot highlighting element congruency.*

4.7.4 Convergence Study

A finite element convergence study was conducted to predict the performance of the CQUAD4 shell elements within the buckling/post-buckling analysis of laminated plates with varied stacking sequences. By comparing results from four mesh density refinements, it is possible to determine the approximate rate of convergence toward correct results for the aforementioned element type.

Each refinement is by subdivision of existing elements with existing nodes retained and not repositioned¹¹⁵. This is known as *h*-refinement. Within the non-linear analysis solver, it is not possible to change element type, i.e, utilize CQUAD8's, a convergence check technique known as *p*-refinement.

The plate representation was modelled with 168, 624, 2496 and finally 9600 elements. Non-linear finite element results for the $[0_2/90_2]_s$ lay-up, configuration number 1, when loaded to approximately $3P_c$, for the aforementioned element quantity utilization, is presented in **Figure 4.6**.

As expected, it can clearly be seen that, for the low density mesh, i.e. 168 elements, the FE model errors by being too stiff. By increasing the number of elements employed in the analysis, the results converge. For the meshes constructed from 2496 and 9600 elements, the numerically obtained displacement results were identical. For this reason, the results obtained from the 9600 element mesh are omitted from **Figure 4.6**.

We do have a high rate of numerical convergence. Quite probably, stresses will converge more slowly than the displacements. The FE convergence study, using 2496 CQUAD4 elements, provides the author with confidence for the obtained results.

For the plate specimens, the results communicated in **Table 4.3 & Chapter 6** were obtained with the geometry of the mesh modelled with 2496 CQUAD4 elements. Each SMA wire was modelled with 54 CBEAM elements.

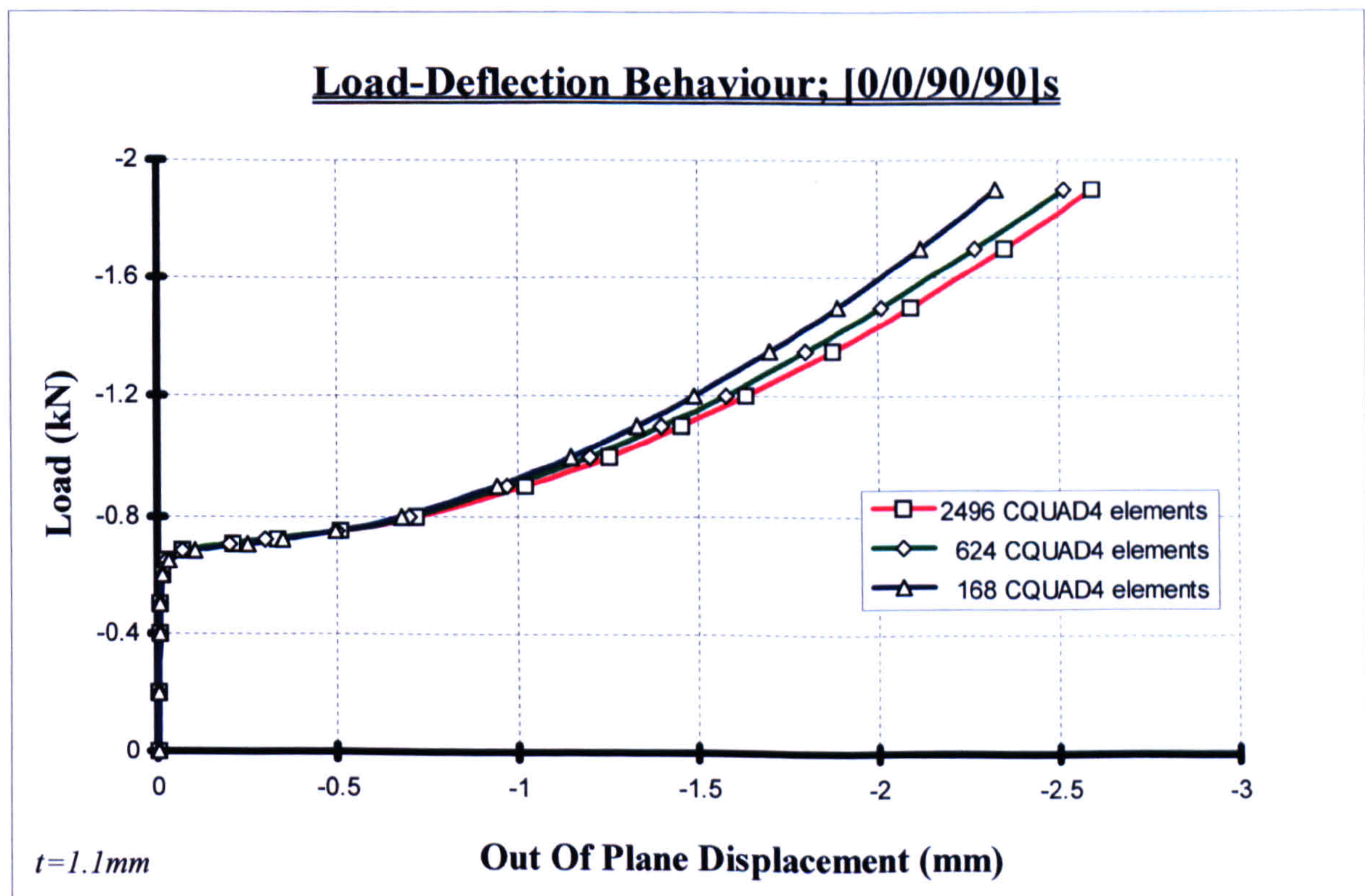


Figure 4.6. Finite element non-linear convergence study for specimen config. no.1.

4.8 Finite Element Representation of Control Strategy 2

The finite element modelling technique employed for the numerical representation of *control strategy 2* is now to be presented.

4.8.1 General Specimen Representation

As with the previous control strategy, the finite element model of a blade stiffened panel was constructed from CQUAD4 two dimensional shell elements. In total 5328 shell elements were employed. For a discussion on the panel boundary conditions and uniform end shortening, utilizing RBE2 elements, while under the influence of a compressive load, see Section 4.7.2.

Figure 4.7 shows the upper left segment of the FE construction employed to model the stiffened panel. When the stiffened panel is in the post-buckled regime, the high in-plane loads are transferred from the panel skin, between the stiffeners, to the stiffeners. For the analysis to simulate this redistribution and accurately capture the local stress gradients, the model requires sufficient local modelling detail near the region of the anticipated load redistribution. A sufficient mesh density is also required in close proximity to the application of the recovery loads. Clearly evident is the finite element refinement at the required locations.

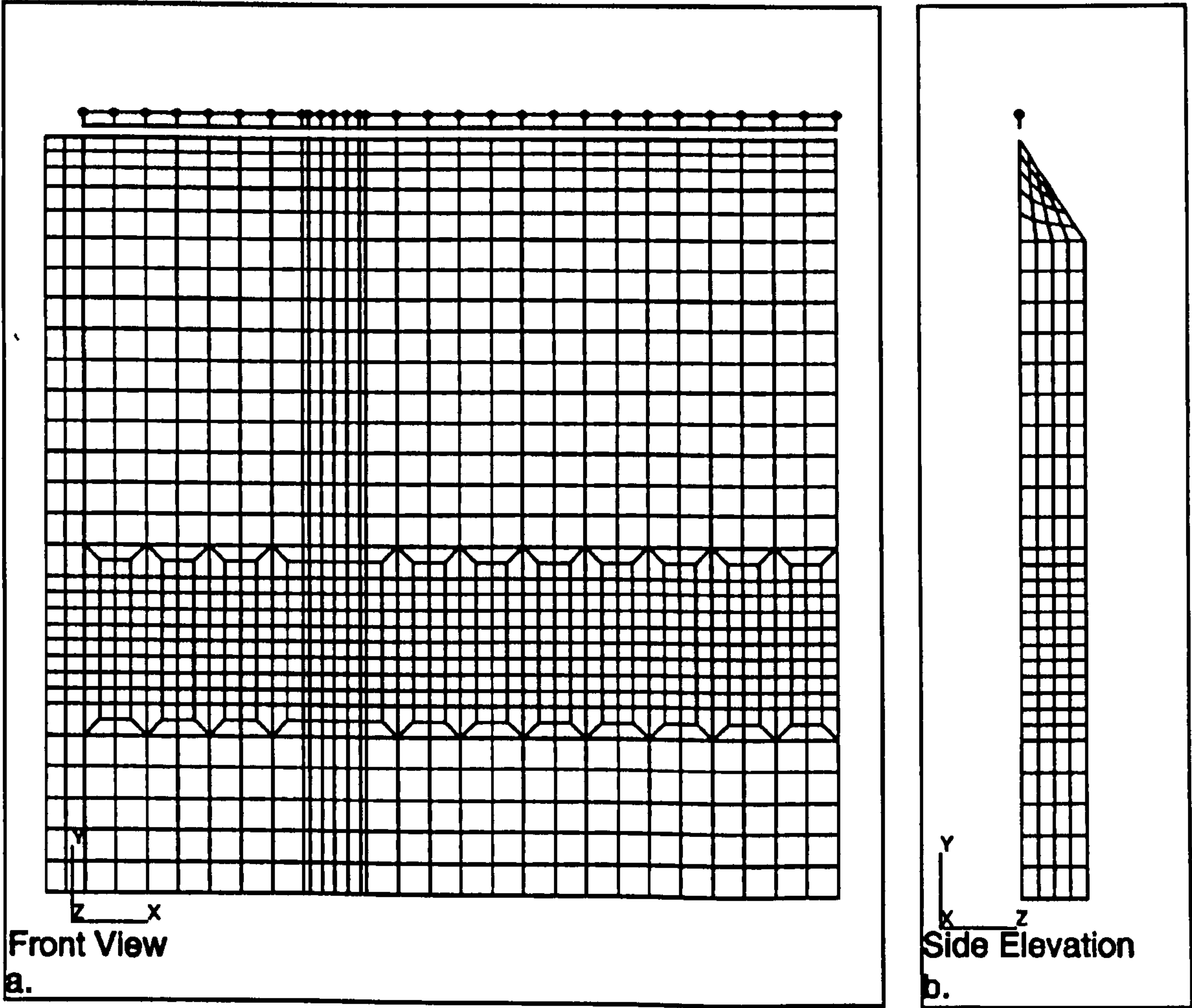


Figure 4.7. Quarter model of FE mesh employed to model the stiffened panel specimens.

Care has been taken to keep the distortions of the elements within the limits suggested in the NASTRAN user manual¹¹⁴. Non-linear analysis being especially sensitive to distortions of the finite elements¹¹⁵. The ideal quadrilateral is square. As seen, other shapes must be used in order to represent the structure geometry and grade the mesh from coarse to fine. In general, distortions in an element reduce its numerical accuracy as the element becomes stiffer than it would otherwise be. Pre-processor checks ensured the elements have satisfy aspect ratio, skew and taper.

4.8.2 *SMA Actuator Representation*

As stated in **Section 3.5**, the location of the SMA induced interfacial shear stresses requires careful consideration so as not to degrade the global mechanical performance. The actuators are offset from the neutral plane, therefore, setting up moments that bend the specimen in a known manner, thereby, alleviating the unwanted post-buckling deformations. Regrettably, membrane stresses will also be present that, depending on their magnitude and distribution, may degrade the post-buckling response.

Results are presented in this section that illustrate the magnitude of lateral displacement that the offset actuators can achieve, at various actuation points, within a $[0_2/90_2]$ lay-up skin section. The associated in-plane force intensities that arise are also presented. Such results allow us to select an appropriate embedded constrained SMA length.

Two approaches can be used for modelling the SMA actuation within this control strategy. The first method utilizes point loads that are applied to the nodes at the location of the desired recovery stress. Non-uniformity in local stress distribution, as a result of the applied point loads, necessitates a high local mesh density. Such point loads will exert the shape recovery membrane stresses and required bending loads. The loads applied assume the non-occurrence of interfacial decohesion between the SMA wire and the composite.

The second approach for representing the SMA actuators utilizes one-dimensional offset beam elements that are constrained to the plate elements. Constraint is enforced by equivalencing the coincident nodes associated with both the beam elements and shell elements. The value of the offset being the dimension from the plates neutral plane to the SMA wires axial centre line. Here, the recovery force associated with the activated SMA would be produced by introducing a fictitious coefficient of thermal expansion for the SMA beam elements that would create the required distributed loading with the imposition of a required thermal load. The first approach was elected over its ease of modelling.

Figure 4.8 shows a half model of the finite element construction that was used to assess the lateral displacements and membrane stress distribution, associated with the recovery forces for embedded activated SMA actuators, at various actuation positions, within the $[0_2/90_2]$ lay-up skin section of configuration number 10. Linear static analysis only was conducted.

Highlighted on the figure are the positions of the activated SMA actuators for a particular load case. Twelve load cases were assessed. Load case 1 predicted the mechanical response when point-load recovery forces were enforced along section AA. Load case 2 predicted the mechanical response when the point-load recovery forces were enforced along section BB, and so on. The distance between each section is 12.5mm. The methodology of inducing the recovery force away from the specimens edges was presented in **Section 3.5.2**.

For this control strategy, 23 embedded SMA actuators were considered. They have a parallel spacing of 12.5mm. Based on the results presented in **Chapter 2**, a point-load of 30N

was enforced at the desired position for each SMA location. Its direction was parallel to the SMA orientation and its effect was to apply compression and bending to the specimen. As the actuators were embedded between plies 1 & 2, bending arises through the offset nature of the loading from the neutral plane. A moment arm of 11.4Nmm was, therefore, applied at the appropriate nodes, **Section 6.1.1.1**. The moment being about the axis parallel to the appropriate section for each load case.

The SPC boundary conditions applied to the model were identical to those previously introduced, i.e. the applied constraints represented the loading rig constraints. The only loads applied in the model were those simulating the SMA recovery forces. The affects of temperature were ignored.

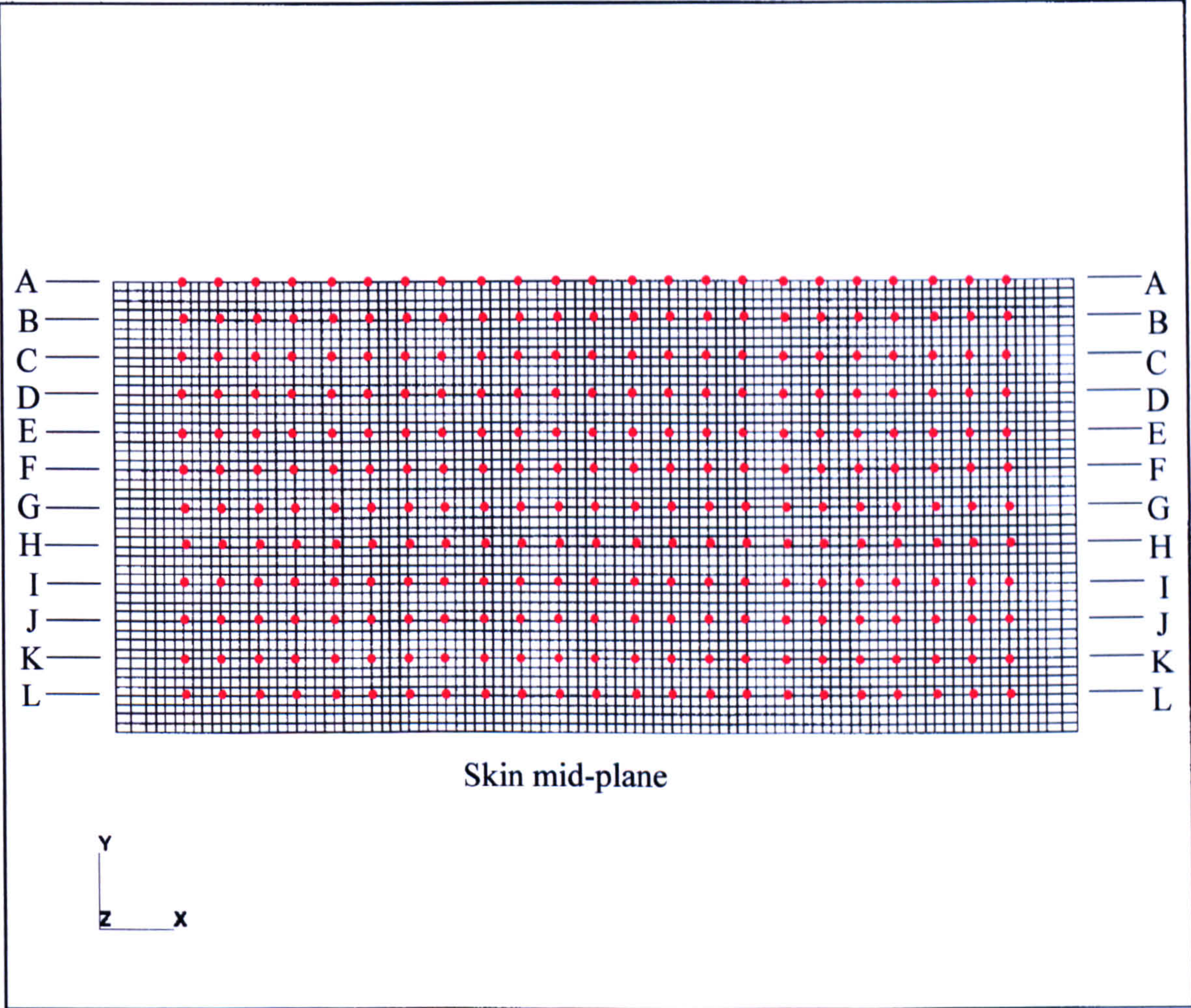


Figure 4.8. *Half model of FE mesh employed to model panel skin deformation at various SMA actuation positions.*

Figure 4.9 shows the out-of-plane displacement profile, along the mid-plane of the specimen, when the recovery forces are applied along the sections illustrated. Clearly, the closer the induced recovery forces are to Section **AA**, the greater the magnitude in peak central out-of-plane displacement. The closer the application of recovery forces to the central region of the plate, the less the displacement.

Figure 4.10a, b, c & d show the in-plane **YY** membrane force intensity profiles when the SMA recovery forces are activated along sections **AA**, **EE**, **II** & **LL** respectively. Clearly the closer the induced recovery forces are to section **AA**, the more uniform the detrimental membrane compressive loads. However, the closer the application of recovery forces are to

the central region of the plate, then, only centrally localised membrane compressive forces arise. The latter forces having a less domineering effect on the out-of-plane displacement.

Ideally, we require large displacement control with minimal increase in the applied compressive loading. From **Figures 4.9 & 4.10**, we can induce large lateral deformations with not-so-significant membrane YY compressive forces when applying the recovery forces along section **HH** or **II**. **II** was chosen, i.e. SMA actuation occurred at a distance of 100mm from the plates edge. Decreasing this distance will unduly increase the uniformity of the compressive loading state within the specimen. Increasing this distance, then, the resulting deformation will not be so conspicuous.

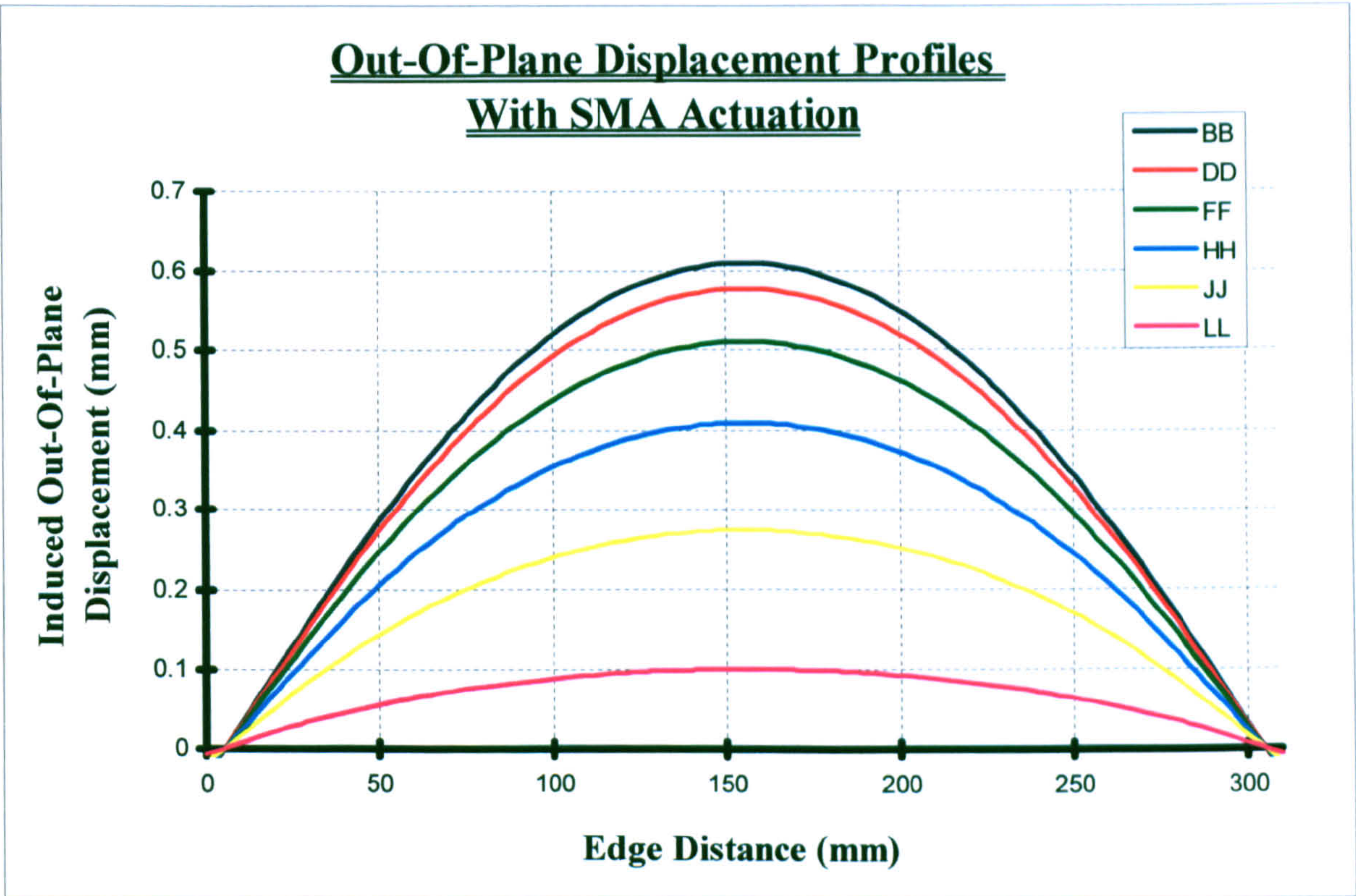


Figure 4.9. The effect of the offset SMA actuation forces on a $[0_2/90_2]_s$ skin section.

For each configuration of *control strategy 2*, the SMA wires are located within sleeves that egress the specimen, **Section 3.5.2**. The sleeves will allow SMA recovery, therefore, preventing constraint. Only the central 100mm length of the embedded SMA is actually constrained to the composite. The ends of which, through interfacial shear, induce the aforementioned bending and membrane loads to the specimen. We seek to utilize the former bending to enhance the post-buckling response.

4.8.3 Stiffener Modelling Considerations

An important issue when modelling blade stiffened panels is on how to join the stiffeners to the skin. Two approaches can be used for modelling stiffener sections. The first approach utilizes beam elements with equivalent material and sectional properties to that of the composite blade section. The drawback here, for MSC/NASTRAN, is the non-capability of the beam elements to account for anisotropy, or account for stiffener run-out.

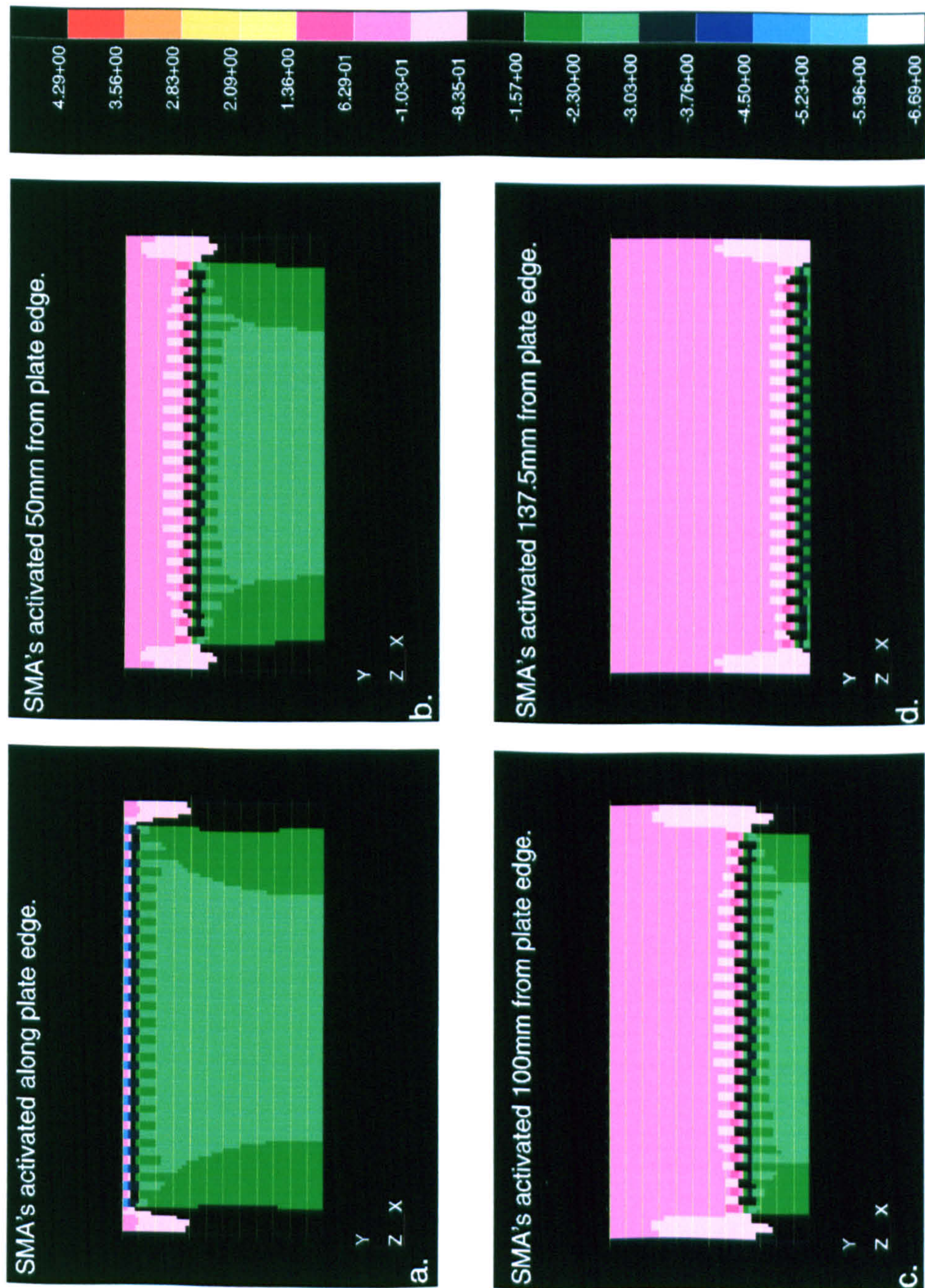


Figure 4.10. YY membrane force intensity distribution when SMA actuators are activated along sections AA, EE, II & LL.

Within the second approach, as used here, the skin and flange neutral planes are assumed to be coincident such that both can be represented, in the appropriate region, and in the through thickness direction, by a single in-plane 2-dimensional shell element. The elements effective thickness being equal to the sum of the thickness of skin plus twice the flange thickness, as depicted in **Figure 4.11**.

The true offset nature of the flanges between the skin is realized, as shown in **Figure 4.11**, by setting the elastic constants in the fictitious plies to zero.

Using the shell elements, more detail in the geometric representation is included. Adequate modelling of the stiffeners using shell elements requires at least four rows of elements along the height of the stiffener, otherwise, an artificially high in-plane bending stiffness will occur in the FE model for the stiffener¹⁰⁷.

The employment of the fictitious plies within the model and the utilization of glass insulating weave, **Section 3.5.1**, requires careful attention when associating the finite elements, utilized in the stiffened panel model, with particular material properties.

In total, 7 material property cards were employed for modelling purposes, 5 of which employ the laminated composite PCOMP card. The coloured fringe plot of **Figure 4.12** highlights the regions of the various element properties. **A, B, C, D & G** employ the PCOMP cards. **E & F** employ PBEAM and PSHELL property cards that reference appropriate 1-dimensional CGAP and 2-dimensional CQUAD4 material properties respectively.

The composite lay-ups associated with the highlighted regions of **A, B, C, D & G** are presented in **Table 4.2**.

In **Table 4.2**, *GW* denotes the glass weave insulating layers that are employed to separate the current carrying SMA actuators from the electrically conductive carbon reinforcement. Each individual glass layer has a thickness of 0.08mm. The following material properties were employed for its representation; $E_{11}=E_{22}=25 \times 10^3 \text{N/mm}^2$, $G_{12}=4 \times 10^3 \text{N/mm}^2$, $\nu_{12}=0.2$ & $\alpha_1=\alpha_2=1.16 \times 10^{-5} \text{ } ^\circ\text{C}$. For ease of modelling, the glass weave is assumed to be continuous throughout section **B**. In practice, 5mm strips were employed to sandwich the SMA actuators that have a parallel spacing of 12.5mm. The assumption made will probably cause excessive stiffness in the numerical model.

FP, within **Table 4.2**, signifies the position of the fictitious plies. Very small values of stiffness properties were employed in their representation. Their presence will have insignificant effect on the results obtained.

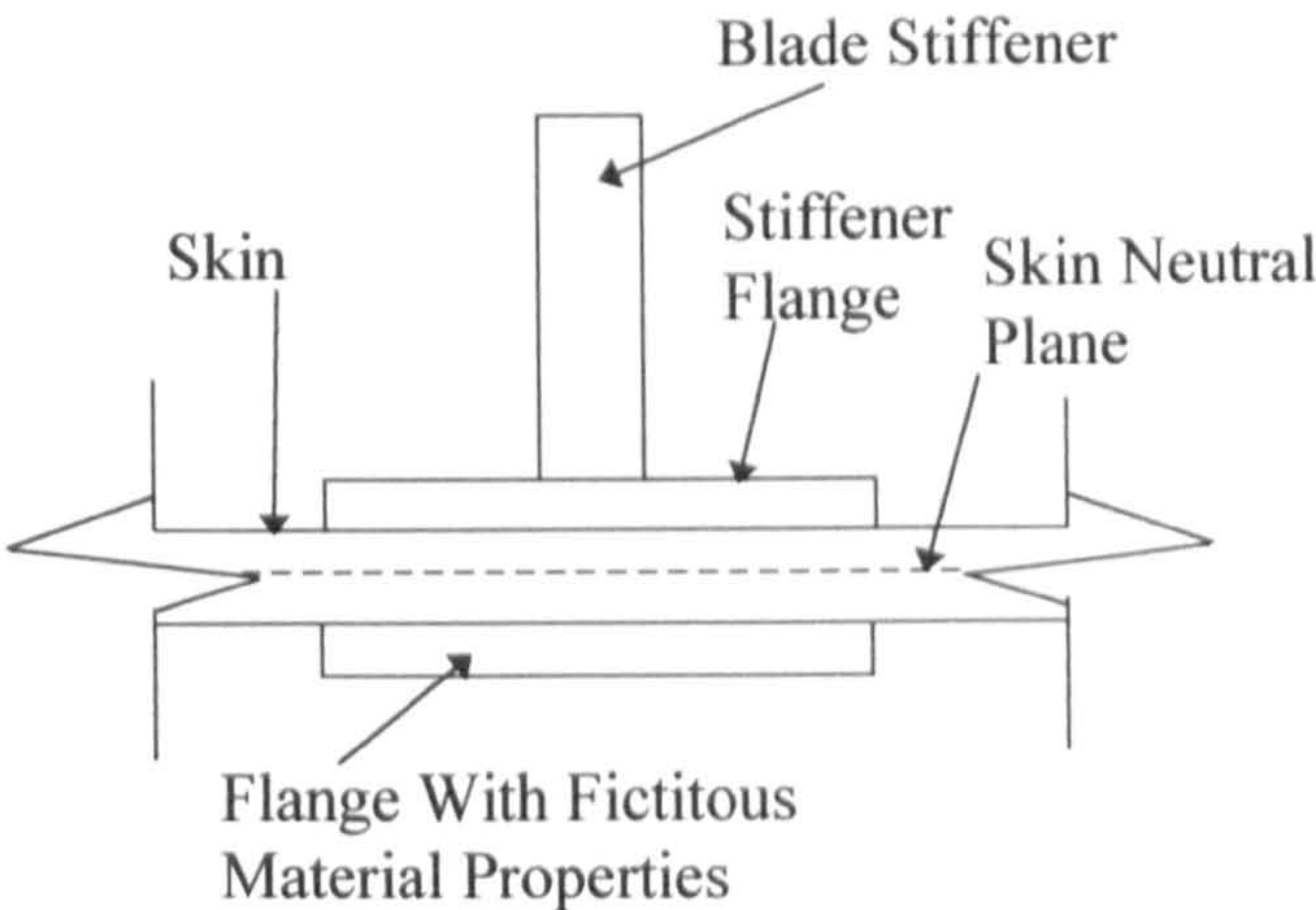


Figure 4.11. Geometric representation of the stiffener/skin interface.

Section	Lay-up
A	$[0_2/90_2]_s$
B	$[0/GW_2/0/90_2]_s$
C	$[FP_4/0/GW_2/0/90_4/0/GW_2/0_2/90_2/0]_{Tot}$
D	$[FP_4/0_2/90_4/0_3/90_2/0]_{Tot}$
G	$[0/90_2/0]_s$

Table 4.2. PCOMP representation of laminated stiffened panel, specimen configuration no.10.

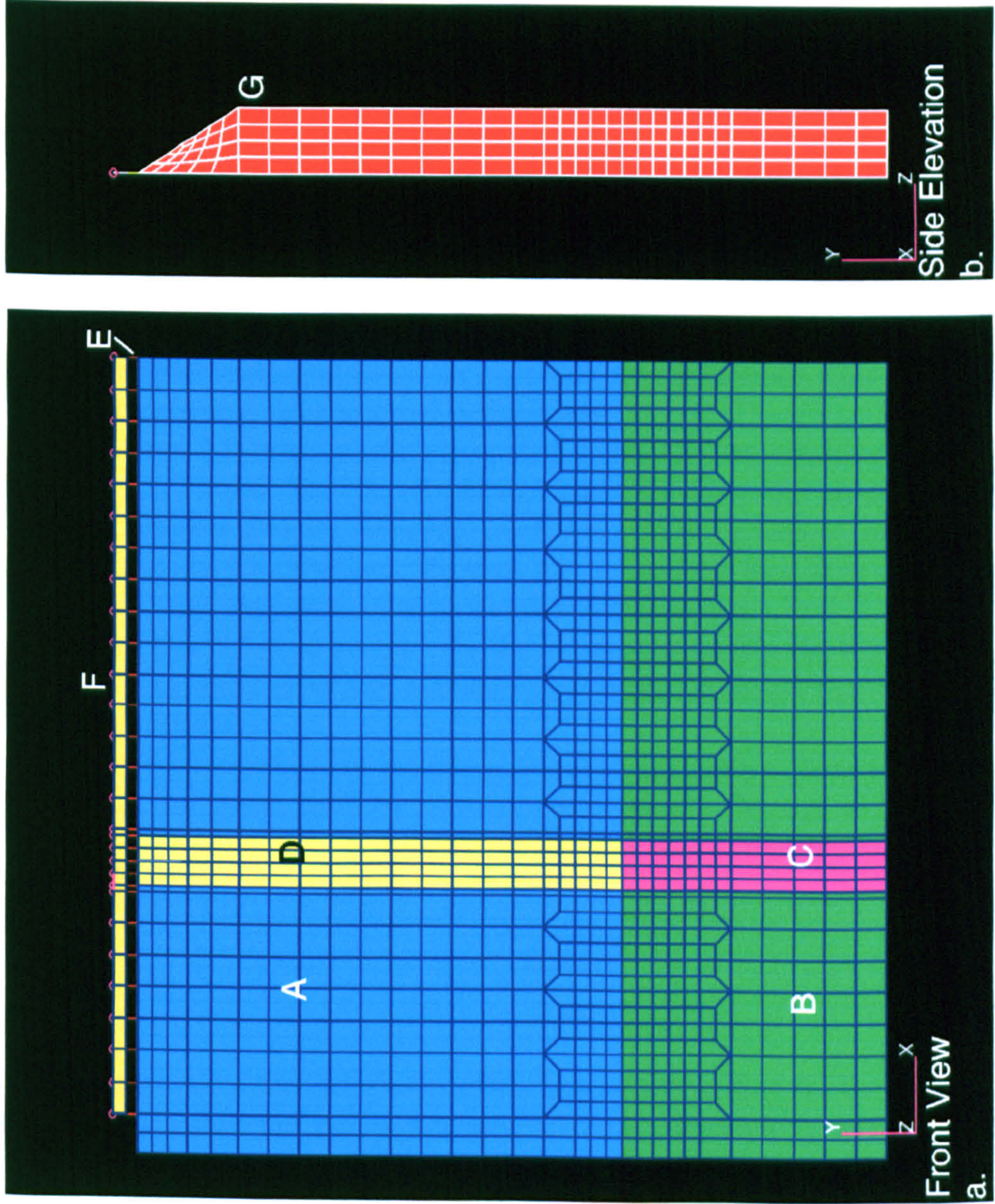


Figure 4.12. Scalar colour plot of employed material properties within stiffened panel FE representation.

Finally, the lay-ups presented in **Table 4.2** represent specimen configuration number 10. Replacing the 90° cross-plys by equal quantities of $\pm 45^\circ$ plies would result with the lay-ups employed to model specimen configuration number 11.

4.9 Numerical & Experimental Buckling Results

The buckling loads for the simply-supported carbon/epoxy laminates were determined from theory and compared with those obtained from experiment. For each configuration, the first two critical buckling values were theoretically obtained. The results of which are presented in **Table 4.3**. A clearly evident observation, as expected, is how the lay-up orientation can effect the critical buckling value.

The theoretical results presented are based on the *as-specified* and *as-built* dimensions. The reader is referred to **Table 4.1** for a reminder of the specimens measured *as-built* thickness. For the *as-built* analysis, several thickness measurements were taken over the plate specimens, away from the embedded inclusions, and averaged. It is important to use the *as-built* thickness because, as shown in **Table 4.3**, any deviation of the true thickness will have a profound effect on the buckling and also the post-buckling behaviour. This is attributed to the increase in flexural stiffness with increase in thickness.

For configurations 1-9, local to the embedded inclusions, the thickness of the specimen could be as much as 1.6mm. This is, of course, dependent upon the lay-up, diameter of the embedded SMA actuator and its sleeving. Such embedded inclusions only pose localised specimen surface topology protrusions, **Figures 3.1-3**, none the less, they will act to increase the flexural stiffness and, therefore, need to be taken into account.

As stated in **Section 4.7.3**, to account for the aforementioned in the FE model, a fictitious bending stiffness was applied to the 1-dimensional beam elements that were employed to represent the SMA actuators.

In reality, local to the embedded inclusion, the host will have a flexural stiffness equal to the product $E_{\text{host}}I_{\text{host}}$. Likewise, the SMA actuator will have a flexural stiffness equal to the product $E_{\text{SMA}}I_{\text{SMA}}$. To account for the increase in flexural stiffness of the host, we manipulate the flexural stiffness of the SMA actuator such that;

$$I_{\text{SMA}} = \frac{E_{\text{host}}I_{\text{host}}}{E_{\text{SMA}}} \quad (4.8)$$

This method eliminates the modelling difficulties of accounting for the localised increase in flexural stiffness within the 2-dimensional CQUAD4 elements. It should be pointed out that it is quite difficult to calculate I_{host} for the surface protrusions. Based on the micrographs of **Figures 3.1-3**, a maximum value of approximately 0.2mm⁴ was determined. Substituting this value and the appropriate material properties of the constituents into equation (4.8), then, I_{SMA} was determined to be approximately 0.1mm⁴. Owing to this approximation, several values were employed to represent the increase in local flexural stiffness. The values chosen are presented in **Table 4.3**. Clearly, their affect is to increase the predicted FE buckling values.

For the non-linear post-buckling results presented in **Section 4.10.1** and **Chapter 6**, the value of I_{SMA} was 0.2mm⁴. Within the analytical buckling solution, equation (4.7), this localised increase in flexural stiffness can not be accounted for.

Config. No.	Lay-up Config.	FE as-specified thickness (1mm)		FE as-built thickness		FE as-built thickness + varying I_{SMA}				Analytical values from Eq.(4.7)		Experimental buckling load
		P_{c1} (kN)	P_{c2}	P_{c1} (kN)	P_{c2}	I_{SMA} (mm ⁴)	P_{c1} (kN)	P_{c2}	P_{c2}	P_{c1} (kN)	P_{c2}	
1	$[0_2/90_2]_s$	0.520	1.538	0.692	2.047	0.1	0.706	2.049		0.638	2.303	No limit load witnessed.
						0.2	0.712	2.050				
						0.3	0.718	2.050				
2	$[0_2/\pm 45]_s$	0.565	1.583	0.751	2.108	0.1	0.765	2.164		0.696	1.954	≈ 0.8
						0.2	0.772	2.174				
						0.3	0.777	2.179				
3	$[0/\pm 45/90]_s$	0.671	1.459	0.942	2.050	0.1	0.958	2.056		0.867	3.485	≈ 1.0
						0.2	0.965	2.059				
						0.3	0.972	2.061				
4	$[0_2/90_2]_s$	-	-	0.655	1.937	0.1	0.670	1.940		0.603	2.177	≈ 0.72
						0.2	0.677	1.941				
						0.3	0.684	1.942				
5	$[0_2/\pm 45]_s$	-	-	0.712	1.995	0.1	0.727	2.053		0.658	1.848	≈ 0.9
						0.2	0.734	2.062				
						0.3	0.740	2.067				
6	$[0/\pm 45/90]_s$	-	-	0.845	1.831	0.1	0.862	1.845		0.777	3.121	≈ 1.0
						0.2	0.870	1.848				
						0.3	0.877	1.851				
7	$[0_2/90_2]_s$	-	-	0.602	1.780	-	-	-		0.555	2.000	≈ 0.8
8	$[0_2/90_2]_s$	-	-	0.655	1.937	0.1	0.671	1.941		0.602	2.177	≈ 0.83
						0.2	0.679	1.942				
						0.3	0.686	1.944				
9	$[0/\pm 45/90]_s$	-	-	0.892	1.942	0.1	0.910	1.949		0.823	3.304	≈ 0.9
						0.2	0.918	1.952				
						0.3	0.925	1.955				
10	$[0_2/90_2]_s$	-	-	1.038	2.165	-	-	-		-	-	≈ 1.0
11	$[0_2/\pm 45]_s$	-	-	1.861	2.493	-	-	-		-	-	≈ 0.9

Table 4.3. Numerical, analytical and experimental critical buckling values for all specimen configurations.

The analytical critical buckling values, obtained from equation (4.7), under predict the first critical buckling value, P_{C1} . This is because the slight overhang at the knife edge supports is not accounted for. With the exception of the $[0_2/\pm 45]_s$ lay-ups, the second critical buckling values are over predicted, P_{C2} . This, probably, is also associated with the non-account for the overhang of the specimen width at the knife edge supports. For the $[0/\pm 45/90]_s$ lay-ups, it is the likely result of not accounting for the bending-twisting couplings inherent within the material. The magnitude of these couplings, along with the remainder of laminated construction's D-matrix constituent properties, are shown in **Table 4.4**.

It should be noted that the analytical and numerical buckling procedures are not actually considering identical specimen parameters. Regarding the analytical procedure, it is not possible to represent the 10mm overhang along the unloaded edges. The analytical results are provided merely for a guide.

Early on in this research programme, the author compared analytical and numerical results for a 300*300mm square specially orthotropic lay-up and attained almost identical results, therefore, providing validity for the numerical procedures employed.

Specimen Configuration Number											
	1	2	3	4	5	6	7	8	9	10	11
D_{11}	1.38e ⁴	1.43e ⁴	1.16e ⁴	1.3e ⁴	1.35e ⁴	1.04e ⁴	1.2e ⁴	1.3e ⁴	1.1e ⁴	1.38e ⁴	1.43e ⁴
D_{12}	2.94e ²	7.25e ²	1.79e ³	2.79e ²	6.87e ²	1.6e ³	2.56e ²	2.79e ²	1.7e ³	2.94e ²	7.25e ²
D_{22}	2.81e ³	1.47e ³	2.94e ³	2.66e ³	1.39e ³	2.63e ³	2.44e ³	2.66e ³	2.78e ³	2.81e ³	1.47e ³
D_{33}	5.55e ²	9.86e ²	2.06e ³	5.25e ²	9.33e ²	1.85e ³	4.82e ²	5.25e ²	1.96e ³	5.55e ²	9.86e ²
D_{13}	0	3.43e ²	7.24e ²	0	3.25e ²	6.49e ²	0	0	6.86e ²	0	3.43e ²
D_{23}	0	3.43e ²	7.24e ²	0	3.25e ²	6.49e ²	0	0	6.86e ²	0	3.43e ²

Table 4.4. Specimen configuration's D-matrix constituent properties.

In general, for anisotropic and quasi-isotropic lay-ups, the results obtained from the simple closed form analytical expression, equation (4.7), give non-conservative approximations for the buckling loads because of its failure to account for the anisotropic stiffness terms.

The experimental buckling load can be found by plotting the curve of the out-of-plane-deflection with load. By drawing tangents along the linear pre-buckling portion of the curve and one along the initial part of the post-buckling curve, then, the intersection between the tangents is defined as the load where *buckling* occurs, **Figure 4.1**. This method is called the point of inflection method¹¹¹. It is the method used to compare experimental data with predicted values for all specimen configurations considered in this research.

Clearly, this approach is somewhat subjective. The slope of the steep and shallow lines are defined by the specific data points included. The selection of the data is not straightforward, it depends on personnel judgement. Every effort was made to be as consistent as possible. The experimentally obtained buckling loads reported are probably only accurate to within $\pm 10\%$.

Given the difficulties in determining the experimental buckling load using the point of inflection method along with loading rig influences and the simulation and representation of the actual simply-supported conditions, it can be seen that the predicted results are in reasonable agreement with the experimental data. There are, however, conspicuous exceptions.

As will be shown in **Section 4.10.1**, no buckling was witnessed for specimen configuration no.1. Instead, rapid out-of-plane deformation was recorded with the onset of loading. The exact cause of which was not ascertained.

For the plate specimens, on average, the experimental buckling loads were higher than those predicted. This may suggest an element of clamping in the fixtures. It was thoroughly assessed throughout the testing phase but could not be quantified. The most extreme discrepancies occurred for specimen configuration numbers 5 & 7 where the experimental buckling values, P_{imp} , are seen to be approximately 22% and 33%, respectively, higher than the predicted values of the finite element model.

It is not an uncommon event to witness experimentally determined buckling values in excess of the values analytically or numerically predicted. A publication, found in the open literature, states a 30% increase in the experimentally obtained buckling loads of a quasi-isotropic lay-up for a specimen having an aspect ratio of 1¹¹. The source of error was primarily given to the experimental difficulties in enforcing simply-supported boundary conditions during the testing phase.

The first mode shape for each specimen was a single half wave in both directions. The second mode shape constituted two half waves in a direction parallel to the applied load. Fringe plots of both the first and second mode shape, for specimen configurations 1 & 3, are shown in **Figures 4.13a,b & 4.14a,b** respectively. They indicate the specially orthotropic deformation symmetry inherent within the $[0_2/90_2]$, lay-up while skewed buckle mode shapes exist for the quasi-isotropic $[0/\pm 45/90]$, lay-up. The amount of skew increases as the anisotropic coefficients increase. The anisotropy is more pronounced for simply-supported plates than that for clamped plates with identical geometry and loading conditions¹¹⁶.

4.10 Numerical & Experimental Post-Buckling Results

Comparisons between the experimentally obtained and numerically determined post-buckling response of some of the composite specimens are now presented. The results presented here are typical of the plate/panel specimens devoid of shape memory adaptation. The results obtained with adaptive control are presented in **Chapter 6**.

4.10.1 Control Strategy 1

The results presented within this section are in the form of load versus lateral displacement comparisons between FE and experiment. Numerically predicted in-plane force intensities, throughout a particular specimen, are also presented. Plots of the out-of-plane deformation versus applied compressive load are presented in **Figures 4.15-17** for specimen configurations 1, 3 and 7 respectively. The specimens chosen portray the general behaviour that was witnessed throughout the testing programme. A detailed account of test set-up and equipment employed is presented in **Section 6.2**.

With the exception of the results close to the predicted buckling loads, the experimental results show the general trend of the behaviour predicted. The experimental results of configurations 3 & 7 show a slightly higher post-buckling stiffness. As suggested in the previous section, this is, perhaps, an indication of some degree of rotational restraint along the unloaded and/or loaded edges. Another possible explanation for this discrepancy is that a constant thickness was assumed within the analysis. The actual imperfections may have local thickness variations far-field from the embedded inclusions. Inaccurate alignment within the

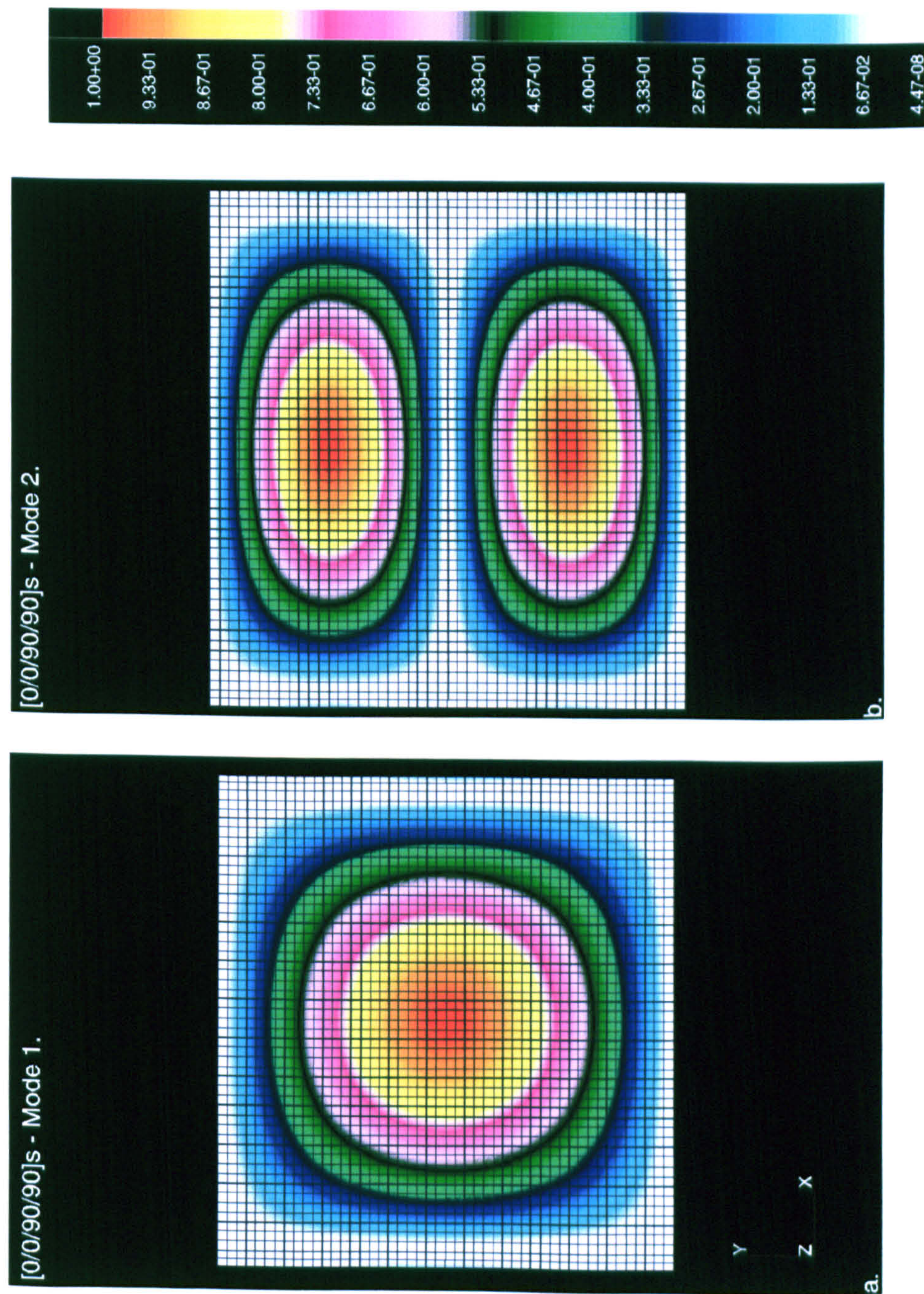


Figure 4.13. Eigenvector fringe plot for specimen configuration number 1.

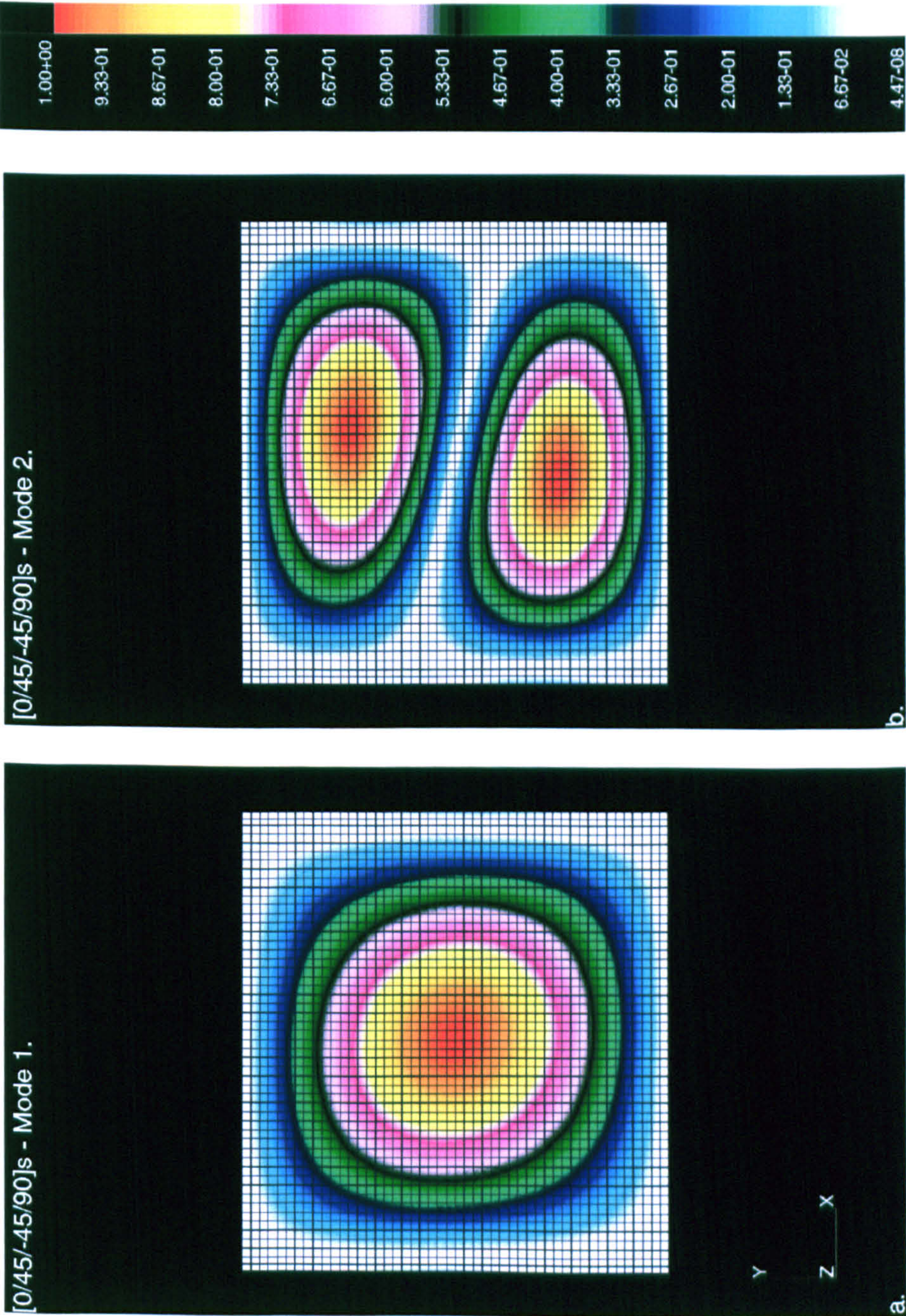


Figure 4.14. Eigenvector fringe plot for specimen configuration number 3.

loading rig is also a possible source of error. Accurate alignment was very difficult to achieve in practice.

The pre-buckling load displacement behaviour for configuration number 1 was seen to have the greatest deviation from the *perfect* theoretical solution. In practice, the fabrication of perfectly flat plates is virtually impossible and, therefore, some degree of initial imperfection will arise.

To make a sensible comparison between experiment and analysis, several imperfections, in the form of lateral forces between 0.01 & 1N, applied at the crest of the predicted buckle, were employed within the non-linear analysis to enforce the initial deviation from flatness. By doing this, it can be seen that, **Figure 4.15**, the initial numerical pre-buckling part of the curve better matches the experimental data.

As the deflections increase in magnitude, the curves of the initially imperfect plate approach the one of their perfect counterpart. The variation in the load carrying capacity of the flat plates is much less sensitive to imperfections in the extended post-buckling range¹⁷.

Figures 4.18a-d & 4.19a-d show the longitudinal compressive membrane force intensity distributions, *YY*, for specimen configurations 3 & 7 respectively. The applied level of loading is indicated on each figure. Near the buckling load, **Figures 4.18a & 4.19a**, the axial force intensity, *YY*, is nearly uniform across the plate, i.e. the membrane force intensity values at the highlighted points A, B & C are approximately equal. After buckling, **Figures 4.18b-d & 4.19b-d**, the longitudinal membrane force intensity, in the vicinity of the crest of buckle, highlighted by C, is relieved by the large deflections associated with plate buckling.

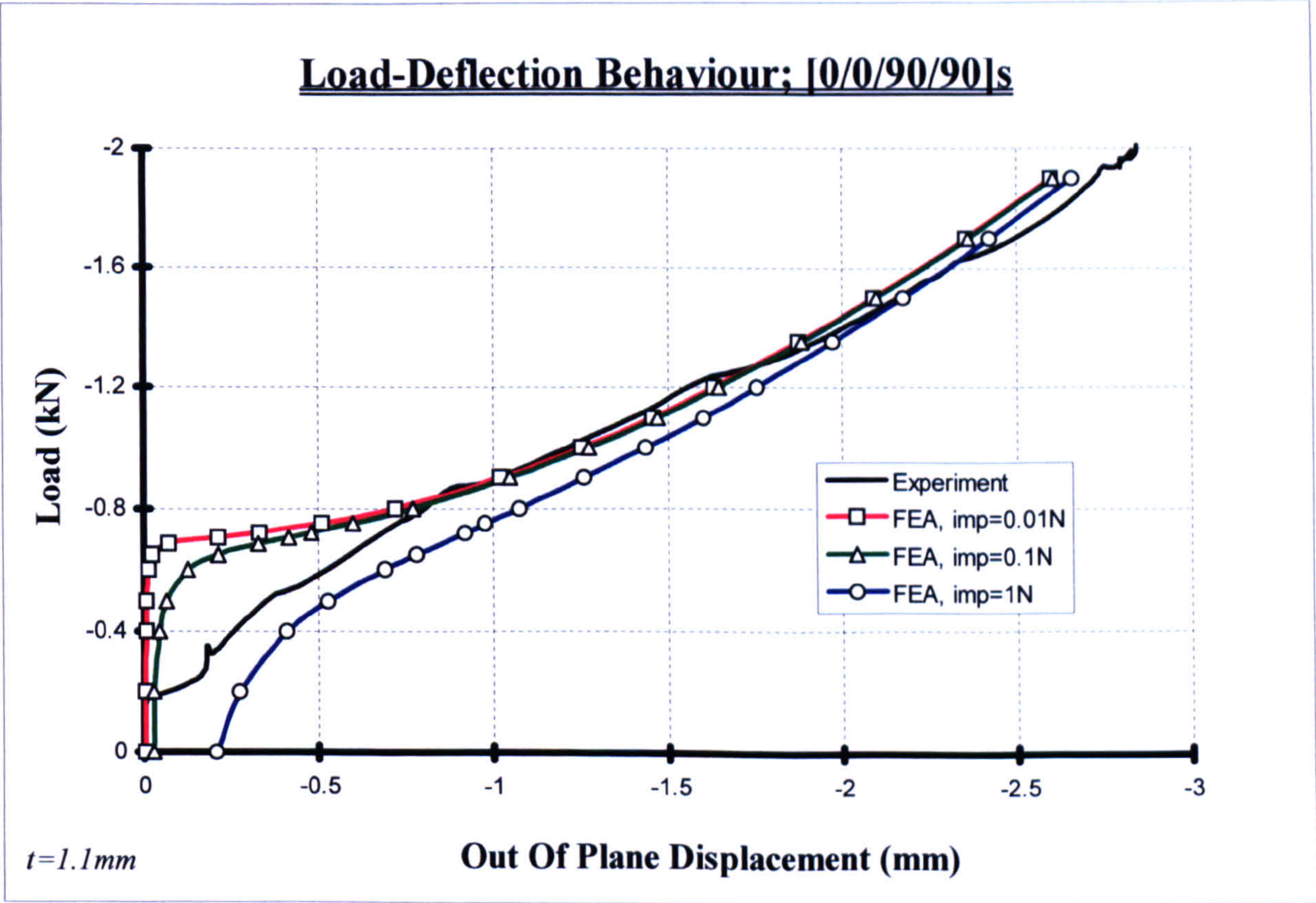


Figure 4.15. Non-linear behaviour of specimen configuration number 1.

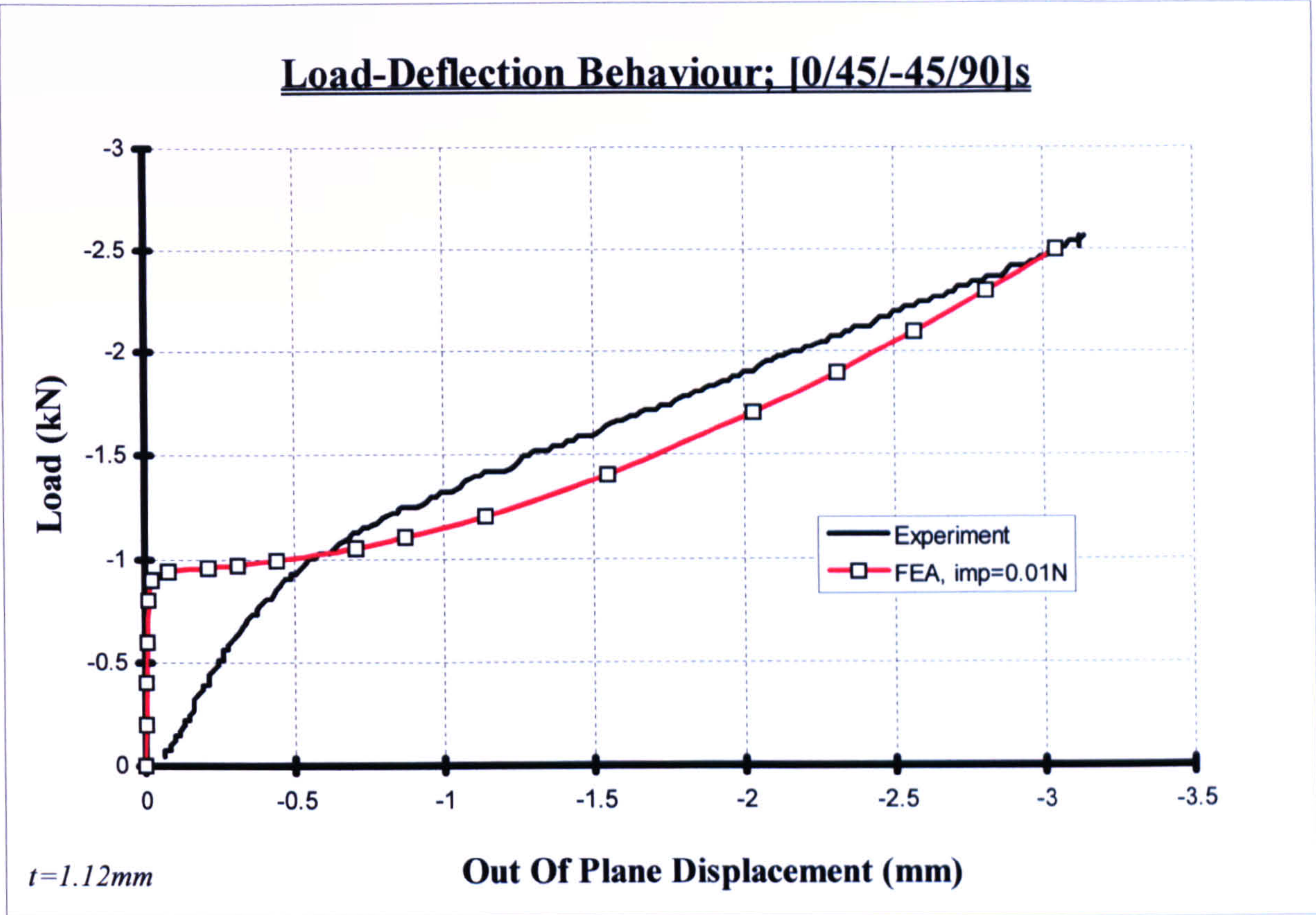


Figure 4.16. Non-linear behaviour of specimen configuration number 3.

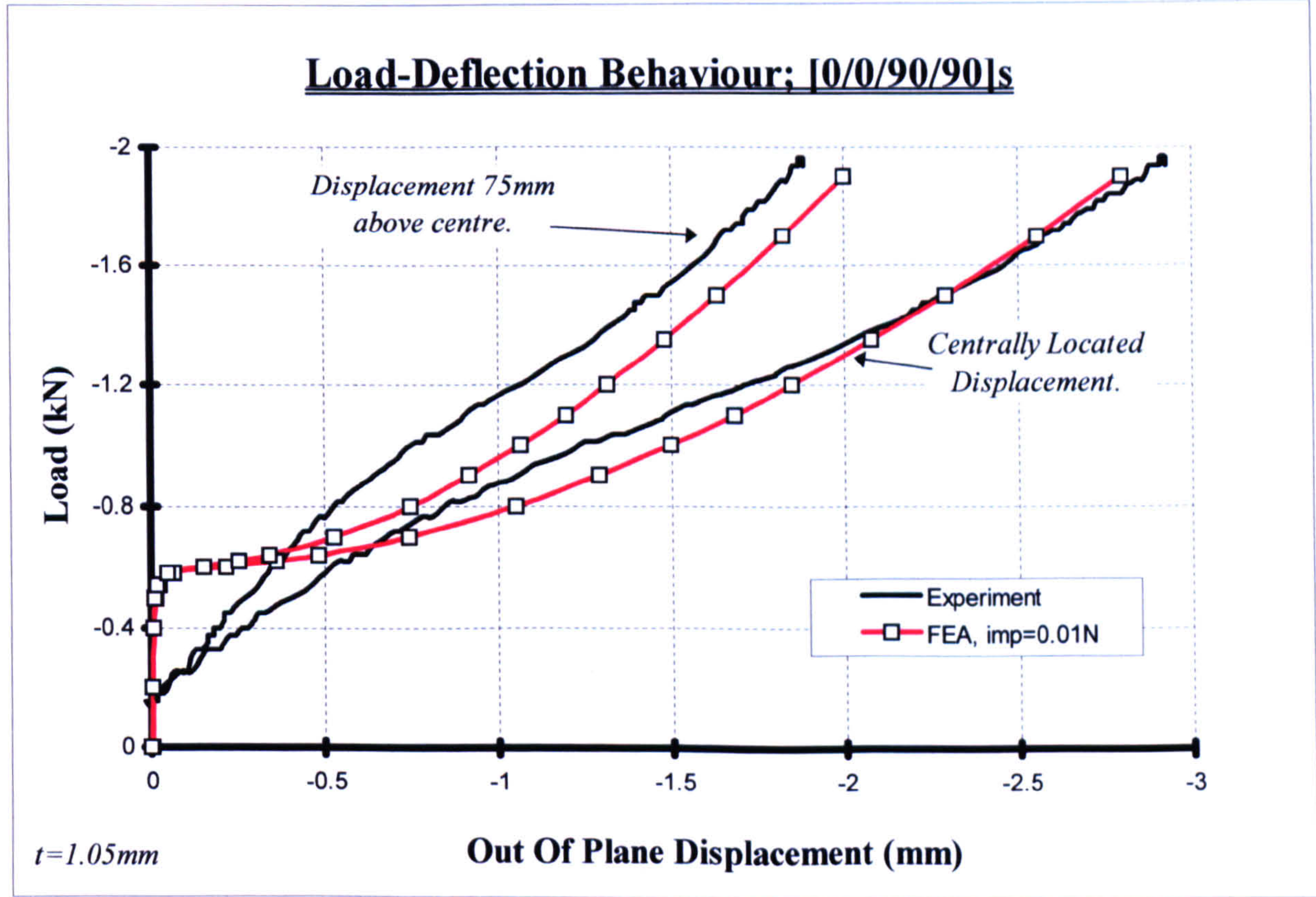


Figure 4.17. Non-linear behaviour of specimen configuration number 7.

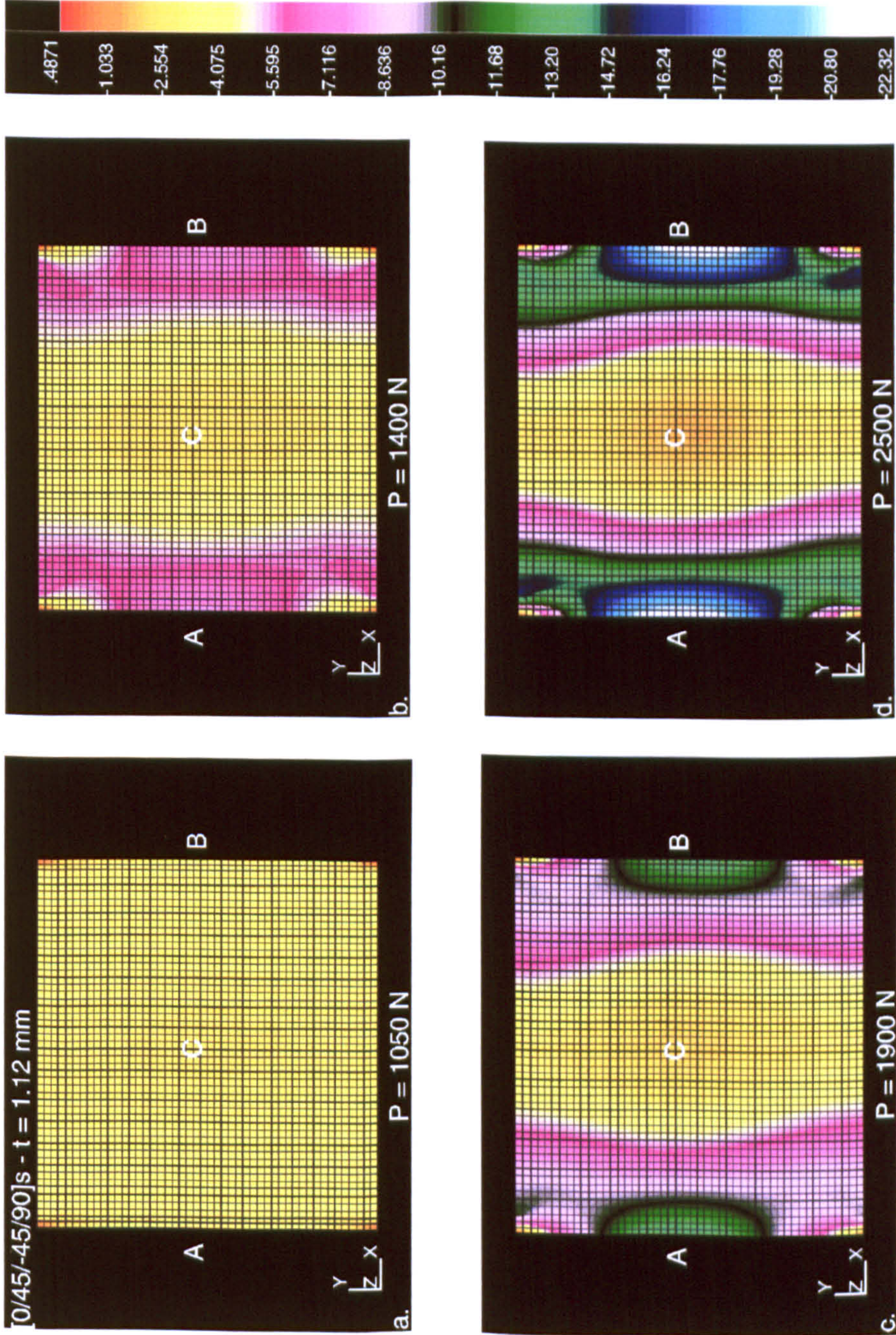


Figure 4.18. YY membrane force intensity distribution for specimen configuration number 3 while under pure compression.

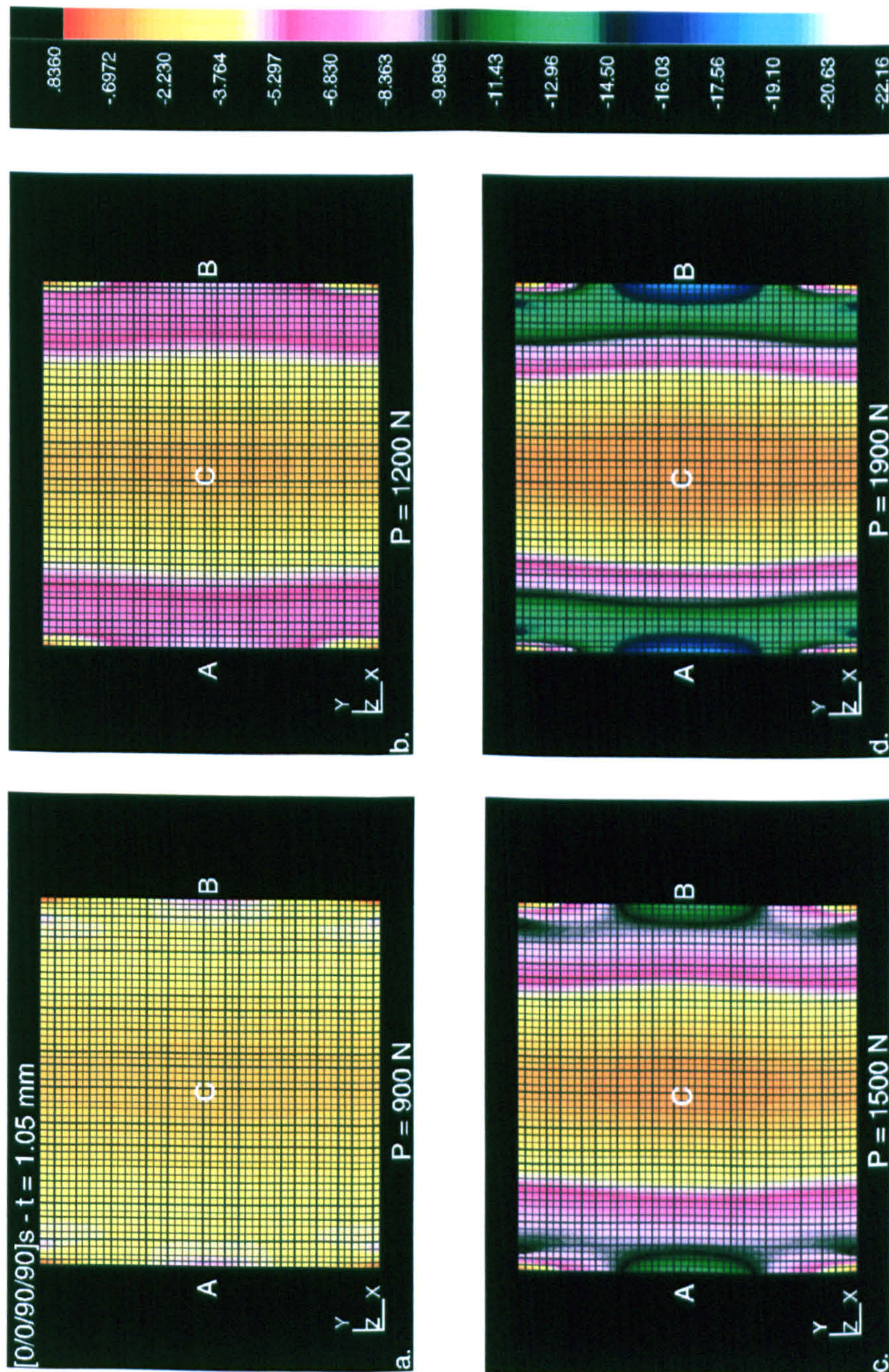


Figure 4.19. YY membrane force intensity distribution for specimen configuration number 7 while under pure compression.

The force is redistributed towards the edges of the plate, A & B. **Figures 4.18d & 4.19d** show a highly non-linear force intensity distribution, the component force being a minimum at the central region, C, and tending to a maximum towards the plate sides, A & B.

Such behaviour is typical of un-controlled post-buckled configurations. For our test specimens, although the axial force intensity is large, the individual ply stress values are well below the material allowable values. Within **Figures 4.18a-d**, close examination reveals the slight effects of anisotropy. Nodal average YY force intensity results are shown for asthetical reasons. The appearance of the results is pleasing but they do convey slight discretization error.

4.10.2 Control Strategy 2

As with the previous section, the results presented within this section are in the form of load versus lateral displacement comparisons between FE and experiment. Plots of the out-of-plane deformation versus applied compressive load are presented in **Figures 4.20 & 4.21** for specimen configurations 10 and 11 respectively.

For the configuration number 10, i.e. the orthotropic lay-up, the linear eigensolution is sufficient to estimate the buckling load factor, **Figure 4.20 & Table 4.3**. For the configuration number 11, i.e. the anisotropic lay-up, the linear eigensolution, however, significantly over estimates the buckling load, **Figure 4.21 & Table 4.3**. This is understandable as the eigensolution does not represent the *real* response which sees bending and buckling in the panel. The anisotropic nature of specimen configuration number 11 promoting the out-of-plane deformation response with increasing compressive load.

For both panel specimens, the experimental results show the general trend of the behaviour predicted. In fact, **Figures 4.20 & Figure 4.21** show very similar post-buckling stiffness between FE and experiment. The numerical results do, however, show higher post-buckling stiffness curves. This is, as anticipated, the likely cause of the continuous account of the glass insulting weave over the central 110mm of the specimen's FE representation, **Section 4.8.3**. As stated previously, the glass weave is only located over discrete regions local to the constrained SMA actuators. The continuous nature will undoubtedly stiffen the structure.

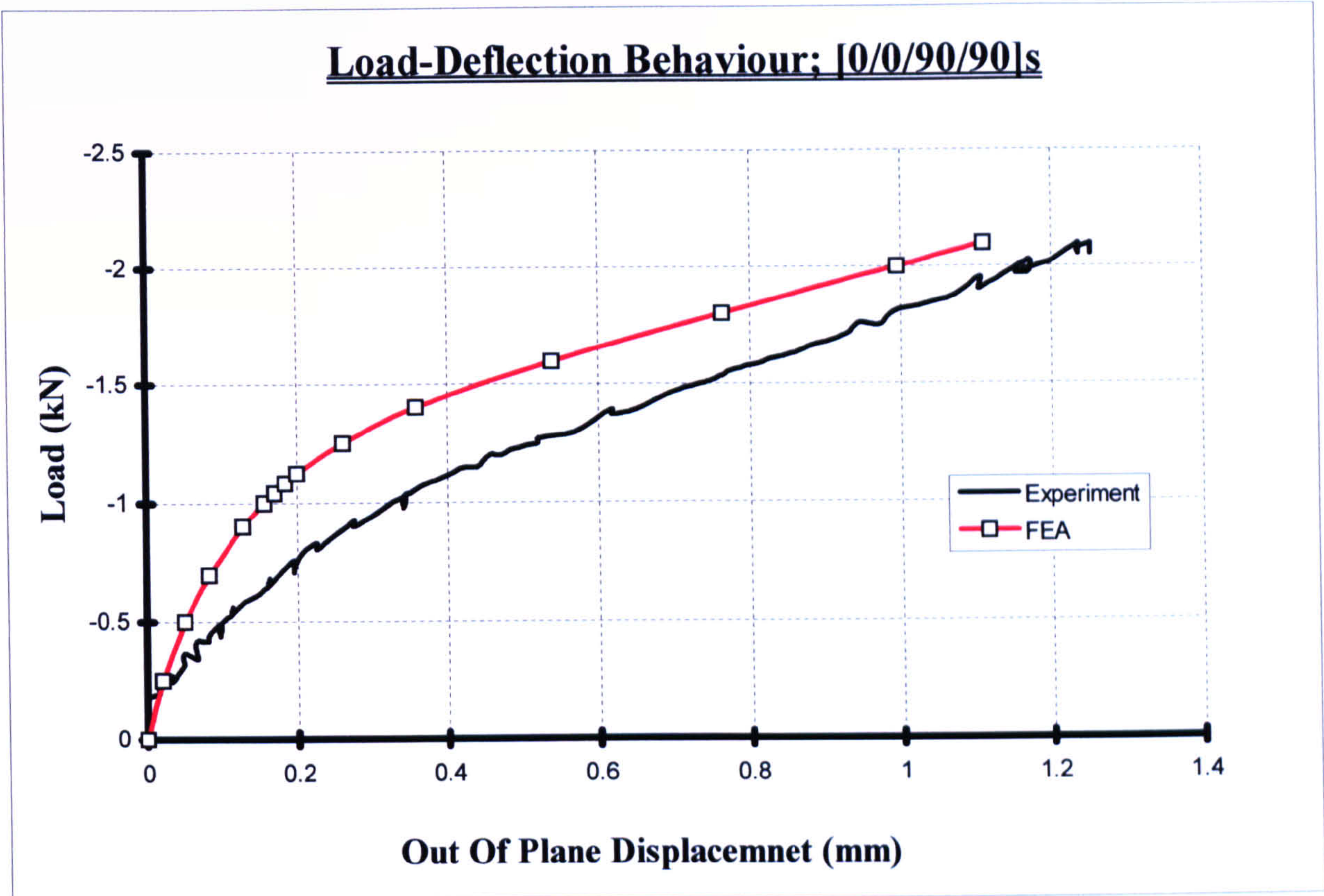


Figure 4.20. *Non-linear behaviour of specimen configuration number 10.*

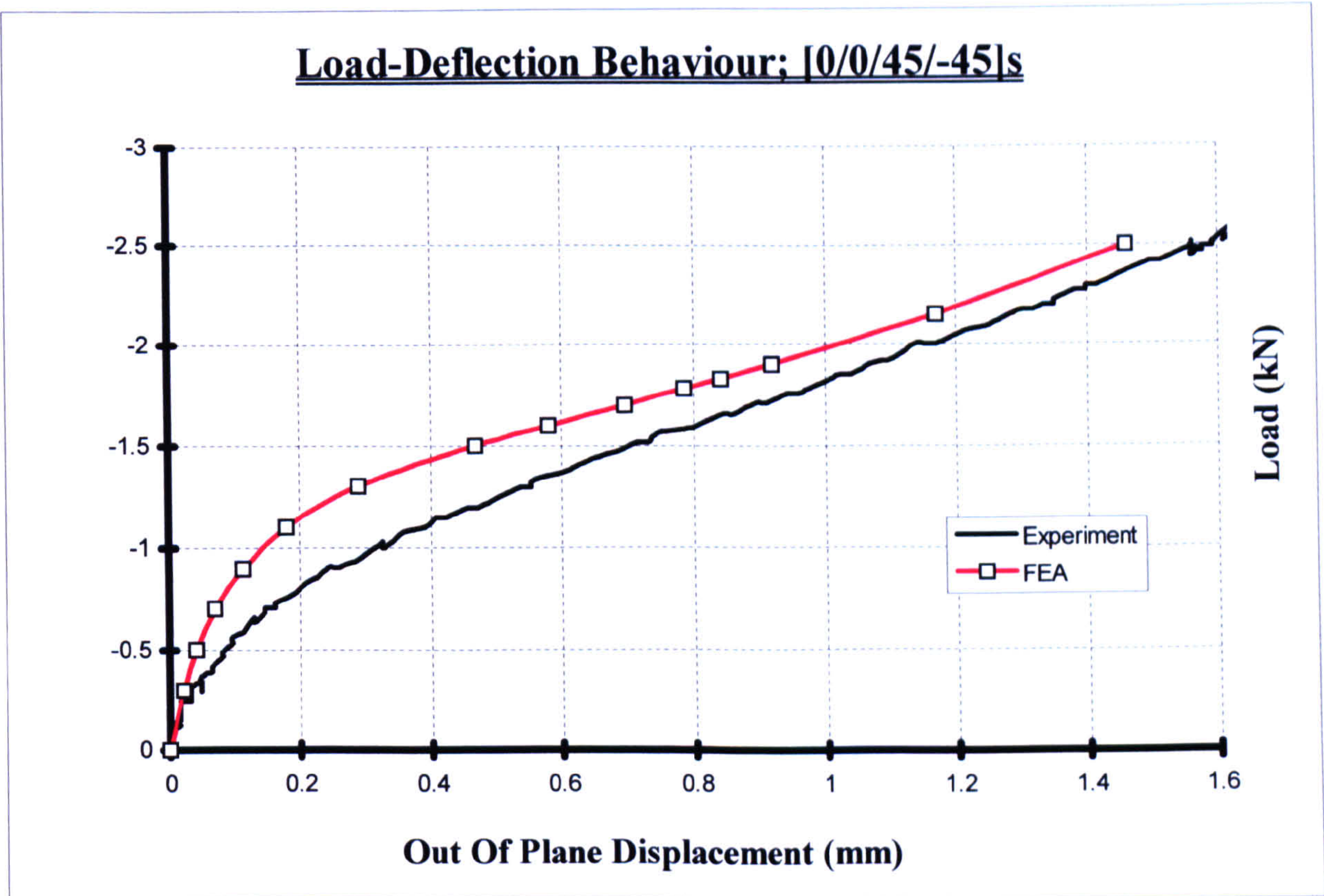


Figure 4.21. *Non-linear behaviour of specimen configuration number 11.*

Chapter 5

Thermal Aspects Attributed To Shape Memory Alloy **Ohmic Heating**

5.1 Introduction

Subjecting the SMA wire actuators to an electrical stimulus will result with resistive, or ohmic, heating. Such heating imposes a non-uniform temperature profile within the laminates. This temperature profile needs evaluation so that an account can be made for the thermal stresses in the non-linear analysis, **Chapter 6**.

For each control strategy employed, the determination of the temperature distribution inside the representative composite lay-ups has been calculated numerically and verified experimentally. The influencing effects that these temperature profiles will have on the fabricated lay-up's mechanical response is discussed.

The elevated temperature is shown to degrade the matrix dominated material properties, as expected. Such degradation accelerating as the hosts glass transition temperature is approached. Creep behaviour, within the polymeric-matrix materials, is briefly reviewed. Such behaviour being highly dependent on the stacking sequence of the chosen laminates. Within this study, owing to the large percentage of zero directional plies parallel to the loading direction, within each of the manufactured plates, and within the current operating time scales, viscoelastic or creep effects of the specimens can be ignored.

First, the governing classical heat flow equations are presented. Complex geometry and/or boundary conditions precludes the use of exact analytical solutions, therefore, we resort to numerical means for the temperature prediction. P3/Thermal was used to obtain the non-uniform temperature profiles.

The P3/Thermal thermal network package was chosen over alternative solution schemes, such as the finite element or finite difference methods, because of the ease of which boundary conditions may be handled and its computational efficiency, particularly for large non-linear or transient problems.

5.2 Classical Heat Flow Analysis

In an isotropic material, the existence of a temperature gradient results with heat flow in a direction parallel to this temperature gradient. Heat flow is defined as the product of heat flux with cross-sectional area. The heat flux is governed by the Fourier heat conduction equation;

$$f_x = -k \frac{\partial T}{\partial x} \quad (5.1)$$

where f_x is the heat flux, or rate of heat transfer per unit area (W/m^2), and k is the thermal conductivity ($\text{W}/\text{m}^\circ\text{C}$), the measure of speed at which heat is conducted through the material¹¹⁵. The negative sign, in equation (5.1), indicates the flow of heat in a direction opposite to the direction of temperature increase.

For our laminated plates, a more general form of the heat flow equation is written as;

$$\begin{Bmatrix} f_x \\ f_y \\ f_z \end{Bmatrix} = -K \begin{Bmatrix} \partial T / \partial x \\ \partial T / \partial y \\ \partial T / \partial z \end{Bmatrix} \quad (5.2)$$

where K is a 3 by 3 matrix of thermal conductivity's and x, y & z are three mutually perpendicular axes. If x, y & z are aligned with the laminate's principal material axis, then, K is a diagonal matrix. Note, for laminated constructions, the heat flow is not necessarily parallel to the temperature gradient¹¹⁵.

The classical heat equation that we wish to numerically approximate is derived from the principle of conservation of energy. For a "representative control element", then, *heat flow into the element - heat flow out of the element + internally generated heat within the element = rate of heat increase within the element*. For a three-dimensional solid, the heat equation is in the form of the following differential equation¹¹⁶;

$$-\left[\frac{\partial}{\partial x} \quad \frac{\partial}{\partial y} \quad \frac{\partial}{\partial z} \right] \begin{Bmatrix} f_x \\ f_y \\ f_z \end{Bmatrix} + q_v = c_p \rho \frac{\partial T}{\partial t} \quad (5.3)$$

The first term represents, through conduction, *heat flow into the element - heat flow out of the element*. The second term, q_v , is the rate of internal heat generation per unit volume (W/m^3) and the latter term represents the *rate of heat increase within the element*. The latter term comprising c_p , the specific heat ($\text{J/Kg}^\circ\text{C}$), ρ , the mass density (Kg/m^3) and t , the time (s). The volumetric internal heat generation is due to electrical resistance heating within the actuator.

For steady-state conditions to prevail, $\partial T / \partial t = 0$. At this point, substitution of equation (5.2) into (5.3) yields;

$$K \left[\frac{\partial^2 T}{\partial x^2} \quad \frac{\partial^2 T}{\partial y^2} \quad \frac{\partial^2 T}{\partial z^2} \right] = -q_v \quad (5.4)$$

the steady-state 3-dimensional heat conduction problem. Using the network technique, a numerical approximation is used to represent the classical energy equation and, with appropriate boundary conditions, will be used to determine the temperature profiles within the hybrid laminated specimens.

So far, consideration has been given only to conduction as a means of heat transfer. Exposing the surface of our specimen, at temperature T , to a fluid of temperature T_f ($T \neq T_f$), then, the transfer of heat between the two media will take place by the mechanism known as convection. There are two types of convection. Natural convection and forced convection. The former being driven by buoyancy forces that arise from temperature differentials and, thus, density differentials within the fluid boundary layer. This boundary layer lies in close proximity to the surface of the stationary specimens. The latter arising as a result of motion of the fluid. We will be concerned with both types of convection.

The following equation is used to treat heat transfer by convection;

$$f = h(T - T_f) \quad (5.5)$$

where f is the heat flux normal to the surface (W/m^2) and h is termed the convection heat transfer coefficient ($\text{W/m}^2\text{°C}$)¹¹⁸. The heat flux is positive and transferred from the surface when $T > T_f$ and negative if the heat is transferred to the surface, i.e., $T_f > T$. Such heat transfer is accounted for by subtracting equation (5.5) from the left hand side of equation (5.3).

5.3 The Patran Thermal Analysis Solver

For any combination of the following; (i) complex geometry, (ii) temperature dependent boundary conditions, and (iii) temperature dependent material properties, then, equation (5.3) cannot be solved analytically. It can, however, and will be approximated numerically.

The numerical analysis, introduced in Section 5.4, accounts for the temperature dependent fluid properties which govern the rate of convective heat flow. The analysis also accounts for a non-uniform convective heat transfer coefficient, its magnitude varying with temperature and, thus, time. While, for *control strategy 1*, the model generated is a simple rectangle, for *control strategy 2*, however, the model becomes more complex with the introduction of stiffeners. Such geometric complexities necessitate the use of a numerical approximating procedure.

P3/Thermal was the software package used to ascertain the temperature profile within the laminated plate specimens featuring the energised SMA actuators. The analysis solver, QTRAN, utilizes the thermal network technique. Such a technique is analogous to resistor-capacitor electrical networks¹¹⁹.

As an example of the network technique, for one-dimensional analysis within the QTRAN solver, a one-dimensional conduction resistor is defined as;

$$R = \frac{L}{k * A} \quad (5.6)$$

where L is the length, k is the thermal conductivity, A is the cross-sectional area, and R is the one-dimensional thermal resistance. The heat flow from node 1 to node 2 is determined from;

$$Q_{[1 \rightarrow 2]} = \frac{T_1 - T_2}{R} \quad (5.7)$$

Substituting equation (5.6) into (5.7) will result with the classical heat flux equation (5.1).

Convective heat transfer is treated in a similar manner to conduction with the exception that the convective resistor is defined by;

$$R = \frac{1}{h * A} \quad (5.8)$$

R , once again, being the thermal resistance, h is the convection coefficient, and A is the area of the surface from which heat is being convected. Heat flow across a convective resistor is

determined by substitution of equation (5.8) into (5.7). Such a substitution yielding equation (5.5).

The one-dimensional conductive problem consists merely of a string of conductive resistors which are connected node to node. For a two-dimensional problem, the network consists of these resistors connected node to node in two directions, and likewise for three-dimensional problems. As we have seen, the network technique adequately describes the classical equations.

As with any structural FEA, the geometry of the model is first constructed and then meshed with appropriate elements and nodes. It is well known, however, that for thermal analysis, an element size and shape dictates the accuracy of the results to a lesser extent than a stress analysis based on the same mesh. Nonetheless, the author has ensured that a satisfactory converged solution is obtained. While ensuring a consistent system of units, material properties, boundary conditions and thermal loads are placed accordingly. Although elements are employed for the geometric construction, prior to submission of the completed model to the analysis solver, the discretized model is translated into resistor - capacitor data through a routine, within P3/Thermal, known as PATQ.

5.4 The 2-Dimensional Thermal Heat Flow Model

Activation of the SMA actuators, through resistive heating, will subject the neighbouring laminate to a significant increase in temperature. Based on the assumption of a uniform temperature distribution in a direction parallel to the SMA wires, a two-dimensional model was used to predict the plate cross-sectional temperature profile. The SMA orientation being normal to the 2-D representation. This two-dimensional approximation was successfully employed elsewhere to determine the temperature profile of transversely isotropic fibreglass beams featuring embedded SMA wires and strips^{32,45,120}.

For *control strategy 1*, a section of the physical problem under investigation is shown schematically in **Figure 5.1**. It features the rectangular laminate cross-section with numerous placed SMA actuators. The actual SMA actuator placement is given in the results figures, **Section 5.5**. A similar physical problem representation may be drawn for the second of our control strategies.

The pre- & post-processor MSC/Patran was used to construct the model. Due to the symmetrical nature of each of the plates/panels employed for both the control strategies, the geometric representations can be simplified. For *control strategy 1*, only half of the lay-up was represented. Within this control strategy, the actuators are located on the plates mid-plane, therefore, upon activation, the temperature profile through the thickness will be mid-plane symmetric.

For *control strategy 2*, as the actuators are no longer on the mid-plane, the full lay-up is required, though only half the panel needs representation. Such symmetry conditions were imposed by leaving the appropriate boundaries free from

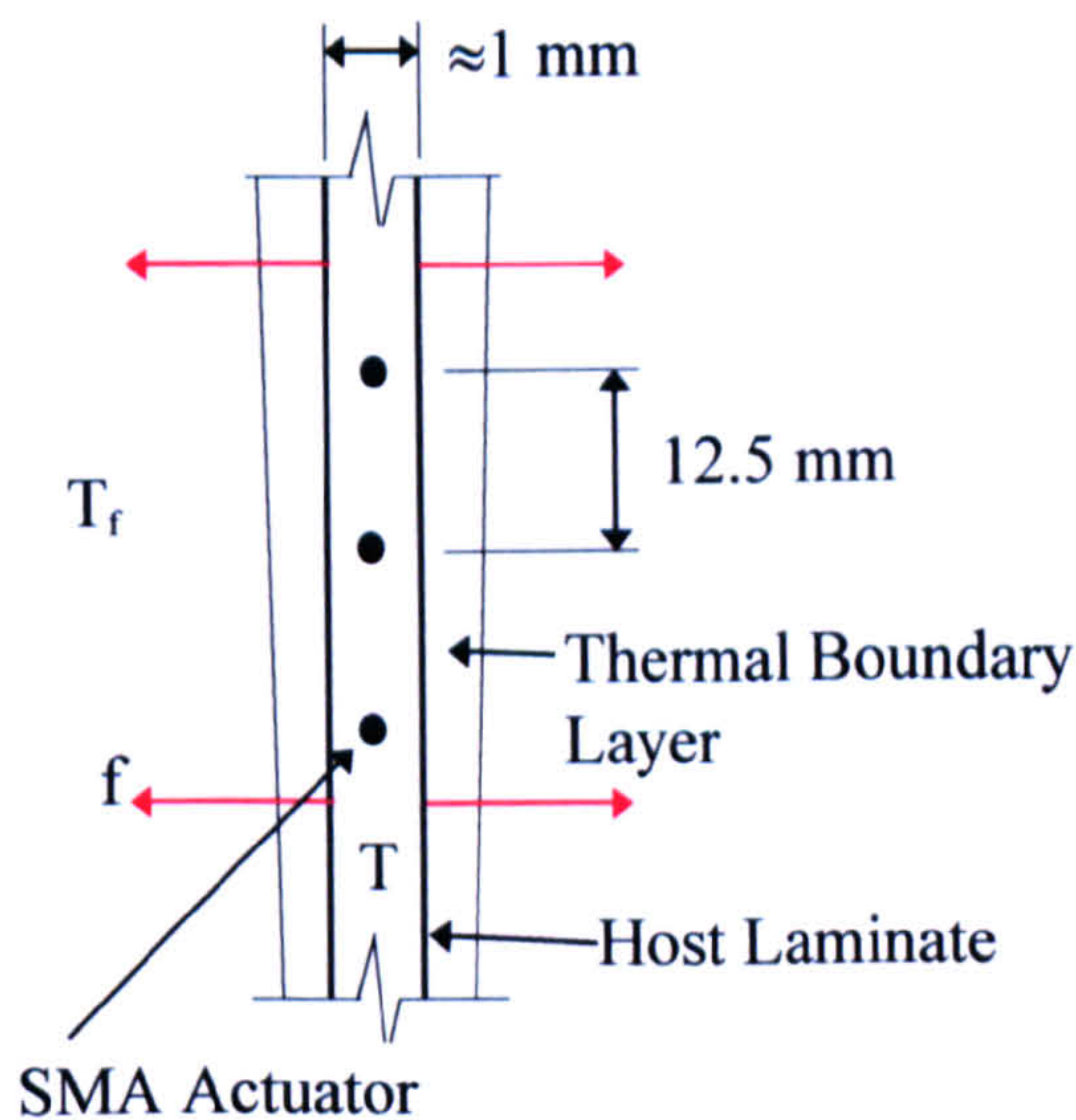


Figure 5.1. Schematic thermal representation of control strategy 1.

thermal constraints. Within the analysis solver, such free constraints are interpreted as adiabatic boundaries, i.e., there will be no heat flow normal to this surface, The free boundaries act like an insulator.

For the plate specimens of *control strategy 1*, for lay-ups $[0_2/90_2]_s$ and $[0_2/\pm 45]_s$, the cross-sectional area under investigation was represented by 1200 2-dimensional thermal quadrilateral elements. The $[0/\pm 45/90]_s$ lay-up was represented by 2400 elements. The former corresponds to an element width equal to the thickness of two plies, whereas the latter corresponds to an element width equal to one ply thickness. The elements employed having unit thickness.

For *control strategy 2*, for both lay-ups considered, 2768 elements were employed. For each strategy, a high mesh density is required in the vicinity of the local heat sources and, for ease of modelling, this mesh density was extended throughout the model. This high mesh density was not seen to be computationally expensive, even during the transient analysis. Upon completion of the mesh construction, a worst case element aspect ratio of 3.79 was witnessed. Such a value is well below the maximum recommended value of 5.

Within the analysis, the average *as built* lay-up thickness was accounted for. No account, however, was made for the surface undulations local to the embedded actuators, as shown in **Figures 3.1-3**.

The results presented yield satisfactory numerically converged solutions. The convergence was checked by varying the mesh density and ensuring that, for each run, upon completion of execution of the QTRAN solver, the temperature change between iterations decreased and approached the recommended value of $1.0e^{-4}$.

The applied element material properties are listed in **Table 5.1**. The composite's directionally dependent thermal conductivity is accounted for by application of $k_{11} = 15W/m^{\circ}C$ in a direction parallel to the carbon reinforcement and $k_{22}= 1W/m^{\circ}C$ in a direction transverse to the carbon reinforcement. The pre-preg manufacturer was unable to provide a value for the longitudinal thermal conductivity. The employed value was taken from the literature¹²¹.

Property	Carbon/Epoxy
ρ (kg/m ³)	1600
c_p (J/Kg ^o C)	837
k_{11} (W/m ^o C)	15
k_{22} (W/m ^o C)	1

Table 5.1. Thermal properties.

Over the temperature range encountered in the experiments of this research, the temperature dependency of the listed properties has been ignored. However, it is possible that the thermal conductivities will actually increase slightly with elevated temperature¹²¹.

No matter what the lay-up, for the 2-dimensional analysis, a thermal conductivity of $1W/m^{\circ}C$ is applied to represent the specimen through thickness direction. For each ply, the global thermal conductivity in the x and y directions are determined from;

$$\begin{bmatrix} k_x \\ k_y \end{bmatrix} = [T]^{-1} \begin{bmatrix} k_{11} \\ k_{22} \end{bmatrix} \tag{5.9}$$

where T is the usual transformation matrix. Should the plies principal material axis be aligned with the global axis, then, the global properties are equal to those of the principal material axes. However, for a ply orientation of θ ($\theta > 0$), then, the necessary transformation is needed. As in our case, for a ply orientation of either $\pm 45^{\circ}$ from the global axis, then, $k_x = k_y = 8\text{ W/m}^{\circ}C$.

The natural convection heat transfer coefficient, h, from the surface of the vertically inclined plates, is calculated through the selection of *Convection Configuration Number 13*, a configuration available from a catalogue of various configurations within the analysis solver.

Convection correlation's are used to support the specified configuration. The correlation employed depends on various parameter ranges, i.e. Rayleigh number, Reynolds number or Prandtl number. Appropriate geometric properties, i.e. plate angle from the vertical, and fluid temperature dependent properties, i.e. specific heat, thermal conductivity, fluid viscosity, CTE and density, are required for adequate property determination of the convective resistors.

Natural convection, on its own, is difficult to achieve in practice. It necessitates an enclosed environment, whereby, external elements cannot influence the results. It was not feasible to test the specimens in such closed quarters, therefore, forced convection will play a part in the surface heat transfer process. Within the numerical analysis, forced convection can be accounted for by selection of *Convection Configuration Number 3*. With this configuration, varying fluid-stream velocities were assessed. It was found, and will be shown, that imposition of a local fluid velocity of 0.1m/s over the surface of the plate yielded satisfactory results. Ignoring the effects of forced convection, then, the numerically determined steady-state temperature profiles where seen to be, at worst, 7-8 °C higher.

Within each numerical simulation, a nodal heat source was applied to the nodes at the SMA locations, thereby, simulating the activation strategy. The actual power level value is given in **Table 5.2** along with the number of activated wires for a respective lay-up configuration. It was elected to use a nodal heating source as it was found in the literature that, for various electrical control inputs on SMA wires embedded in carbon laminates, then, the temperature variation over a particular SMA wire cross-sectional area was less than 1%¹²².

Config. No.	Lay-up Config.	No. Activated SMA's Actuators	Individual SMA Power Level (W/m)
1	[0 ₂ /90 ₂] _s	10	19.94 ^{*1}
2	[0 ₂ /±45] _s	10	19.94 ^{*1}
3	[0/±45/90] _s	12	19.94 ^{*1}
4	[0 ₂ /90 ₂] _s	12	19.94 ^{*1}
5	[0 ₂ /±45] _s	12	19.94 ^{*1}
6	[0/±45/90] _s	14	19.94 ^{*1}
7	[0 ₂ /90 ₂] _s	16	16.41 ^{*2}
8	[0 ₂ /90 ₂] _s	18	13.14 ^{*3}
9	[0/±45/90] _s	17	14.56 ^{*4}
10	[0 ₂ /90 ₂] _s	23	16.53 ^{*5}
11	[0 ₂ /±45] _s	23	16.53 ^{*5}

*¹ I=1.7A, P.D=6.01V.

*² I=1.56A, P.D=5.47V.

*³ I=1.4A, P.D=4.88V.

*⁴ I=1.47A, P.D=5.15V.

*⁵ I=1.087A, P.D=7.37V.

Table 5.2. *Specimen configuration and power level required for actuation.*

Note: (1) Config. Nos. 1-9 are associated with *control strategy 1*. Config. Nos. 10-11 are associated with *control strategy 2*.
 (2) The above power levels, for configs. 1-9, were determined over a 520mm length of SMA wire, whereas, configs. 10 & 11 were determined over 485mm.

This macro-type analysis is based on the assumption that there is perfect thermal contact between the SMA actuators and the host composite. Particular to the first of our control strategies, there is a required tolerance between the two constituents. This tolerance, or conduit type void, was ignored. Its omission is based on conjectural radial expansive behaviour of the actuator and tubing. Consider the case where PTFE tubing is employed. Upon activation, the PTFE will radially expand in a direction that causes gap closure. It

actually expands by an amount that causes compressive normal stresses around the actuators. This is not thought to be of significance for the plate recovery behaviour, **Chapter 6**. This is not the case, however, for the 0.025mm wall thickness polyimide tubing where, at maximum, a gap of 0.06mm exists, nonetheless, its effects have been ignored.

Activation of the SMA wires is achieved by connecting the actuators in parallel to a 25A, 30V d.c. power supply, manually controlled. Owing to matrix constraints, **Section 5.7**, a maximum of 1.7 amps was applied to the 0.4mm diameter actuators, employed for *control strategy 1*, and a maximum of 1.087 amps was applied to the 0.3mm diameter actuators utilized in *control strategy 2*. Due to power supply constraints, within the first of our control strategies, increasing the volume fraction of the SMA material beyond 14 actuators results with a drop in input power stimulus, **Table 5.2**. Note, within the analysis, when employing mid-plane symmetric boundary constraints, then, the actual power level applied to the model is half that tabulated.

It is assumed that the temperature profile parallel to the SMA wires is uniform. For such an assumption, then, the 2-dimensional analysis is warranted. However, for both control strategies, this assumption is not strictly true. For the actual tested panels, such an assumption will be unfound at the edge supports. Such supports acting as heat sinks. The extent of temperature error was not fully accounted for in this study and is one of the proposals for future work.

For *control strategy 1*, the error in predicted temperature is predominant at the knife edge supports, whereas, for *control strategy 2*, the predicted temperature error is predominant at the loading platens.

For *control strategy 1*, as previously stated, the loaded edge supports will act as heat sinks (*of infinite mass*) and can be accounted for by specifying a constant temperature to the nodes congruent with such supports. This condition was enforced to the lower loaded edge of the specimens by specification of the ambient temperature to the nodes. For reasons that will soon become clear, it was decided to leave the upper loaded edge free. Utilizing the 2-dimensional approximation, then, the loaded edge supports cannot be accounted for when analyzing the second of our control strategies, however, due to the orientation of the SMA actuators within this strategy and the cross-sectional area of interest, knife edge influences can be modelled.

The effect of the *heat sink* boundary conditions will be a temperature profile that is more elevated at the centre of the plate than along the surrounding supported edges. Spatial temperature gradients, together with panel imperfections, may induce out-of-plane deformation due to the formation of compressive membrane forces, as cited elsewhere¹²³, **Section 5.6**.

Finally, the ambient and initial specimen temperature was measured to be in the range of 23-25°C and applied to the associated model.

Figure 5.2 shows an enlarged section of the thermal finite element representation used for the temperature determination of *control strategy 2*. The location being at a skin/stiffener interface. The arrows show the direction of the convective heat flux from the specimen surface. Each ply is modelled in the through thickness direction by one thermal element. The differing coloured elements are for illustrative purposes only. Each particular colour is associated with a particular element property and, hence, material property.

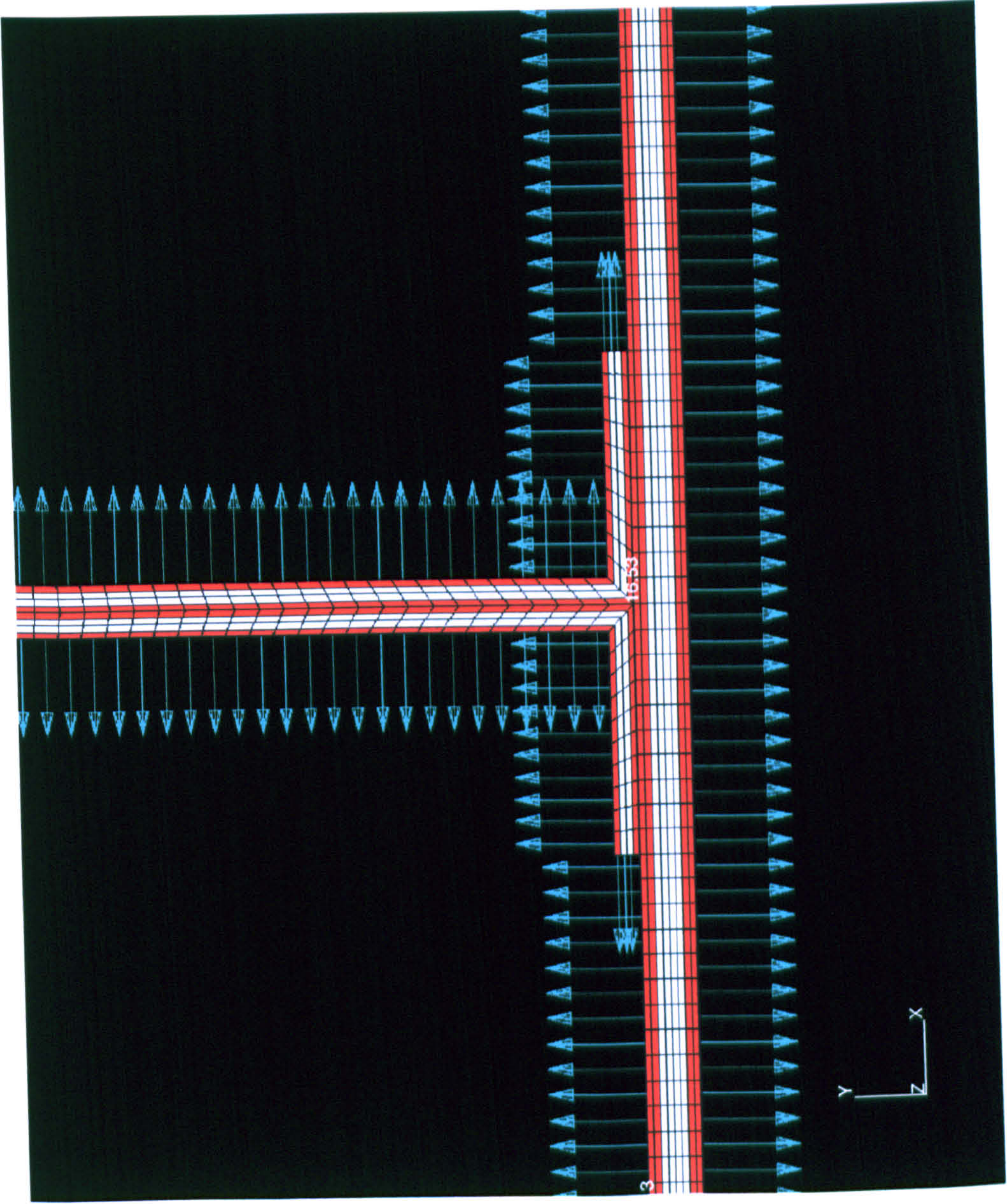


Figure 5.2. Thermal finite element representation of control strategy 2.

5.5 Thermal Heat Flow Results

For the activation strategies proposed, transient and steady-state temperature profiles have been numerically determined. Transient results were output at various time increments. From such results, relevant thermal stress and deformation profiles can be computed. At key locations, the numerical temperatures are verified experimentally. Experimental measurements were made using fast response k-Type thermocouples^a brazed onto a metal disc 3.5mm in diameter. Such thermocouples allow ease of installation and re-use. They also have a reported accuracy of $\pm 3^{\circ}\text{C}$.

The thermocouples are taped to the surface of the specimens at the appropriate place using a polyimide tape. A thermally conductive paste, with high thermal conductivity but low electrical conductivity, was used to aid thermal contact and, thus, relieve thermal resistance between the thermocouples and the specimens. Any thermal resistance may be a cause of error within the thermocouple readings. The magnitude of this error is uncertain.

The thermocouple output was logged using PC based Strawberry Tree “DynaRes” data acquisition software, control board and terminal panel. The terminal panel providing accurate cold junction compensation. The temperatures were logged at 1 Hz.

Table 5.3. provides a list of the specimen configurations previously described, **Table 5.2**, and the associated results figure of reference.

The application of the power level to activate the SMA wires results with the transient-to-steady-state surface temperature profiles shown in **Figures 5.3-16**. The experimentally recorded thermocouple readings, abbreviated as TC, are shown for comparison. It should be pointed out that the experimental readings should be compared only with the final stage of the transient analysis, shown in seconds, or the steady-state profiles, SS.

The comparative thermocouple readings were the maximum readings obtained over the latter stages of the test where there is the tendency towards the steady-state. A selection of typical transient thermocouple response curves, for the duration of one complete SMA actuation cycle, are shown in **Figures 5.4, 5.10 & 5.14**. From these figures the temperature is seen to rise in an exponential fashion. The rate of increase in temperature slowing down as the steady-state temperature is approached.

The SMA actuator locations are illustrated in the numerically determined results figures. For *control strategy 1*, specimen configuration numbers 1-9, the abscissa of the appropriate results figure is parallel to the applied mechanical loading direction of the actual tested specimens. For *control strategy 2*, specimen configuration numbers 10-11, the abscissa of the appropriate results figure is parallel to the applied mechanical loading platen. Only surface temperature profiles are presented as the temperature distribution through the plate thickness was such that it can be assumed constant.

A general observation is that there is reasonable agreement between experiment and analysis. The correct thermal trend is predicted. In particular to specimen configurations 1-9, the analysis does not, however, accurately account for the thermal boundary layer associated

Config. No.	Fig. Of Ref.
1	5.3-4
2	5.5
3	5.6
4	5.7
5	5.8
6	5.9-10
7	5.11
8	5.12
9	5.13
10	5.14-15
11	5.16

Table 5.3. Configuration number and results figure of reference.

^a Purchased from RS Components.

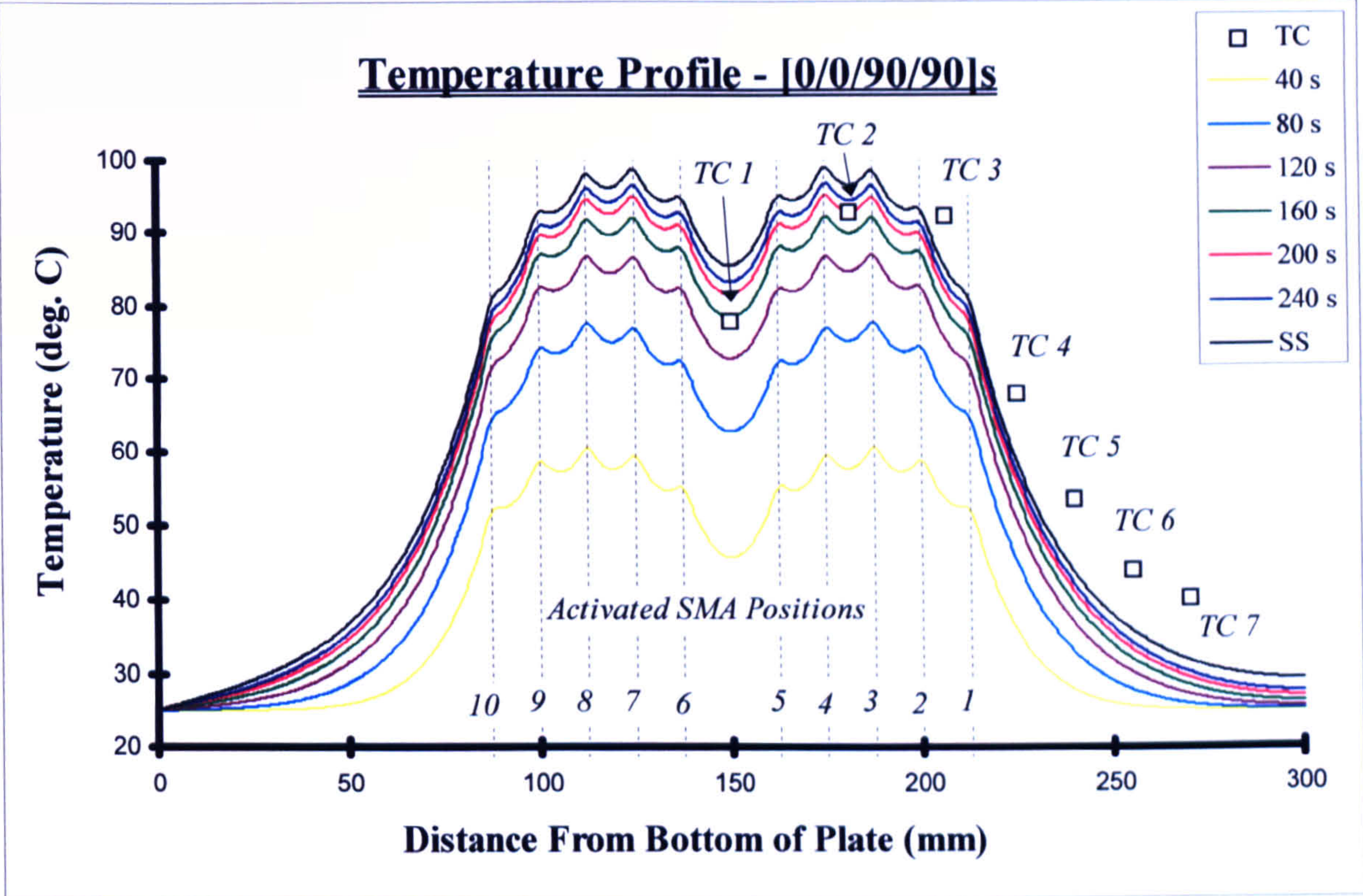


Figure 5.3. Numerical & experimental temperature profile for specimen Config. No. 1.

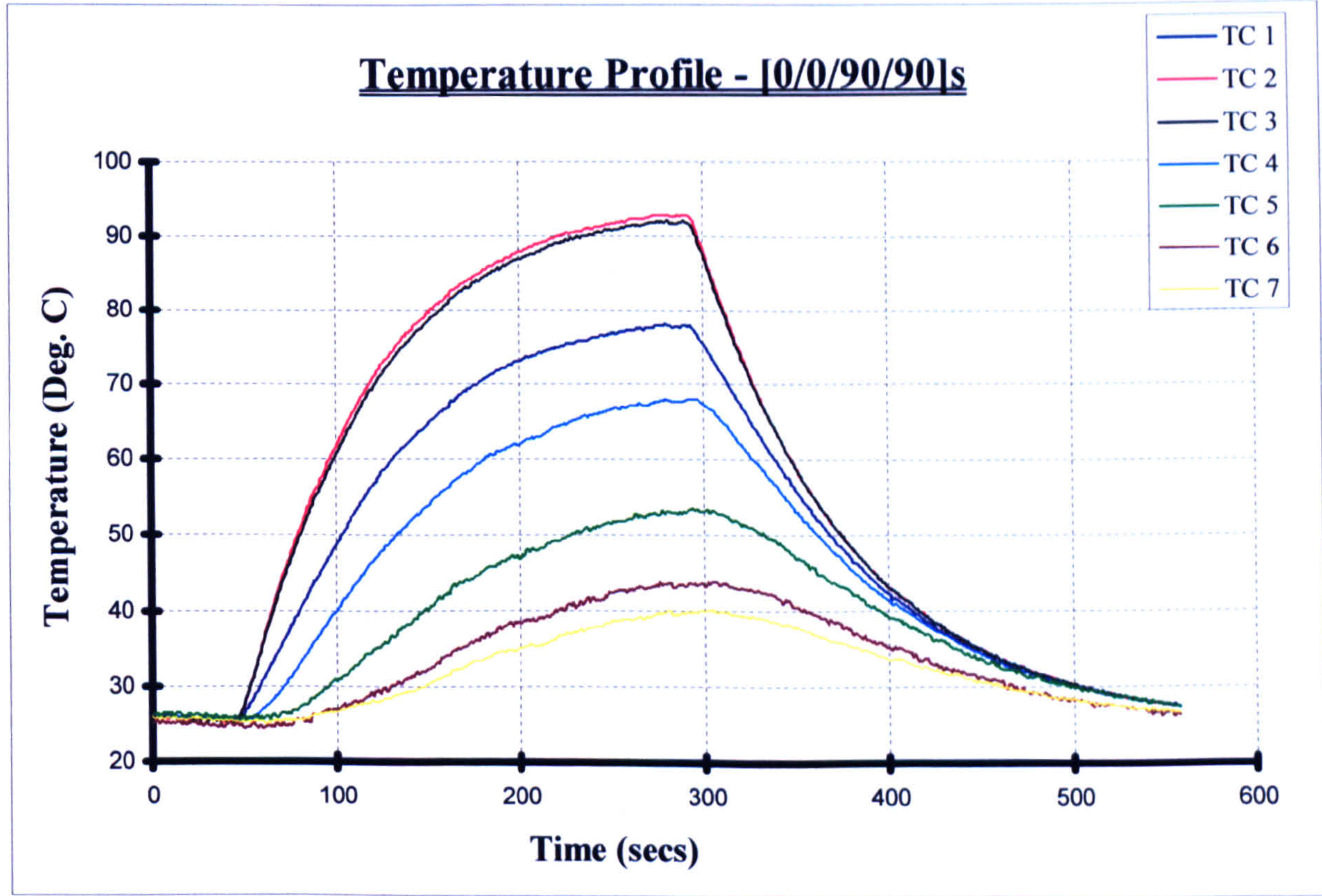


Figure 5.4. Transient thermocouple readings for specimen Config. No. 1.

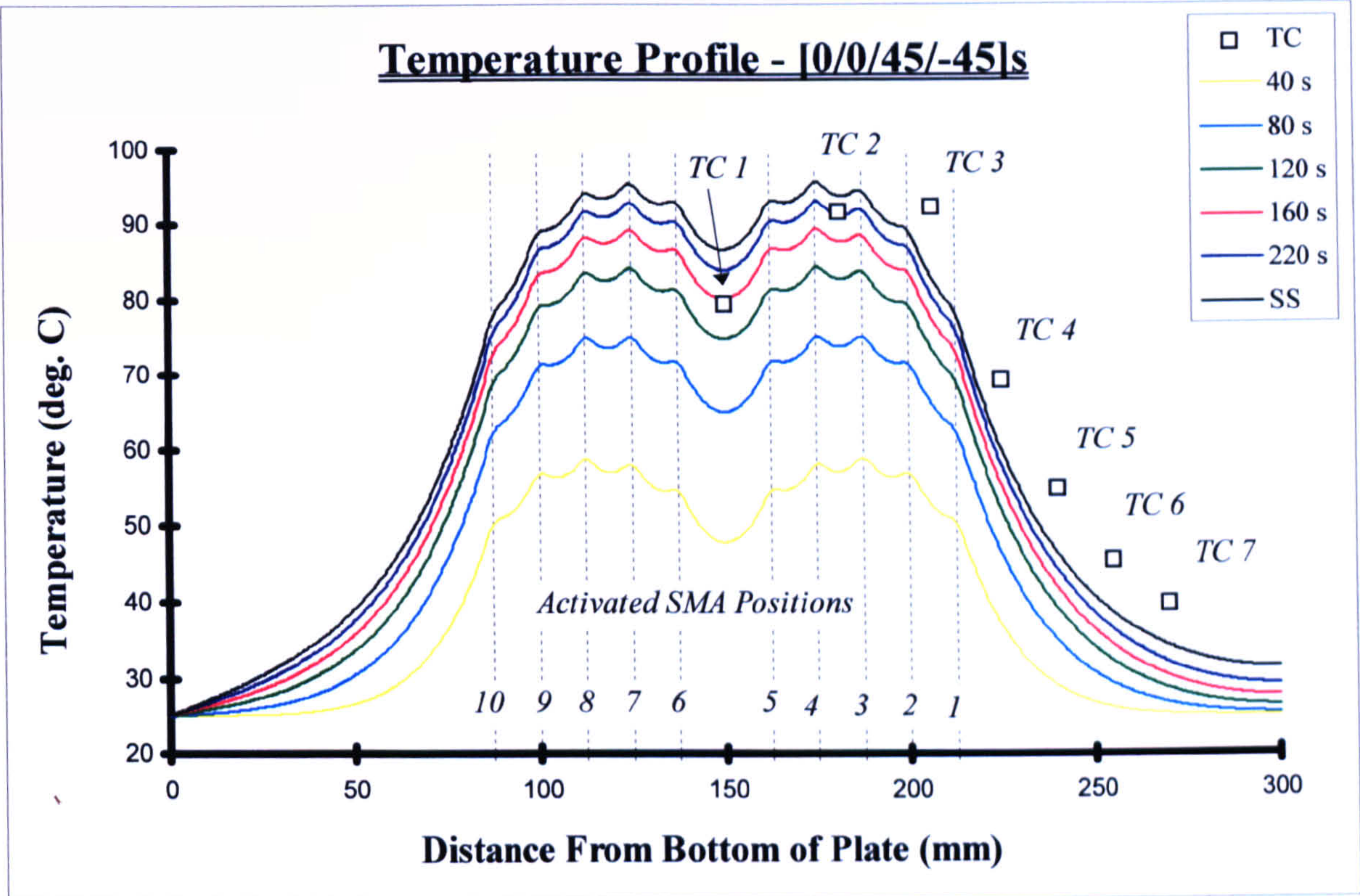


Figure 5.5. Numerical & experimental temperature profile for specimen Config. No. 2.

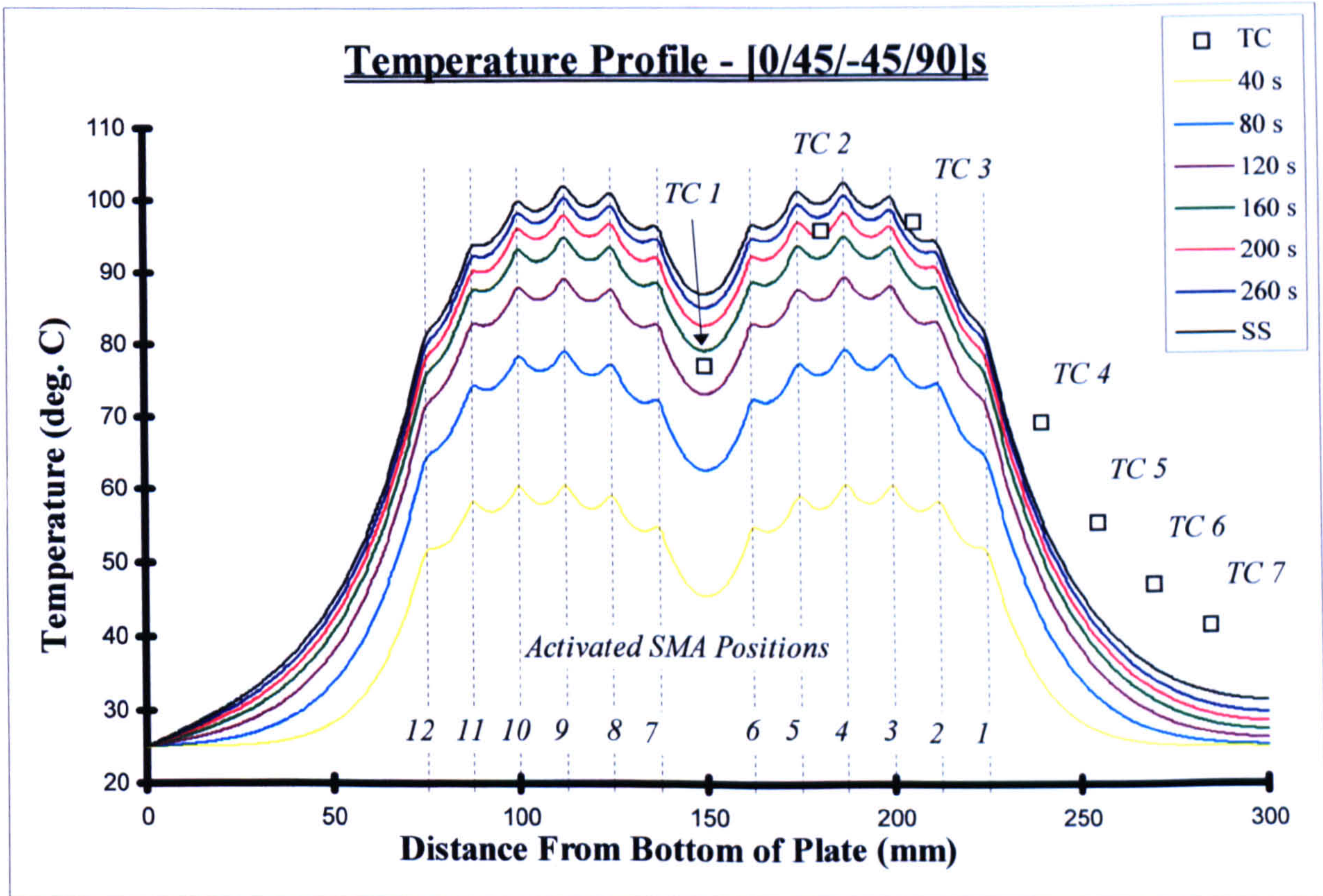


Figure 5.6. Numerical & experimental temperature profile for specimen Config. No. 3.

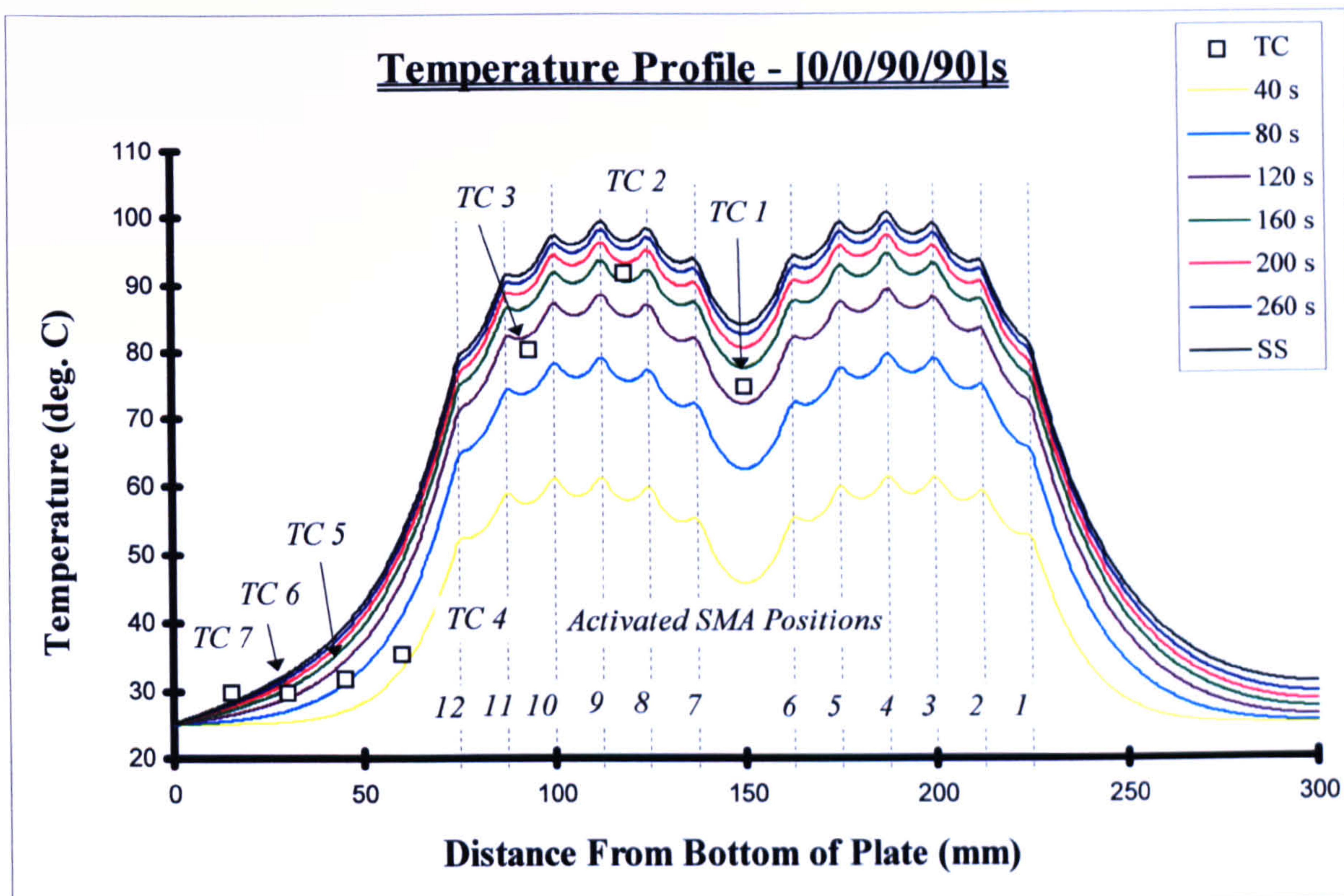


Figure 5.7. Numerical & experimental temperature profile for specimen Config. No. 4.

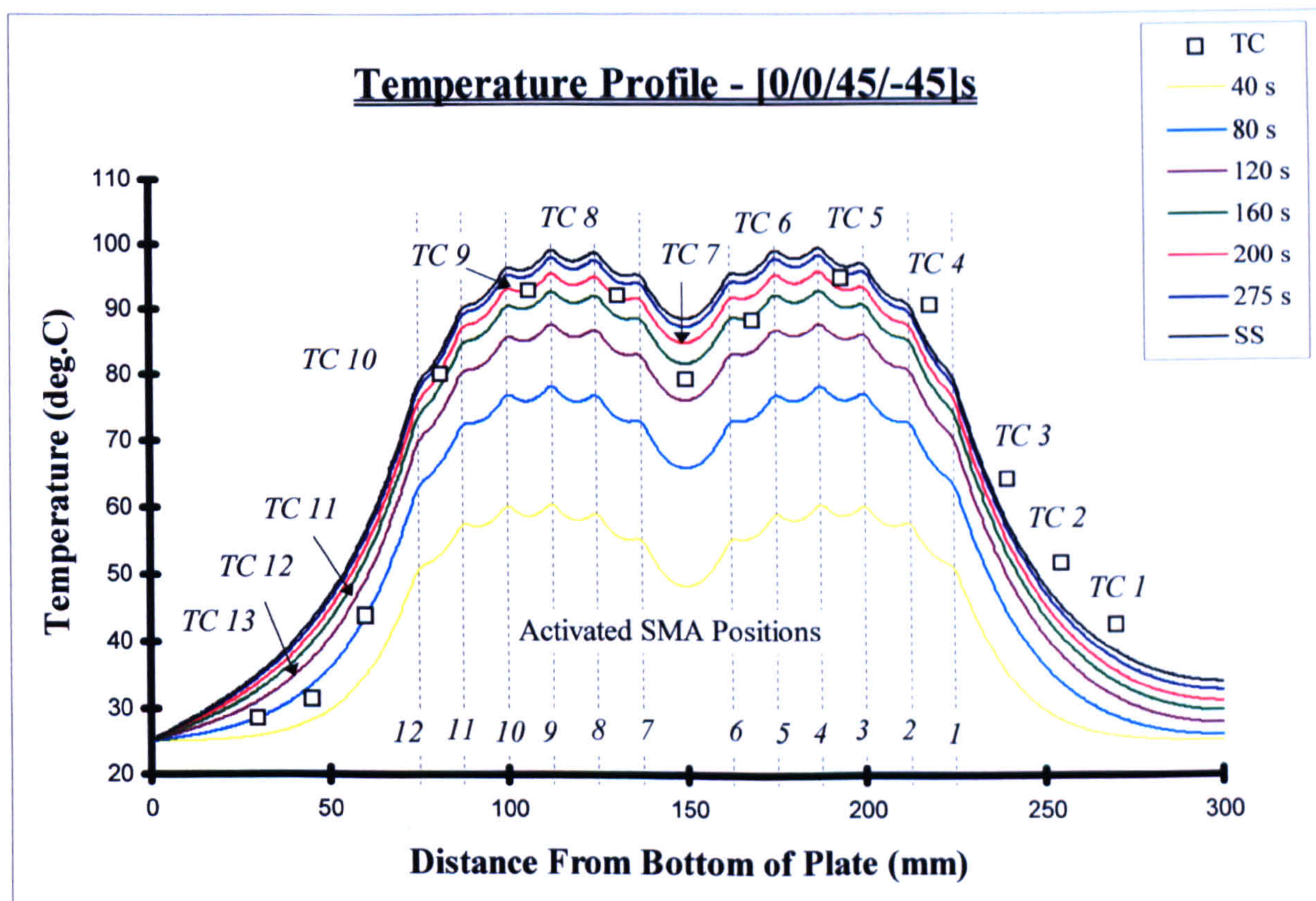


Figure 5.8. Numerical & experimental temperature profile for specimen Config. No. 5.

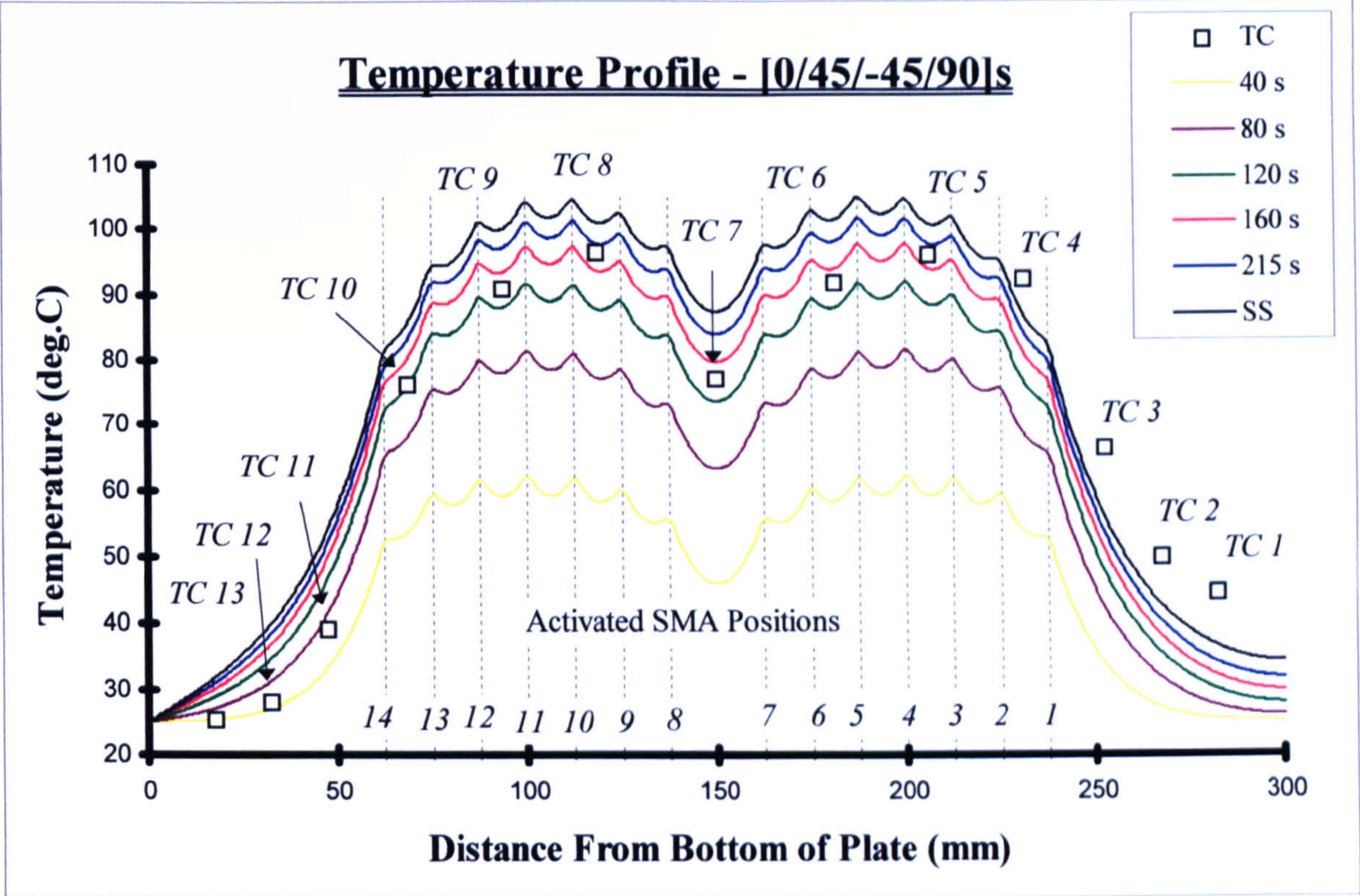


Figure 5.9. Numerical & experimental temperature profile for specimen Config. No. 6.

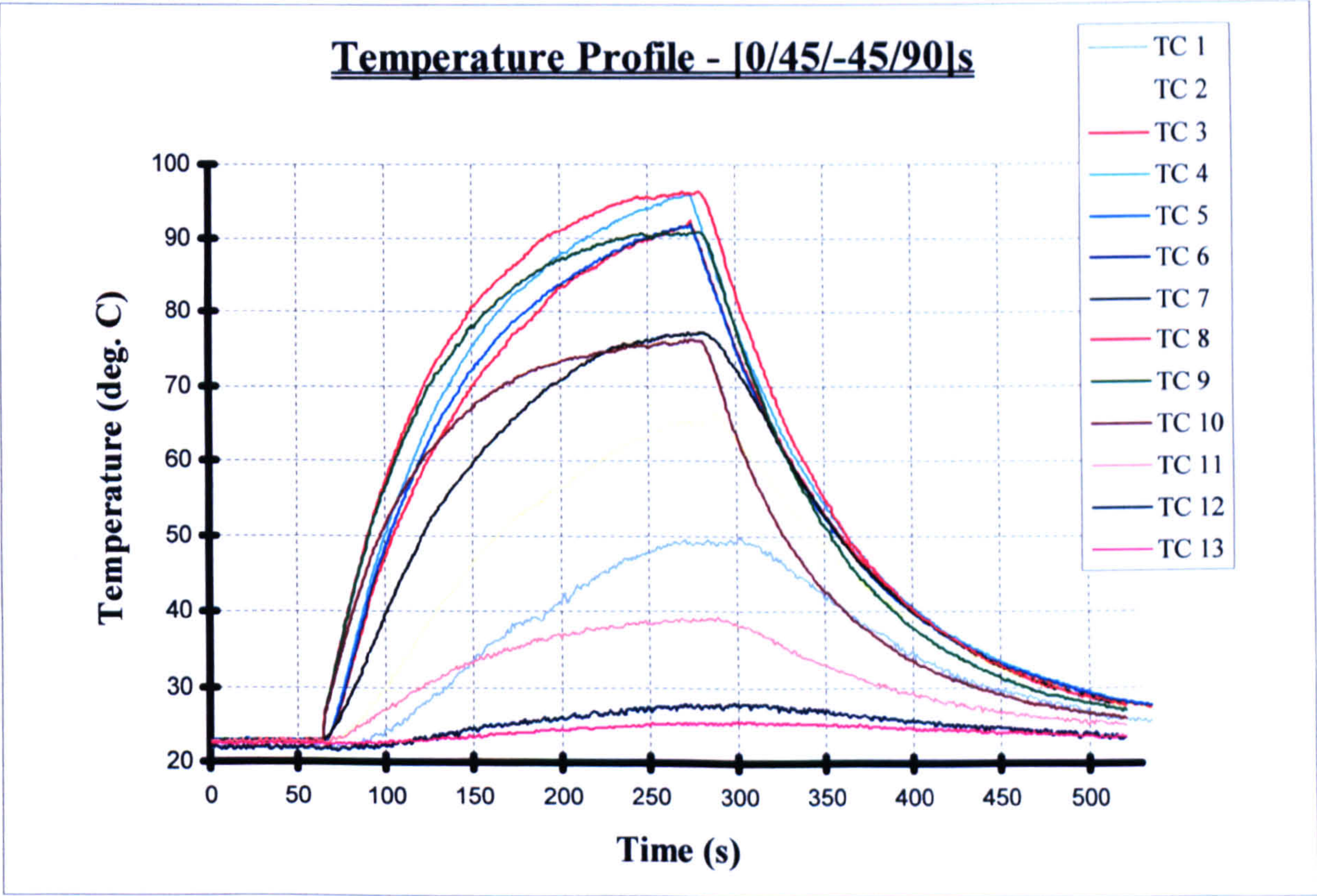


Figure 5.10. Transient thermocouple readings for specimen Config. No. 6.

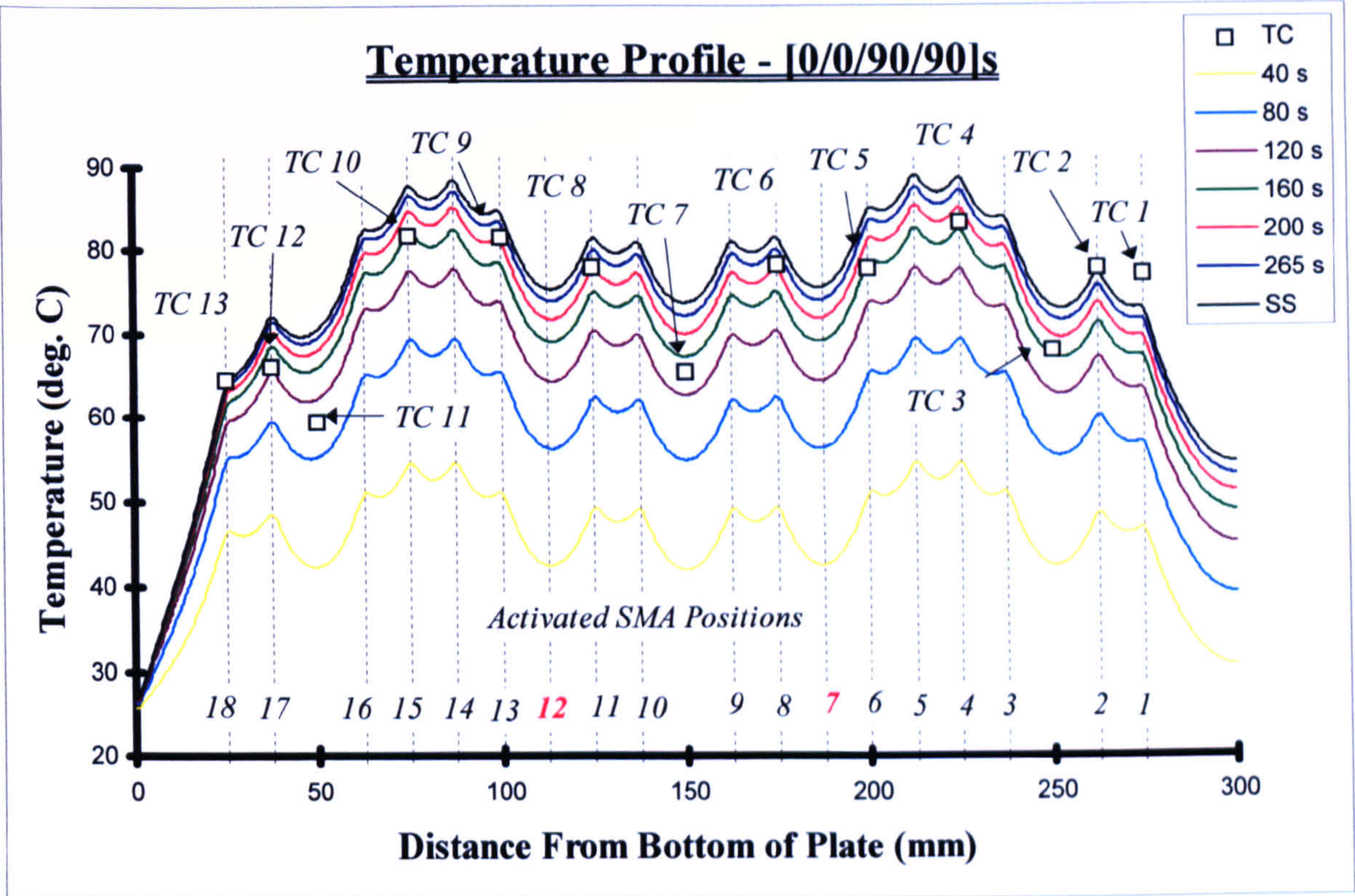


Figure 5.11. Numerical & experimental temperature profile for specimen Config. No. 7.

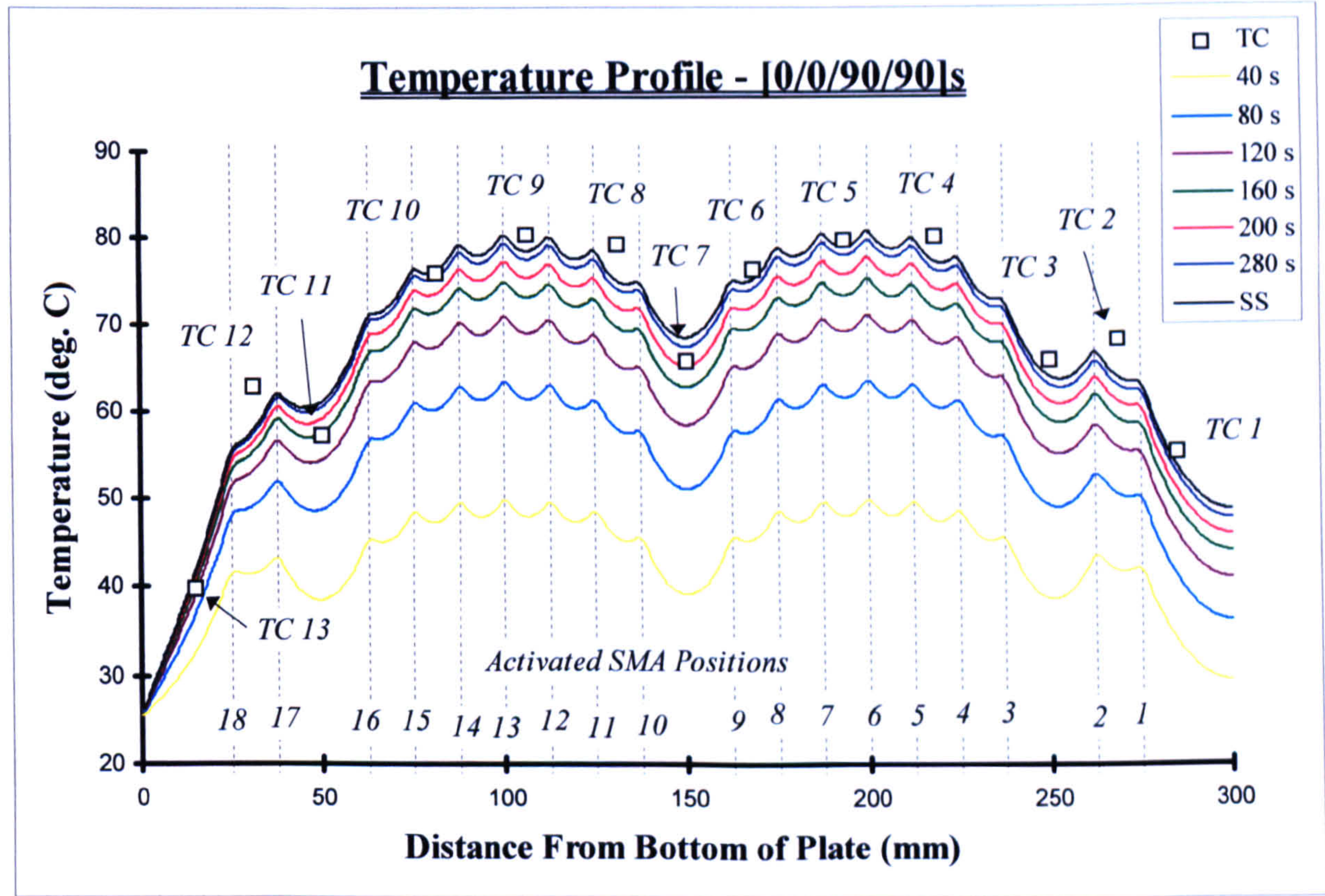


Figure 5.12. Numerical & experimental temperature profile for specimen Config. No. 8.

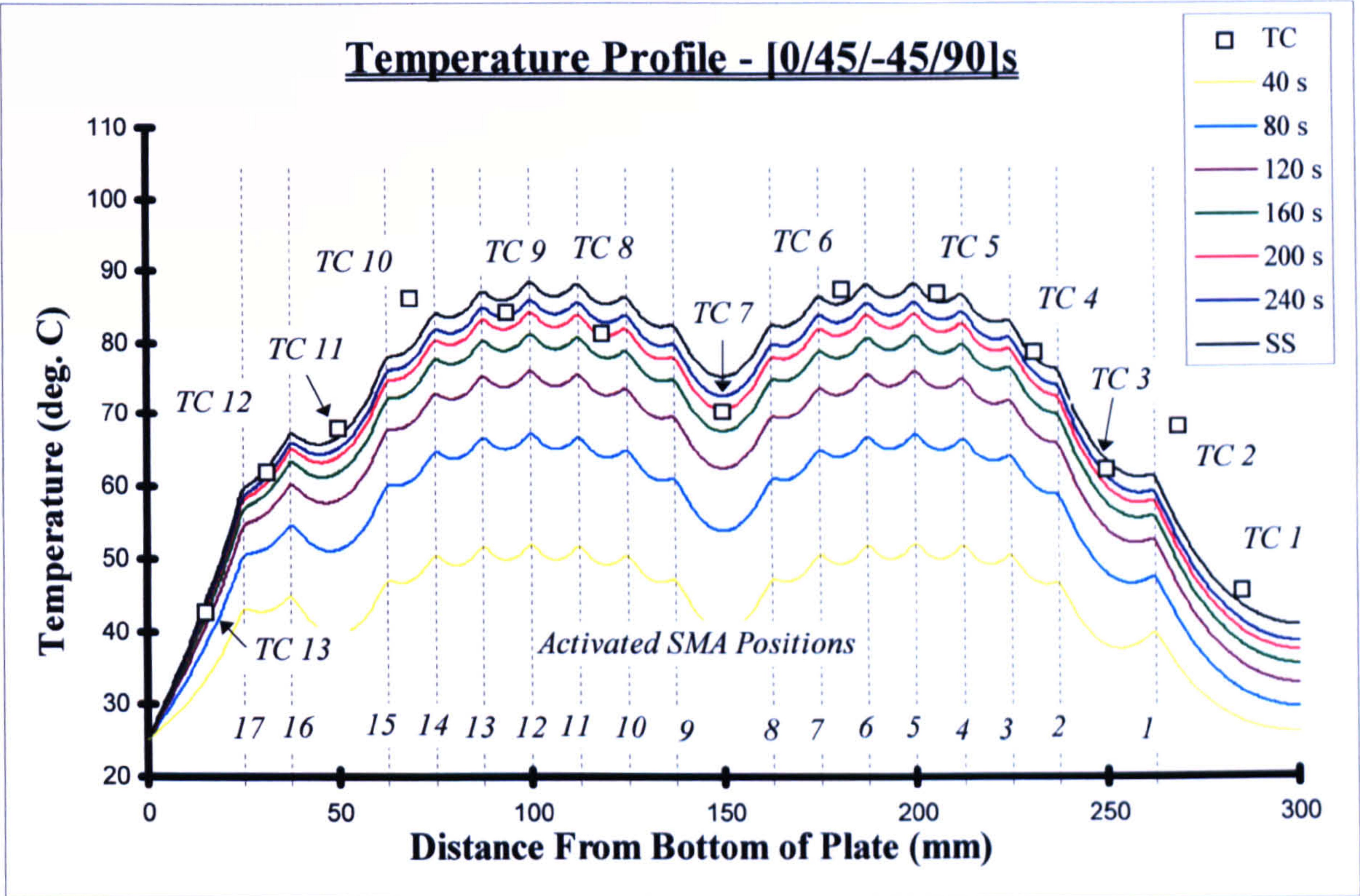


Figure 5.13. Numerical & experimental temperature profile for specimen Config. No. 9.

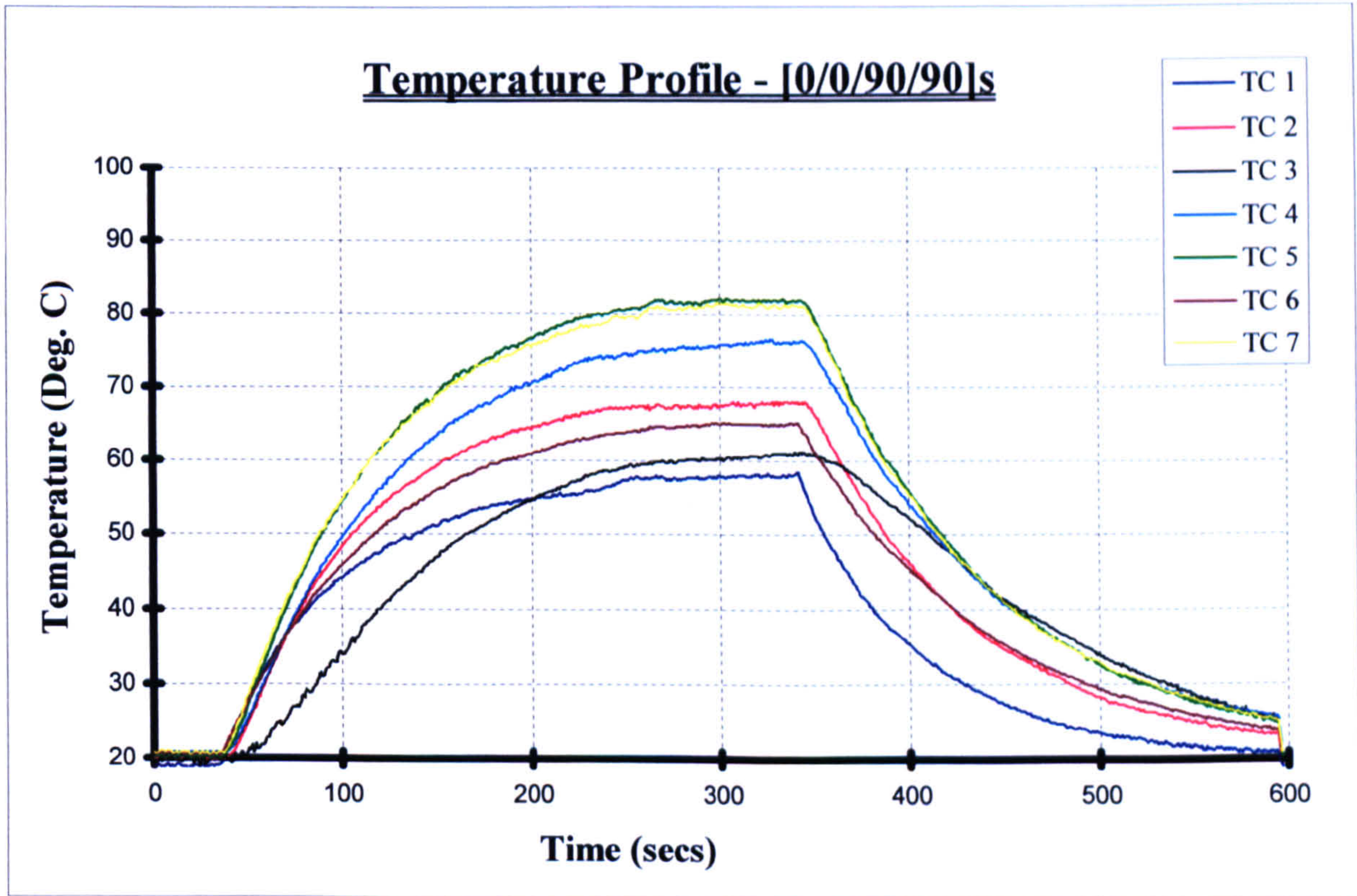


Figure 5.14. Transient thermocouple readings for specimen Config. No. 10.

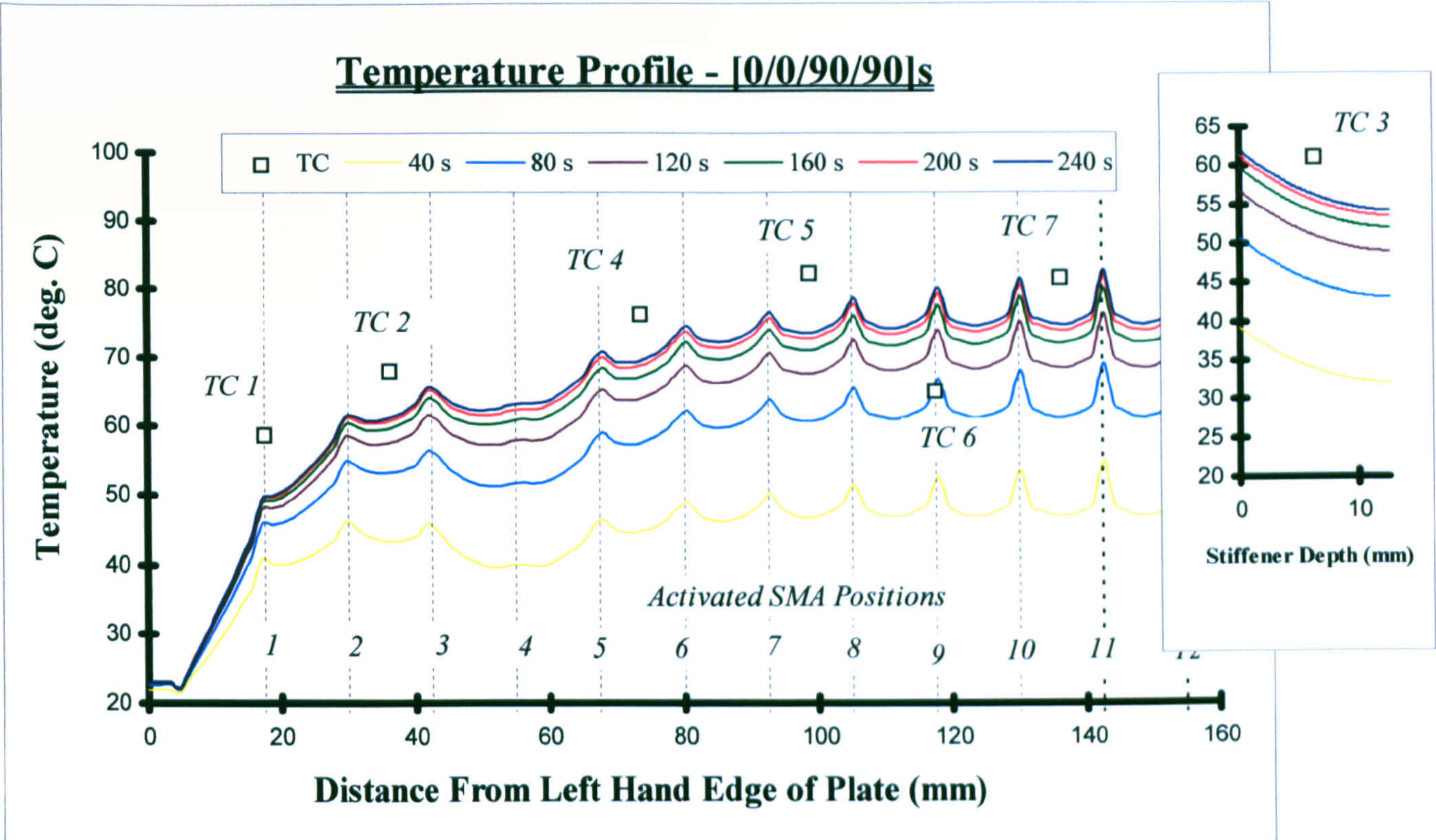


Figure 5.15. Numerical & experimental temperature profile for specimen Config. No. 10.

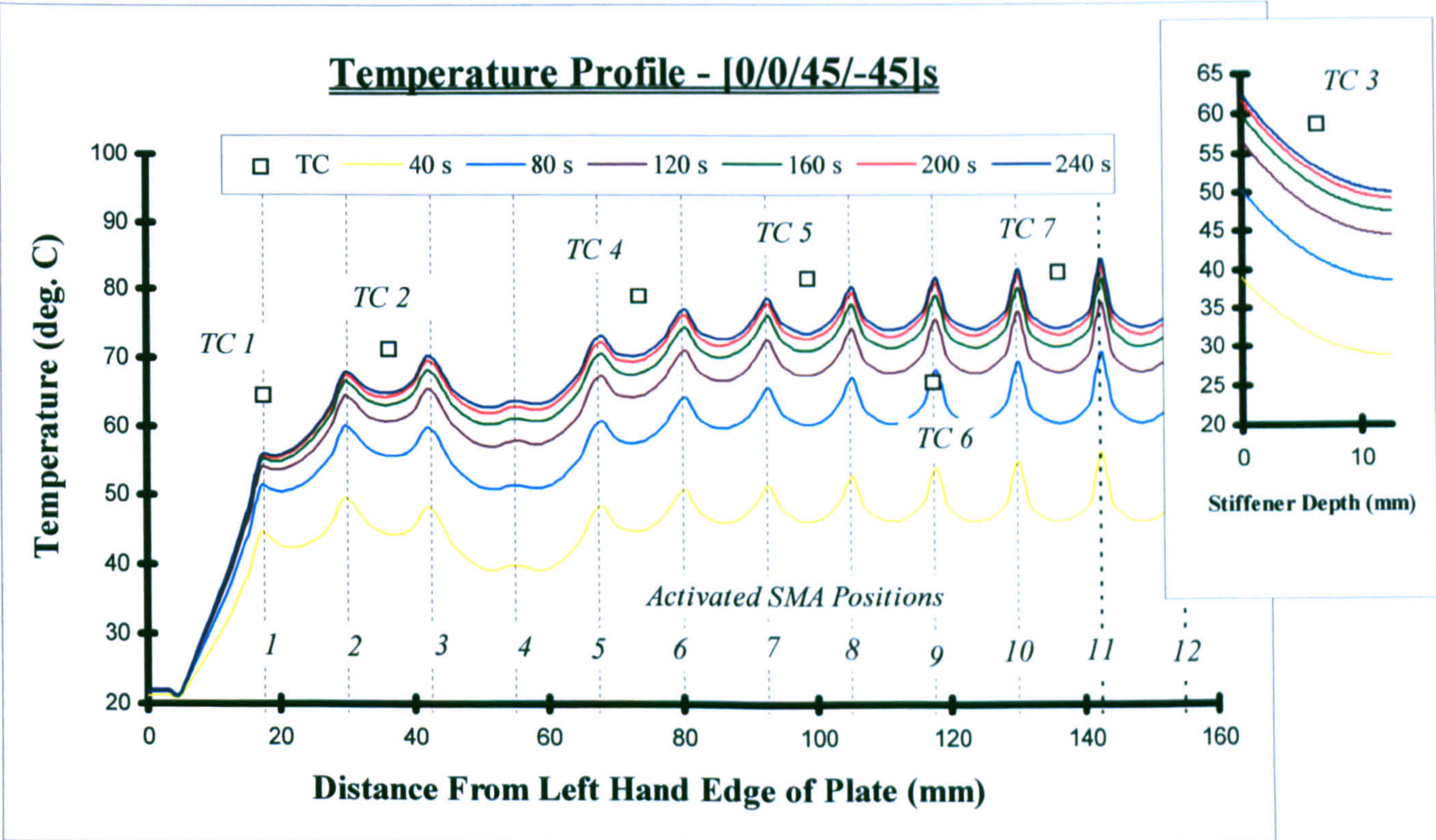


Figure 5.16. Numerical & experimental temperature profile for specimen Config. No. 11.

with the vertically inclined plates. This being the likely cause for the higher thermocouple readings in the upper half of each plate and the slightly lower readings in the lower half.

It is approximated, however, by enforcing the lower loaded edge to a temperature of 23-25°C, thus, ensuring an adequate representation of the heat sink effects at this boundary, attributed to contact with the *cold* compression rig. An adiabatic boundary condition was then left at the upper loaded edge. The asymmetry in the generated thermal results, between the upper and lower plate halves, is shown to be reasonably representative of a vertical thermal boundary layer.

For *control strategy 2*, owing to the 2-D cross-sectional area of interest that is employed for the numerical modelling and a horizontal, as opposed to the previous vertical, arrangement of thermocouples, the thermal boundary layer cannot easily be taken into consideration, **Figures 5.15 & 5.16**. Accuracy between analysis and experiment will depend on the distance between the row of thermocouples and the bottom of the plate.

The first batch of fabricated plates contained a relatively low volume fraction of SMA actuators centrally located within the specimens. It is clearly evident that with such electrically energized SMA actuators present, **Figures 5.3 & 5.5-9**, a highly non-uniform temperature profile forms over the plate surface. In an attempt to create a more uniform temperature profile, a higher volume fraction of SMA wires, with greater distribution, were located throughout the laminate, **Figures 5.11-13**. Here, the transient response exhibits a more uniform temperature distribution, though, its non-uniformity is still conspicuous with the major non-uniformity at the plates loaded edges.

It is evident that with the electrical activation strategies employed, then, dramatic variations of specimen surface temperature are present. Such variations may well influence the mechanical characteristics of the composite plates/panels. The incorporation of such temperature distributions with the mechanical post-buckling modelling is, therefore, essential to attain a complete understanding of the adaptive response of SMA/composites, **Chapter 6**. Within **Chapter 6**, the thermal results presented will be referenced.

5.6 Numerical Thermal Stress Predictions

As shown, upon SMA energization, the SMA/composite plates/panels will be subjected to a significant non-uniform temperature distribution. This will have two domineering effects on each specimen. One, it will affect the thermal stresses within the composite layers and two, it will influence the material properties. The extent to which the material properties are affected is discussed in the next section.

Regarding the thermal stresses, the stress distribution over the surface area of the structure entails compressive as well as tensile stresses, equilibrium being maintained. The compressive stresses are particularly important to ascertain as they may rise to such an extent that they initiate buckling.

Based on the predicted temperature profiles shown in the previous section, NASTRAN FE models were constructed to ascertain the resulting free thermally induced in-plane force resultants, N_x and N_y . If desired, the individual ply stresses can then be determined from classical lamination plate theory. A separate 2-D linear static plane stress model was constructed. The plain stress idealisation being applicable to thin plates that are subjected to temperature distributions that do not vary through the thickness of the plate. The temperature profiles were exported from the thermal model and mapped onto the new structural mesh.

For *control strategy 1*, three models were constructed with varying mesh densities. The models comprised of 624, 2496 and 9984 2-D CQUAD4 shell elements represented by PSHELL and MAT8 property cards. The use of 2496 elements gave a satisfactory numerically converged solution, therefore, such a mesh density was employed for the full analysis, results of which are presented in **Chapter 6**. Regrettably, due to time constraints, such linear static analysis was not conducted for the second of our control strategies.

For the models constructed, in-plane displacements were allowed to occur freely on all edges though lateral constraints were imposed at regions representing the loading rig boundaries. Additional constraints were imposed to prevent rigid body motion.

Within the framework of this study, the stresses of interest are on a macro-mechanical scale. To ascertain such stresses, it is important to know the correct values for the coefficient of thermal expansion and have an appreciation for its dependency with temperature. Whilst room temperature values for both the longitudinal and transverse coefficient of thermal expansion are readily available from the manufacturer, temperature dependency is scarcely documented. It was found within the literature¹²⁴ that the longitudinal thermal expansion behaviour of T300/5208 graphite epoxy, a material with similar properties to T300/Fiberdux 913, which is employed in this study, strongly depends upon temperature. A linear transverse coefficient of thermal expansion, up to 121°C, however, was reported.

It was stated and shown¹²⁴, that, when building a laminate with an arbitrary symmetric stacking sequence, it is not essential to describe the highly non-linear expansion characteristics in the fibre direction. This is due to the individual ply's large transverse thermal expansion characteristics, α_2 , that *swamp out* the α_1 non-linearity. Based on this finding, the following calculated macro-scale thermal force resultants are based on the manufacturers recommended linear expansion coefficients, **Table 3.1**.

Figures 5.17-19 illustrate the highly non-linear effects that the non-uniform temperature profiles, at steady-state, induce on several of the plate specimens. The actual specimen configuration number is shown on the associated figure. **Figure 5.17a** shows the resulting maximum deformation profile for lay-up $[0_2/90_2]$, with ten activated SMA wires, located as shown in **Figure 5.3**. It is clearly noticeable that a high level of thermal deformation, in a direction parallel to the activated SMA wires, exists local to the heat sources. Positive thermal expansion exists in both the x and y directions for this lay-up as $\alpha_x = \alpha_y = 1.8 \times 10^{-6} \text{ } \epsilon/\text{ } ^\circ\text{C}$.

Figures 5.17b & c show the N_x & N_y in-plane force resultants for the aforementioned lay-up, respectively. On these figures, regions of interest, denoted by A, B, C, D, E, F & G, are highlighted.

It is clearly evident that significant thermal stresses can develop even when the plate edges are unrestrained and free to deform in the xy plane. The nature of these force intensities arises as the local hot spot regions, central to the plate, wish to expand more than the cooler supported edges. The warmer regions are constrained from expanding the desired amount, hence, are in compression, C & D. Likewise, the cooler regions, at the supported edges, are being stretched, hence, they are in tension, A & B.

Similar force intensity profiles are presented in **Figures 5.18b & c** for the $[0_2/\pm 45]$, lay-up, specimen configuration 2, albeit, the magnitudes vary. The magnitudes vary as a result of the lay-up's differing global coefficient of thermal expansion. For this configuration $\alpha_x = 7.7 \times 10^{-6} \text{ } \epsilon/\text{ } ^\circ\text{C}$ and $\alpha_y = -6.02 \times 10^{-7} \text{ } \epsilon/\text{ } ^\circ\text{C}$. The negative CTE in the y direction results with specimen contraction, in this associated direction, when the SMA's are energized. The nature of this contraction is shown in **Figure 5.18a**.

Thermal force intensities for the $[0/\pm 45/90]$, lay-up, specimen configuration 3, are shown in **Figures 5.19a & c**. These figures can be compared with **Figures 5.19b & d**, respectively.

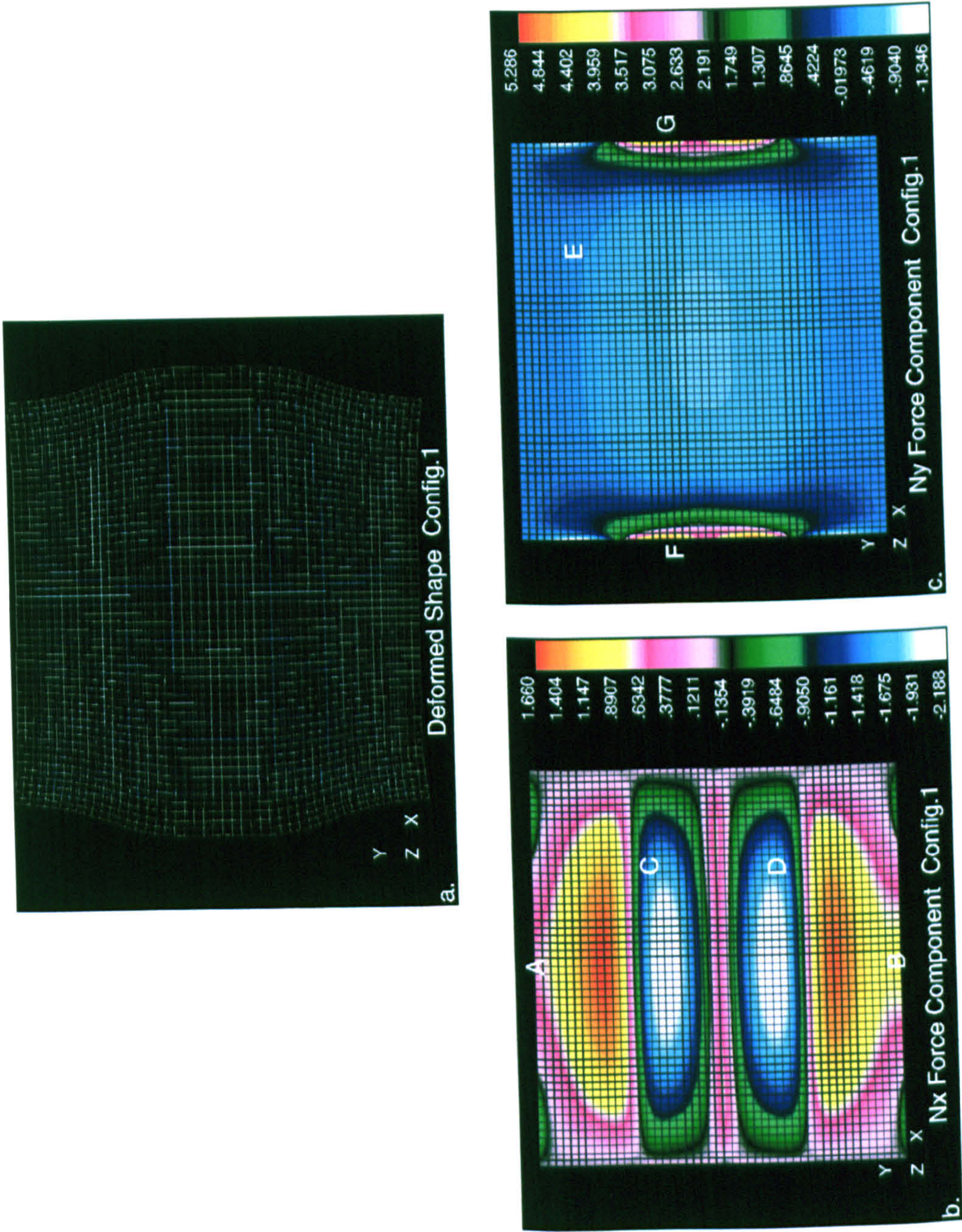


Figure 5.17. Deformation profile, N_x & N_y force intensity profiles for specimen configuration No. 1.

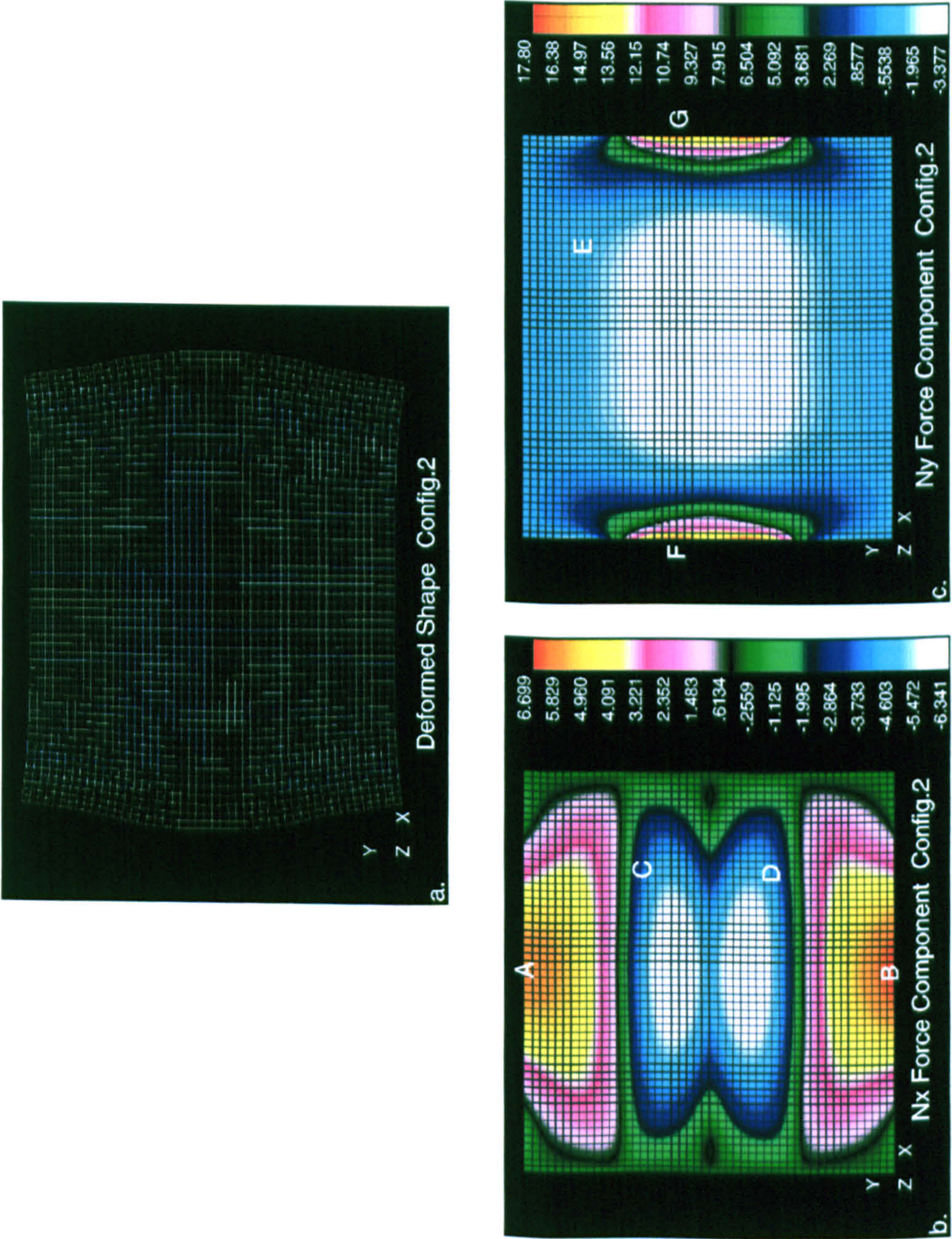


Figure 5.18. Deformation profile, N_x & N_y force intensity profiles for specimen configuration No. 2.

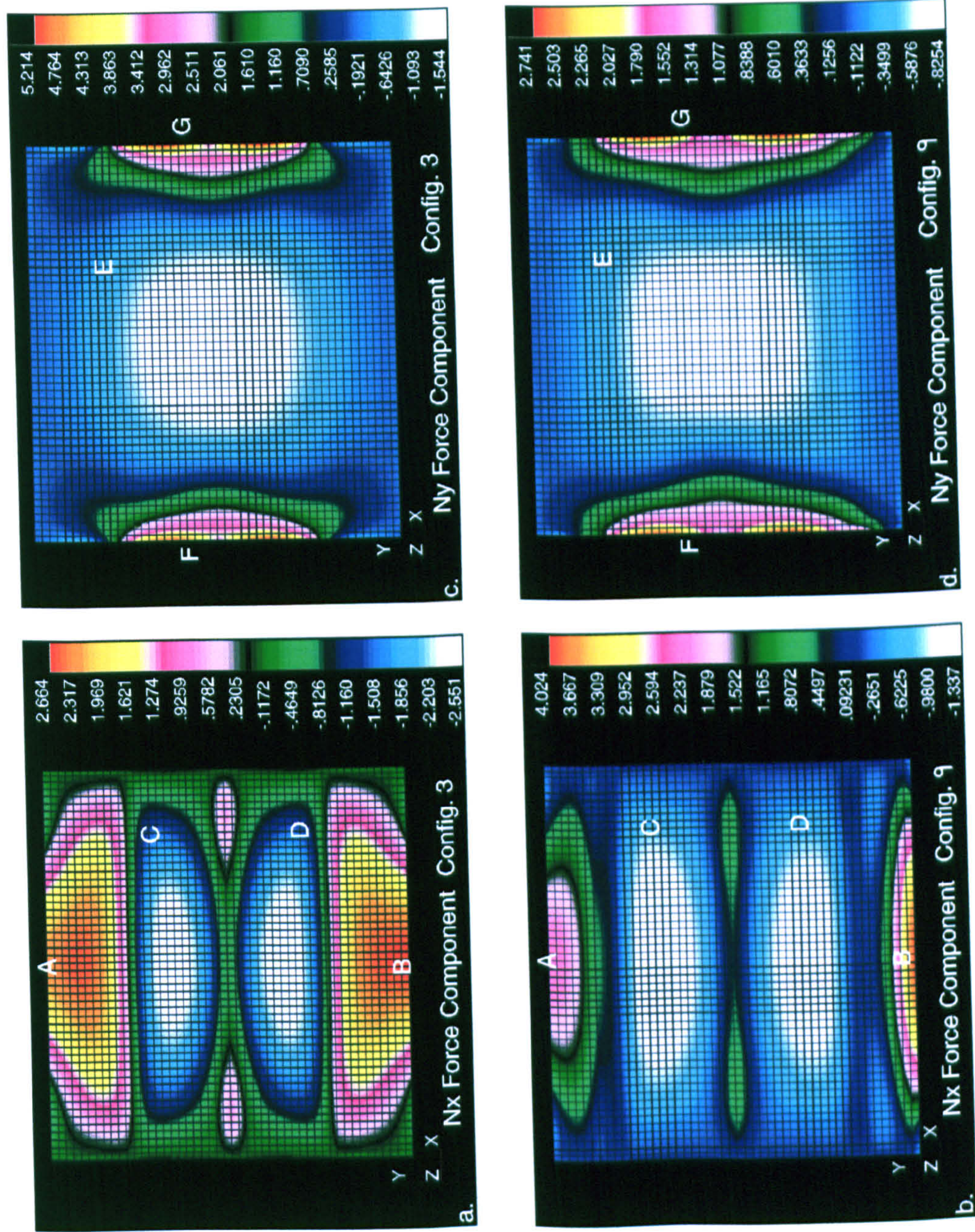


Figure 5.19. N_x & N_y force intensity profiles for specimen configuration Nos. 3 & 9.

The latter results were obtained for the same lay-up, though, has differing SMA activation v_f , specimen configuration 9. Quite clearly, the more evenly distributed energized SMA actuators results with a significant drop in the compressive force intensities local to the central region of the specimens, **C**, **D** & **E**. For this lay-up the calculated global coefficients of thermal expansion are the same as those for the $[0_2/90_2]_s$ lay-up.

The aforementioned force intensity drop is particularly important as the unrestrained plates may still experience thermal buckling due to the compressive stresses induced by the spatial temperature gradients¹²⁵. Critical uniform thermal buckling temperature differentials for configurations 1-3 were calculated to be 16.2°C, -48.5°C and 31.1°C respectively. The boundary conditions applied in determining such values were those simulating simply-supported conditions with the two opposing loaded edges constrained in the direction normal to their edge. As with the mechanical buckling analysis, the unloaded edges were allowed to freely deform in-plane. The critical temperatures are based on a uniform temperature increase. The thermal buckling values were calculated based on the constant material properties of **Table 3.1**. The method of calculation was that outlined in **Section 4.5.1**.

The negative differential buckling temperature for specimen configuration 2 is, of course, attributed to the negative global CTE in the direction perpendicular to the constrained edges. Note, in the tests conducted, constraint is not imposed on specimen shortening.

The effect of non-uniform temperature distributions on the thermal buckling behaviour of plate specimens with varying lay-up configurations has been investigated¹²⁶. It was stated that the critical buckling temperatures of laminates subjected to gradient temperature distributions are much higher than those subjected to a uniform temperature rise¹²⁷. This finding being of benefit to the present study. It was shown, however, that the greater the temperature gradient within the specimen, the lower the buckling value. From the latter statement, as will be shown in **Chapter 6**, reducing the magnitude of the stress gradients, through activation of a more uniformly distributed SMA arrangement, has a profound beneficial effect on the adaptive post-buckling response of the plate specimens tested.

The magnitude of the thermal force intensities at the highlighted regions, for all the plate specimens, are summarized in **Table 5.4**. All results are in N/mm.

A final point worthy of mention is that the degree of thermal anisotropy (k_{11}/k_{22}) for this carbon reinforced material is quite low. Higher values, up to 180, can be achieved for the pitch based fibres. A high thermal conductivity may be desirable, particularly when wishing to control reinforced plastics utilizing shape memory alloys, as this will help reduce local hot spots, thus, alleviating high stress gradients.

Config.	A,B	C,D	E	F,G
1	1.683	-2.220	-0.654	4.297
2	6.703	-6.376	-3.385	15.63
3	2.706	-2.580	-1.546	4.690
4	1.746	-2.062	-0.624	4.020
5	7.835	-5.700	-3.411	14.78
6	3.397	-2.311	-1.425	4.229
7	3.858	-1.430	-0.257	2.351
8	3.143	-0.888	-0.270	1.990
9	4.049	-1.353	-0.826	2.467

Table 5. 4. Summarized thermal force intensities.

5.7 Temperature Dependent Properties

As seen, application of the necessary energy source to drive the SMA wire actuators through their transition temperature will subject the host composite material to a considerable temperature rise. It is important to understand how this elevated temperature will affect the host material, particularly the matrix.

Polymeric-matrix composites are sensitive to changes in temperature. Such sensitivity is the result of matrix temperature-dependent material properties as well as the previously introduced generation/relaxation of internal residual stresses.

One of the most important parameters in the design of composite components is the elastic modulus. Numerous texts provide valuable insight pertaining to its behaviour at elevated temperature. The effects of temperature on the tensile and compressive moduli have been studied¹²⁸. This cited reference makes the following general conclusions;

- A temperature range of -73 to 177°C has negligible effects on the longitudinal elastic moduli of a 0° or ±45° laminate. *“Such an upper bound temperature will depend on the constituent materials employed”*.
- When considering a unidirectional ply’s transverse properties, an increase in temperature will cause a decrease in elastic moduli. *“Again, the actual effects will be dependent on the constituents employed”*.

The transverse compressive behaviour of a carbon/epoxy (T300/Fiberdux 914) unidirectional composite has been investigated at various temperatures and test rates¹²⁹. A linear decrease in the transverse compressive modulus occurs with increase in temperature up to 150°C. The glass transition, T_g , for such a material being approximately 175°C. It was shown that between room temperature and 100°C the modulus drops by approximately 1.5GPa.

It is a general rule that matrix properties determine the maximum upper working temperature for a polymeric-matrix composite. As discussed, its properties, such as stiffness and strength, under transverse, off-axis or shear loading, are altered when exposed to an elevated temperature. The increased temperature causes a gradual softening of the polymeric-matrix material. If the temperature is increased beyond the so called glass transition point, T_g , the polymer becomes too soft for use as a structural material.

The glass transition temperature, T_g , is defined as the approximate mid-point of the temperature range where the matrix changes from a glassy state to a rubbery/viscous state. Beyond T_g , the resin properties would be expected to decrease by at least an order of magnitude making it a poor load transfer medium. T_g for the Fiberdux 913, employed within our investigation, is 131°C. To ensure that the matrix remains in the glassy state and that the associated temperature dependent properties are not significantly degraded, the manufactured specimens are not thermally loaded above 100°C.

This selected upper bound temperature value was based on guidelines from airworthiness authorities recommending that composite structural use be restricted to a maximum service temperature 28°C lower than T_g , where, T_g is determined on a material with worst case moisture conditioning. Due to the environment in which the specimens were fabricated and the time scales to test, the affect of moisture has been ignored.

The thermal degradation of a composite matrix property can be estimated by using an empirical equation to degrade the corresponding matrix property and then using this degraded property in the appropriate micromechanics equation for the composite matrix property'. With the exclusion of the hygroscopic environment, the procedure is based on the equation;

$$F = \frac{P}{P_o} = \sqrt{\left(\frac{T_g - T}{T_g - T_o}\right)} \quad (5.10)$$

where F is the matrix mechanical property retention ratio, P is the matrix property after thermal degradation, P_o is the reference matrix property before degradation, T_g is the dry glass transition temperature (°C), T is the temperature at which P is to be predicted (°C) and T_o is the test temperature at which P_o was measured (°C).

Equation (5.10) is based on the experimental observation that the degradation is gradual until the temperature T approaches the glass transition temperature, whereupon, the degradation accelerates. This method has been shown to accurately predict the degradation of unidirectional glass and graphite epoxy composites up to 80°C¹³⁰. Results were not presented for the behaviour up to 100°C. This analytical technique does not represent the material properties beyond T_g. Such properties are of no concern for this study.

The equations employed to determine the transverse and shear moduli of the composite matrix are based on *improved mechanics of material* formulations' and are as follows;

$$E_2 = E_m \left[(1 - \sqrt{v_f}) + \frac{\sqrt{v_f}}{1 - \sqrt{v_f} \left(1 - \frac{E_m}{E_\Omega}\right)} \right] \quad (5.11)$$

and

$$G_{12} = E_m \left[(1 - \sqrt{v_f}) + \frac{\sqrt{v_f}}{1 - \sqrt{v_f} \left(1 - \frac{G_m}{G_\Omega}\right)} \right] \quad (5.12)$$

where the nomenclature have their usual meaning.

At room temperature, say T_o=25°C, substituting E_m=5.0*10³N/mm², E_Ω=13.8*10³N/mm² and v_f=0.6, equation (5.11) predicts a transverse modulus of 8.77*10³N/mm². By comparison, a *simplified* rule of mixtures approach predicts 8.09*10³N/mm². This latter underestimated value was expected and is the result of invalid assumptions within the equation's analytical derivation. Multiplying the matrix properties, E_m, of equation (5.11) by the predetermined F, equation (5.10), for a temperature range up to glass transition, we can quantify the degradation of E₂. Such degradation is shown in **Figure 5.20**. A similar procedure can be used to predict the shear modulus of equation (5.12).

It is seen that at 100°C, E₂ degrades to 6.1*10³N/mm². From documentation obtained from the manufacturer, E₂ is seen to degrade to 6.5*10³N/mm² at 80°C. No information regarding the properties at 100°C were available. Referring to the lower curve within **Figure 5.20**, similar behaviour can be shown to exist for the matrix dominated shear properties. Having a

clear understanding of the matrix properties at an elevated temperature, an accurate determination of the adaptive post-buckling displacement and stressed state may be obtained.

It was the intention to account for the matrix-dominated property degradation, **Figure 5.20**, in our non-linear finite element analysis, **Chapter 4 & Chapter 6**. The use of the PCOMP property cards, assigned to the 2-dimensional CQUAD4 elements, within MSC/NASTRAN do not, however, allow for material temperature dependency. The material temperature dependency can only be accounted for when using solid elements that reference appropriate solid element property cards.

As will be shown in **Chapter 6**, good experimental and numerical agreement, however, can be achieved when ignoring the effects of temperature on the material properties.

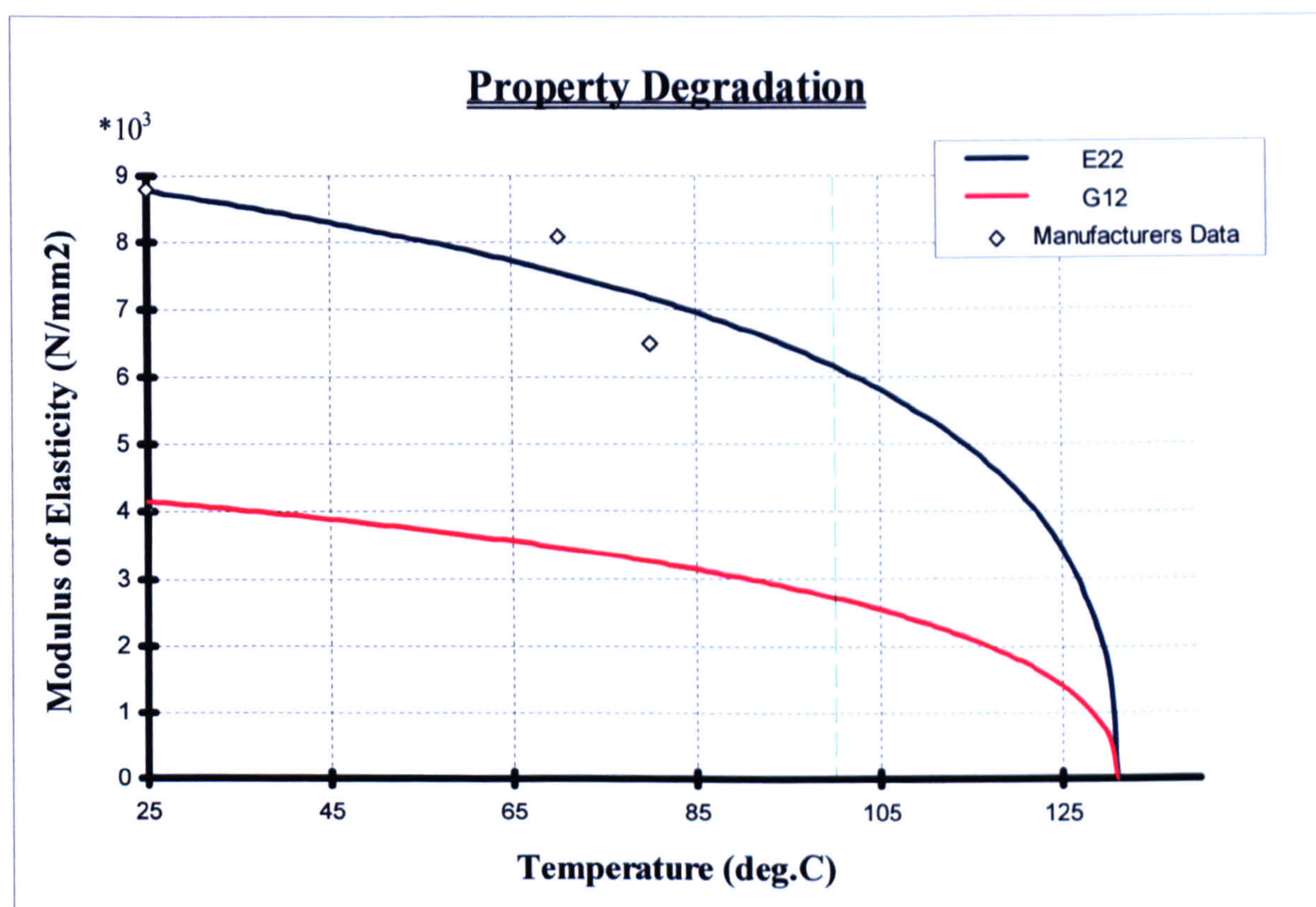


Figure 5.20. Temperature degradation of E_2 and G_{12} .

5.8 Creep Affects In Polymeric-Matrix Composites

To conclude this chapter, the subject of creep within polymeric-matrix composites is briefly discussed. All polymers are made up of long molecules with a covalent *backbone* of carbon atoms¹³¹. These long molecules are bonded together by weak Van der Waals and hydrogen (“secondary”) bonds. The Fiberdux 913 is a highly crosslinked thermoset plastic. Such covalent cross-links form during the curing process and lead to the formation of a tightly bound three dimensional rigid plastic. Thermoplastics are absent of such a network of crosslinks and as a result, they are usually less stiff than the thermosets.

For the thermoset epoxy under consideration, **Figure 5.20**, when exposed to an elevated temperature, the modulus of the matrix decreases. This softening effect is due to the low melting point of the weaker bonds. Entire melting of the matrix, characteristic to

thermoplastics, however, is hindered by the covalent crosslinks. An excessive temperature will cause the matrix to decompose¹³¹.

As an example of creep within a PMC, tests have been conducted on a $[90_2/\pm 45/90/\pm 45/90]$, notched graphite epoxy laminate (IM7/8551-7) under an applied stress of 24.33 MPa when exposed to a dry 121°C environment for 48 hours¹³². The results indicate that in the vicinity of the hole, there is a significant creep rate during the first few hours that eventually levels off as the test proceeds. A maximum increase of 19% in axial deformation arises at the hole edge after the 48 hour duration. Such behaviour stems from the viscoelastic time dependent matrix properties.

A characteristic feature of the matrix component of polymeric-matrix composites, even at room temperature, is its time dependent or viscoelastic response to an applied stress or strain. Thermosets, compared to thermoplastics, are generally more resistant to such effects. However, at elevated temperatures, both thermosets and thermoplastics may have notable creep behaviour.

Viscoelastic behaviour will depend on the ply orientation with respect to the loading direction, stress level, temperature, humidity and thermal history. Most commercial reinforcing fibres do not exhibit viscoelastic behaviour except at very high levels of applied load and temperature. Therefore, for many continuous fibre reinforced composites, the time dependence of the fibre dominated mechanical properties, E_{11} and ν_{12} , is negligibly small compared with the highly time-dependent matrix dominated properties.

As we are considering lay-ups with an arbitrary percentage of plies parallel to the loading direction, then, the viscoelastic/creep response, under the influence of the applied mechanical and thermal loads, is governed by the behaviour of the fibres. Should the fibre angle increase with respect to the loading direction, then, creep of the matrix becomes the controlling factor.

Since the fibres do not generally exhibit viscoelastic/creep behaviour, the overall response of a 0° lamina is effectively linear elastic. However, when a load is applied to a 0° lamina, the matrix is restrained from creeping by the elastic fibres. The matrix, instead, undergoes stress relaxation, transferring part of its load to the reinforcing fibres. This causes the fibres to extend so that a small amount of creep may be detected, though, at a microscopic level.

Within this study, owing to the large percentage of zero directional plies parallel to the loading direction, within each of the manufactured plates, and within the current operating time scales, viscoelastic or creep effects of the specimens can be ignored.

Chapter 6

Numerical & Experimental Adaptive Post-Buckling Results

6.1 Introduction

This chapter presents the numerical and experimental results that verify the whole concept of an adaptive post-buckling capability within carbon/epoxy plates/panels, loaded in compression, when employing either of the control strategies previously described in the earlier chapters.

The out-of-plane deflection of inherently imperfect plates/panels, associated with a uniaxial compressive loading, will be controlled via activation of embedded SMA wires. The actuators will be activated by means of resistive heating. It is anticipated, however, that efficient use in future aeronautical systems will utilize an ambient thermal environment to enforce the SMA material through its transformation temperature. Potential applications could be skin sections local to engine housings or the wing skin sections of high speed aircraft.

Utilizing the SMA constraining mechanisms of either control strategy, the pre-strained SMA's, embedded within the laminated constructions, effectively enhance the bending stiffness of the host specimens. For a detailed account of the control strategies and their manufacturing procedure, the reader is referred to **Chapter 3**.

The experimental set-up is first presented. This will be followed by detailed experimental and numerical results for each specimen configuration. The numerical and experimental results presented will be in the form of applied load and out-of-plane displacement with respect to time. Where appropriate, numerical in-plane force intensity profiles will be provided. The discussed results will later be summarised in tabular format within the **Conclusions & Further Work** section of this document, **Chapter 8**. Envisaged structural applications have been identified that could utilize the findings presented in this chapter. Such applications are presented in **Chapter 7**.

6.2 Experimental Set-Up

Owing to the nature of the surface topology of the specimens of *control strategy 1* that employ the PTFE tubing, metal shims were required to be bonded to both sides of the surface of such specimens, away from the embedded inclusions, and along the unloaded edges. The *as-built* thickness of the shims plus the specimen equalled that of the thickness of the specimen at the positions of the embedded inclusions. This will ensure that direct lateral support is provided to the whole length of the unloaded edges by the knife edge supports. The locally bonded metal shims are not thought to have any affect on the numerical results.

For both control strategies, the specimens were located within the loading rig supports such that in-plane movement was possible without free lateral movement. The unloaded edges were located between knife edges and the loaded edges within the 0.5mm deep locating

grooves, **Section 4.4**. The supports were initially well greased, therefore, alleviating any friction.

For *control strategy 1*, the inserted pre-stained SMA wires were then constrained at one end to an external fixture, **Figure 6.1**. Two identical external fixtures were employed for SMA clamping. They were bolted to either side of the loading rig. Each individual SMA wire was then subjected to an initial tensioning of 1.5N prior to complete clamping.

An important issue to consider when using such wire actuators is that, due to the magnitude of the generated activation forces, **Chapter 2**, soldered connections are structurally unreliable. Clamping was foreseen to be the only effective attachment technique for this application.

For *control strategy 2*, the embedded SMA wires were first fed through the appropriate holes within the loading platen prior to connection to the external power supply unit.

For both strategies, the SMA wires were connected to the power source in parallel. Each actuator was sufficiently insulated from the loading apparatus.

An Instron Universal Testing Machine was employed to enforce the required load or displacement. For the initial tests conducted within the research programme, the testing machine was configured such that it maintained load control. Later on, experiments were conducted in load control and then repeated in displacement control. The load/displacement was applied via two rigid platens. The top being fixed while the bottom was driven by the actuating cylinder of the test machine.

Linear variable displacement transducers, LVDT's, were employed to measure the lateral deflections at three key positions. The three out-of-plane displacements of interest were at the quarterly points along the vertical centre line. The first and third quarter points are local to the anti-nodal points of the second mode shape for each specimen configuration tested. The second quarter point is local to the anti-nodal point of the first mode shape.

For the initial tests, only the central displacement, or second quarter point, was of interest. The remaining two displacements were experimentally recorded from configuration 6 onwards. This is because, for this particular specimen, modal instability arose, more of which will be discussed in **Section 6.3.3.3**.

Each LVDT displacement sensor is capable of measuring a displacement of $\pm 35\text{mm}$ with an accuracy of $\pm 0.003\text{mm}$. Thermocouples were secured to the specimens as outlined in **Chapter 5**.

Applied load and LVDT displacement readings were recorded at 5Hz using PC based data logging software. The thermocouple readings were recorded at 1Hz. Later in the experimental

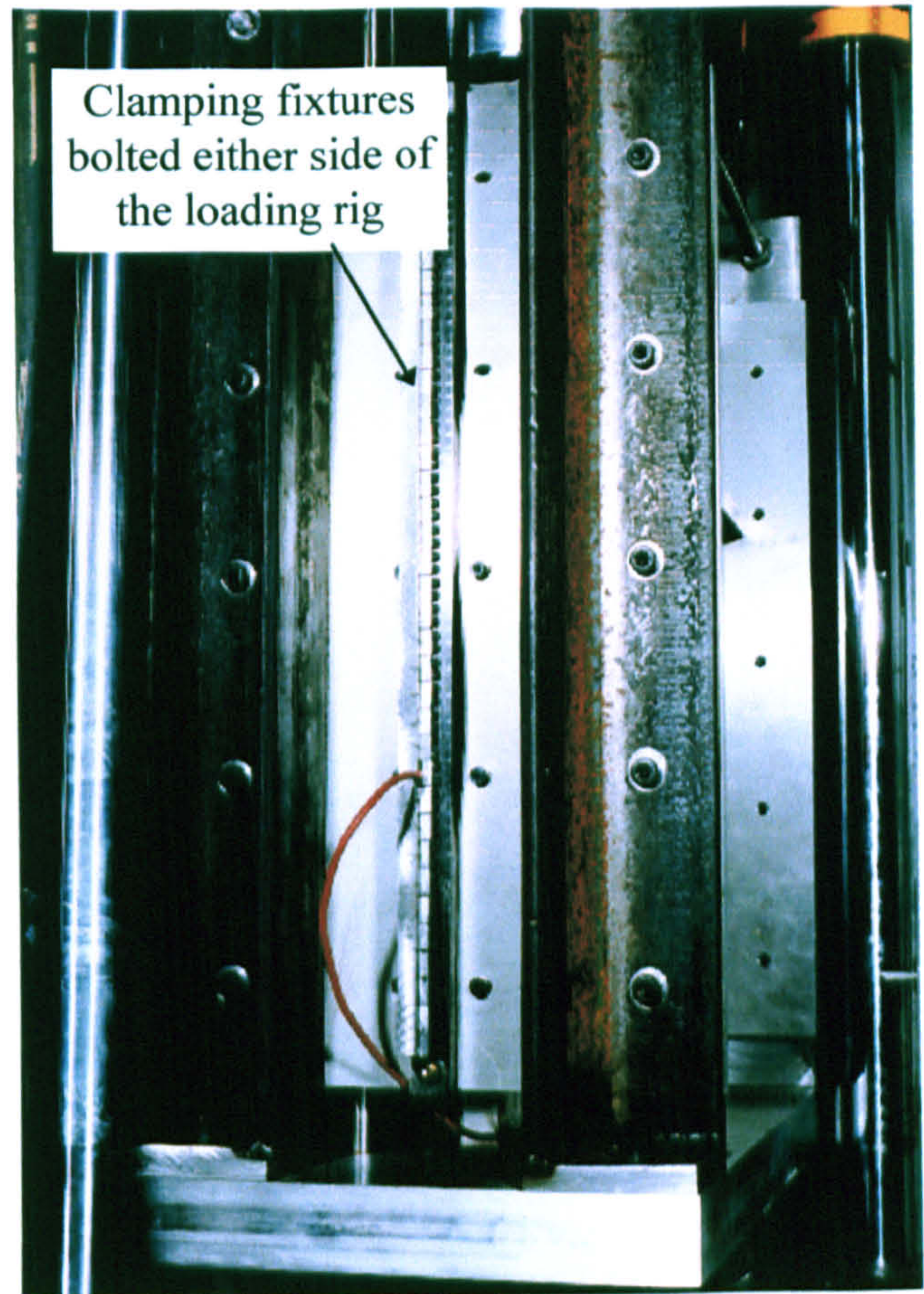


Figure 6.1. *SMA clamping arrangement for control strategy 1.*

programme, the recovery force of a single SMA actuator, within the test set-up, was recorded, though, only for *control strategy 1*.

Prior to the actual test, each test specimen was subjected to a small load, therefore, allowing the specimen to settle into the test rig by alleviating any *slack* between adjacent components. This load was then removed. A typical experimental set-up is shown in **Figure 6.2**. Experimental and numerical results will now be presented.

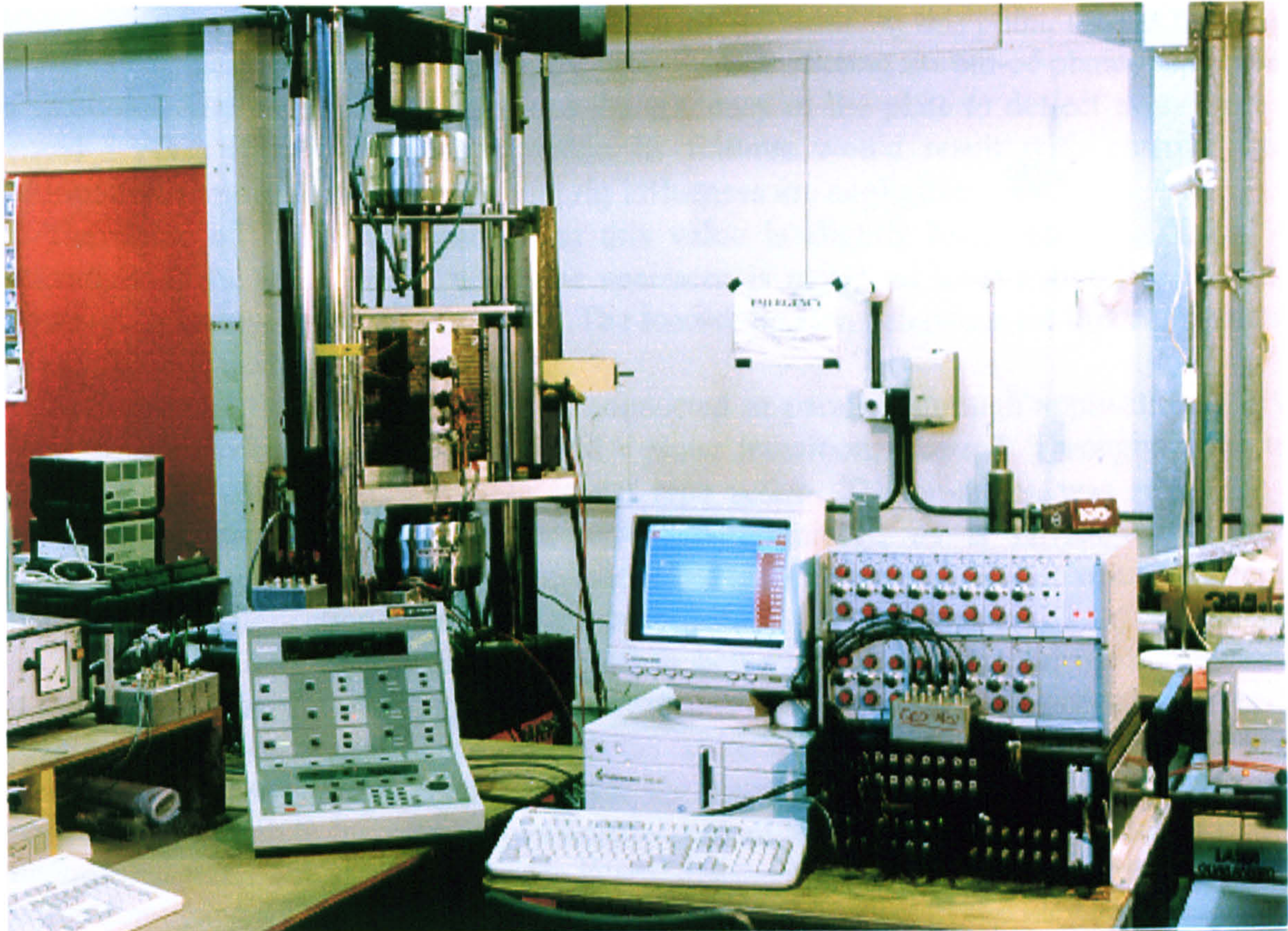


Figure 6.2. *Typical experimental set-up for control strategy 1.*

6.3 Adaptive Post-buckling Response For *Control Strategy 1*

6.3.1 Results For Configuration Numbers 1-3

The results of the adaptive behaviour of composite plates featuring the 49.8-50.0 at.% Ni SMA actuators, 0.4mm in diameter, for configuration numbers 1-3, will first be presented. The plate behaviour featuring the higher atomic percentage nickel actuators will be discussed in **Section 6.3.3**.

With the exception of those results presented in earlier chapters of this thesis, all the main results figures pertaining to the adaptive post-buckling response of composite plates/panels in compression can be found at the end of this chapter.

6.3.1.1 Configuration Number 1

Results are first presented for specimen configuration number 1, that being the $[0_2/90_2]_s$ lay-up, **Figure 6.3**. For illustrative purposes, the applied load and the corresponding peak amplitude central deflection are shown with respect to time. Presenting the results with time allows us to break down the out-of-plane-displacement curve into five evident phases.

During *phase 1*, a 1.9kN compressive load is applied. The corresponding experimental out-of-plane displacement reaching a peak value of -2.79mm. At this point, there is reasonable agreement between experiment and FEA. The FEA predicting an out-of-plane-displacement of -2.60mm. The negative sign indicates the tendency of the plate to deflect away from the central LVDT. Rotating the plate within its fixtures would result with positive lateral deformation, therefore, ensuring loading rig influences are negligible.

The value of 1.9kN was chosen as this value is slightly lower than P_{c2} , **Table 4.3**. Activation of the SMA wires, when the specimen is at a load level above P_{c2} , as will be shown, can result with modal instability. The load-deflection behaviour for this configuration, during *phase 1*, is shown in **Figure 4.15**.

Activation of the 10 SMA actuators connected in parallel, through application of a DC power source, **Table 5.2**, results with SMA phase transition, *phase 2*. Throughout the tests conducted, the control was a simple on-off type action. The on-action was sent once the loading state reached the prescribed value. Alternatively, as is envisaged for future applications, should the system be powered electrically, then, the power would be applied once a threshold deflection value has been exceeded.

The resulting SMA restoration forces act to pull the plate back to the flat configuration. At the end of *phase 2*, the energized plates recorded out-of-plane displacement is seen to be -1.67mm, a reduction of 40.1% compared to the uncontrolled state.

With the recovery force maintained for the duration shown, *phase 3*, the out-of-plane displacement is seen to increase to a value of -1.84mm, this value being 34.1% lower than the uncontrolled state. The slight increase in lateral deformation is attributed to degradation of the matrix dominated material properties, associated with exposure to the elevated temperature, **Figure 5.20**, and to the non-uniform thermal stressed state set-up across the plate, **Section 5.6 & Table 5.4**. At the end of *phase 3*, the plate deflection tends to its constant value as the temperature profile within the laminate approaches the steady-state, **Figures 5.3 & 5.4**.

When the actuator power source is removed, the out-of-plane displacement profile is seen to increase and overshoot the initial peak displacement amplitude, *phase 4*. The overshoot is the result of the degradation in the aforementioned material properties in the presence of the now diminishing thermal stress profile. As the plate cools, the plate tends to the initial value of -2.79mm. The test ends with the removal of the applied load, *phase 5*. As the load is removed, the lateral displacement is seen to decay to zero.

The loading-activation-deactivation-unloading behaviour, just described, was shown to exist for many cycles, all of which showed repeatable behaviour. The results presented in **Figure 6.3** correspond to the tenth cycle. Prior to each subsequent test, the specimens were allowed to rest at room temperature for the length of time necessary to relieve the residue stresses that result with thermal expansion of the carbon/epoxy specimen during the activation process.

Non-linear finite element results of the post-buckling deflection alleviation, *phase 2*, predict an out-of-plane displacement of -0.36mm or -1.35mm when restoration recovery forces of 75N or 60N, respectively, are generated within each of the energized SMA

actuators. These forces approximate the initial cycle, and the repeatable behaviour of the transformed alloy, **Figure 2.23**.

The experimental out-of-plane displacement can only be reduced by 34.1%, i.e. not quite realising the desired FEA behaviour. This is due to the following.

The application of the power level to activate the SMA material results with the surface temperature profile shown in **Figure 5.3**. From **Figure 5.3**, it can clearly be seen that the temperature distribution at the outer actuator locations, i.e. SMA's 1 & 10, is considerably lower than the adjacent actuator locations. The net effect of the lower temperature will be a lower restoration force. Based on the experimental observation of the recovery forces during the tests reported in **Section 6.3.3**, this specimen was re-tested later in the experimental phase of the research programme. Within the re-test, the recovery force for one of the inner most actuators, SMA 6, was recorded to be 48N. The outer most actuators are thought to be slightly less than this. This actuator recovery force was not seen to degrade by any significant amount with successive testing. This is the likelihood of incomplete phase cycling to austenite and, therefore, a lower rate of dislocation formation. Dislocations acting to degrade the shape memory recovery response, **Section 2.6.3**.

The actual power level applied to energize the actuators falls short of the amount required for complete phase transition, therefore, only partial actuation results. None the less, significant control authority is evident. Increasing the power level further, enabling greater restoration forces, will subject parts of the laminate to a temperature in excess of 100°C. This is considered to be undesirable for the host composite material. Employing a partially activated recovery force of 48N for each SMA wire within the FEA results with an out-of-plane displacement of -1.58mm at the end of *phase 2*. Having a clear understanding of the recovery force within the specimens now enables a more realistic prediction of the actual adaptive post-buckling behaviour.

As previously stated, the contributory factors that increases the post-buckled deflection, *phase 3*, is matrix degradation and the non-uniform stressed state set-up as a result of the non-uniform temperature profile. With referral to **Section 5.6**, thermally induced compressive membrane stresses can initiate buckling. For the case of an initially laterally deflected plate specimen, the thermally induced compressive forces will act to increase the post-buckled lateral deformation.

For the testing arrangement under consideration, the important parameter of such a non-uniform thermal distribution is not the actual temperature of the plate but the temperature differential between the heated centre and the cooler edges, as cited elsewhere^{123,125}. Under such circumstances, we have an advantageous lay-up in that, owing to the global coefficients of thermal expansion, the in-plane force intensities, **Table 5.4**, are not so prodigious, however, as will be shown for our next configuration, it would be desirable to minimise such temperature gradients.

One of the reasons for the use of the higher distribution of SMA actuators within the later specimens, **Section 6.3.3**, was an attempt to decrease the specimens surface temperature differential upon actuation, therefore, minimising the increase in post-buckling deflection witnessed during *phase 3*.

During *phase 3*, **Figure 6.3**, it is shown that the non-linear FEA can accurately predict the trend of the adaptive post-buckled response under the influence of the increasing non-uniform temperature distribution, **Figure 5.3**. Temperature profiles, at key time intervals, were extracted from the numerical model and mapped onto the structural mesh. Within the numerical analysis, the mapped temperature profiles were linked to a particular load case. The results of which were obtained at the appropriate point in time.

The reader should note that the temperature profiles in **Chapter 5** are presented at certain time intervals after actuation. Within this chapter, global test times are presented. Slight care is, therefore, required when reading from the figures.

Restoration recovery forces not only reduce the peak displacement amplitude, they also alleviate the higher stress levels, at the boundary supports, typical to post-buckled plate configurations as a result of load shedding due to out-of-plane displacements. **Figure 6.4** shows the FEA longitudinal, N_y , membrane force intensity, along the plates centreline, when subjected to 1.9kN, with and without the adaptive control. At the end of *phase 2*, utilizing adaptive control, it can clearly be seen that, at the plate's knife edges, the in-plane compressive force intensity is reduced from -14.4N/mm to -8.2N/mm, a reduction of 43.1%. The negative sign indicating compression. The tendency of the adaptive plate is to redistribute the loading back towards the plates central region, such that, a more uniform stressed state exists.

It is also shown in **Figure 6.4** that the influence of the steady-state non-uniform temperature profile, i.e. after an actuation time of 240s, has only a modest effect on the in-plane N_y adaptive stressed state. Under the influence of the 1.9kN mechanical load, the SMA's actuation recovery forces and the associated thermal profile, the N_x force intensity has a similar profile and magnitude as that shown in **Figure 5.17b**.

6.3.1.2 Configuration Number 2

Figure 6.5 shows the experimental and numerical behaviour for the $[0_2/\pm 45]_s$ lay-up, configuration number 2, loaded to 1.9kN. At the end of *phase 1*, the experimental out-of-plane deflection falls short of the predicted FEA result by 9.3%. The contributing factors that possibly cause this stiffening affect were discussed in **Section 4.9**. The prominent feature of *phase 2* is the 49.1% reduction in the experimental post-buckled deflection. This is followed by the proliferate deflection behaviour, *phases 3 & 4*.

The proliferate deflection behaviour was attributed to the affects of the highly non-uniform surface temperature profile, **Figure 5.5**, and on the resulting in-plane thermal membrane force intensities, **Table 5.4**. In the global-x direction, the compressive force intensity being more prominent than for specimen configuration 1. This being the result of the associated global coefficient of thermal expansion. Such prominent affects could be overcome by increasing and uniformly distributing the SMA volume fraction.

During *phases 2 & 3*, the FEA underestimates the experimentally observed out-of-plane displacement. It does, however, accurately predict the appropriate trend in the witnessed behaviour. The deviation between the two sets of results is the likely attribution of the undesired experimental boundary constraints that elevate the global stiffness of the specimen and hinder the free in-plane movement of the specimen while under the influence of the thermal environment. Here, additional in-plane compressive stresses will form within the specimen that act to increase the out-of-plane displacement.

Another contributory factor will be the non-account for temperature dependent properties of the host laminate within the FEA. This non-account of temperature dependent properties within the analysis is not so conspicuous for configuration number 1. For the current lay-up under discussion, when using data from the manufacturer that is subjected to a temperature of 80°C, the bending stiffness about the axis parallel to the loaded axis will degrade by 26% compared to that at room temperature. The actual maximum temperature that the plate is exposed to is approximately 100°C. This will result with further mechanical degradation.

Figure 6.6 shows the predicted longitudinal, N_y , membrane force intensity profiles for several load cases along the specimens centreline. The finite element analysis shows an almost uniform N_y profile when under the influence of 1.9kN and the SMA recovery forces only, *phase 2*. Each SMA wire exerting a recovery force of 48N. This load case predicts the peak out-of-plane displacement of -0.7mm, Figure 6.5.

A reduction in N_y of 60.3% is seen, at the knife edges, when compared to the non-adaptive load case, i.e. the specimen under the influence of 1.9kN only, *phase 1*. Significant N_y non-uniformity soon becomes apparent as the thermal energy, associated with SMA actuation, propagates through the laminate, *phase 3*. Upon SMA activation, the longitudinal expansion at the specimens unloaded edges, Figure 5.18a, and the associated thermal tensile stresses, Figure 5.18c, causes the reduction in the N_y force intensity local to the knife edge supports and its redistribution towards the centre region of the plate specimen.

It is not entirely the N_y membrane force distribution that causes the proliferate deflection increase. Predominantly, it is the resulting self-equilibrating thermal N_x compressive force intensity, much like that presented in Figure 5.18b.

6.3.1.3 Configuration Number 3

As with the $[0_2/90_2]_s$ lay-up, configuration number 1, similar results are shown in Figure 6.7 for the lay-up configuration $[0/\pm 45/90]_s$, specimen configuration number 3. The exceptions are that the applied load equals 2.5kN, and 12 SMA wires were employed.

The applied load of 2.5kN was chosen as this value was initially thought to be slightly less than the specimen's second critical buckling value, P_{c2} . For this specimen, an error, however, was initially present within the FE model, hence, the obtained results over estimated the true FE results. Re-running the correct FE buckling analysis, the true P_{c2} was calculated to be approximately 2.05kN. We are, therefore, applying a load in excess of the second buckling value. As will be shown, this may have serious implications, in that, modal instability may arise with SMA adaptation. Fortunately, for this configuration, modal instability did not occur.

At the end of *phase 1*, excellent agreement exists between FEA and the experimentally determined plate deformation. FEA predicted a value of -3.04mm, while the experimentally recorded value was -2.93mm, Figure 6.7. Figure 4.16 illustrates the nature of the out-of-plane displacement versus applied load up to the end of *phase 1*.

From the experimental results, an initial reduction of 33.2% in the peak lateral deformation can be realized by activation of the low volume fraction SMA material. This, however, will degrade to a 28% reduction due to the affects of temperature, as previously discussed. The surface temperature profile is depicted in Figure 5.6.

FEA, utilizing 75N and 48N recovery forces, predict out-of-plane displacements of -1.26mm and -1.98mm respectively. Excellent FE results are apparent when exerting a 48N load to each of the SMA actuator representations. As with the previous configurations, a 75N recovery force within each of the SMA actuators, for the initial cycle, cannot be realised as this will subject the host laminate to a temperature above the recommended safe operating limits.

In general, very good correlation is apparent between FEA and experiment. For this configuration, however, it is interesting to note the *slight* over-estimation in the FE predicted out-of-plane displacements during *phase 3*. The previous configurations under-predicting the out-of-plane displacements using FEA.

As computed by the FEA, **Figure 6.8** shows a 38.4% reduction in the N_y longitudinal membrane force intensity, local to the knife edge supports, when activating the SMA wires. Again, these force profiles are predictive along the specimens centreline. It is shown that the effects of thermal energy propagation has a negligibly small effect on N_y .

The increase in the post-buckled deflection response during *phase 3*, **Figure 6.7**, is again attributed to N_x , **Figure 5.19a**.

6.3.2 Preliminary Conclusions

Activation of constrained pre-strained SMA wire actuators, located on a laminated plates neutral plane, can result with a significant post-buckled deflection alleviation for the laminated constructions while under the influence of an external compressive load approximately three times the critical buckling value.

For the laminates so far considered, such post-buckling deflection alleviation is achieved by incorporating a relatively low volume fraction of SMA material. The effect of such deflection alleviation is to redistribute the resulting internal membrane force intensity, N_y , such that, a more uniform axial mechanical membrane force intensity exists.

The stability of the adapted shape is dependent upon the laminate stacking sequence. Due to the elevated temperatures required for SMA transition, the stacking sequence chosen should be such that temperature effects have minimal influence on the structural performance.

Modifications have been made to improve the control strategy. Improvements to the manufacturing methodology may be met by utilizing the higher recovery force of a higher at.% Ni alloy, increasing the volume fraction of the SMA wires and utilizing the micro-tubing in which the SMA wires are fed through upon completion of the fabrication process, **Section 3.3.2**. Such micro-tubing would significantly reduce the cross-sectional area of the embedded inclusion resulting with less disruption to the neighbouring ply reinforcement and plate surface topology.

6.3.3 Results For Configuration Numbers 4-9

The results of the adaptive behaviour of the composite plates featuring the higher percentage nickel, 50.0-50.2 at.%, SMA actuators, 0.4mm in diameter, will now be presented. As with **Section 6.3.1**, the plate specimens feature SMA actuators, initially pre-strained by 6%, fed through tubing after the curing process and constrained to external boundaries.

6.3.3.1 Configuration Number 4

Figure 6.9 shows the first loading cycle for the $[0_2/90_2]_s$ lay-up, configuration number 4, containing 12 centrally located SMA wire actuators. As with **Section 6.3.1**, the applied load and recorded out-of-plane displacement are presented with time, therefore, allowing clear illustration of the five characteristic phases. During *phase 1*, the specimen is loaded in compression to 1.9kN. Such a load manifests a peak out-of-plane experimental displacement of -3.01mm, FEA predicting a value of -2.66mm.

When considering the $\approx 50N$ increase in the measured experimental buckling value compared to the value predicted by FEA, **Table 4.3**, the under-prediction of the FEA post-

buckling deflection is surprising. The former increase was thought to be due to additional undesired boundary constraint. If this were the case, the FEA would over-predict the post-buckling displacement.

With activation of the SMA actuators, *phase 2*, 66.1% of the lateral deformation is removed. Activation of the SMA actuators is realised with the application of 1.7Amps to each individual actuator connected in parallel, **Table 5.2**. For this and the remaining configurations, the recovery force of an individual SMA actuator was recorded throughout the test.

The magnitude of the recorded recovery force will be dependent on the position of the actuator. As will be shown, the greatest recovery force was recorded to be approximately 80N. Its position being at SMA locations 5 & 8, **Figure 5.7**. The lowest recovery force recorded was approximately 50N. Its position being at SMA locations 1 & 12, **Figure 5.7**. Applying an average FEA recovery force of 65N to each SMA actuator results with a predicted out-of-plane deflection of -0.80mm. Utilizing this average value, reasonable agreement is obtained between experiment and FEA.

Allowing the associated temperature profile, **Figure 5.7**, to tend towards the steady-state, *phase 3*, the experimental out-of-plane displacement increases slightly to a value of -1.14mm. At this point, the displacement is 62% lower than the uncontrolled plate behaviour. Deactivation of the actuators results with the proliferate lateral displacement, *phase 4*, though now, the inactivated plate's peak displacement is only -2.18mm, 27% lower than the initial reading. This is the result of the SMA incomplete phase cycle resulting with the SMA residual stresses, or more precisely, a remaining partially activated recovery force which provides specimen lateral restraint even when the power is removed, **Figure 2.20**. This is characteristic for each specimen employing such actuators, post-cycle 1.

During *phase 5*, the load is removed, and the recorded lateral displacement is seen to decay back to zero.

Such displacement alleviation was seen with successive cycles, of which, the tenth cycle is presented in **Figure 6.10**. The notable variation with **Figure 6.9** is the peak experimental out-of-plane displacement of -1.90mm, while under the influence of 1.9kN, with the SMA actuators inactivated. Regarding the activated state, excellent cyclic behaviour is evident.

In general, good correlation exists between FEA and experiment. Obviously, utilizing the higher recovery forces for the higher at.% Ni alloy actuators and the slightly higher volume fraction results with greater post-buckled displacement alleviation when compared to the same lay-up of configuration number 1.

As with configuration number 1, post-buckled displacement alleviation has the effect of alleviating the highly non-uniform in-plane force intensities, much like those presented in **Figure 6.4**.

The previous two figures illustrate the adaptive response of specimen configuration number 4 while the specimen is under the influence of a constant load, *load control*. **Figure 6.11** depicts the behaviour while enforcing a fixed displacement, *position control*. Results are presented for the twentieth cycle.

During *phase 1*, the test machine cross heads displace such that the specimen is under the influence of the predetermined load. A slight overshoot was seen such that the actual compressive load is 1.96kN. With the energization of the actuators, out-of-plane displacement alleviation is witnessed, though not by the same previous amount. The initial rise in the recorded load is the result of the fixed cross head restraint against the lateral force imposed on the post-buckled plate by the actuators.

During *phase 3*, the experimentally recorded out-of-plane displacement is seen to increase by 0.31mm. This significant rise, as well as the additional increase in the load cell reading

from 1.96kN to 2.50kN, is the result of the specimen's longitudinal free thermal expansion. Regarding the out-of-plane displacement, other contributory factors will also come into play such as the effect of the non-uniform temperature profile on the internal stressed state and also matrix softening.

While FEA accurately predicts the trend of *phase 3*, it does over-estimate the displaced peak value. Again, this is the likely cause of failure to account for thermal degradation of the laminate properties. This would undoubtedly decrease the flexural stiffness, thus, making it easier for the FE actuator representation to pull the plate back to the flat configuration.

Upon deactivation of the actuators, the maximum out-of-plane deflection reading is seen to rise to a peak value of -2.11mm, *phase 4*, which subsequently decays back to a consistent value of -1.93mm as the plate cools. At the instant of SMA deactivation, the load cell reading is seen to decrease, eventually decaying back to its original value.

As predicted by finite element analysis, **Figure 6.12** shows the nature of the longitudinal force intensity, N_x , along the specimens centreline when (i) mechanically loaded to 1.9kN, (ii) how this N_x profile differs when under the influence of the SMA recovery forces while the specimen is compressed between the two constrained loaded edge boundaries and (iii) with the second condition, (ii), plus the effects of the non-uniform thermal profile after a 240s energization period, i.e. thermal steady-state. The mechanical results were obtained by enforcing the boundary conditions to displace the required amount, therefore, representing the experimental displacement control test.

From **Figure 6.12**, it would appear that force intensity profiles, along the unloaded edges, are less likely to decrease with SMA adaptation when tested in displacement control as opposed to load control. It is worth mentioning again, however, that with SMA adaptation, when in displacement control, the specimen will be subjected to an increase in the in-plane compressive load of 27.6%.

6.3.3.2 Configuration Number 5

When testing in load control, employing the previous SMA activation strategy for the $[0_2/\pm 45]_s$ lay-up, configuration number 5, results with an initial 74.3% experimental post-buckled displacement alleviation for cycle 1, **Figure 6.13**. Regrettably, the out-of-plane displacement steadily rises again such that the consistent displaced value is 39.5% lower than the original value, *phase 3*. It is clearly evident, *phase 4*, that the non-uniform temperature profile, **Figure 5.8**, contributes significantly to the out-of-plane traversed shape. This behaviour is much like that presented for configuration number 2.

Owing to the 22.6% increase in experimental buckling compared to the value predicted by FEA, **Table 4.3**, the disparity in post-buckling FEA and experimental results, *phase 1*, **Figure 6.13**, was expected.

For this particular lay-up, as a result of the new SMA alloy composition and the employed volume fraction of SMA material, the out-of-plane deflection does not rise to the same extent as with configuration number 2. This is solely the result of the increase in the constrained SMA recovery force.

With successive cycles, the consistent deflection value at the end of *phase 1* is lower than that obtained for the first cycle. As with the previous configuration, this is because the actuators are generating a partial recovery force. Utilizing the adaptive response, an improvement in the plate behaviour is only apparent when comparing the response to the plate behaviour with no activated SMA wires, *phase 1* cycle 1.

Good correlation exists between FEA and experiment during *phase 3*. As with configuration number 2, the predicted FEA out-of-plane displacement value at the end of *phase 1* over predicts the experimentally recorded value.

Owing to the initial significant lateral deformation removal with SMA excitation, the in-plane force intensity profiles within the specimen will re-distribute such that the force intensity profiles are approximately uniform. As the resulting temperature profile tends to the steady state, **Figure 5.8**, the resulting thermally induced stresses act to significantly increase the lateral deformation such that the resulting thermal and mechanical in-plane force intensity profile once again exists in a state of high non-uniformity. It would appear that no significant benefits are associated with the electrical energization of the parsimonious distribution of SMA actuators.

Application of the thermal influence only, associated with SMA actuation, to a mechanically post-buckled $[0_2/\pm 45]_s$ plate specimen results with an increase of 65.7% in the peak out-of-plane displacement, *phases 2-3*, **Figure 6.14**. The temperature profile, **Figure 5.8**, was imposed upon the specimen through application of the necessary power level, **Table 5.2**, to the *unconstrained* SMA actuators, hence, the SMA's are free to reform to their memorized configuration. The SMA's no longer exert a recovery force on the plate specimen. With the removal of the power, the plate lateral deflection is seen to decay back to its original value, *phase 4*.

By comparing the results at the end of *phase 3*, for **Figures 6.13 & 6.14**, then clearly, a 63.5% reduction can be achieved when utilizing the recovery response of the SMA actuators on the anisotropic plate under the influence of the externally applied load and an imposed thermal environment. SMA utilization would only be of benefit when such components are subjected to the thermal environment by a means other than electrical energization. As an example, heating, associated with skin friction, may be sufficient to drive the actuators through their phase transition such that they exert stabilising recovery forces on the skin sections of high speed aircraft.

It is conjectured that the higher experimental buckling result and the lower recorded post-buckled deflection is caused by undesired *clamping* at the boundaries. Upon energization, **Figure 6.14**, the recorded out-of-plane displacement is seen to rise by 65.7%. FEA predicts a rise of 39.0%. The higher percentage increase of the former experimental readings, compared to the FE percentage increase, will be associated with, to some degree, matrix degradation. The remainder is probably associated with in-plane clamping, particularly if clamping emanates at the knife edges.

Again, finite element analysis has adequately predicted the correct trend in post-buckled out-of-plane deformation.

Figures 6.15a-d show FEA fringe plots of the N_x membrane force intensity when loaded to 1.9kN at ambient conditions and with superimposed temperature profiles, associated with SMA actuation, at time intervals of 40, 120 and 220s after SMA actuation. The actual load case is shown on the figure.

Under mechanical loading only, **Figure 6.15a**, the central region of the plate exists in a state of tension. This fringe plot is characteristic of all the plate specimens tested, albeit, the magnitude of the tensile load will vary. Upon SMA actuation, the N_x membrane force intensity profiles deviate from the norm, **Figures 6.15b-d**. As the temperature approaches the steady-state condition, the central region of the specimen is predominantly under a state of compression, **Figure 6.15d**.

Figure 6.16 shows the N_y membrane force intensity profiles for the load cases representative of **Figures 6.15a & 6.15d**. Clearly, the effects of the imposed temperature

distribution has marginal effect on the magnitude of N_y . This verifies that N_x is the domineering factor behind the witnessed increase in post-buckled deformation, *phase 3*.

Owing to the nature of the negative coefficient of thermal expansion in the global-y direction, this specimen was not tested in displacement control.

6.3.3.3 Configuration Number 6

The adaptive post-buckling response for the first activation cycle of the $[0/\pm 45/90]_s$ lay-up, configuration number 6, is presented in **Figure 6.17**. The current specimen incorporates 14 SMA wires. Owing to the initial FE modelling error for this lay-up type, **Section 6.3.1.3**, the plate is loaded to the higher compressive load of 2.5kN, *phase 1*. Three LVDT traces are presented in the figure. The central displacement reading is presented along with displacement readings 75mm above and below the plates geometric centre, experimental displacement readings 1 & 3 respectively.

With energization of the SMA material, the peak experimental lateral displacement is seen to decrease by an initial 56.8%. However, the displacements soon rise again. At some point, the top and bottom displacement readings are seen to diverge. Midway through *phase 3*, modal instability arises and the plate snaps into a two half wave mode shape, much like that presented in **Figure 4.14b**. This instability was accompanied by an audible pop. Upon unloading the panel, the reverse process occurred.

It was this instability that prompted the use of three LVDT's. After the occurrence of modal instability, the upper and lower displacement curves are -1.3mm and +1.11mm, respectively. With SMA deactivation, these increase to maximum values of -1.53mm and +1.2mm, *phase 4*. To some extent, therefore, the SMA wires contain the second mode shape, though, they are not in an optimal position to do so effectively. Refer to **Figure 5.9** for a reminder of the positions of the activated SMA material.

Upon removal of the applied load, the central displacement is seen to increase. The plate deflects back into its single half wave.

Subsequent adaptive post-buckling cycles resulted with a two half wave mode shape occurring with activation. Such mode jumping is not desirable and, based on the previous behaviour of configuration number 2, was not expected.

Even without the means for shape adaptation, at certain load levels above the first critical buckling value, some plate configurations encounter a secondary instability which initiates a dynamic *snap* to a different waveform¹³³.

The issue of such instability is quite important as the stressed state in the second mode will be quite different from that of the first mode. Of particular concern can be the interlaminar shear stress along the nodal lines¹⁰³.

Finite element analysis has been used to study a compressive loaded plate with laterally supported edges which exhibits a secondary instability in the post-buckling regime. Material stresses during the dynamic snap were found to exceed the pre-snap stresses by as much as 17%¹³³.

It is also reported that secondary buckling is accompanied by a corresponding sharp drop in the load necessary to produce the end shortening¹⁰³.

In general, the change in the buckled pattern occurs when the energy stored in the plate is sufficient to carry the plate from one buckled pattern to another. In most post-buckling analyses the buckled pattern of the plate is assumed to remain unchanged. Intersection of the load shortening curves indicate possible changes in the buckled pattern¹³⁴. The mode which

has the minimum value of the total potential energy is assumed to represent the actual buckled pattern.

Due to the boundary conditions imposed, our square plates under consideration will buckle into and remain in the first buckling mode without a buckled pattern change if the mechanical load is gradually increased and there are no disturbing forces. As the specimens feature embedded *functional* SMA actuators we, therefore, do not conform to the latter.

As we have seen, the instability phenomenon would not have occurred when actuating the SMA material within a specimen that is mechanically loaded below P_{c2} .

Figure 6.18 illustrates the nature that the non-uniform temperature profile only, **Figures 5.9**, has on the plate specimen's out-of-plane displacement behaviour. Such a temperature profile will act to increase the plate's peak lateral deflection by 28.9%, *phases 2-3*. A much lower increase than the previous lay-up. This is because the thermally isotropic in-plane CTE value, which is associated with the quasi-isotropic lay-up, is much lower than that for the former configuration.

As with configuration number 5, poor agreement between the predicted FEA and the experimental displacement is apparent. Such specimens proved very problematical. The contravening mechanism causing results disparity was not isolated.

6.3.3.4 Configuration Number 7

From the optical micrographs presented in **Figures 3.1-3**, embedding 0.4mm diameter SMA wires within PTFE tubing results with significant ply reinforcement and surface topology disruption. Depending on the neighbouring ply orientation, such local disruption can be completely alleviated by locating the actuators within micro-tubing, **Figure 3.4**.

A $[0_2/90_2]_s$ plate was manufactured with 18 such embedded inclusions. Steel wires were first inserted and withdrawn upon completion of the cure. SMA actuators were then inserted and clamped accordingly. Inspection of the manufactured plate revealed no discernible surface topology disruption. The plate was then tested for its adaptive response. Cycles 1 and 5, while in load control, are presented in **Figures 6.19 & 6.20** respectively. The tenth cycle, while in position control, is presented in **Figure 6.21**. The specimen's surface thermal characteristics are presented in **Figures 5.11**.

At the end of *phase 1*, **Figure 6.19**, there is good agreement between the experimentally recorded out-of-plane displacements and FEA. For this particular specimen, the reader is referred to **Figure 4.17** for the time-independent characteristics of *phase 1*.

With SMA activation, *phase 2*, quick inspection of the adaptive post-buckling curves, **Figures 6.19-6.20**, reveals unpromising displacement alleviation. Only a 52.2% alleviation in the peak experimentally obtained out-of-plane displacement is realized. This is because difficulty arose when trying to insert the actuators after the manufacturing process. Such was the difficulty that an inserted SMA wire, at position 7, **Figure 5.11**, failed to egress the other end. For symmetrical purposes, it was decided to remove the actuator from position 12. Such positions are deemed critical for the plates displaced mode alleviation. Utilizing the PTFE tubing, SMA ingress and egress has previously caused no difficulty.

Although the placement of the energised actuators results with a more uniform temperature profile, **Figure 5.11**, detrimental in-plane compressive membrane forces still exist, their effect is illustrated on the unconstrained SMA post-buckled plate behaviour shown in **Figures 6.22 & 6.23**. **Figure 6.22** depicts the response while the specimen is compressed in load control. An increase in plate out-of-plane displacement of 14% is observed with superposition of the thermal environment imposed by the unconstrained actuators. However,

an 18.9% increase is observed when the specimen is tested in position control, **Figure 6.23**. The restraint imposed by the fixed loaded edge boundary prevents free longitudinal thermal expansion causing both the registered load and the plate lateral deflection to increase.

As predicted by finite element analysis, **Figure 6.24** shows the nature of the longitudinal force intensity, N_y , along the specimens centreline when, first, mechanically loaded to 1.9kN only, and, second, how this N_y profile differs when under the influence of the mapped thermal response with constrained loaded edge boundaries. At this point, the experimental applied compressive load was seen to be 2.18kN, **Figure 6.23**. The mechanical results were obtained by enforcing the boundary conditions to displace the required amount, δ , therefore, representing the experimental displacement control test. With no boundary constraint enforced, then, N_y was not seen to vary by any significant amount.

The shape memory recovery force measured during the testing process, at SMA location 15, for ten cycles, is presented in **Figure 6.25**. The curves show that, for the power level applied, the generated recovery force is not at its full potential. Better cyclic behaviour was expected.

The adaptive non-linear finite element results, presented in **Figures 6.19 & 6.21**, were obtained with an average recovery force of 65N applied to each SMA FE representation. It is seen that the correct trend in behaviour is predicted, however, close conformity with the actual experimental values may only be obtained when utilizing finite elements that account for material temperature dependency.

6.3.3.5 Configuration Number 8

A second [0₂/90₂]_s plate was manufactured with 18 embedded inclusions at the same locations as with the previous specimen configuration, though, this time PTFE tubing was employed.

While in load control, **Figure 6.26** illustrates the first cycle of the adaptive post-buckling displacement response. Upon SMA activation, *phase 2*, 71.5% of the experimental out-of-plane deflection is removed. This degrades to a consistent 69.4% as the temperature approaches the steady-state. Upon removal of the actuator power source, the out-of-plane displacement increases rapidly prior to stabilising at -2.02mm.

During *phases 1 & 2*, **Figure 6.26**, good agreement between FEA and experiment exists. The FEA results are not shown during phase 3 as they were not seen to vary by any noticeable amount.

As predicted by FEA, **Figure 6.27** shows the uniformity in the longitudinal membrane force intensity, N_y , along the centreline of the specimen when loaded to 1.9kN and with activation of the actuators. Compared to the inactivated load case, the N_y force component at the knife edge supports is reduced by 62.7%. Within the subsequent FEA load cases, when simulating load control, the effects of temperature on N_y are of minimal consequence.

Enforcing displacement control to the plate specimen results with the adaptive post-buckled behaviour shown in **Figure 6.28**. As with the tests conducted in load control, the adaptive response shows displacement alleviation. At the end of *phase 3*, the maximum out-of-plane displacement is seen to be 17% lower than the inactivated phase of cycle 1. At this point, however, the load is seen to be -2.91kN as opposed to -1.9kN, a rise of 53.2%.

As predicted by FEA, **Figure 6.29** shows the N_y force intensity profiles when the specimen is first loaded to 1.9kN, through enforcing and maintaining the required edge displacement, δ . Subsequent load case profiles illustrate the effect that the shape memory adaptation and the thermal characteristics, **Figure 5.12**, have on the specimen.

The major non-uniformity within the specimen's temperature response, **Figure 5.12**, is again at the plates edges and is highlighted by thermocouple locations 1 & 13. Its effect on the unconstrained SMA plate post-buckling behaviour is shown in **Figures 6.30 & 6.31**. Maintaining a constant applied load to the specimen of -1.9kN, the plates lateral deflection will increase by 11.4%. However, maintaining a constant edge displacement, the maximum central deflection will increase by 15.4%. Such percentage increases were the lowest obtained, while the out-of-plane deflection removal was one of the highest. The latter is applicable for the tests conducted in load control only.

As predicted by finite element analysis, **Figure 6.32** shows the nature of the longitudinal force intensity, N_y , along the specimens centreline when, first, mechanically loaded to 1.9kN only, and, second, how this N_y profile differs when under the influence of the mapped thermal response with constrained loaded edge boundaries. The mechanical results were obtained by enforcing the boundary conditions to displace the required amount, therefore, representing the experimental displacement control test. With no boundary constraint enforced, then, N_y was not seen to vary by any significant amount.

Owing to the power constraints, each actuator only received a current of 1.4 Amps, therefore, resulting with incomplete austenite phase cycling. Higher power levels would result with greater recovery forces and therefore a greater percentage increase in the post-buckling deflection alleviation.

The shape memory recovery force of an SMA wire at position number 7, **Figure 5.12**, during plate cycling is shown in **Figure 6.33**. The repeatability in each trace is more apparent than those of **Figure 6.25**.

6.3.3.6 Configuration Number 9

Finally, **Figure 6.34** shows the adaptive post-buckling deflection behaviour for the last specimen in this control strategy.

17 SMA actuators were employed for adaptive purposes. Owing to the thermal boundary layer associated with SMA energization it has typically been observed that the upper half of the plate specimens are hotter than the lower half. In an attempt to create a more uniform and symmetric vertical thermal response SMA number 1, **Figure 5.12**, was omitted from the test and analysis.

Within **Figure 6.34**, the prevalent observations are apparent. At the end of phase 1, a 69.9% removal in the recorded out-of-plane deflection is achieved. This being the highest deflection removal for such a lay-up architecture. This decreases to 66.2% as the steady-state thermal response is approached, **Figure 5.13**.

For such actuator positioning, the degradation in lateral deflection removal is minimal as the predominant detrimental in-plane thermal forces that cause such degradation are minimal, **Figures 5.19b, 5.19d** and **Table 5.4**.

As with the previous discussions on the specimen configurations within *control strategy 1*, at the end of *phase 2*, and throughout *phase 3*, a more uniform distribution of N_y , internal to the specimen, accompanies the out-of-plane deflection removal.

6.4 Adaptive Post-buckling Response For *Control Strategy 2*

6.4.1 Results For Configuration Numbers 10-11

The results of the adaptive behaviour of the composite panels featuring 23 50.0-50.2 at.% Ni SMA actuators, 0.3mm in diameter, partially constrained to the host, for configuration numbers 10-11, will now be presented.

Both specimen configurations within this control strategy employed the testing procedure previously outlined. Pertaining to the generated results, as with **Section 6.3**, the applied load and the corresponding peak amplitude central deflection will be shown with respect to time.

6.4.1.1 Configuration Number 10

For both configurations, after each individual panel specimen was positioned within the loading rig, the desired load was applied. As with the previous plate specimens, the value of the maximum applied load was slightly less than the second mode theoretical pseudo-buckling value. This value was predicted by finite element analysis, **Table 4.3**.

Generally, within stiffened panel specimens, there is a greater propensity for modal instability when loaded above P_{c1} . Should the employed stiffeners be relatively compliant, then, the secondary buckling phenomenon will behave much like that of the un-stiffened plates within **Section 6.3**.

Highly stiff stiffeners, however, can promote the onset of secondary instability. As previously discussed, **Section 6.3.3.3**, this phenomenon is due to the existence of a bifurcation point on the post-buckling load-deflection path. For all the test specimens considered in this research, its presence is dependent on the magnitude of any in-plane restraint imposed along the unloaded edges.

In general, for a given specimen, the greater the unloaded in-plane edge restraint, the lower the level of end shortening required to cause secondary buckling¹⁰³. Depending on their position, highly stiff stiffeners can enforce a uniform unloaded edge displacement which, while not being so prominent, acts in much the same manner as prohibition of free in-plane movement. The compliance associated with the stiffeners chosen within this investigation should prohibit the significance of modal instability along the post-buckling path.

As with *control strategy 1*, activation of the SMA actuators is enforced with the application of the appropriate power source, **Table 5.2**. The restraint imposed by the host on the SMA actuators induces recovery forces that act to remove the out-of-plane post-buckling deflection. The final stable out-of-plane deformation, however, will be a complex function of the applied mechanical load, each individual SMA recovery force and the resulting imposed non-uniform temperature response associated with SMA activation.

The completely fixed constraints within *control strategy 1* prevent SMA recovery. Within *control strategy 2*, spring-type constraints prevent complete shape memory recovery. The spring coefficient being dependent upon the panels in-plane stiffness. When under the influence of the external mechanical load and internal SMA recovery forces, the amount of strain imposed within each of the panel specimens will not be high enough to cause significant SMA shape recovery. **Figure 2.24** shows only a modest variation between the recovery forces generated within 0.3mm diameter alloy actuators initially pre-strained by 3.5% and 6%. Upon SMA energization, based on the assumption of finite strain recovery, we can, therefore, assume the actuators behave as if they were completely fixed.

For the benefit of the finite element analysis, unlike *control strategy 1*, it is not easily feasible to measure the SMA recovery forces generated during the panel testing stage. For a certain power level, the partially abraded SMA recovery force obtained for the 49.8-50.0 at.% Ni SMA material, 0.3mm in diameter, was 35N, **Figure 2.23**. As opposed to the 0.4mm diameter actuators, both 0.3mm diameter alloy compositions showed similar recovery force characteristics, **Figures 2.23 & 2.24, Section 2.6.3**. The higher at.% Ni 0.3mm diameter alloy actuator had a slightly higher recovery force response. It is expected, therefore, that the partially abraded recovery force response of the 50.0-50.2 at.% Ni alloy would reach a peak value between 35N and 40N, the average being 37.5N.

Within *control strategy 1*, when energized at the same power level, the SMA recovery force response in the actual tested specimens, **Section 6.3**, was approximately 20% lower than the recovery response presented in **Figures 2.23 & 2.24**. Quite simply, this is attributed to thermal energy propagation from the SMA to the surrounding host material. Conduction being the thermal energy propagation mechanism.

Based on this observation, the recovery force to be applied to each stiffened panel finite element SMA representation is 20% of 37.5N, i.e. 30N. This is the in-plane membrane recovery force, or FE point force, that is applied at the appropriate nodal points, the actual positions being discussed in **Section 4.8.2**.

As the actual positioning of the SMA material is off the neutral plane, a moment is present. The moment arm, applied at the same point as the aforementioned in-plane membrane point force, equals 11.4Nmm. This value being the product of 30N * 0.38mm. The latter value being the distance between the centre point of the SMA actuator and the neutral plane of the panel's skin. This value was obtained from the optical micrograph presented in **Figure 3.12**.

Figure 6.35 shows the loading-activation-deactivation-unloading cycle for specimen configuration number 10, cycle 1. **Figure 4.20** shows the time-independent characteristics of the mechanical loading stage of *phase 1*. Considering the assumptions made within the FE representation, **Section 4.8**, reasonable agreement between FE and experiment is apparent. At the end of *phase 1*, the experimentally recorded peak/central out-of-plane-deformation was recorded to be 1.25mm. Finite element analysis predicted a value of 1.11mm.

Activation of the 23 0.3mm diameter SMA actuators, their orientation being parallel to the applied loading direction, results with modest experimental displacement alleviation, *phase 2*. The experimental out-of-plane displacement is reduced by 14.4%. Application of the FE SMA recovery force approximation to the post-buckled stiffened panel representation results with an initial reduction of 21.6% in the peak out-of plane deformation.

Unfortunately, as with several of the specimens tested within **Section 6.3**, the out-of-plane deflection soon rises to a value higher than the value at the end of *phase 1*. This is certainly not desirable.

The exponential type rise in lateral deformation is again attributed to the thermal response within the specimen. At the end of *phase 3*, the recorded out-of plane deflection is 1.39mm. Finite element analysis predicted a value of 1.46mm. The finite element response was determined by mapping the predicted temperature profiles, **Figure 5.15**, onto the structural model. If we omit the SMA recovery force response from the FEA, then, at the end of *phase 3*, for the specimen under the influence of the compressive load of 2.1kN and the imposed temperature profile after 240s of SMA actuation, then, the out-of-plane deflection is predicted to be 1.59mm. Actuators energised by a thermal environment could, therefore, be used to enhance the mechanical response of the panel.

The rise shown during *phase 3* is the predominant result of temperature non-uniformity. The effects of material temperature dependency are not thought to be so prodigious as the

magnitude of the peak temperature is not as high as those previously witnessed in Section 6.3.

To some degree, the rise is attributed to the differing global coefficients of thermal expansion present within the various sections of the panels skin. Recall from Section 4.8.3 & Figure 4.12, that several layers of glass insulating weave are employed local to the placement of the central region of the SMA actuators, Figure 3.8. Complete coverage over the section denoted by B, Figure 4.12, was assumed for ease of modelling. Based on this assumption, CLPT was employed to determine the CTE values for both sections A & B, Table 4.2. For section A, the determined values of α_x & α_y were $1.80 \cdot 10^{-6} \text{ }^\circ\text{C}^{-1}$ & $1.80 \cdot 10^{-6} \text{ }^\circ\text{C}^{-1}$ respectively. For section B, the determined values of α_x & α_y were $2.43 \cdot 10^{-6} \text{ }^\circ\text{C}^{-1}$ & $2.43 \cdot 10^{-6} \text{ }^\circ\text{C}^{-1}$ respectively.

From these values, the section denoted by B, when under the influence of a thermal elevation, will wish to expand more in the x-direction, Figure 4.12, than section A. It will be constrained from doing so by section A, therefore, compressive stresses will initiate that promote post-buckling.

The differing α_y constituents within the skin will have minimal effect on the post-buckling response. The interaction of the cooler stiffener and the warmer skin, however, will have an effect. Due to time constraints, it has not been assessed.

Ignoring the effect of the glass weave's coefficient of thermal expansion and the SMA recovery response, then, at the end of *phase 3*, the predicted FE out-of-plane displacement is 1.46mm. This value being 8.18% lower than the analysis that accounts for the glass weave's thermal expansion characteristics.

6.4.1.2 Configuration Number 11

Figure 6.36 shows the loading-activation-deactivation-unloading cycle for specimen configuration number 11, cycle 1. Figure 4.21 shows the time-independent characteristics of the mechanical loading stage of *phase 1*. At the end of *phase 1*, the experimentally recorded peak/central out-of-plane-deformation was recorded to be 1.66mm. Finite element analysis predicted a value of 1.48mm.

Activation of the 23 0.3mm diameter SMA actuators again results with only a modest experimental displacement alleviation, *phase 2*. The experimental out-of-plane displacement is only reduced by 3.1%. Application of the FE SMA recovery force approximation to the post-buckled stiffened panel representation results with an initial reduction of 4.7% in the peak central out-of plane deformation. The higher applied mechanical load may be the sole cause of out-of-plane deformation alleviation prevention.

Unfortunately, as previously discussed, the experimental out-of-plane deflection soon rises to a value higher than the value at the end of *phase 1*. Although not as prominent as configuration number 10, the exponential type rise in lateral deformation is again attributed to the thermal response of the specimen. At the end of *phase 3*, the recorded out-of plane deflection is 1.82mm. Finite element analysis, however, predicted a value of 1.36mm. Clearly, the finite element results show a decrease in out-of-plane deformation as the thermal energy propagates throughout the specimen.

This is the first time that the experimental trend has not been followed by the adaptive FE solution. Owing to the lay-up's architecture, this specimen does have a high transverse thermal expansion response when subjected to a temperature increase. There could be an element of clamping along the loaded edges where the SMA actuators egress the specimen. Such SMA wires are fed through the holes located within the loaded edge supports, Figure

4.3. The fixed constraints of the egressed SMA wires that are fed through such holes may prevent the desired transverse thermal deformation. This should, however, be investigated further. To some extent, skin stiffener influences, in-plane thermal stresses and the non-account for temperature dependency will also influence the results.

As with all other specimens tested in this research programme, the finite element response was determined by mapping the predicted temperature profiles, **Figure 5.16**, onto the structural model. If we omit the recovery force response from the FEA, then, at the end of *phase 3*, for the specimen under the influence of the compressive load of 2.5kN and the imposed temperature profile after an SMA activation time of 240s, the out-of-plane deflection was predicted to be 1.46mm. A value not too dissimilar from the FE result at the end of *phase 1*.

Regarding the numerical results, the influence of the thermal response on the adaptive post-buckling behaviour is to alleviate the peak central out-of-plane deformation. As with the previous section, CLPT was employed to determine the CTE values for both sections A & B, **Table 4.2, Figure 4.12**. For section A, the determined values of α_x & α_y were $7.77 \cdot 10^{-6} \epsilon/^\circ\text{C}$ & $-0.60 \cdot 10^{-6} \epsilon/^\circ\text{C}$ respectively. For section B, the determined values of α_x & α_y were $8.38 \cdot 10^{-6} \epsilon/^\circ\text{C}$ & $-0.13 \cdot 10^{-6} \epsilon/^\circ\text{C}$ respectively.

From these values, the section denoted by B, when under the influence of a thermal elevation, will wish to expand more in the x-direction, **Figure 4.12**, than section A. It will be constrained from doing so by section A, therefore, compressive stresses will initiate and act to promote post-buckling.

Owing to the negative CTE in the y-direction, however, when subjected to the non-uniform elevated temperature, the skin will wish to contract. From **Figure 5.16**, the central region of the skin will wish to contract more than the cooler edges and the cooler stiffeners. The cooler sections will act as constraints such that tensile membrane stresses, N_y , form within the panels skin. Their effect will be to cancel the effect of the accompanying N_x compressive stress.

Ignoring the effect of the glass weave's coefficient of thermal expansion and the SMA recovery response, then, at the end of *phase 3*, the predicted FE out-of-plane displacement is 1.21mm. This value being 17.12% lower than the analysis that accounts for the glass weave's coefficient of thermal expansion.

While not as effective as *control strategy 1*, the concept behind *control strategy 2* has been shown to work. Its efficient, or optimal, utilization, however, has yet to be demonstrated. More of which will be discussed in **Chapter 8**.

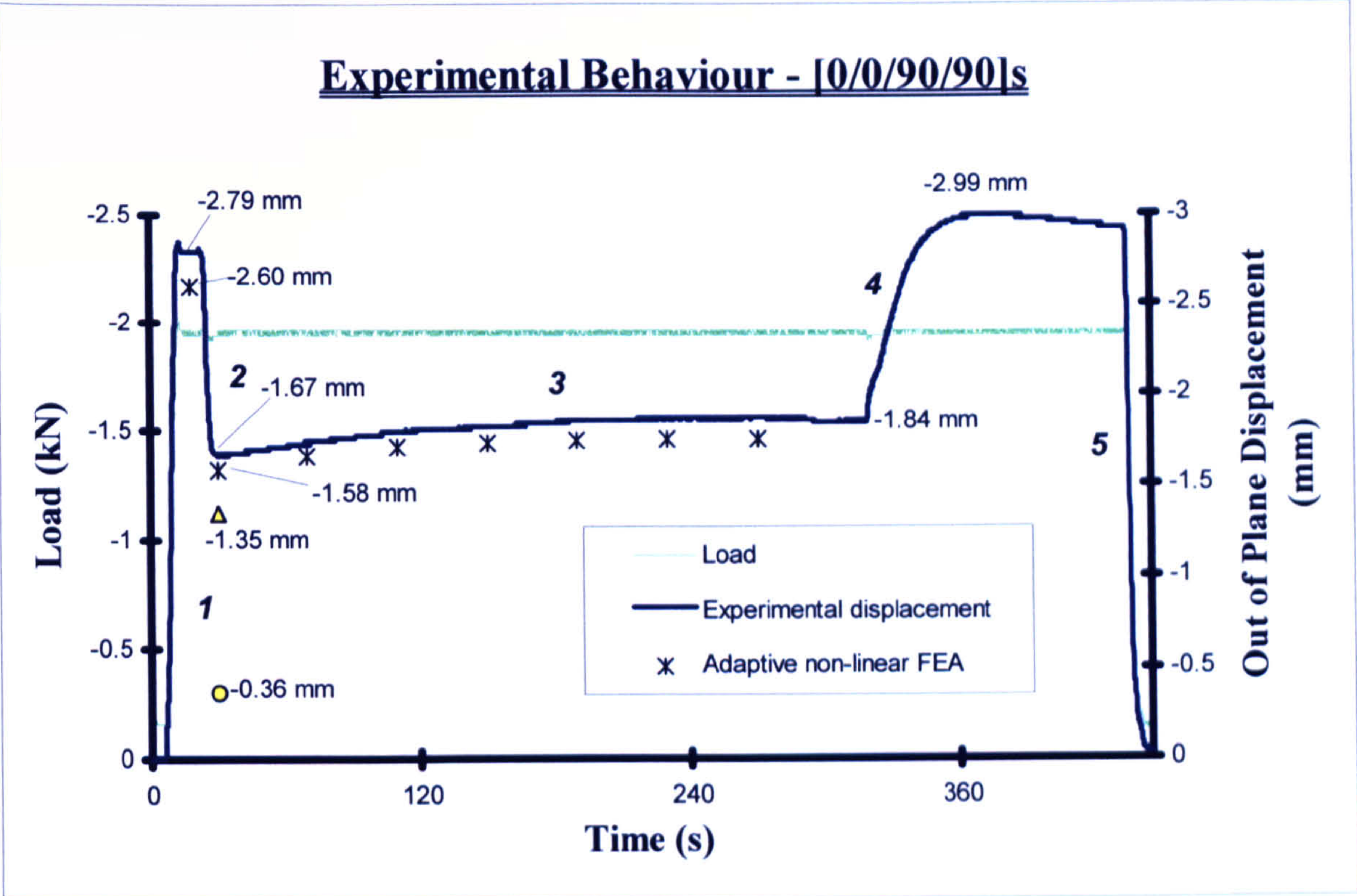


Figure 6.3. Adaptive post-buckling deflection behaviour for specimen configuration number 1, cycle 10.

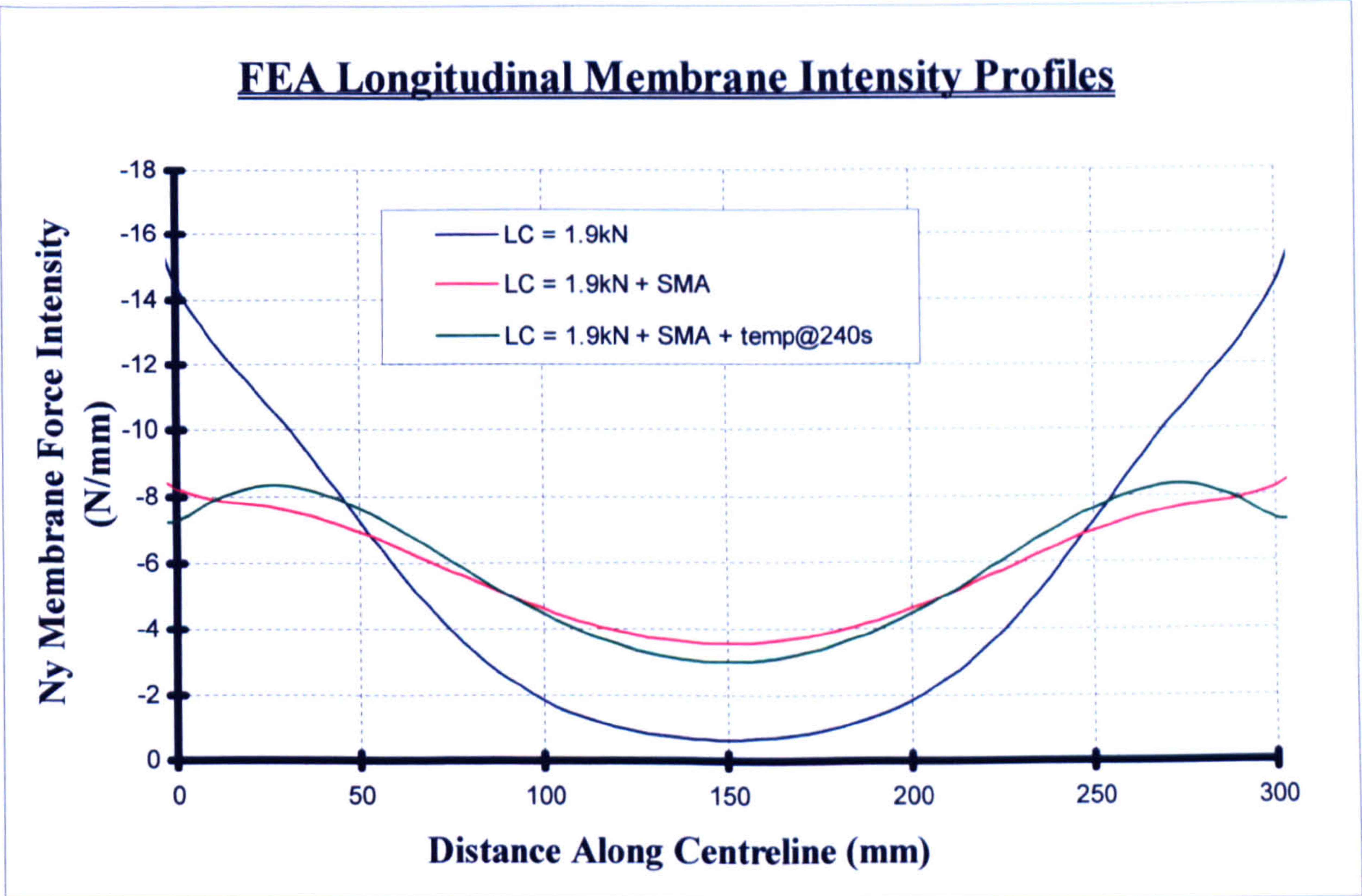


Figure 6.4. Non-adaptive & adaptive longitudinal membrane force intensities, N_y , within specimen configuration number 1.

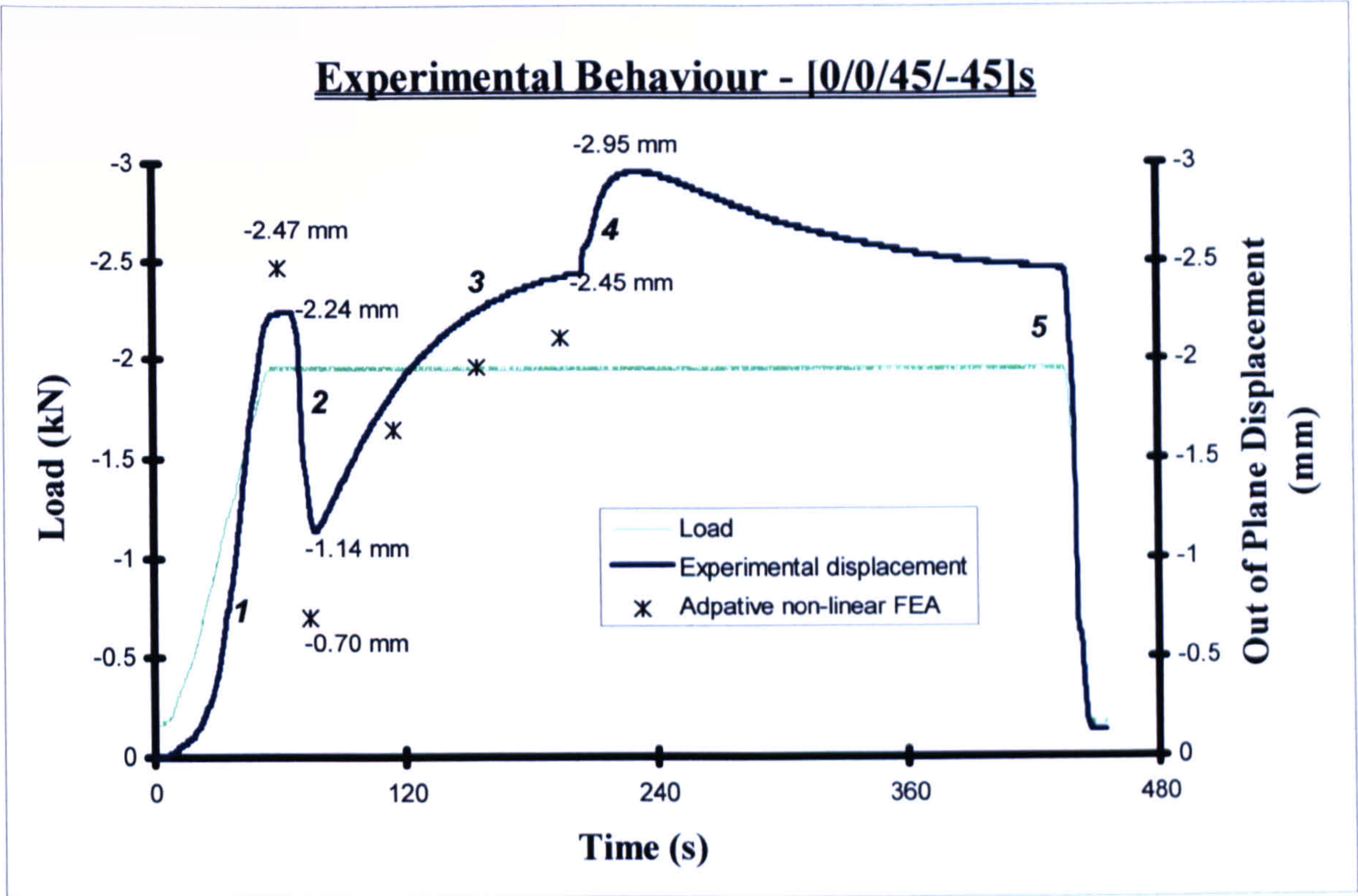


Figure 6.5. Adaptive post-buckling deflection behaviour for specimen configuration number 2, cycle 1.

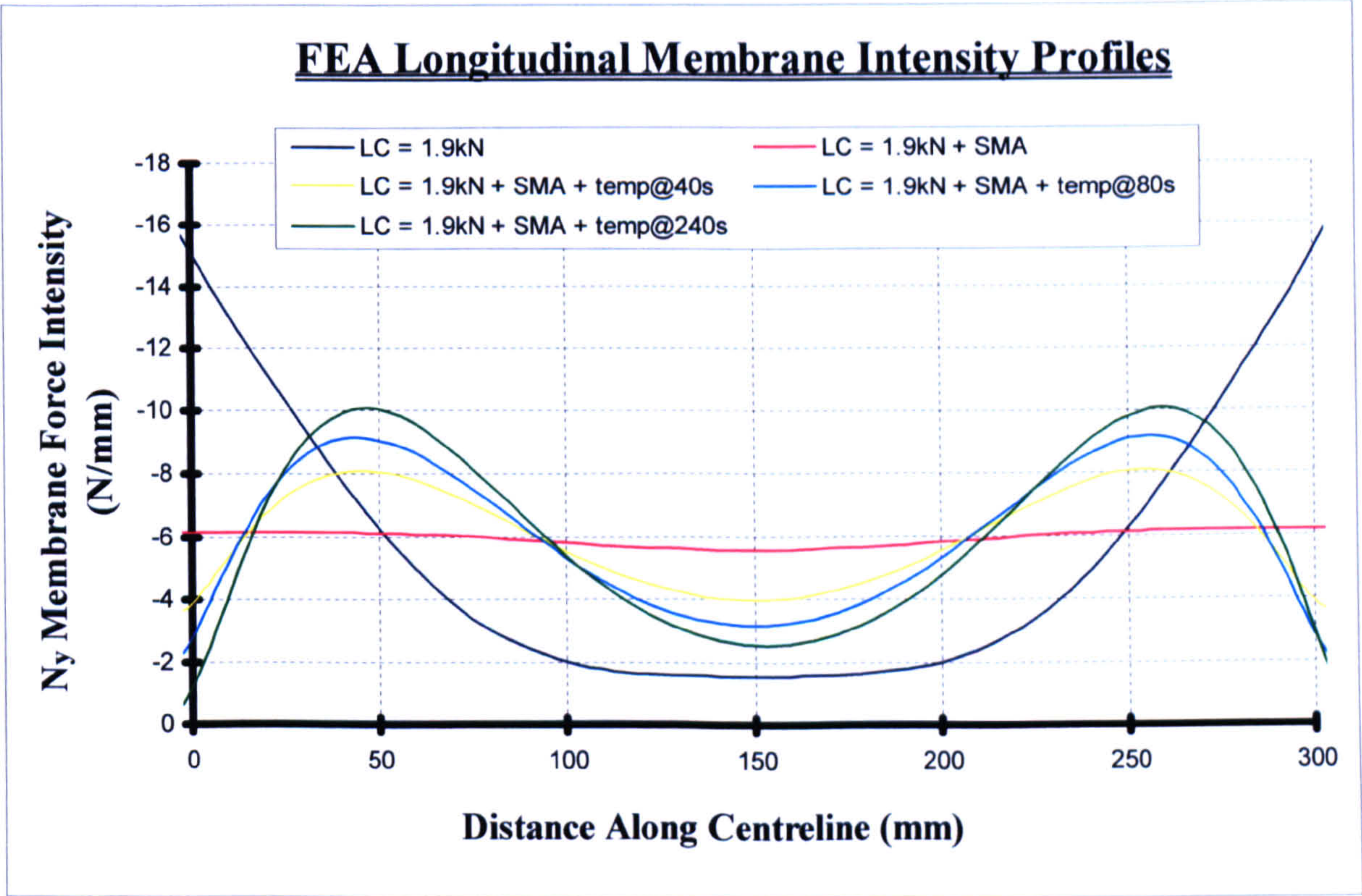


Figure 6.6. Non-adaptive & adaptive longitudinal membrane force intensities, N_y , within specimen configuration number 2.

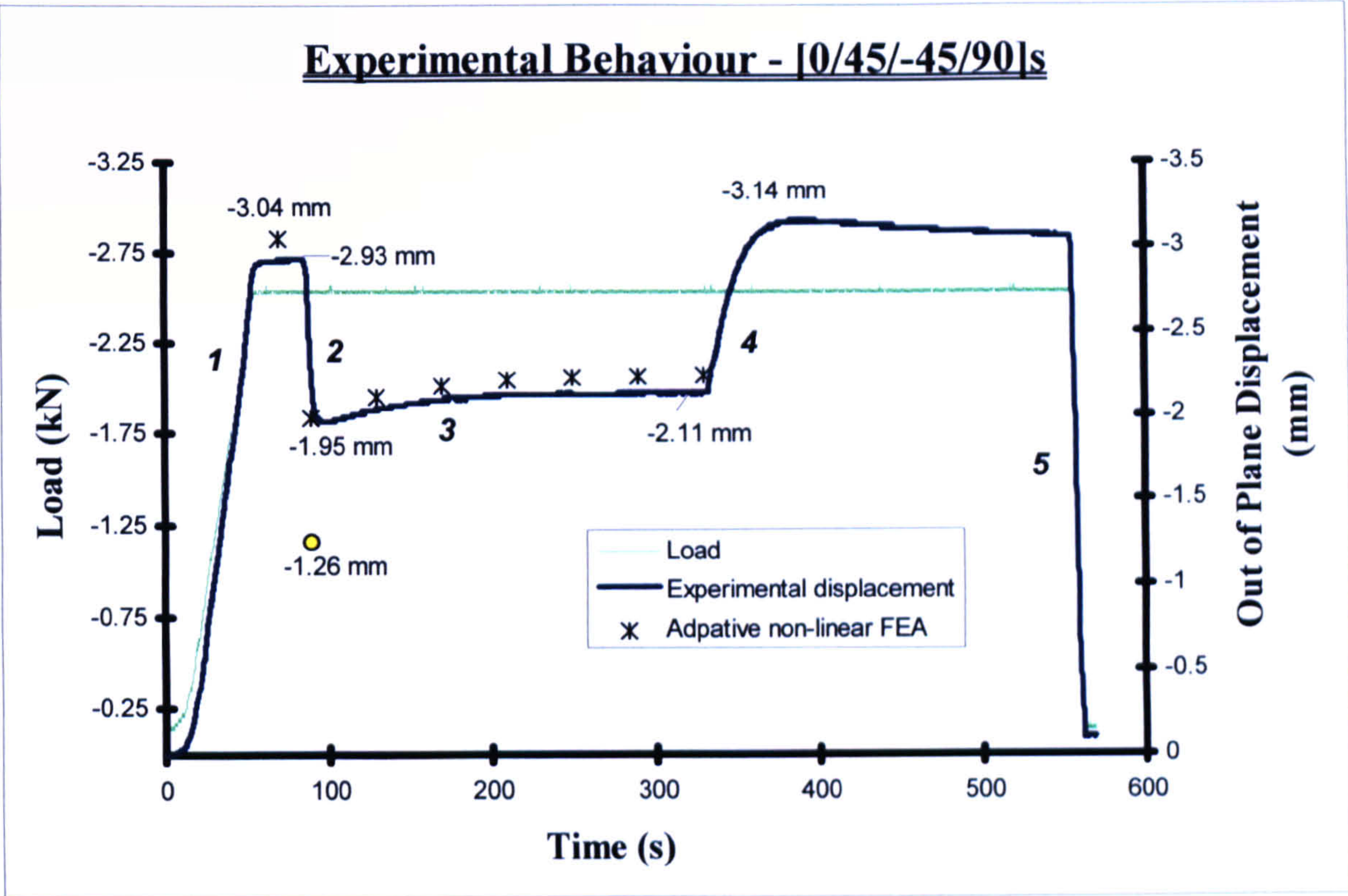


Figure 6.7. Adaptive post-buckling deflection behaviour for specimen configuration number 3, cycle 1.

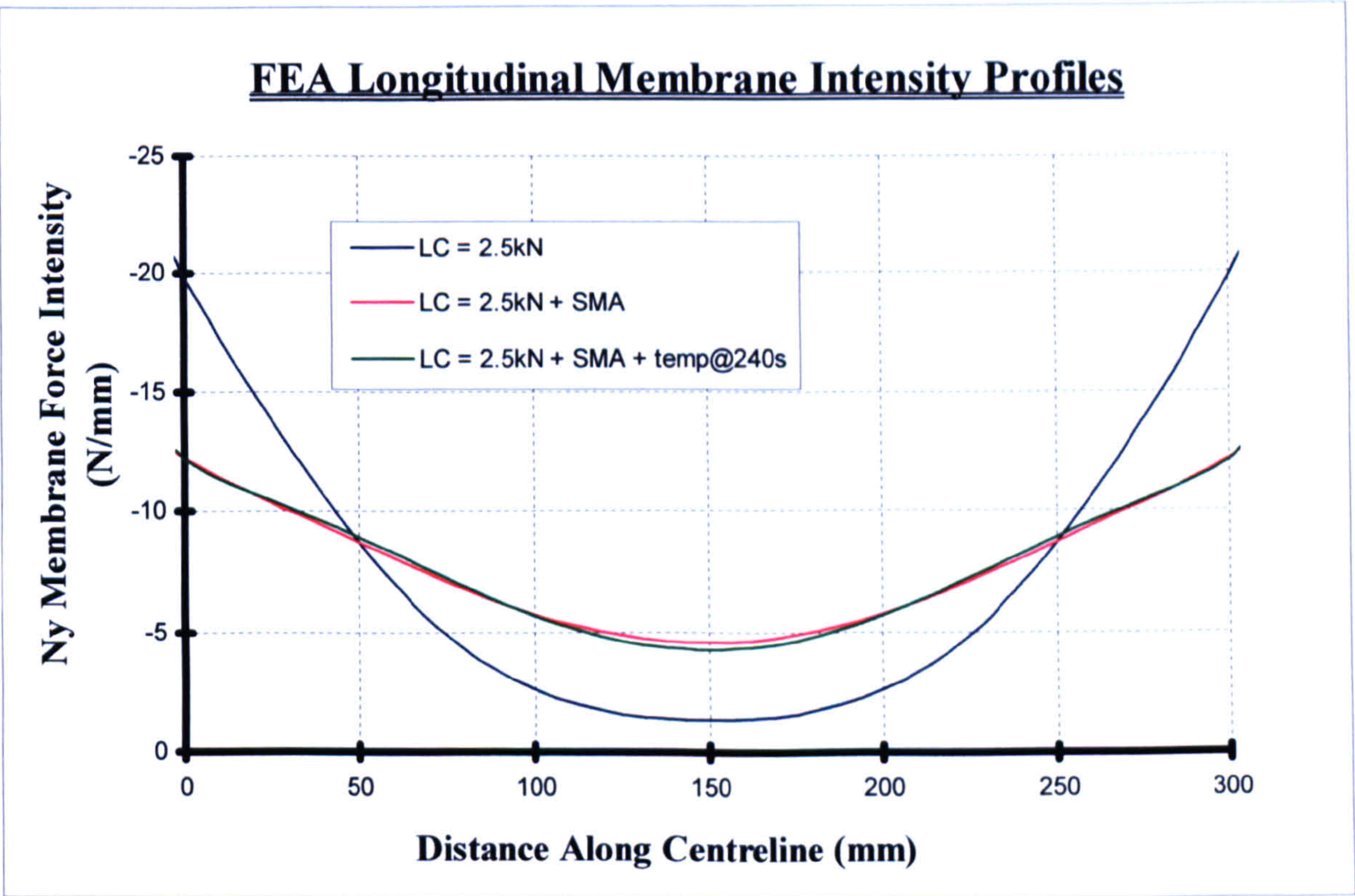


Figure 6.8. Non-adaptive & adaptive longitudinal membrane force intensities, N_y , within specimen configuration number 3.

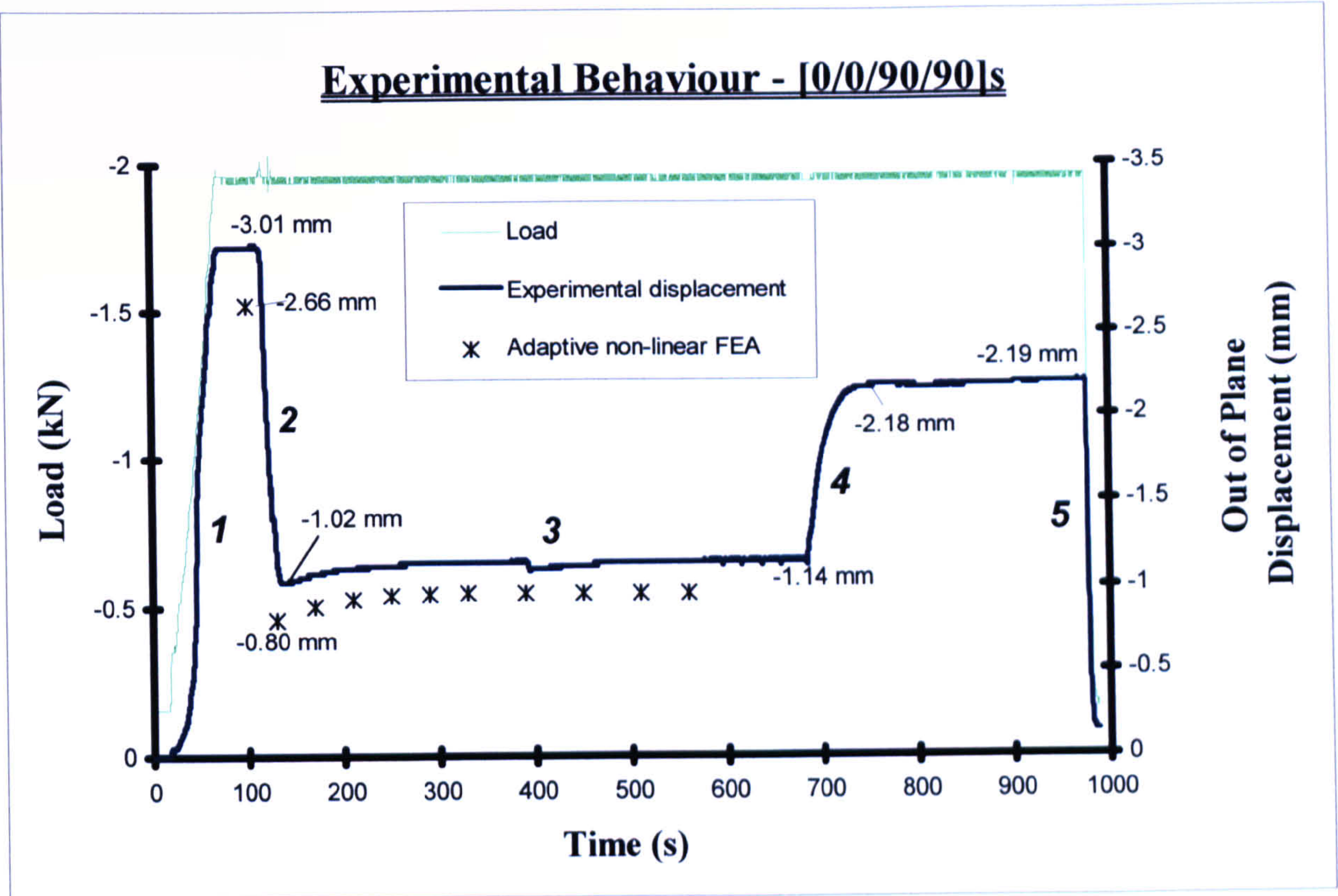


Figure 6.9. Adaptive post-buckling deflection behaviour for specimen configuration number 4, cycle 1.

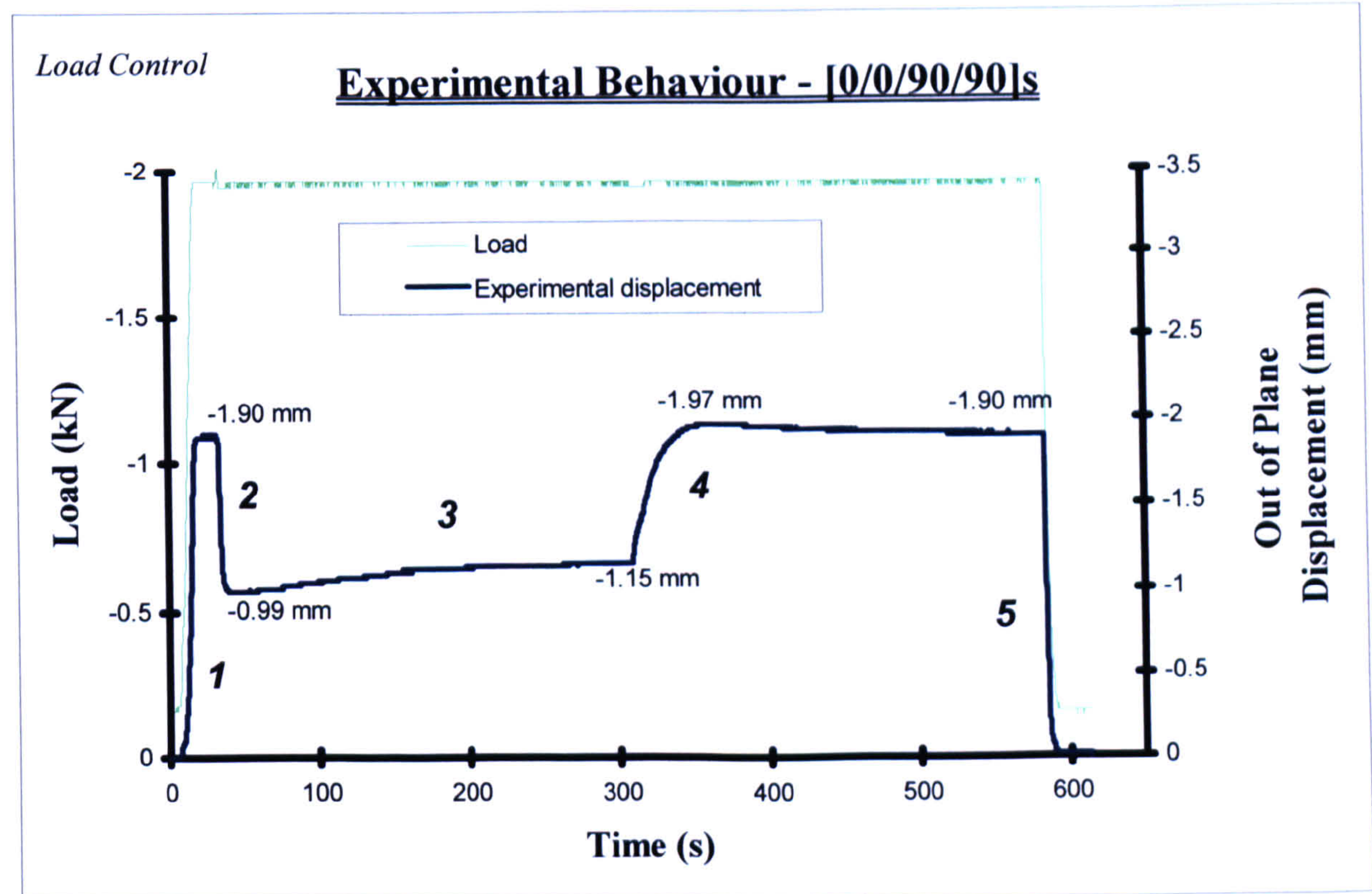


Figure 6.10. Adaptive post-buckling deflection behaviour for specimen configuration number 4, cycle 10.

Experimental Behaviour - [0/0/90/90]s

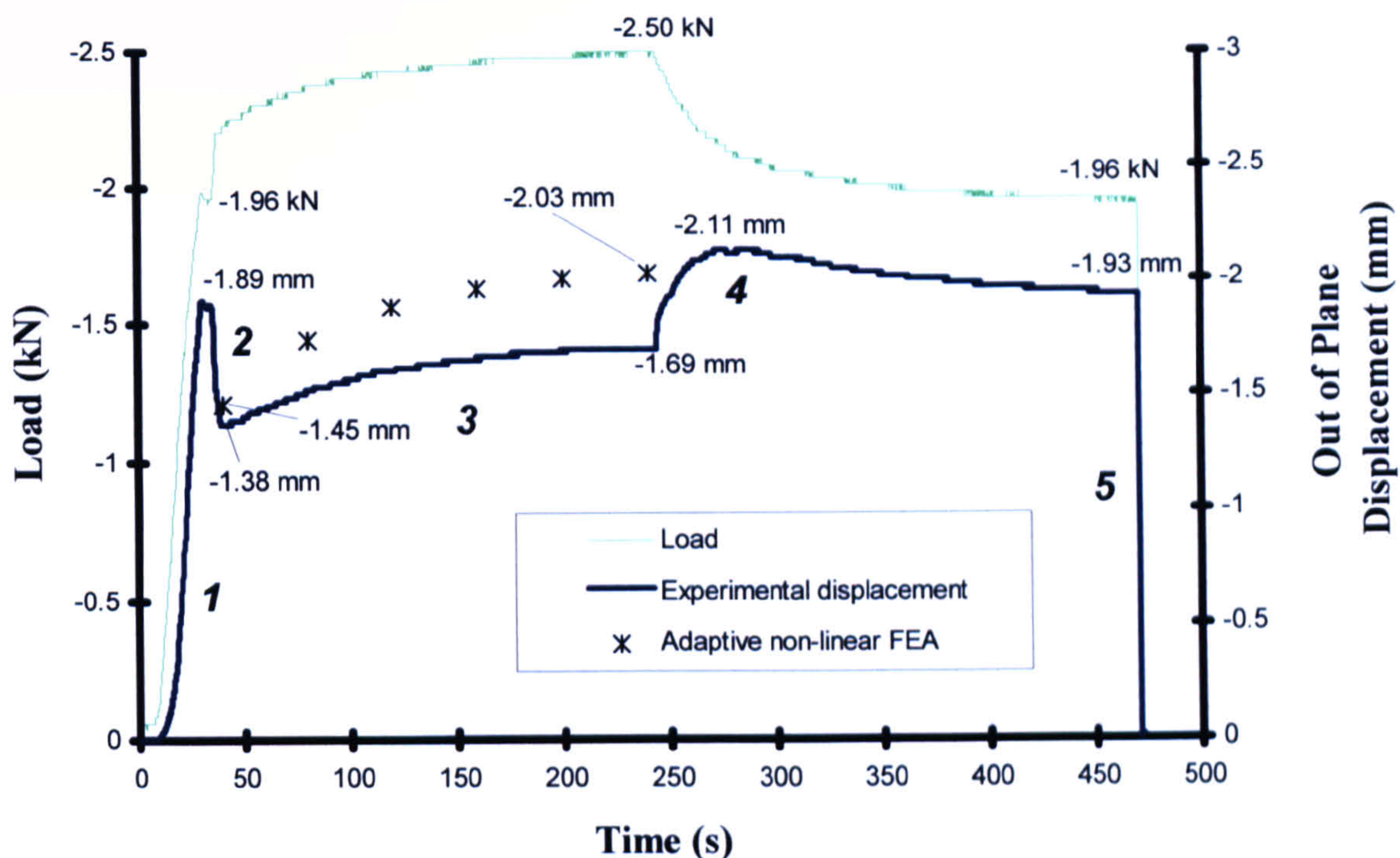


Figure 6.11. Adaptive post-buckling deflection behaviour for specimen configuration number 4, Cycle 20.

FEA Longitudinal Membrane Intensity Profiles

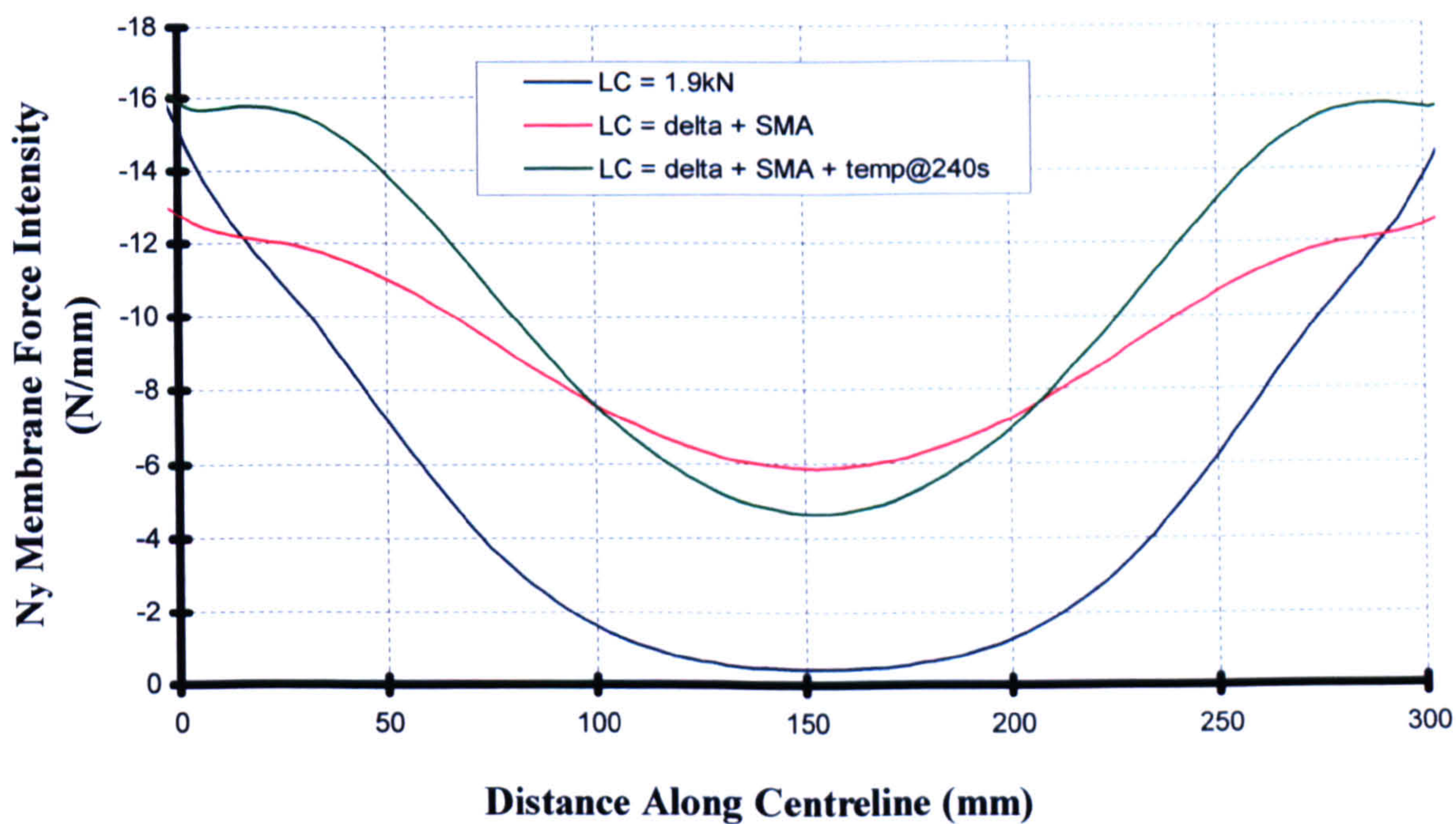


Figure 6.12. Adaptive & non-adaptive longitudinal membrane force intensities, N_y , within specimen configuration number 4.

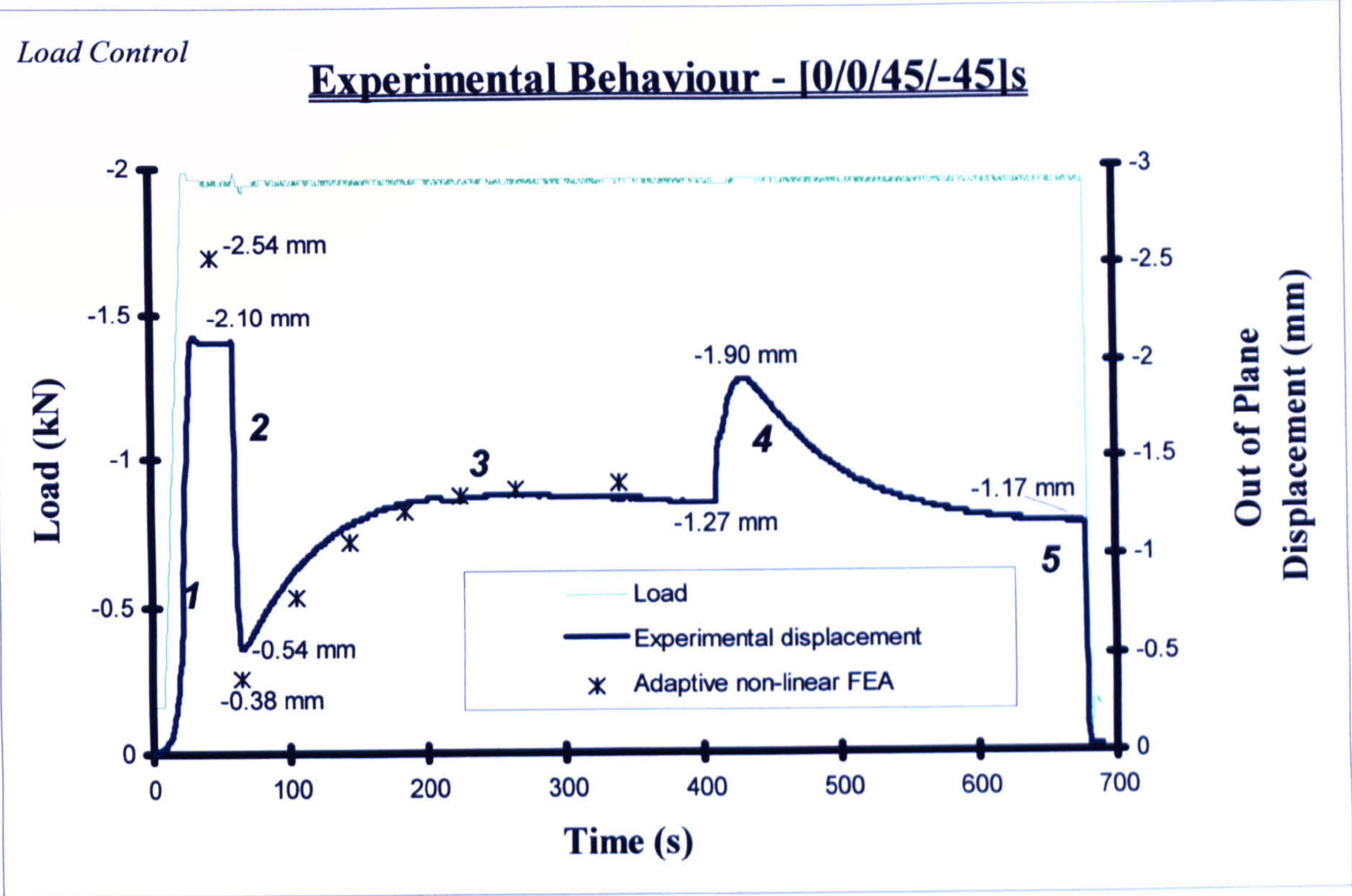


Figure 6.13. Adaptive post-buckling deflection behaviour for specimen configuration number 5, Cycle 1.

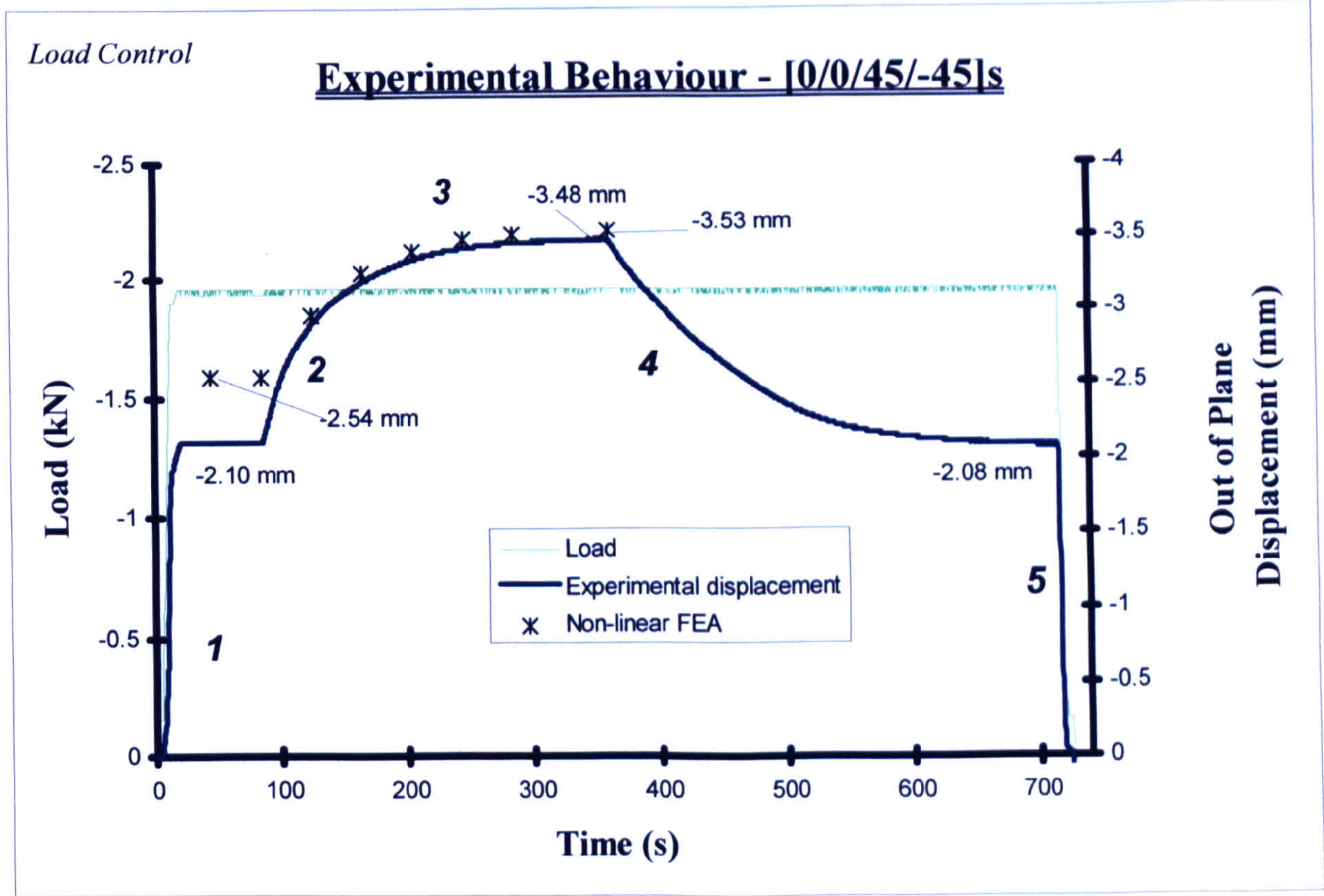


Figure 6.14. SMA temperature effects only on the post-buckling deflection behaviour for specimen configuration number 5.

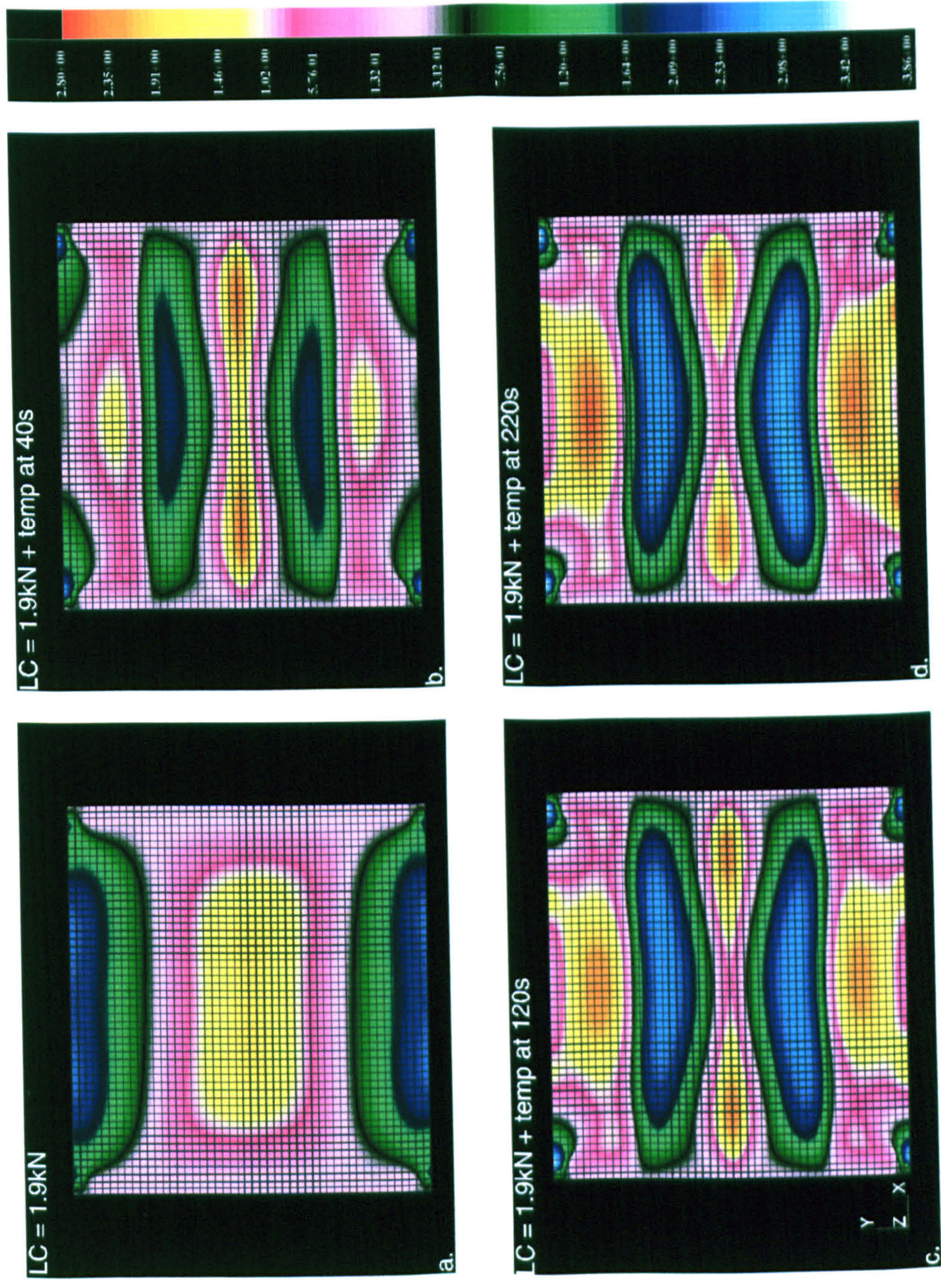


Figure 6.15. N_x force intensity profiles for specimen configuration No.5.

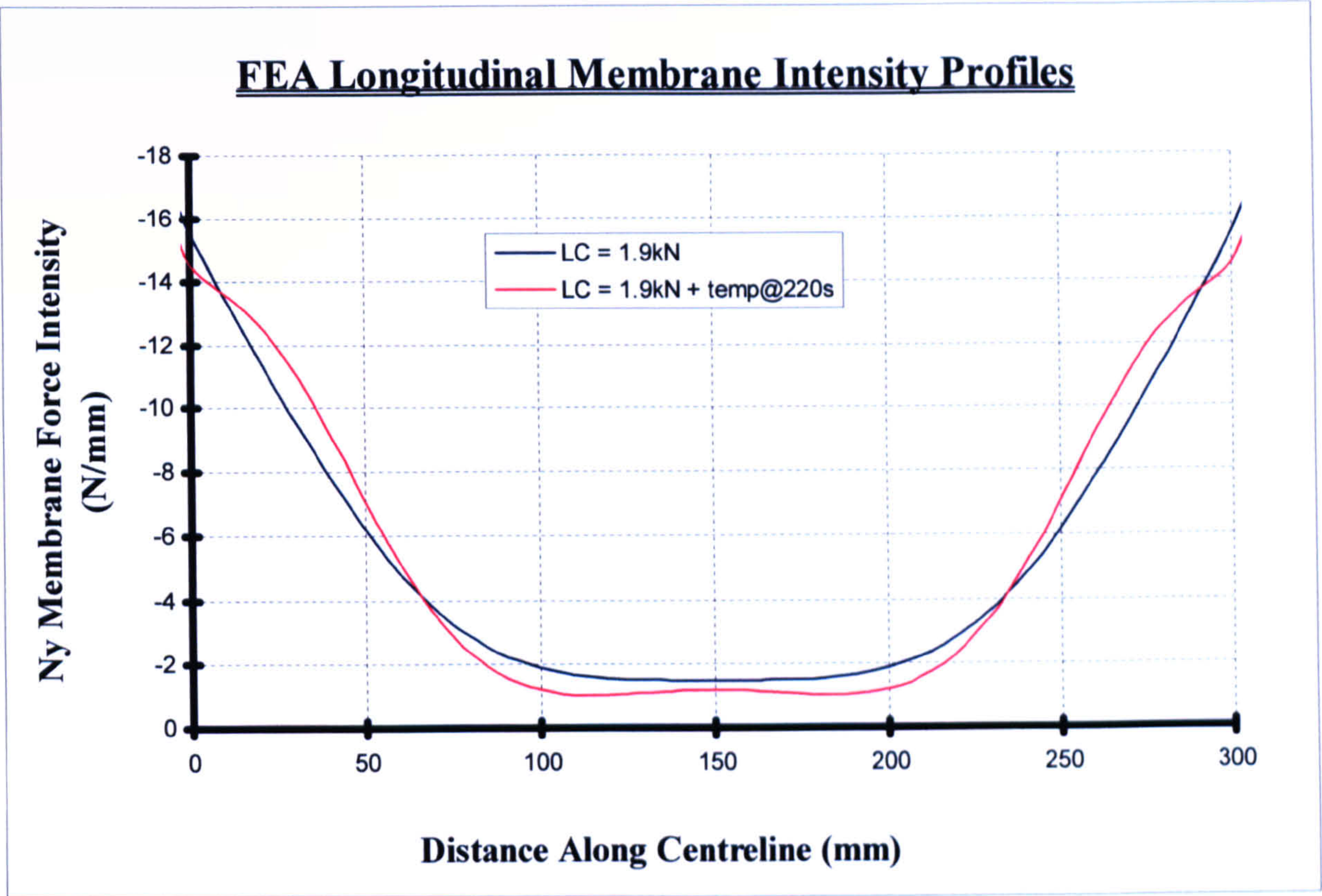


Figure 6.16. Non-adaptive longitudinal membrane force intensities, N_y , within specimen configuration number 5.

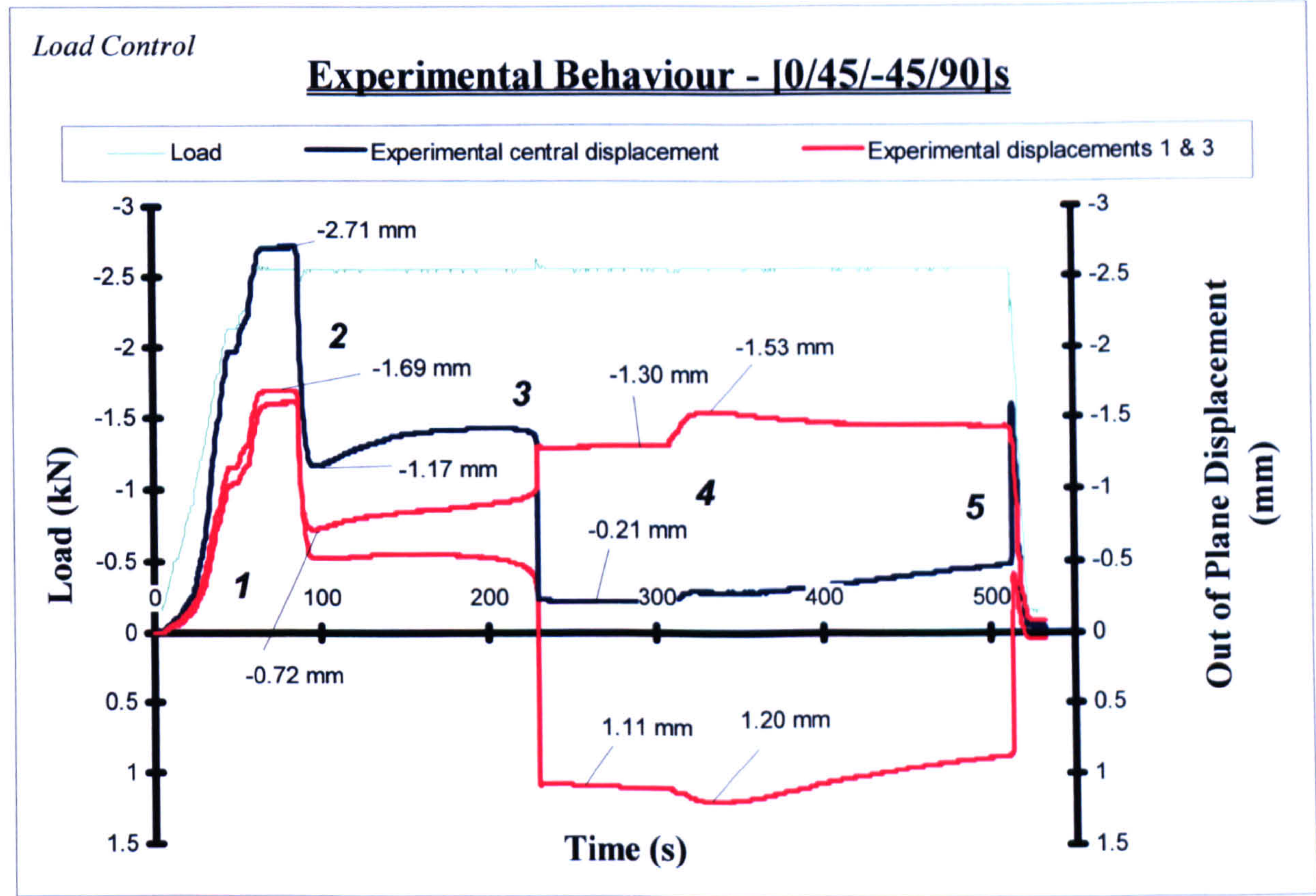


Figure 6.17. Adaptive post-buckling deflection behaviour for specimen configuration number 6, Cycle 1.

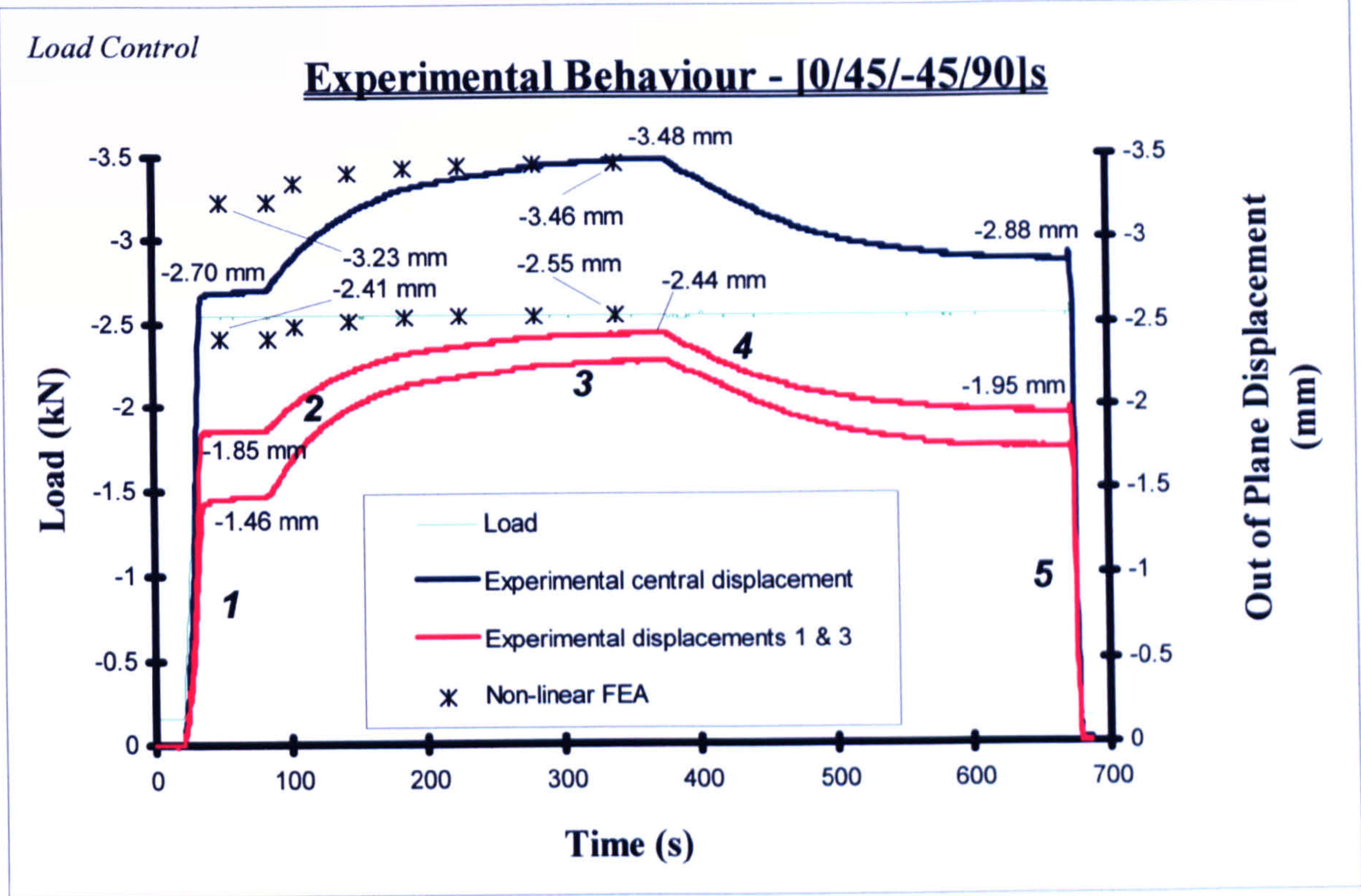


Figure 6.18. SMA temperature effects only on post-buckling deflection behaviour for specimen configuration number 6.

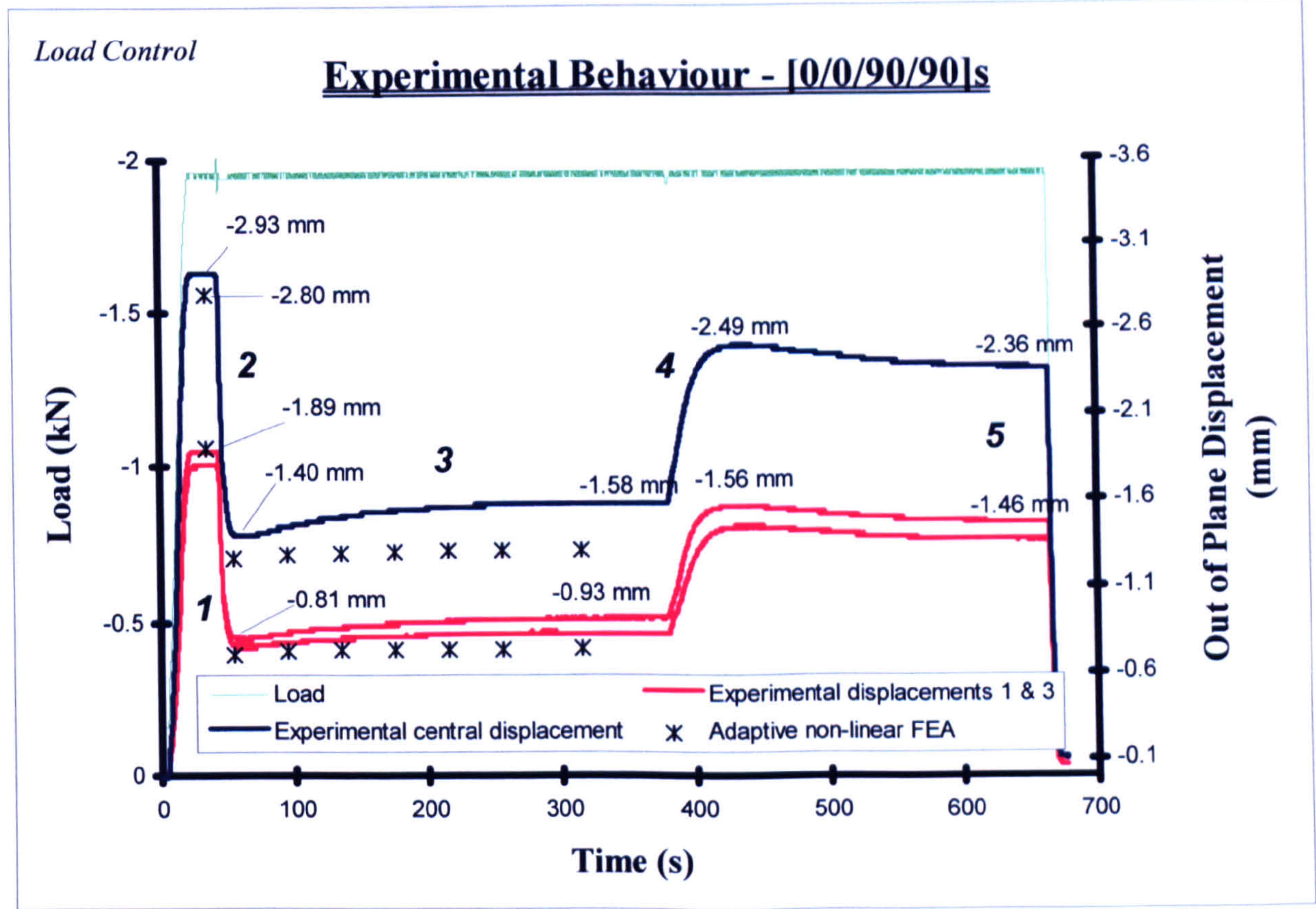


Figure 6.19. Adaptive post-buckling deflection behaviour for specimen configuration number 7, Cycle 1.

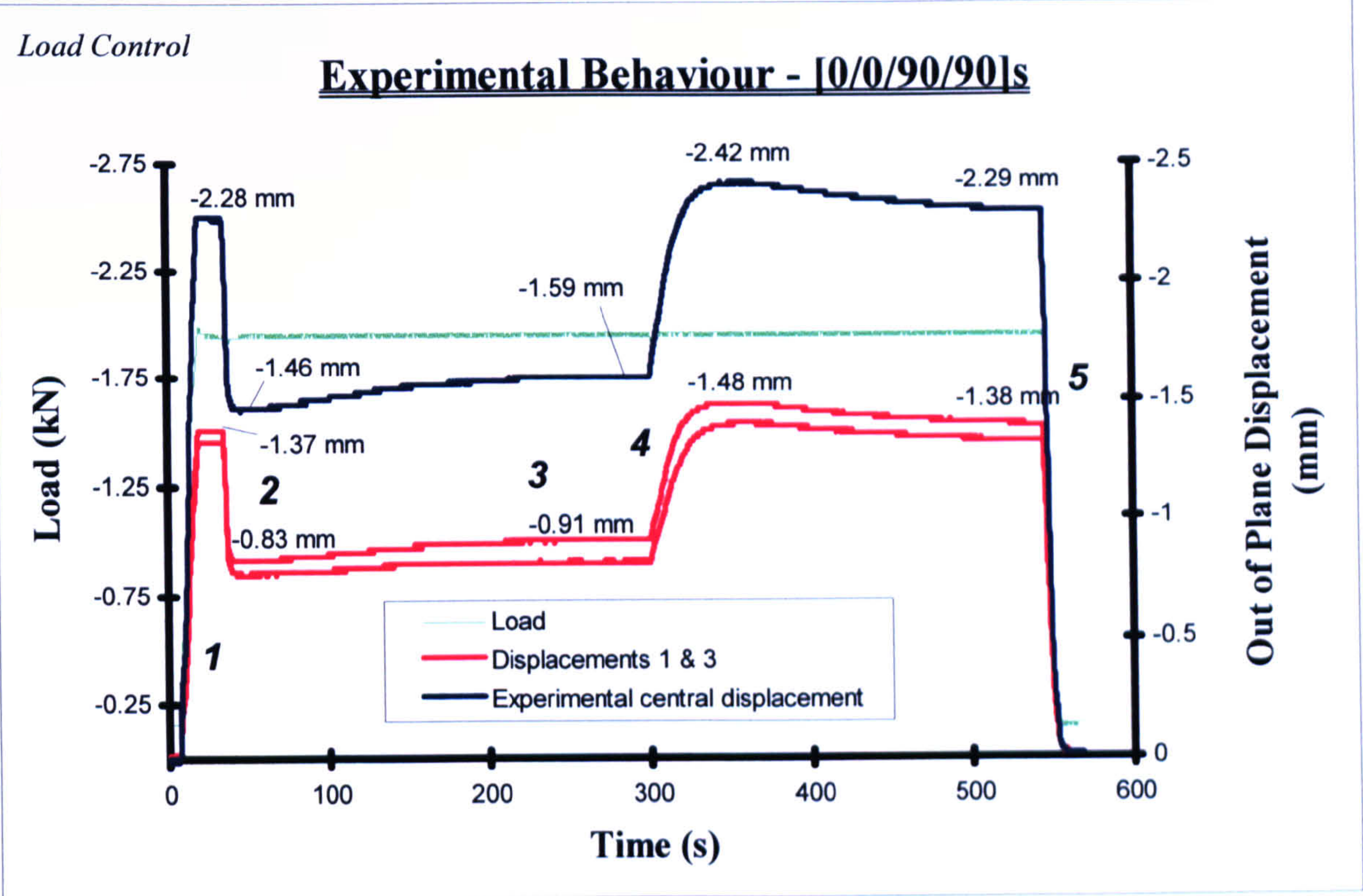


Figure 6.20. Adaptive post-buckling deflection behaviour for specimen configuration number 7, Cycle 5.

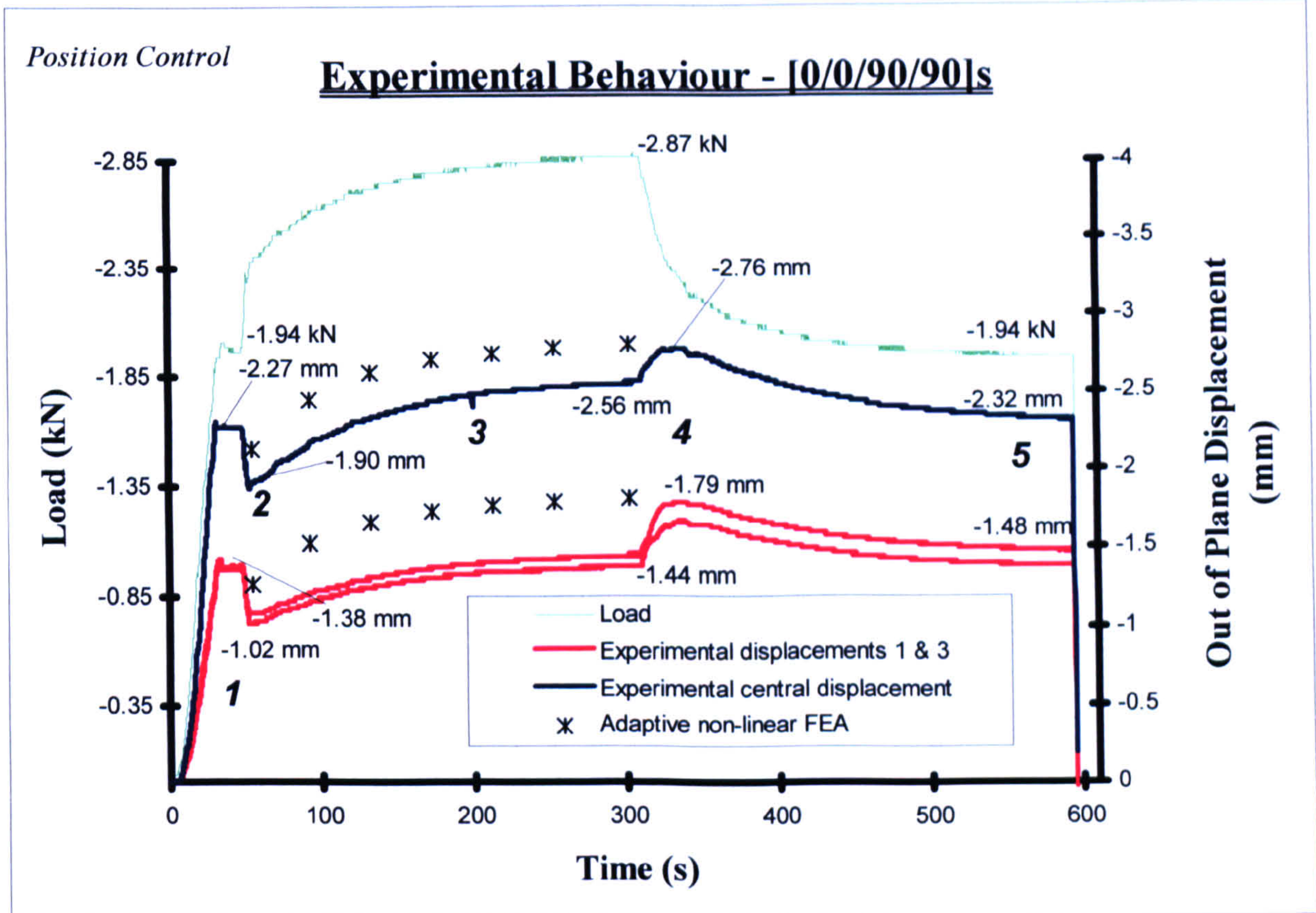


Figure 6.21. Adaptive post-buckling deflection behaviour for specimen configuration number 7, Cycle 10.

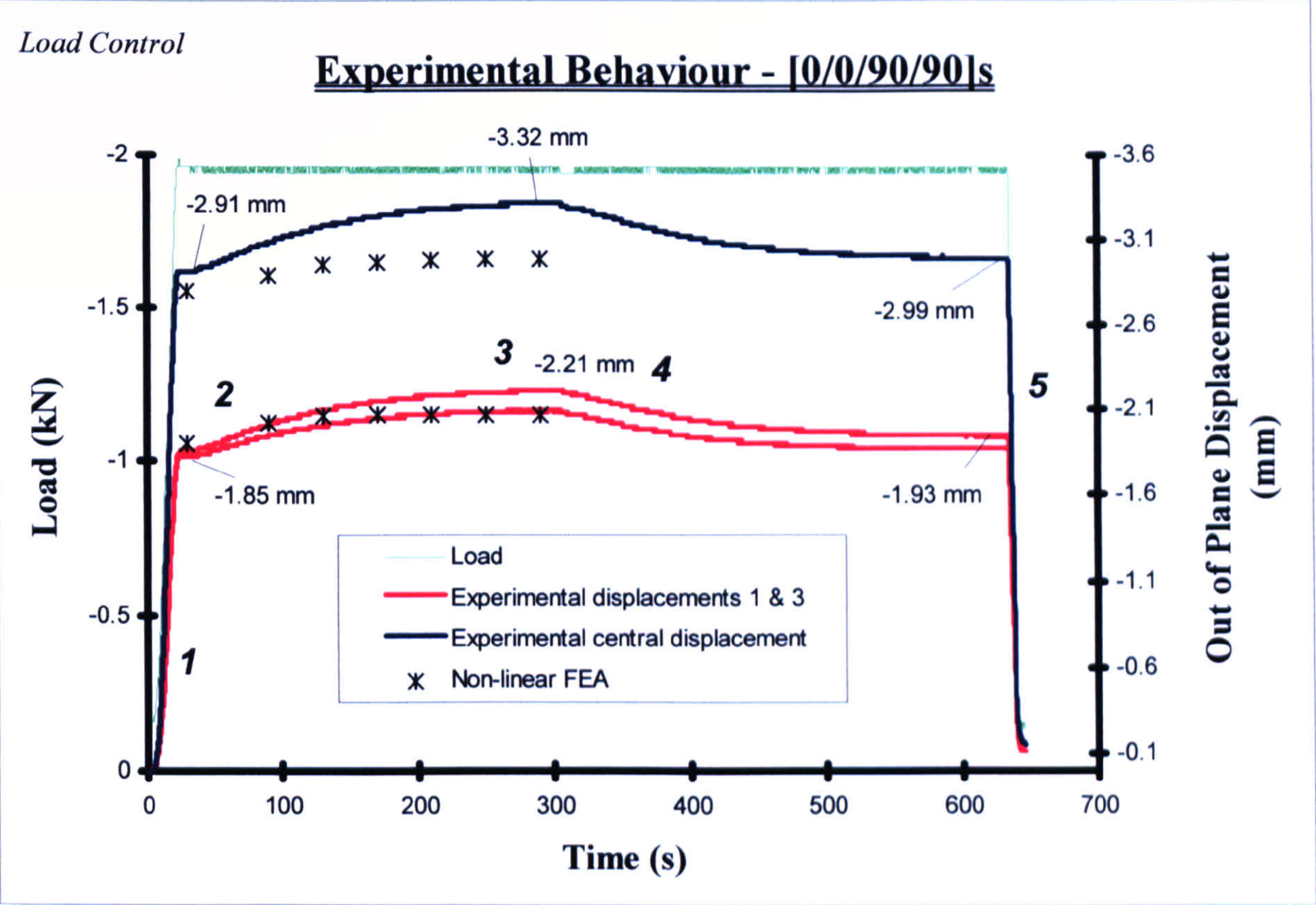


Figure 6.22. SMA temperature effects only on post-buckling deflection behaviour for specimen configuration number 7.

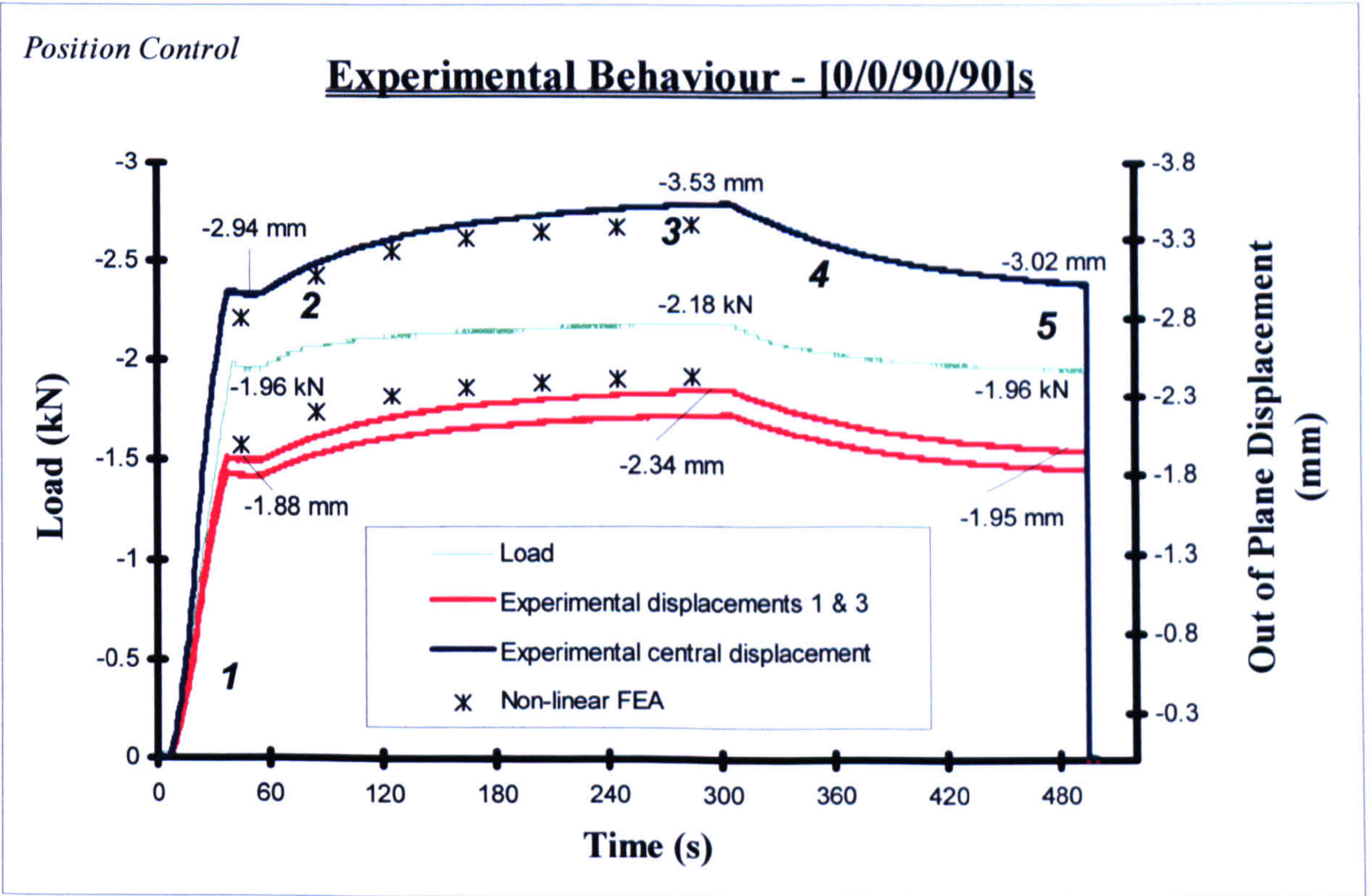


Figure 6.23. SMA temperature effects only on post-buckling deflection behaviour for specimen configuration number 7.

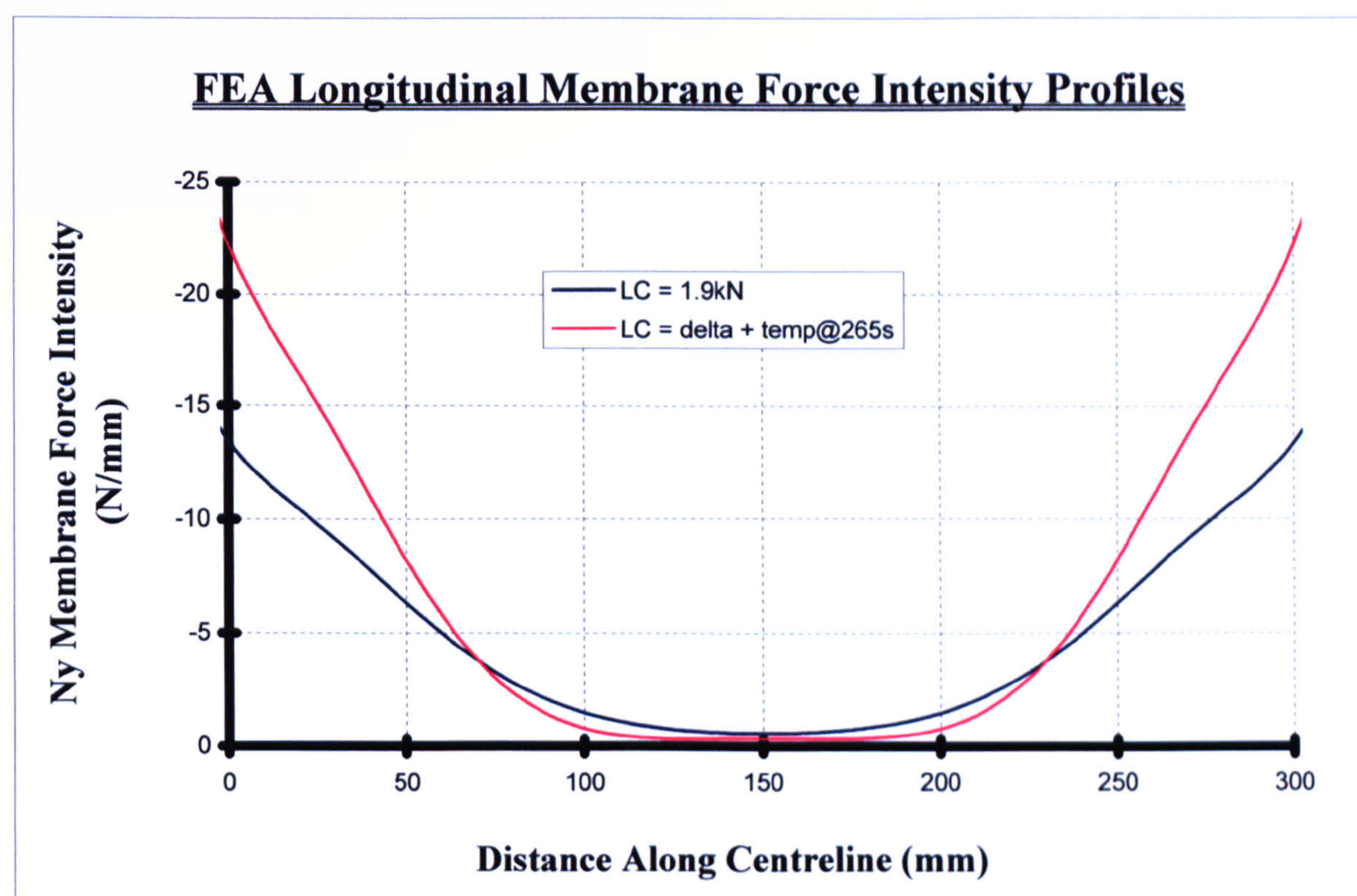


Figure 6.24. Non-adaptive longitudinal membrane force intensities, N_y , within specimen configuration number 7.

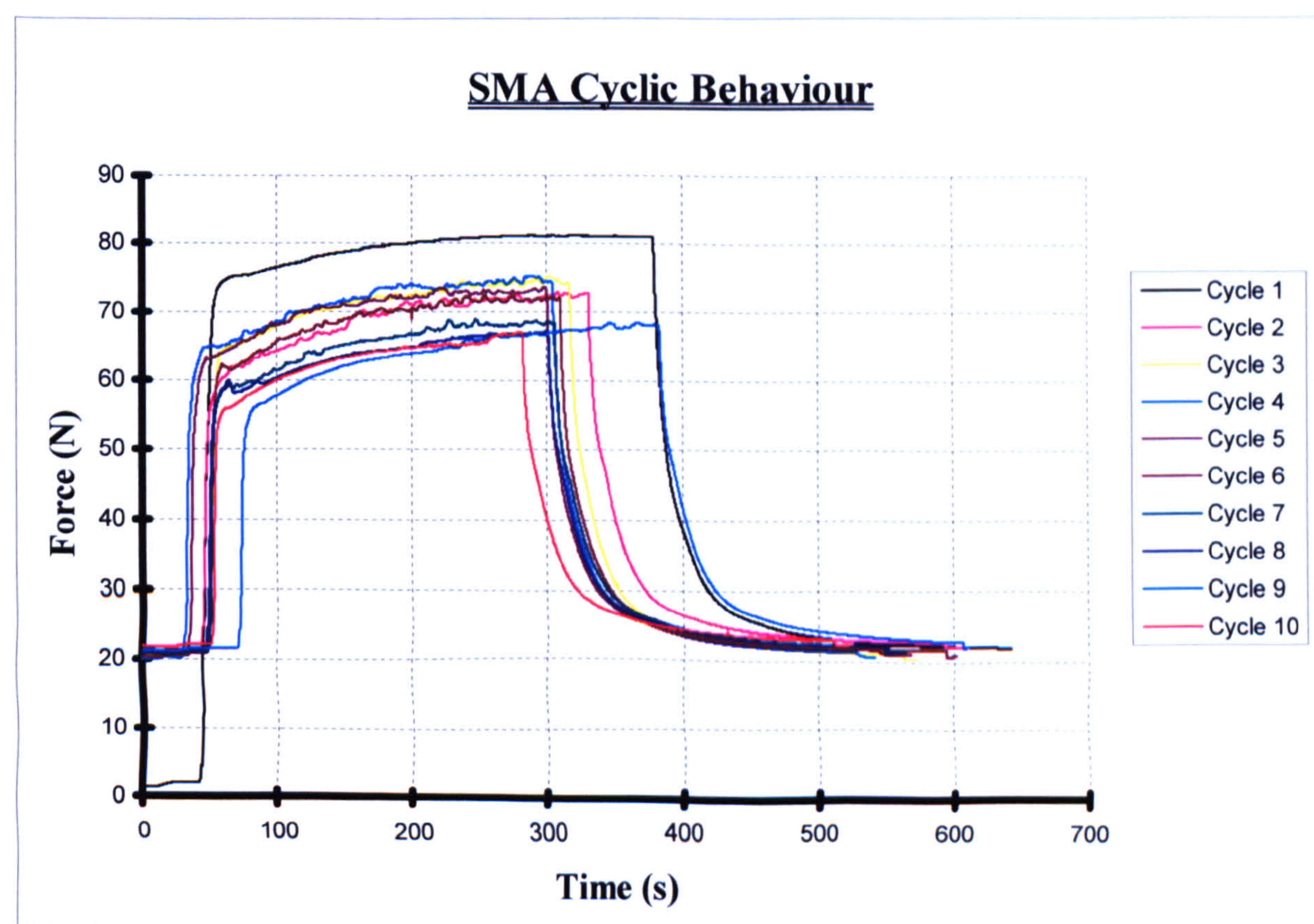


Figure 6.25. Generated recovery force behaviour at SMA position number 15 during testing of specimen configuration number 7.

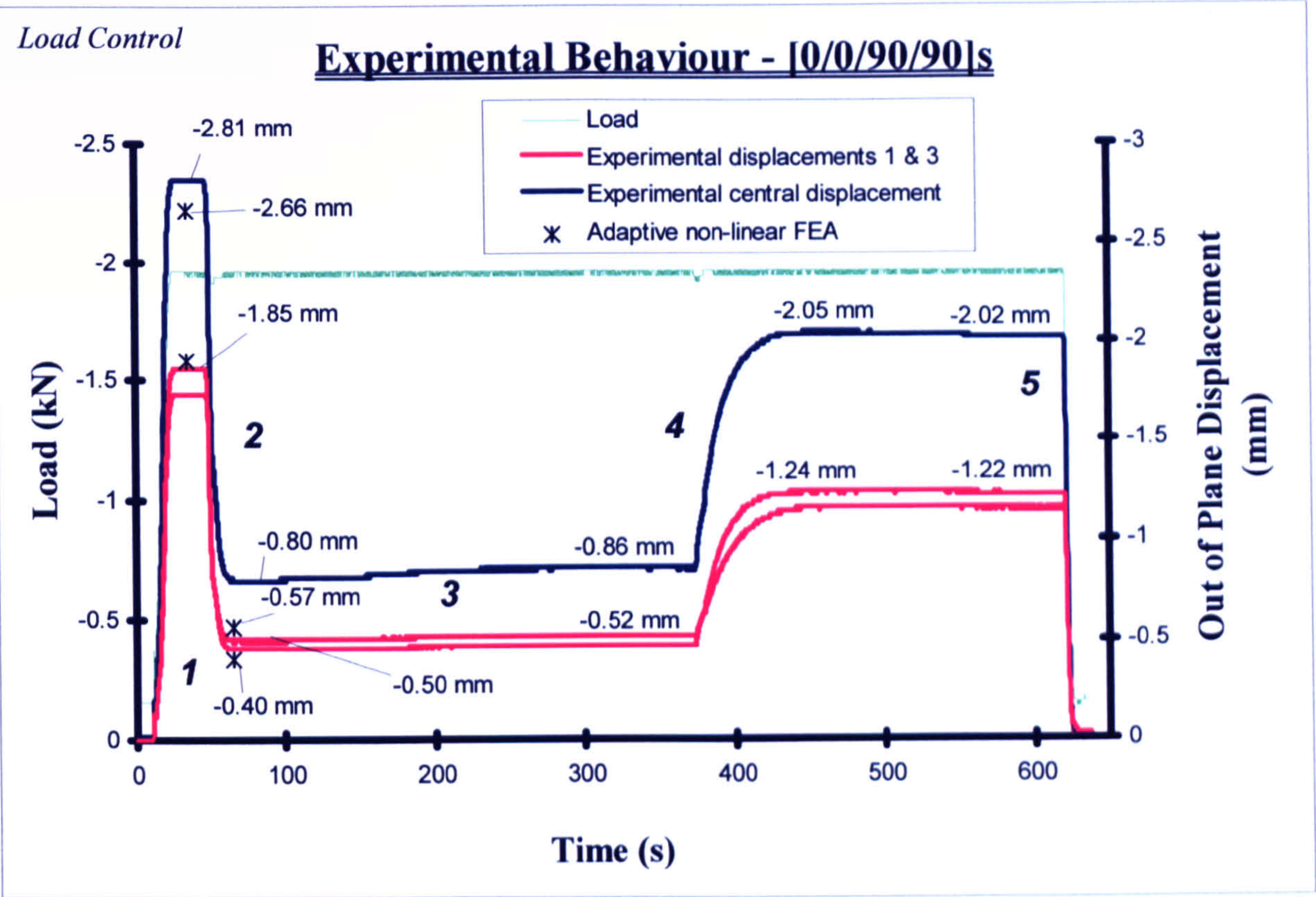


Figure 6.26. Adaptive post-buckling deflection behaviour for specimen configuration number 8, Cycle 1.

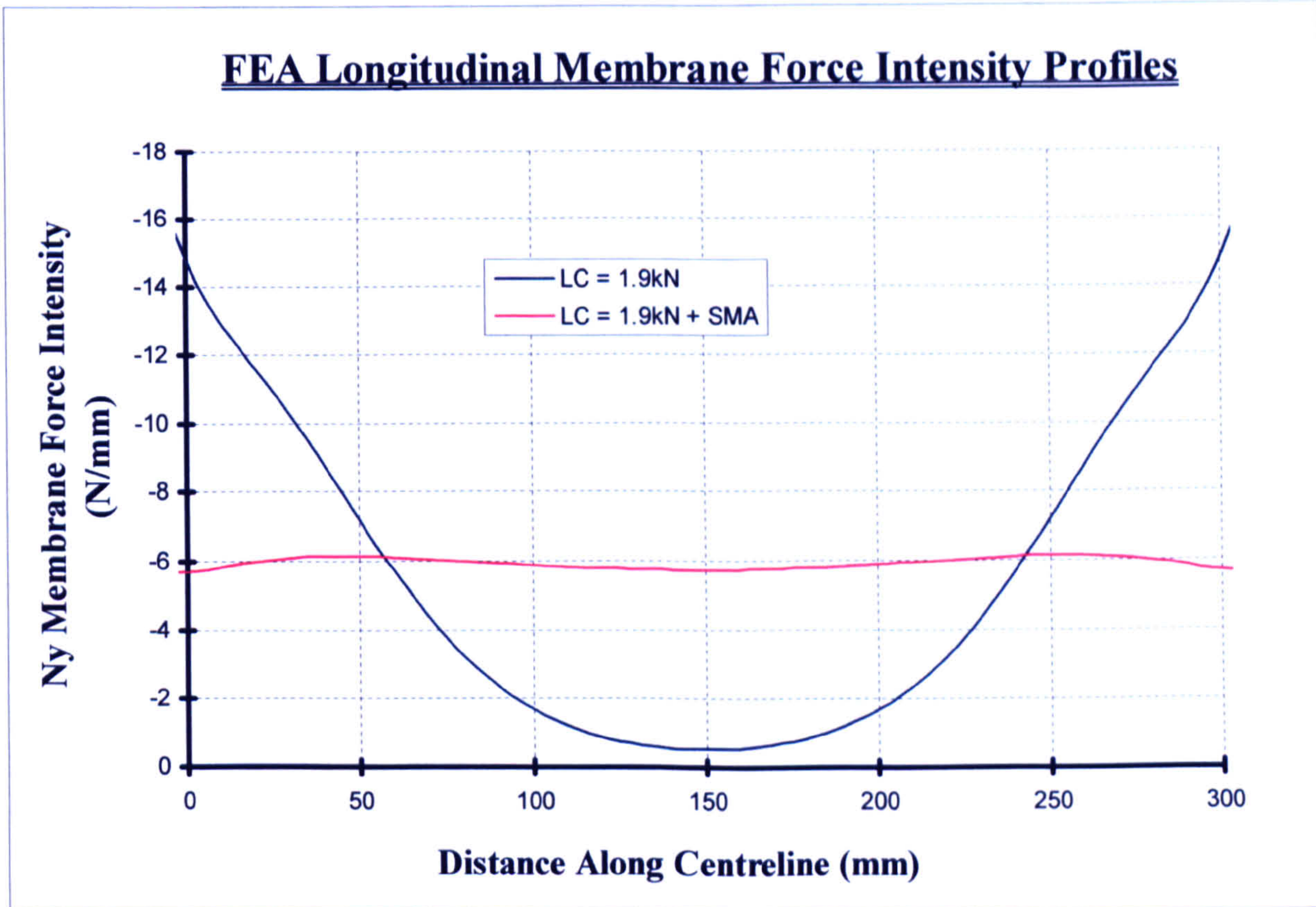


Figure 6.27. Non-adaptive & adaptive longitudinal membrane force intensities, N_y , within specimen configuration number 8.

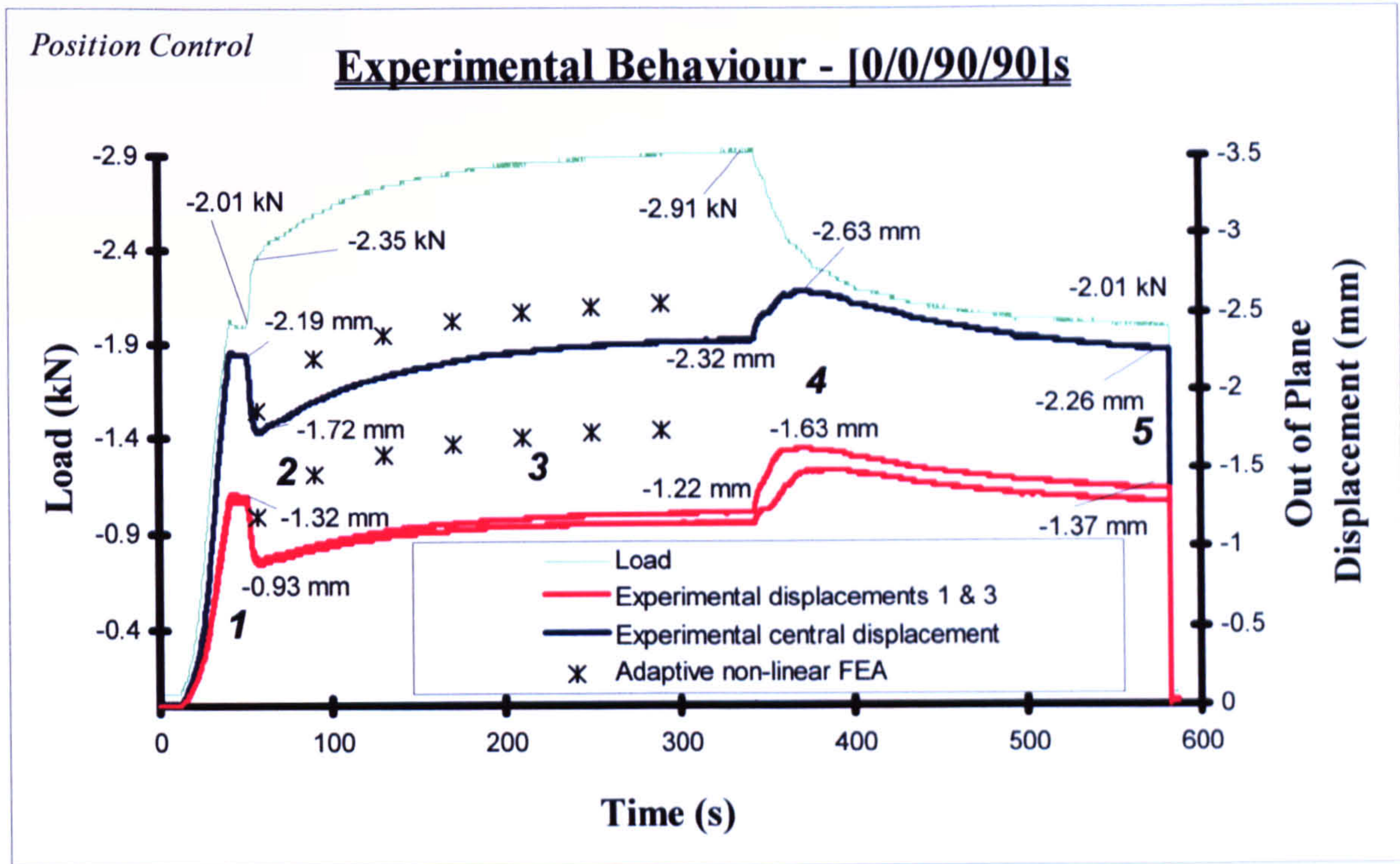


Figure 6.28. Adaptive post-buckling deflection behaviour for specimen configuration number 8, Cycle 10.

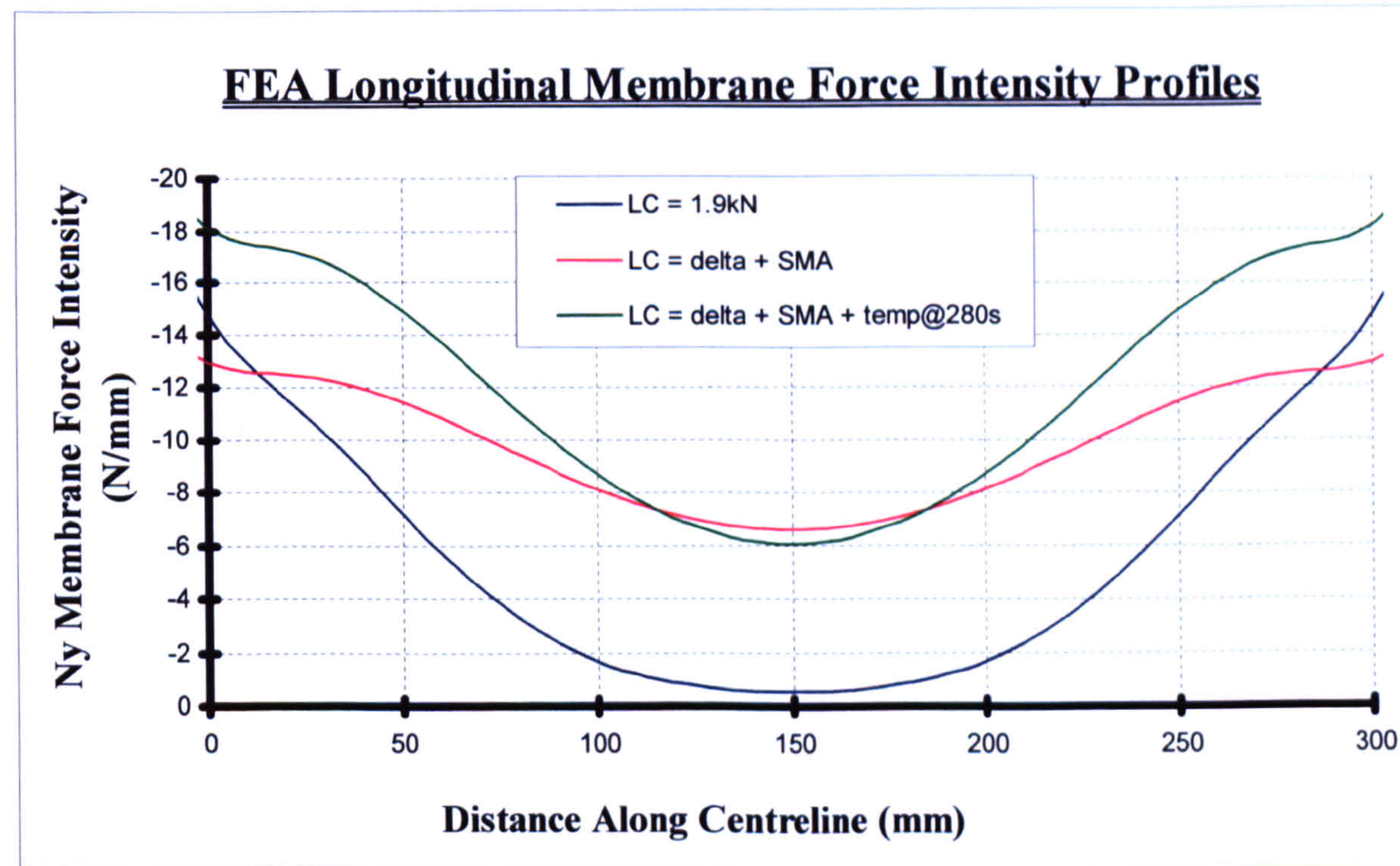


Figure 6.29. Non-adaptive & adaptive longitudinal membrane force intensities, N_y , within specimen configuration number 8.

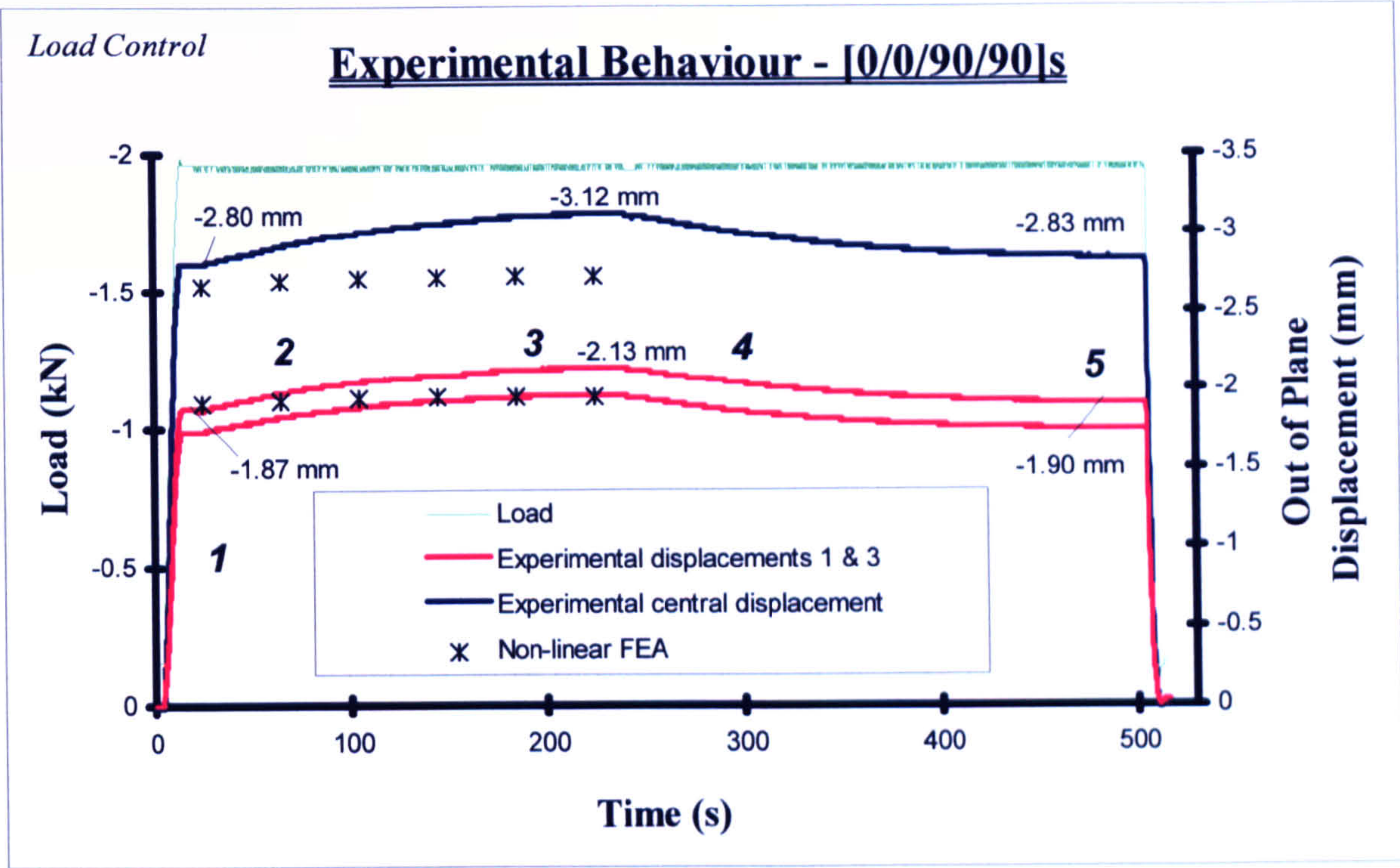


Figure 6.30. SMA temperature effects only on post-buckling deflection behaviour for specimen configuration number 8.

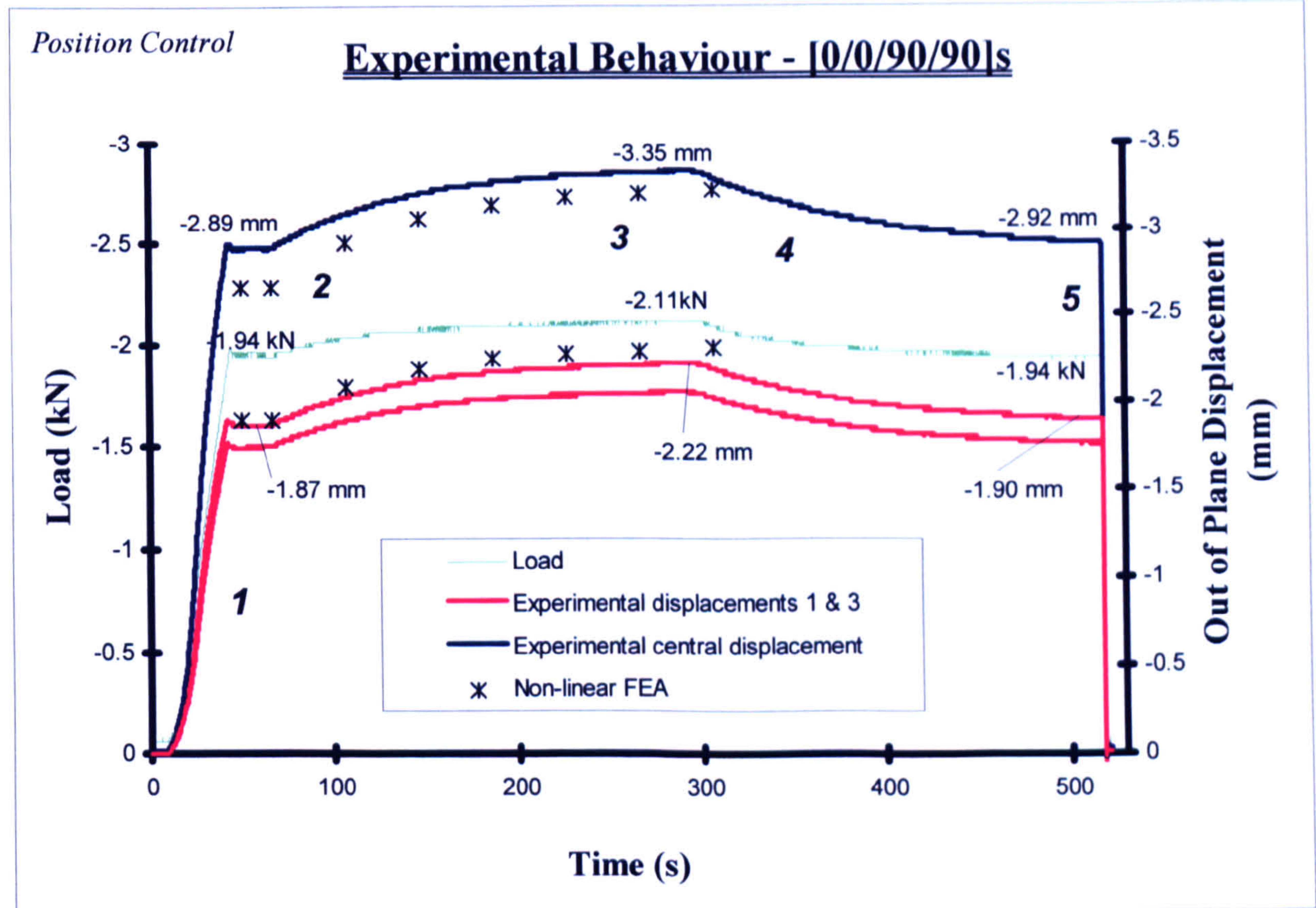


Figure 6.31. SMA temperature effects only on post-buckling deflection behaviour for specimen configuration number 8.

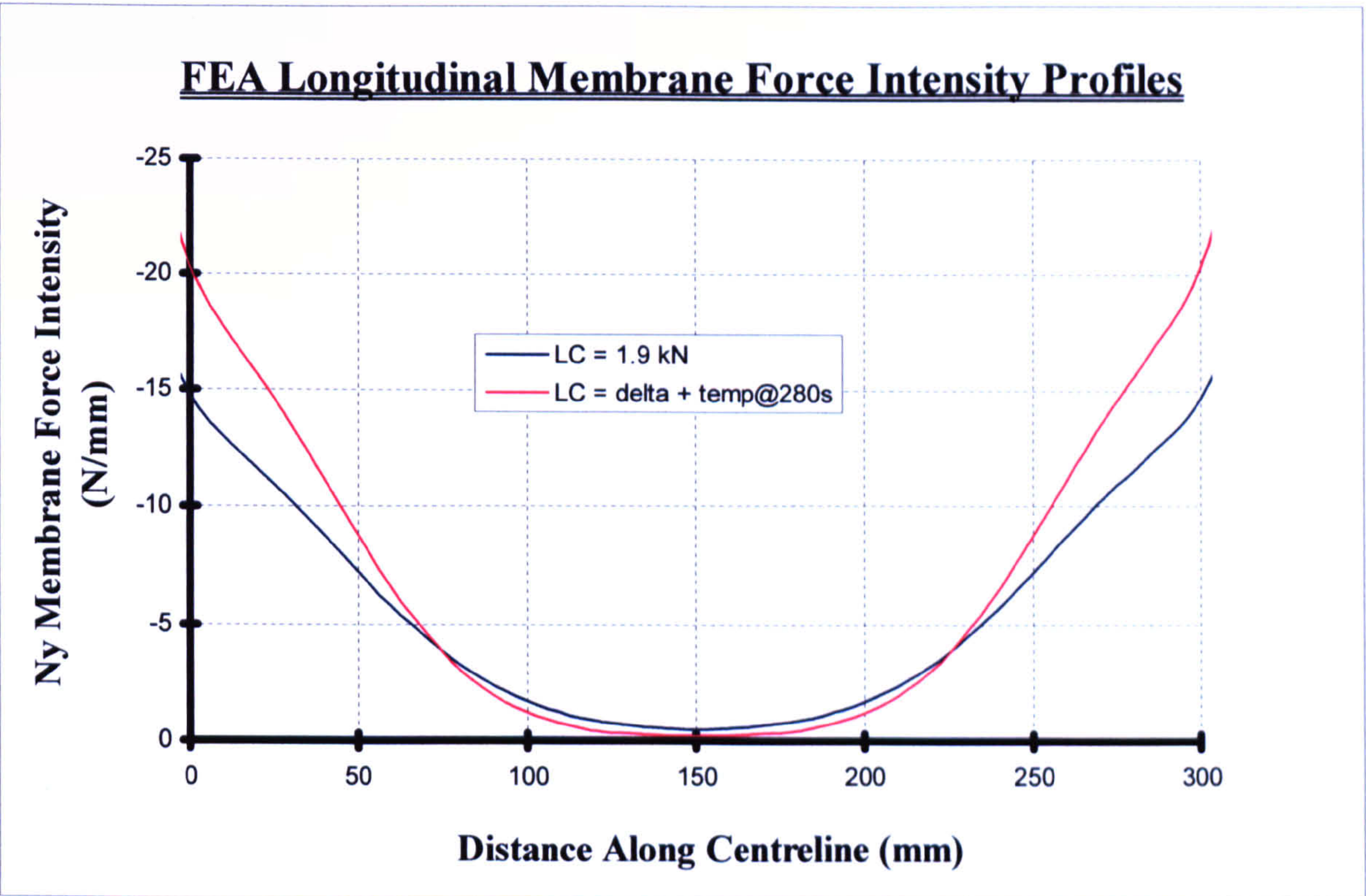


Figure 6.32. Non-adaptive longitudinal membrane force intensities, N_y , within specimen configuration number 8.

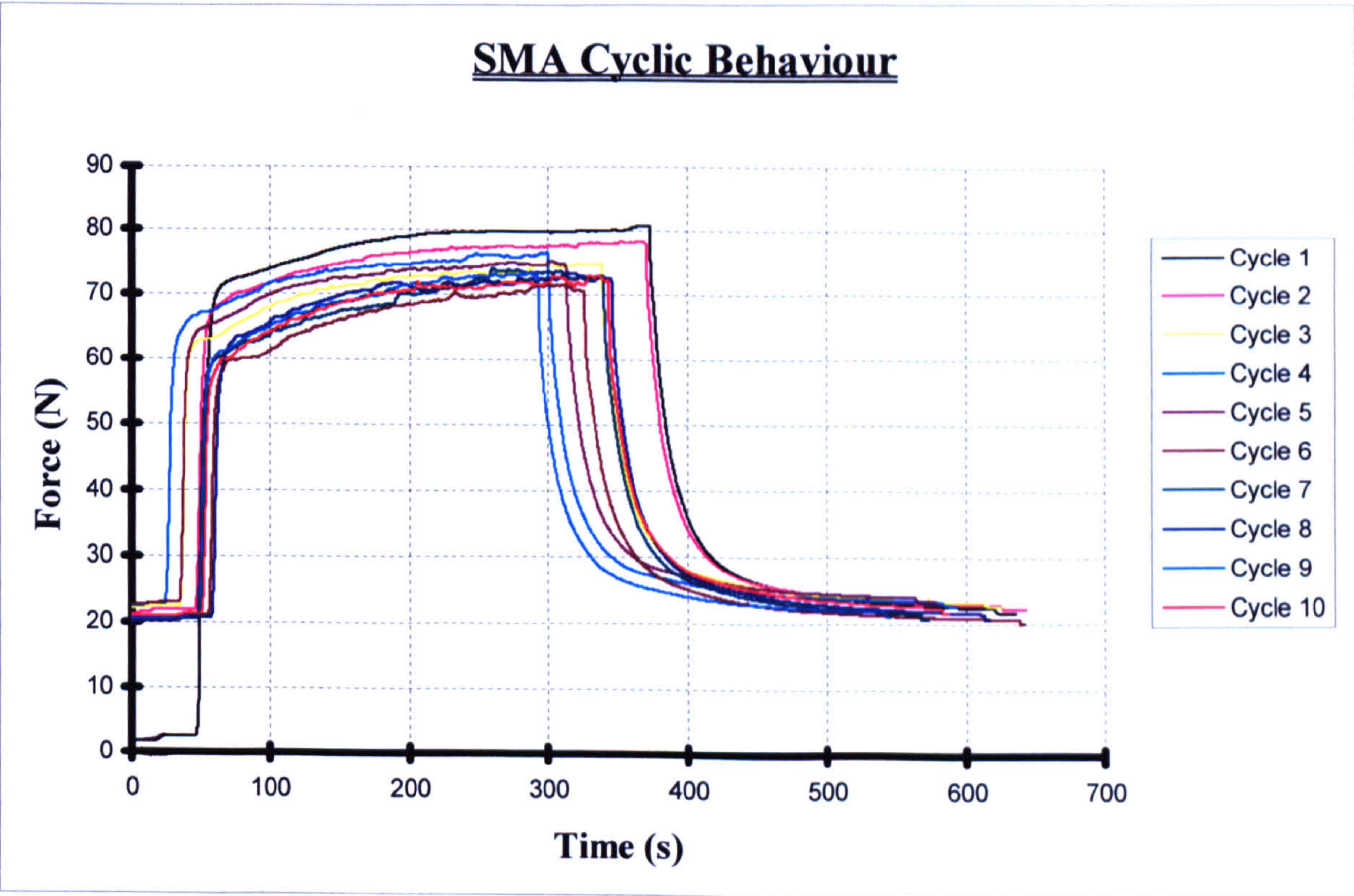


Figure 6.33. Generated recovery force behaviour at SMA position number 7 during testing of specimen configuration number 8.

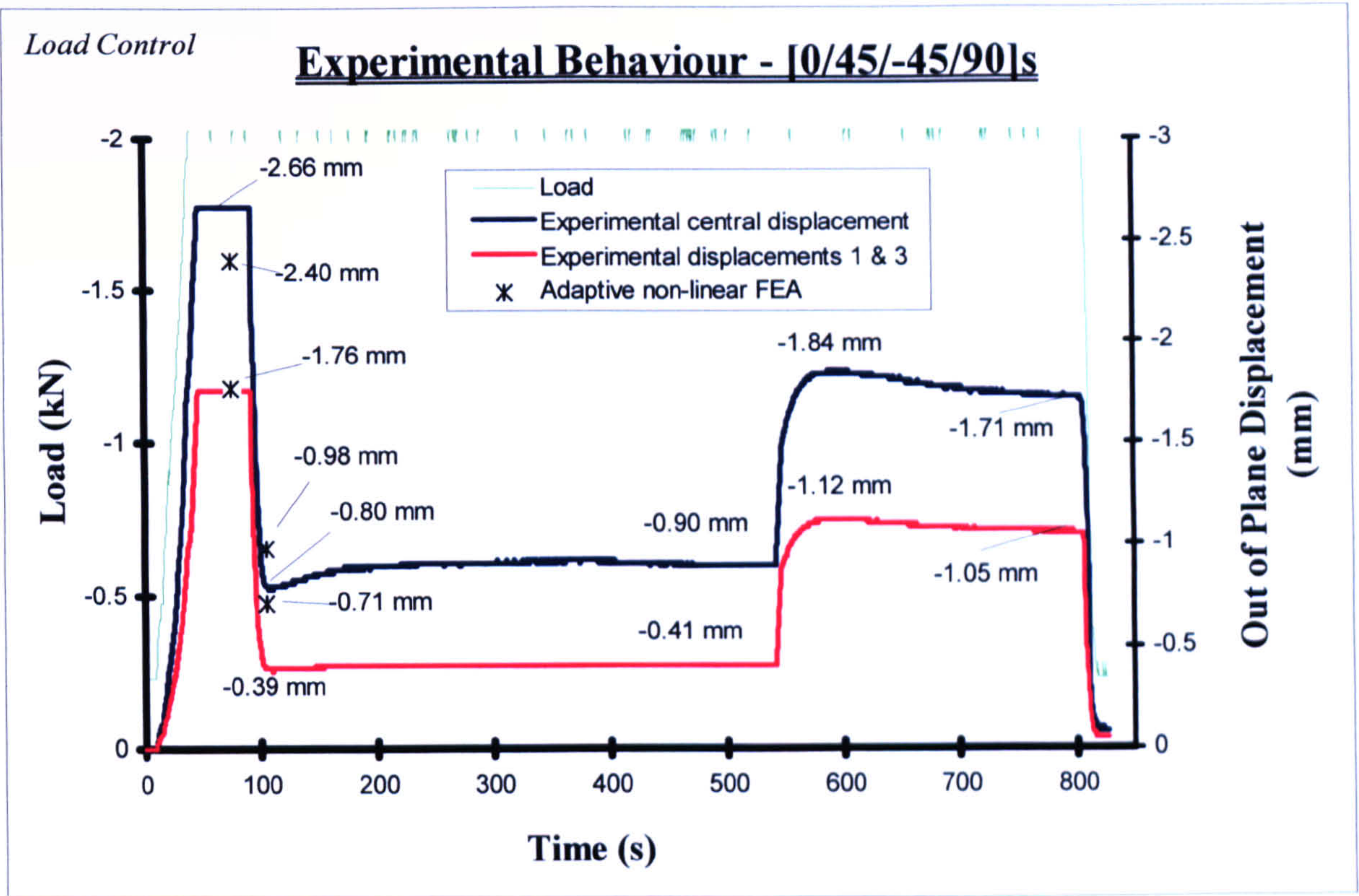


Figure 6.34. Adaptive post-buckling deflection behaviour for specimen configuration number 9, cycle 1.

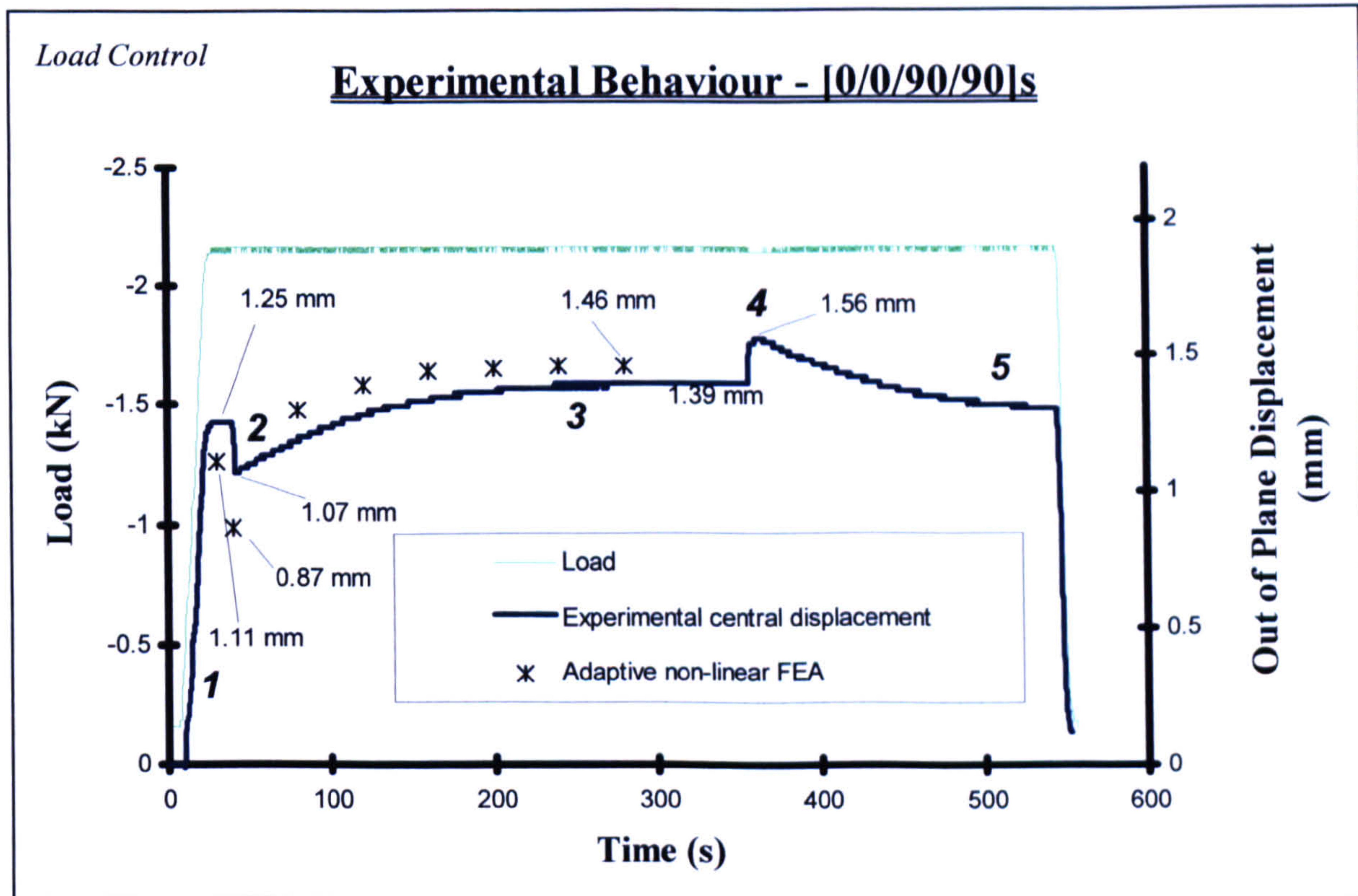


Figure 6.35. Adaptive post-buckling deflection behaviour for specimen configuration number 10, cycle 1.

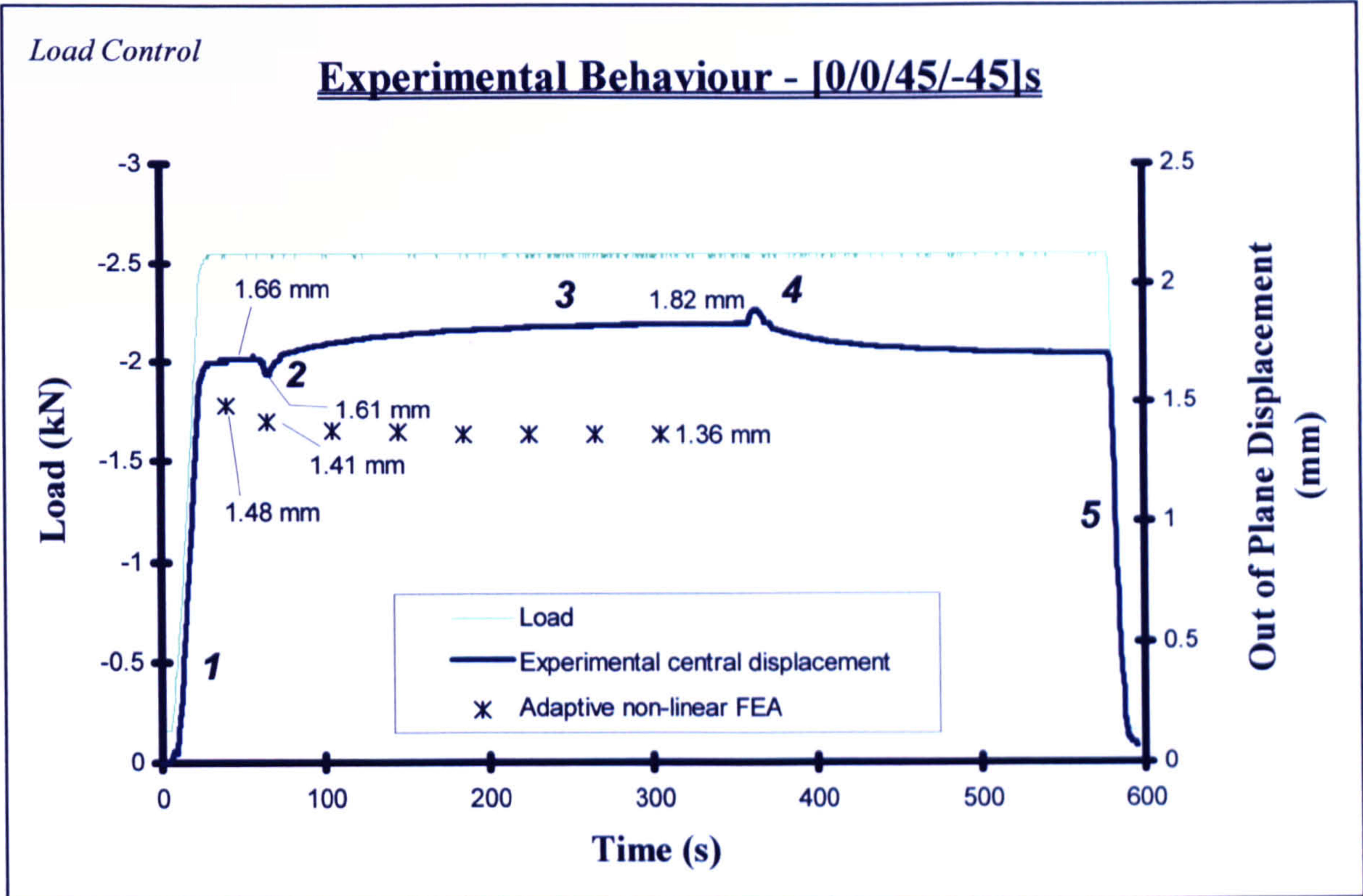


Figure 6.36. Adaptive post-buckling deflection behaviour for specimen configuration number 11, cycle 1.

Chapter 7

Smart Advanced PMC Materials Within The Marketplace

7.1 Introduction

This chapter focuses on some of the commercial aspects pertaining to the introduction of smartness within polymeric-matrix composite aerospace platforms. This chapter does not limit itself to SMA/PMC smart structures, the constituents predominantly discussed throughout this thesis. It also calls upon the analogous PMC sensory integration, first introduced in **Chapter 1**, therefore, enabling a wider perspective of the marketplace, in which smart PMC materials will enter, to be established.

The understanding and full appreciation of the marketplace, in which smart composite aerospace components are anticipated to operate within, will be vital to the smart components success.

The chapter first discusses the desired performance benefits anticipated with the utilization of PMC materials throughout an aerospace platform and how the performance benefits can further be enhanced with the integration of smartness to the host. The vitally important issue of affordability is first introduced.

The next section discusses new product development. The focal point of new product development being the utilization of passive PMC and smart PMC constituents within aerospace products. After introducing a typical high-technology product life-cycle, the means of extending the product life-cycle, through progressive development, is elaborated upon. In general, the extension of the product life-cycle ensures product differential advantage. The section ends with a discussion pertaining to the marketing of products within the marketplace. Here, the important parameter within marketing is the deliverance of the *marketing mix* to the marketplace.

The chapter continues with a discussion pertaining to the current utilization rate of advanced polymeric-matrix composites within the marketplace. A non-exclusive list of future aerospace products that will significantly extend this current PMC utilization rate is presented. Subsonic civil aircraft, supersonic civil aircraft, military aircraft and space based platforms are all discussed. Such products also hold the greatest promise for incorporating smart PMC materials.

A more detailed account of the marketplace for future second generation supersonic civil aircraft is provided for as the author feels that such an aircraft offers great potential for incorporating SMA/PMC materials. Within the employed aircraft's structural configuration, an SMA/PMC host has the potential for minimising the effects of thermal differentials over the exterior of the aircraft, **Chapter 6**.

The following section breaks down the all important issue of aerospace affordability into the governing topics of procurement cost and cost of ownership. The focus of the following subsections will pertain to PMC and smart PMC affordability implications. The Operating costs section is subdivided into smart airframe maintenance and engine maintenance.

Finally, the chapter ends with a bold statement regarding smart PMC innovation.

7.2 Smart PMC Performance Benefits

With the exception of military air vehicles, to date, advanced polymeric-matrix materials have predominantly been incorporated within secondary structural components of aerospace platforms. Their utilization within such components has demonstrated both weight savings and structural confidence. In general, however, secondary structures account for a relatively small amount of the total aerospace platform's weight. A significant weight saving potential exists, therefore, when incorporating PMC materials within primary components such as aircraft wing spars or aircraft fuselage sections.

Within aerospace applications, weight savings manifest performance improvements. Typical performance improvements being lower fuel consumption, increased range and/or increased payload. The former lower levels of fuel consumption ensuring reduced levels of pollution.

In today's economic climate the word performance, however, should encompass factors such as operating and manufacturing costs. While weight savings reduce fuel costs, the use of polymeric-matrix composites over, for example, aluminium alloys, can require significantly higher raw material purchase and manufacturing costs. The latter depending on the fabrication route chosen.

Within aerospace establishments, however, composite designers strive to achieve a 30-40% weight saving and a 10-30% cost reduction when utilizing PMC materials compared to conventional metals. Realization of the latter requires the integration of innovative structural concepts and low cost manufacturing processes. A low cost PMC fabrication technique receiving much attention is the Resin Transfer Moulding, RTM, process. Here, complex parts can be fabricated in one piece with minimal machining and assembly cost.

As with conventional metallic components, affordability, therefore, can be associated with the performance characteristics of passive polymeric-matrix composite materials.

It is anticipated that the incorporation of the smart structure technology within polymeric-matrix fabrications will yield further improvements in performance. Now, the word performance encompasses improvements in the global platform's safety, supportability, availability, operational capability and even survivability.

The most significant performance improvement will manifest from greater confidence in the use of advanced PMC materials and, therefore, the weight saving achieved through avoidance of overdesign¹¹. This will be of particular importance to the aerospace community because of the substantial multiplier affect weight savings have on aircraft and spacecraft structures¹¹.

The performance benefits of smart PMC materials, however, must carefully be assessed against issues of technical risk, producibility, maintainability, reliability, and, of course, cost. Improved performance must be at an affordable price. In the fiercely competitive civil and military aerospace industries, the main discriminator between the aerospace product is cost. Within such industries, both initial procurement and cost of ownership play an important role in the cost equation, more of which will be discussed later in this chapter.

7.3 New Product Development

7.3.1 The Product Life-Cycle

For all products, be they aerospace or non-aerospace related, there is a finite cycle during which they will have a place in the market before becoming obsolete. **Figure 7.1** illustrates this cycle.

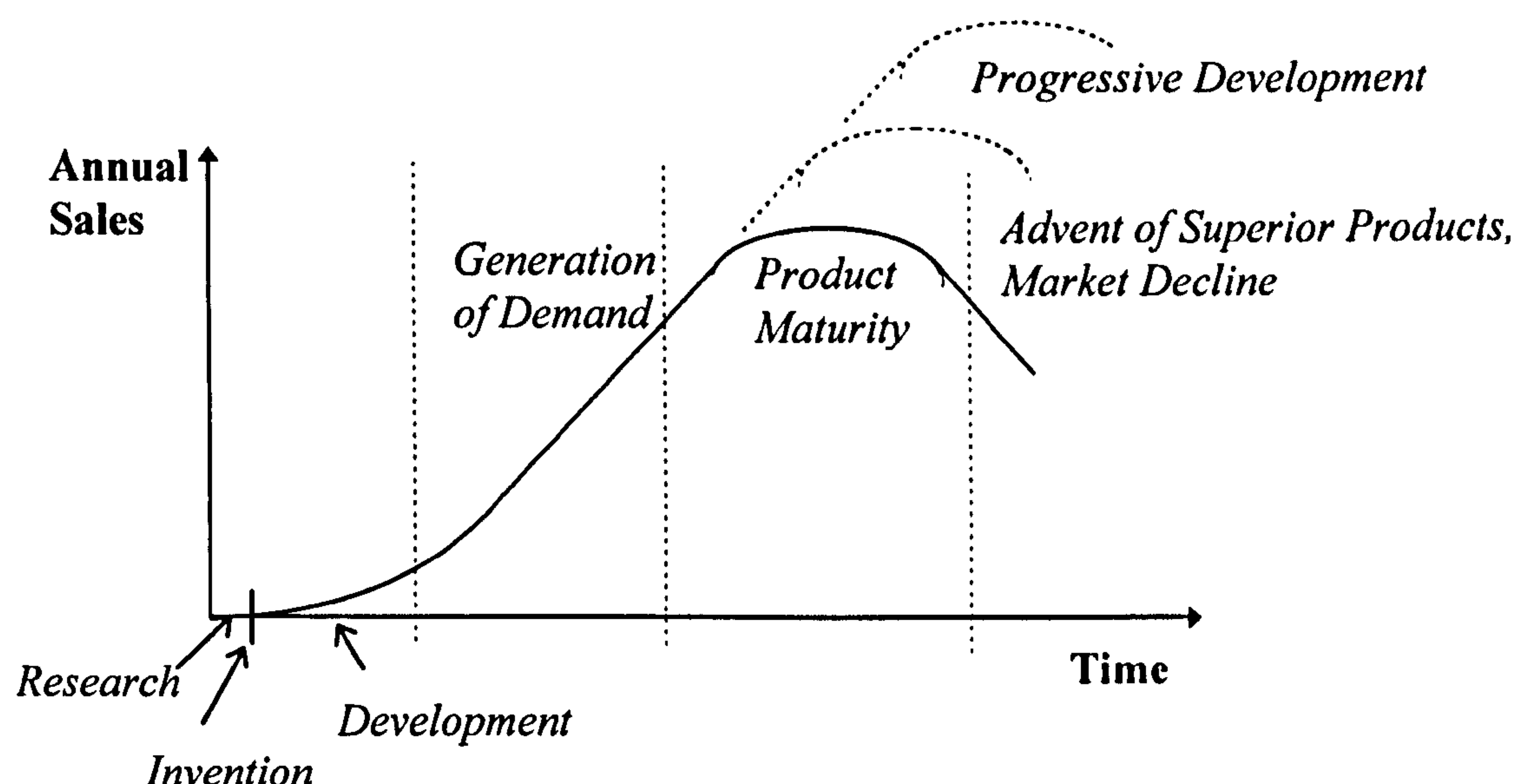


Figure 7.1. *Product life-cycle.*

Pertaining to high-technology products, if a producing company wishes to maintain its share of the market, besides advertising and promotional activities, it is necessary for them to adapt and promote innovation continuously¹³⁵.

Within a highly competitive market, very sophisticated customers may force the pace of change by demanding design solutions that involve significant changes in technology. This is particularly true in military aviation. It also applies to commercial aircraft. Here, the predominant competing item between aircraft types is what airlines value most; direct operating costs, DOC, per seat mile.

New product development, i.e. the development of new products that have a clear differential advantage, is, therefore, recognised by *all* industrial sectors as essential to business success.

In ensuring product differential advantage, unless they are overtaken by some vastly superior technology, throughout the period of market/product maturity, **Figure 7.1**, the life of a product can be increased by a series of progressive developments. The developments need not be breakthroughs in technology but merely carry an enhanced capability or increased customer appeal¹³⁵. Airliner manufacturers have been among the greatest exponents of this approach keeping basic designs in production for 25 years or more.

7.3.2 Smart PMC's Enabling Progressive Product Development

Technology development within military air-vehicles is motivated by the perception of a threat from an enemy. Civil aerospace technology development is motivated by the perception of a threat from a competitor in search of a greater market share. Throughout industry led aerospace establishments, customer focused aviation research is driven by the need to stay ahead of the competition.

Future goals for civil aircraft include less structural weight, lower life-cycle costs, shorter development & production times, higher engine thrust-to-weight ratios, less noise, increased safety and increased productivity⁶.

For military aircraft, as well as those for civil aircraft, future goals include improved affordability, improved ordnance capability, increased lethality/agility, extended life and reduced detectability⁶.

The realization of planned and future aerospace systems requires the appropriate integration of technological advances in a number of disciplines. Such disciplines including materials, structures, dynamics, aerodynamics, propulsion, avionics, etc etc.

The use of PMC materials, within an airframe design, is a very promising candidate for reducing acquisition and life cycle costs. It is cited that the use of composite fuselage and wing structures have the potential for reducing cost and weight by 25% and 30-50% respectively⁶.

Today, through product progressive development, **Figure 7.1**, the optimal use of polymeric-matrix composites, such that they fulfil their potential for cost and weight saving, is being increasingly pursued by the aerospace manufacturers as one means of obtaining product differential advantage. Such product differential advantage is achievable through the performance benefits associated with PMC integration, **Section 7.2**.

In the near future, smart PMC materials and structures will be considered as the next step enhancement to achieve that same necessary differential advantage.

In the preceding section, it was stated that the manufacturer of high technology products must promote innovation continuously. Regarding smart PMC materials, innovation through progressive development, resulting with enhanced capability products, has attractive propositions for the manufacturer. Here, the overall technology and market risk is relatively low as acceptability of the product has already been established.

7.3.3 Marketing New Product Development

Customer's needs and desires define products. As these change so must the product. From a manufacturers point of view, for any new high-tech product to be successful, i.e. give a suitable return to the organisation, the developed product must not only deliver a significant differential advantage but also satisfy the following three criteria;

- The product must satisfy a market need.
- The product must be technically feasible.
- The product must be capable of being made for a cost that will allow a satisfactory profit when offered at an acceptable selling price.

A product that fails in any of these areas fails overall. To make a product successful in the marketplace requires the help of an organisation's marketing department¹³⁶. This department's role is to seek an understanding of (i) the requirements of existing and potential customers, (ii) the market of which the organisation operates within, (iii) the opportunities which exist within the market and (iv) the best ways to compete with its rivals.

In marketing terms, a very important relationship exists between the supplying organisation and its customers. This relationship is managed through the activities involved by an organisation in delivering the *marketing mix*^{136,137}. A classic description of the marketing mix involves the 4 P's; **Product, Price, Place & Promotion**, **Figure 7.2**.

<p><u>Product</u></p> <ul style="list-style-type: none"> • What type of product or service should be provided? • Durability, ease of maintenance and functional performance are typical ingredients of the Product make-up. 	<p><u>Price</u></p> <ul style="list-style-type: none"> • What price should be set for each product? • Consideration of leasing arrangements and financial deals comes under the Price heading.
<p><u>Place</u></p> <ul style="list-style-type: none"> • What would be the best channel of distribution? • What is the best way to provide <i>after-purchase</i> support? 	<p><u>Promotion</u></p> <ul style="list-style-type: none"> • What is the best way to communicate with targeted customers? • What is the best way to persuade them to buy?

Figure 7.2. *The classic constituents of the marketing mix.*

The four P's constitute the offer which an organisation presents to the marketplace. Customers are constantly performing a matching exercise between their wants and needs and the products and services they see in the market place. When the match is sufficiently good they will purchase¹³⁶.

When a customer makes a purchase, what they are responding to is not just the product but the whole range of variables which constitute the marketing mix, **Figure 7.2**. If the offer is sufficiently well matched to the customers needs, it should lead to sales. If the offer is sufficiently well managed, the sales should be profitable to the organisation.

The success of the smart products of the future, therefore, will depend heavily on their resulting effect on the 4P's that comprise the products marketing mix.

7.4 Advanced PMC Utilization Within Future Aerospace Platforms

Having introduced PMC and smart PMC performance objectives, the requirement for these objectives within new product development and some basic marketing attributes, the author has sought an understanding of the marketplace in which advanced PMC materials play a significant role. Such a market will undoubtedly accommodate the smart technology.

7.4.1 Current Advanced PMC Material Utilization Rate

A variety of industries utilize polymeric-matrix composite materials. Such industries being transportation, aerospace, building & construction, marine, electrical/electronic and sports & recreation. The aerospace segment is a specialised portion of the PMC market. It differs from the others due to the requirements for superior performance under extreme mechanical and/or environmental conditions.

Advanced aerospace PMC's are the high value, low volume segment of composite materials. Over recent years, however, its consumption has risen substantially. The world-wide advanced PMC carbon fibre shipment, between 1992-1996, is presented in **Figure 7.3**¹³⁸. The data is based on industry released statistics within north America, western Europe and the far east. When compared to its utilization in 1992, it can be seen that in 1996, the advanced PMC carbon fibre shipment/utilization rose by 58.9%, quite a significant rise.

In the development of economically feasible next generation aerospace platforms, it is necessary to apply high performance composites with low cost manufacturing techniques to primary structures.

The resulting structural weight saving has the potential to affect the overall aircraft design. As an example, for a wide body commercial airliner, the incorporation of advanced PMC materials within the

fan and compressor stages of the high by-pass ratio aero-engines will result with significant weight savings. This can translate into less structural reinforcement and, hence, less weight in the engine wing mounts, wing box structure, the wing root joints and eventually the undercarriage.

Owing to the specific needs of future aerospace products, the rise in PMC constituent consumption, **Figure 7.3**, will, therefore, certainly continue.

Initially, the utilization of smart PMC materials will probably be too expensive for the mass PMC market. They will first be incorporated into components that require the highest level of performance, i.e. military aircraft or science based space missions.

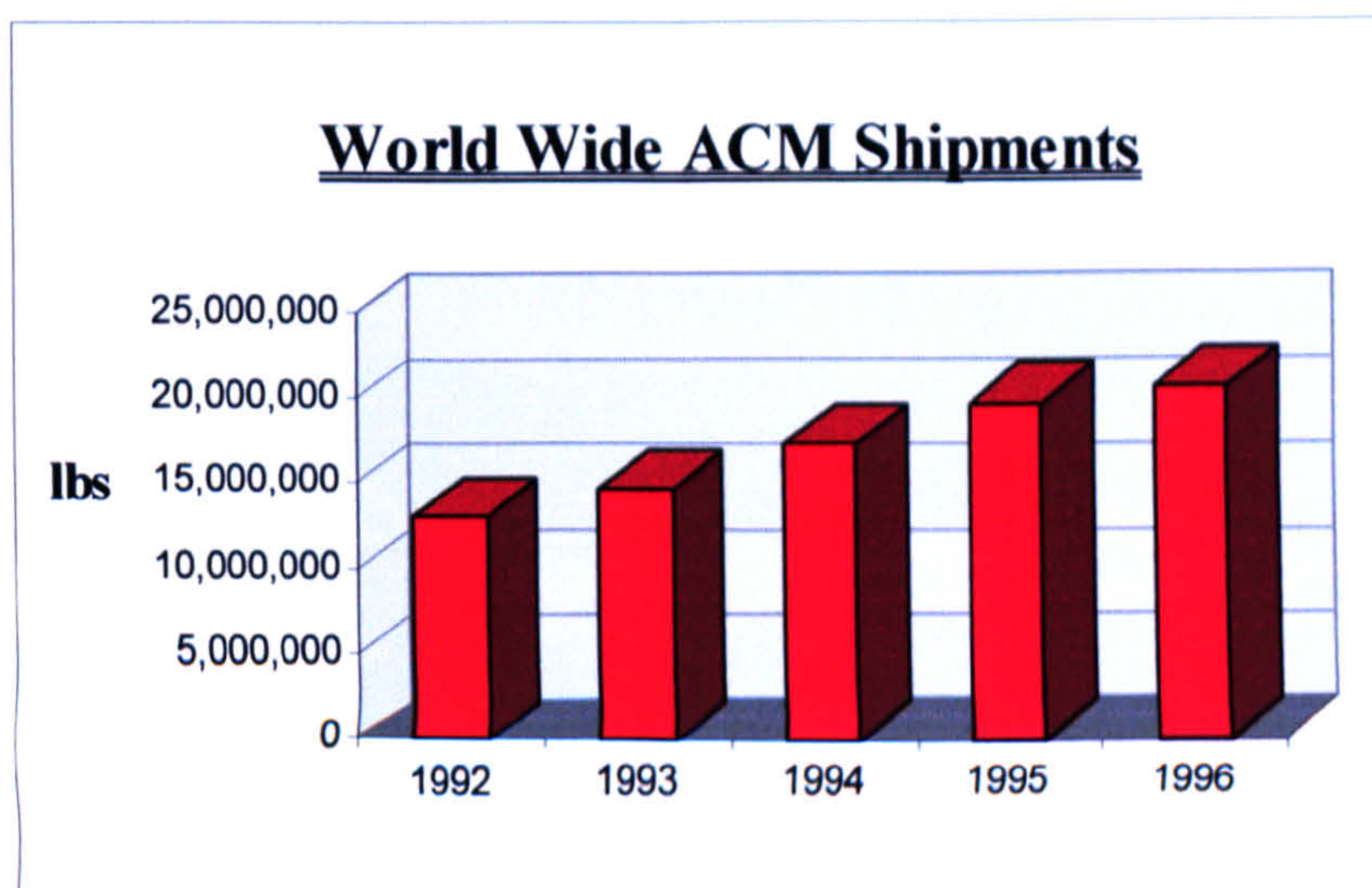


Figure 7.3. Carbon-fibre shipment between 1992-1996.

7.4.2 Market Outlook For Next Generation Aerospace Platforms

7.4.2.1 Subsonic Civil Aircraft

All recently issued market forecasts agree that the civil airline industry, as a whole, will continue to grow. The forecasts predict that the volume of traffic in 20 years time will be several times that of the present level.

To meet this increased passenger demand, the world's jet aircraft fleet is expected to more than double. The requirement for new aircraft to meet the additional passenger carrying capability, as well as the new aircraft to replace those that will be retired, results in a market forecast for over 16,000 new passenger aircraft deliveries. Such aircraft having 100+ seats.

As a result, over the next 10 years, mainstream jet aircraft demand is likely to average around 600 units a year. Sales of these aircraft will go to the manufacturer who can offer the best *marketing mix*. This is true, of course, only if the manufacturer has a sufficient production capability strategy in place.

Cost of ownership will continue to dominate the civil aircraft operating economics equation¹³⁹. To capitalise on the vast potential for PMC materials, and later smart PMC materials, within the civil airline industry, the fabricated components must provide fuel consumption and maintenance benefits as well as a production cost reduction. Pertaining to smart PMC airliner components, the production costs are deemed as cause for concern.

Generally speaking, the outlook for passive PMC materials within commercial aircraft is extremely positive. The latest Airbus A340-500 and 600 models have approximately 15% airframe composite content¹⁴⁰. Future subsonic civil aircraft, such as the A3XX, **Figure 7.4**, will include an even larger portion of polymeric-matrix composite material.



Figure 7.4. Airbus Industrie's A3XX concept.

Based on published data¹⁴¹, in providing the necessary weight reductions, the A3XX program will *need* to rely heavily on PMC

materials. The PMC airframe structural content could even exceed 100,000lb per aircraft. This being more than 3 times that of the quantity used within the aforementioned A340.

Within this new aircraft, primary structures within the wing & fuselage sections are strong candidates for advanced PMC materials. As is the norm on recent large scale civil aircraft entering into service, the entire vertical & horizontal tail will also be manufactured from PMC materials.

Airbus Industrie forecast a market for 1,400 A3XX aircraft. This translates into a very large advanced PMC utilization rate. Strong A3XX customer satisfaction may pave the way to smart PMC structures in follow on A3XX type programmes.

Within North America, the increasing number of scheduled short-haul flight delays and airport passenger congestion, both being associated with the conventional air transportation system, have engendered significant customer dissatisfaction⁶. There is, therefore, growing interest in advanced rotorcraft for short-haul inter-city transport.

An air transportation vehicle with a vertical take-off and landing, VTOL, capability, such as a civil tiltrotor, has great potential for the future. Tiltrotors offer higher speed and longer range compared to conventional helicopters and have lower infrastructure costs compared to those of existing short-haul twin-jet/turboprop aircraft.

Market studies show that a civil tiltrotor system could absorb 10-15% of the current short-haul air traffic⁶. This would relieve much of today's airport congestion. Providing the tiltrotors can be produced at reasonable cost, the largest market demand is for relatively large aircraft in the 36-45 passenger category.

The technological development of a civil tiltrotor will focus on, amongst others, vibration reduction, noise reduction, lightweight composite structures and reduced operating costs. All of which can be met with advanced smart PMC materials.

7.4.2.2 *Supersonic Civil Aircraft*

Compared to subsonic long distance travel, supersonic flight is extremely appealing. By flying faster, a large scale supersonic civil airliner would allow the operating airline to boost its number of passenger miles while reducing the time in the air for the passenger.

At present, the operating costs of the only in-service commercial supersonic transport, the Concorde, necessitates a fare surcharge which is only acceptable by the affluent few.

Within the first quarter of the next century, airline industry forecasts predict a reasonable size market for a second generation supersonic civil aircraft¹⁴². It will, however, require approximately twice the range and approximately three times the passenger capacity of Concorde while meeting strictly imposed legislative noise standards. It will require, at most, a modest fare surcharge, therefore, it will require long distance operating efficiency and minimal on-the-ground maintenance.

Compared to a subsonic aircraft, a second generation supersonic civil aircraft will almost certainly be more expensive for an airline to buy. To some degree, however, this will be offset by the greater productivity arising from its high speed¹⁴².

To date, supersonic civil aircraft market forecasts have concentrated on the top 150 routes that are over 2,000 nautical miles. Routes flown entirely over populated land have not been considered. Both the north pacific and the north Atlantic, with their long over water segments, are ideally suited to supersonic travel¹⁴². In fact, 28% of the international routes can be considered to have second generation supersonic potential.

Prior to the 1998 Asian economic crisis, the pacific-rim region was forecast to have the largest market requirement for supersonic travel. Amongst others, the regions economic recovery will play a significant role in a potential manufacturers decision to participate in a large scale supersonic transport programme.

Studies in the US, Europe and Japan provide evidence that technologies can be developed for enabling an economically viable second generation supersonic civil aircraft with a sufficient passenger capability. **Figure 7.5** shows a typical second generation supersonic commercial aircraft concept as proposed

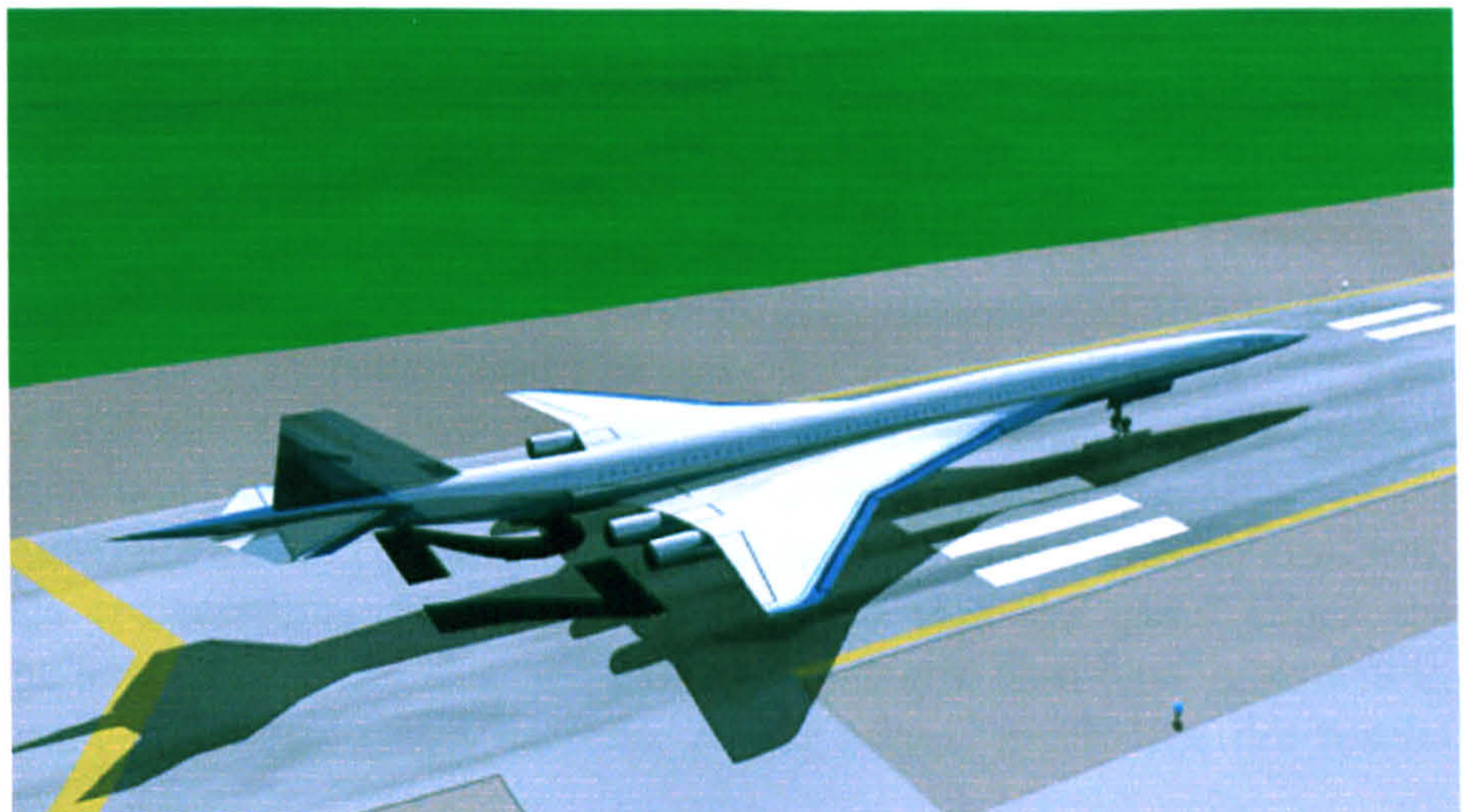


Figure 7.5. *2nd generation supersonic civil aircraft concept.*

by NASA.

Market forecasts indicate that a substantial supersonic aircraft market would exist early next century if the developed aircraft would (i) not harm the Earth's atmosphere, (ii) meet acceptable standards for aero-engine noise and (iii) ensure that any fare premium would not substantially exceed competing new-generation long-haul subsonic transports⁶.

Compared to subsonic aircraft, if a significant increase in ticket price is required, market demand may reduce to an unacceptable level¹⁴³. It is forecast that with a 45% increase in fare surcharge, a fleet of 300 aircraft would meet the market demand¹⁴². A 15% fare surcharge, however, would require 1,000 aircraft¹⁴².

To be economically and environmentally successful, any second generation supersonic civil aircraft will require exceptionally high structural efficiency. This will only be realised with innovative design and widespread utilization of advanced PMC materials^{144,145}.

Within the airframe, new technology developments will need to focus on higher temperature capability PMC materials¹⁴⁶. As yet, there are no commercially available PMC materials that have the proper combination of properties required for a civil supersonic application at speeds of Mach 2.0+. Several experimental materials, however, offer promise.

It is envisaged that SMA/PMC structural components, similar to those presented earlier in this thesis, be utilized in the design of a second generation supersonic large scale aircraft. Foreseen structural applications for the use of SMA/PMC's are the wing skins, control surface skins, vertical & horizontal tail sections and a major portion of the fuselage.

As presented in the earlier chapters, the adaptive response of the devised control strategies requires a significant power input to drive the SMA actuators past their phase transition temperature. Within the framework of this study, this has been realized via the input of an electrical current. The current source being a heavy cumbersome power supply unit.

Typically, supersonic aircraft panels are subjected, not only to aerodynamic loading, but also to aerodynamic heating. From a typical flight thermal spectrum for a supersonic civil aircraft, the polymeric-matrix components will be expected to operate in a temperature range between -55 and 120°C. The low temperature will arise for a short duration at subsonic speeds, i.e. pre- & post-supersonic cruise. The higher temperature arising due to friction with the air when the aircraft cruises at, say, Mach 2.05. The duration of supersonic cruise being 3-4 hours. Higher supersonic cruise temperatures would result at higher Mach numbers.

If a supersonic wing skin structure, for example, within an elevated uniform temperature field were allowed to expand freely, no macro-scale thermal stresses would arise. If such a temperature field were non-uniform, macro-scale thermal stresses will build-up, **Chapter 5**.

In actual applications, structural skin sections are attached to relatively cooler sub-structures, i.e. spars, webs and ribs. The latter components act as heat sinks, such that, even in the presence of a uniform thermal environment, temperature non-uniformity will arise within the skin. Along with the constraint imposed by the spars etc, this results with the build-up of macro-scale thermal stresses.

Mechanical and/or thermal compressive stresses within skin sections can rise to initiate buckling, **Chapter 4**. Buckling is objectionable, not only for structural reasons, but also because the buckled surface alters the flow of air. The latter leading to aerodynamic inefficiency, aerodynamic induced disturbances and, ultimately, structural failure.

During supersonic cruise, the heating over an individual skin panel is relatively uniform, however, the skin surface temperatures are seldom uniform because the panel edges are attached to heat sinks. The temperature rise over the panel surface will then have a plateau in the large central region and will taper down near the cooler edges, much like the results presented in **Chapter 5**.

It is this elevated temperature which can provide the necessary energy required for SMA phase transition, therefore, enabling the structure to tailor itself so that it is best suited to operate in the mechanical and thermal environment. Such self-adaptation eliminates the need for the electrical input power stimulus which, in turn, would make savings on mass and maintenance of its necessary control system.

It is the second generation supersonic civil aircraft market that could best exploit SMA/PMC components within its design. Additional foreseen applications of SMA/PMC materials include airframe skin structures of military aircraft that are subjected to intensified acoustic and thermal loads from vectored thrust engines, supersonic military aircraft skin structures that are subjected to a moderately short exposure time of 150-200°C and missiles that require brief exposure to elevated temperatures.

Much to the authors regret, noise legislation is predominately prohibiting a second generation supersonic aircraft developmental commitment from competing aircraft manufacturers. The Boeing company has recently postponed the launch date of its high speed civil transport, HSCT, **Figure 7.5**, from 2010 - 2020. The major reason being that the propulsion system's noise level had not been sufficiently reduced¹⁴¹.

7.4.2.3 Military Aircraft

To date, highly manoeuvrable military air vehicles have predominantly been the greatest employer of advanced PMC materials. As an example, Dassault Aviation's Mirage 2000 employs approximately 1,200lbs of advanced PMC in its airframe. Typical PMC airframe components are the vertical fin skins, rudder, wing flaps, elevators, ailerons, landing gear doors and fuselage access panels¹³⁸. The company's more recent Rafale is over 25% composite. Its structural weight constitutes 3,100lbs of PMC material¹³⁸.



Figure 7.6. Eurofighter 2000.

The twin-engine supersonic multi-collaborative Eurofighter 2000, **Figure 7.6**, is approximately 50% advanced PMC material¹³⁸. This translates into 3,600lbs of material. In fact, approximately 82% of the EF2000's wetted surface consists of advanced PMC constituents.

Based on 820 aircraft sales, in excess of 2,952,000lbs of advanced PMC materials will be used within the entire forthcoming Eurofighter production run.

The percentage of advanced PMC materials in the fighter planes of tomorrow, i.e. the joint-strike-fighter, JSF, will certainly rise. This is all good news for the material suppliers. With market estimates for a total of 5,000 JSF aircraft¹⁴⁷, **Figure 7.7**, the material suppliers have yet to net a lucrative contract.

Military aircraft are, at present, the major potential market for smart structures technology. This is due to the significant benefits that smart structure concepts offer in the areas of survivability and supportability.

As an example, military aircraft are designed to participate in combat. The aircraft may well be subjected to damage from enemy fire. In the event that damage is incurred, the pilot must decide whether the aircraft can continue to be flown, or if he/she must eject. Smart structure concepts offer the potential to improve this situation. As discussed in **Section 1.4.2**, detecting the occurrence of ballistic damage and assessing the extent of damage will improve the aircraft's survivability².



Figure 7.7. *Boeing's X-32 JSF concept.*

As today's military air vehicles feature a high percentage of visibly exposed PMC material, i.e. the air washed surfaces, it is quite conceivable that tomorrow's air vehicles will feature integrated sensory functionality within such PMC hosts, thereby, providing a real-time status of the airframe capability to the cockpit and/or ground personnel.

7.4.2.4 Space Based Platforms

Future space systems include manned space transportation systems, unmanned space transportation systems, scientific spacecraft and communication satellites. Owing to the starting price of approximately \$2,000-\$8,000/kg for payload access into low earth orbit, a current major design driver is for a dramatic cost reduction in achieving space access⁶. Key technology requirements include ultra-light weight composite primary structures.

Future space based communication and scientific platforms will require less weight, higher dynamic frequency characteristics, lower thermal/structural distortion in the space environment and adaptability⁶.

According to market forecasts¹⁴⁴, approximately 1,700 commercial/civil satellites will be launched over the next 10 years. 95% will form multi-satellite mobile communication systems. Such systems will have a life of 4-7 years prior to replacement.

Pertaining to Motorola's Iridium satellite constellation in LEO, each satellite bus weighs 180lbs. It is predominantly made from Hexcel F584 epoxy resin, Toray's M60J carbon fibre and aluminium honeycombe¹⁴⁸.

As seen, the use of passive polymeric-matrix composites has been and will continue to be utilized extensively within future space systems. As presented in **Section 1.6.2**, space systems of the future will also widely exploit the enabling active elements that constitute a smart structure.

While active elements will enable the alleviation of unwanted disturbances within orbiting space systems, sensory elements will enable rapid reusable launch vehicle turnaround time. The latter being the subject of much recent research within the US.

The recent NASA/former McDonnell Douglas Delta Clipper-Experimental Advanced, DC-XA, re-usable rocket program included the development of a structural health monitoring system, SHMS, to acquire and present structural health information to the DC-XA flight team in real-time. It is reported that, throughout the ground and flight test programme, the system successfully demonstrated advanced structural component data acquisition, processing, and display capabilities using conventional and fibre optic sensing technologies¹⁴⁹.

7.5 Smart PMC Affordability Considerations

The preceding section highlights the market potential for both passive and smart polymeric-matrix composites. This section breaks down the all important issue of affordability into the two governing topics of procurement cost and cost of ownership of aerospace platforms. The focus of the following subsections will pertain to PMC and smart PMC affordability implications.

7.5.1 Procurement Cost Implications

When deciding to use certain materials in the construction of aerospace platforms, several parameters must be considered. These include the following¹⁵⁰; (i) the cost of the raw materials, (ii) the cost of tooling, (iii) the costs associated with a particular fabrication process/technique, (iv) the cost of assembly of the final structure, (v) the scrap rate, (vi) the likelihood of repair costs, (vii) environmental factors such as toxicity of the materials used, and finally (viii) certification issues.

One of the important advantages of utilizing PMC components within aerospace platforms is the potential for reducing acquisition and/or life cycle costs. This will, of course, be dependent on the actual component in mind. Reduced acquisition and/or life cycle costs may be effected through weight savings, lower tooling costs, reduced number of parts and, therefore, fewer assembly/disassembly operations.

This advantage is somewhat diluted, however, when considering the high cost of the constituent PMC raw materials, i.e. the fibres, matrix, and in some circumstances the auxiliary materials used in fabrication. On the other hand, the low cost of conventional structural materials, i.e. aluminium, is offset by the high cost of tooling, machining and assembly.

The cost of the advanced PMC constituents is, however, decreasing due to market expansion. The constituent price will further decrease with successful advanced PMC utilization in high volume applications such as civil engineering infrastructure and automotive vehicles.

In addition to the decrease in the PMC constituent costs, the fabrication process is becoming less costly as novel cost effective constituents are employed, automation is introduced and more experience is accumulated.

Pertaining to PMC fabrication, cost effective RTM'd advanced composite components recently contributed to Lockheed Martin's low price winning bid to develop and build the joint air-to-air-stand-off missile, JASSM, as selected by the US airforce and navy¹⁵¹. It was reported that the winning bid was \$275,000 per missile. The amount well below the set target of \$400,000. While the exact composite content is classified, it was stated that the RTM process enabled Lockheed Martin to significantly reduce the cost per missile and underbid the competition, Boeing, by a large margin. Boeing's design constructed from aluminium.

When considering integrating functional elements within PMC constituents, emphasis must be placed on additional key factors that drive the cost equation, these being; (i) their quantity & distribution, (ii) their performance/reliability, (iii) the effect on production and schedule, (iv) risk impact, (v) the overall complexity of the system, and finally, (vi) logistic support.

In determining the weight-to-cost relation of structures made from smart advanced PMC materials, it may well be that the high material, fabrication and tooling costs may all add up to make the use of such light weight and high performance materials cost ineffective as compared to the commonly used metal alloys or passive PMC materials.

Integrating smartness within any conventional passive structural design can be considered as innovative. Based on widespread industrial experience, the more innovative a project is, the greater the likelihood for cost over-run during development and/or production. **Figure 7.8** is presented as a purely subjective guide on the potential for cost increase, during production, according to the degree of innovation involved¹³⁵.

Within the figure, the remit of the smart structure technology can be placed anywhere between *new design*, *improved technology* and *highly innovative, new concept*. This will, of course, be dependent on the degree of smart constituent integration. For any manufactured smart product it will be quite conceivable to have a cost over-run in the range of 25-700%. The upper value will have the domineering effect of the products omission from the mass market.

Emphasis will then be on the benefits in increased operational performance. Under

close scrutiny will be the potential for savings in structural mass, reduced maintenance time, reduced maintenance manpower and increased payload. Such benefits, however, could easily translate into increased structural repair, less availability due to increased repair and even greater mass, all of which having the likely effect of the smart capability being left on the ground.

It would be desirable for any smart integration to have minimal production cost implications. Cost effective, reliable, readily available elements should be exploited throughout.

As witnessed within the current research programme, the failure of any building block of the smart package can prove costly. With referral to the test of specimen configuration number 7, **Section 6.3.3.4**, utilizing 18 SMA wire locations necessitates 7.2 metres of tubing. At £15.22 per meter for the polyimide tubing, compared to £0.17 per meter for the PTFE tubing, failure of the plate in meeting the desired SMA integration proved costly. Hence, for the remainder of the research programme, utilization was made of the highly reliable cost effective PTFE tubing.

Regarding civil and military aircraft, cost considerations include more than just the cost of producing an item. Cost considerations must also include the products life cycle cost. In fact, life cycle costs over the lifetime of the airframe may be even more important than the procurement costs⁶. Particularly to civil aircraft, any new technology must buy its way onto an

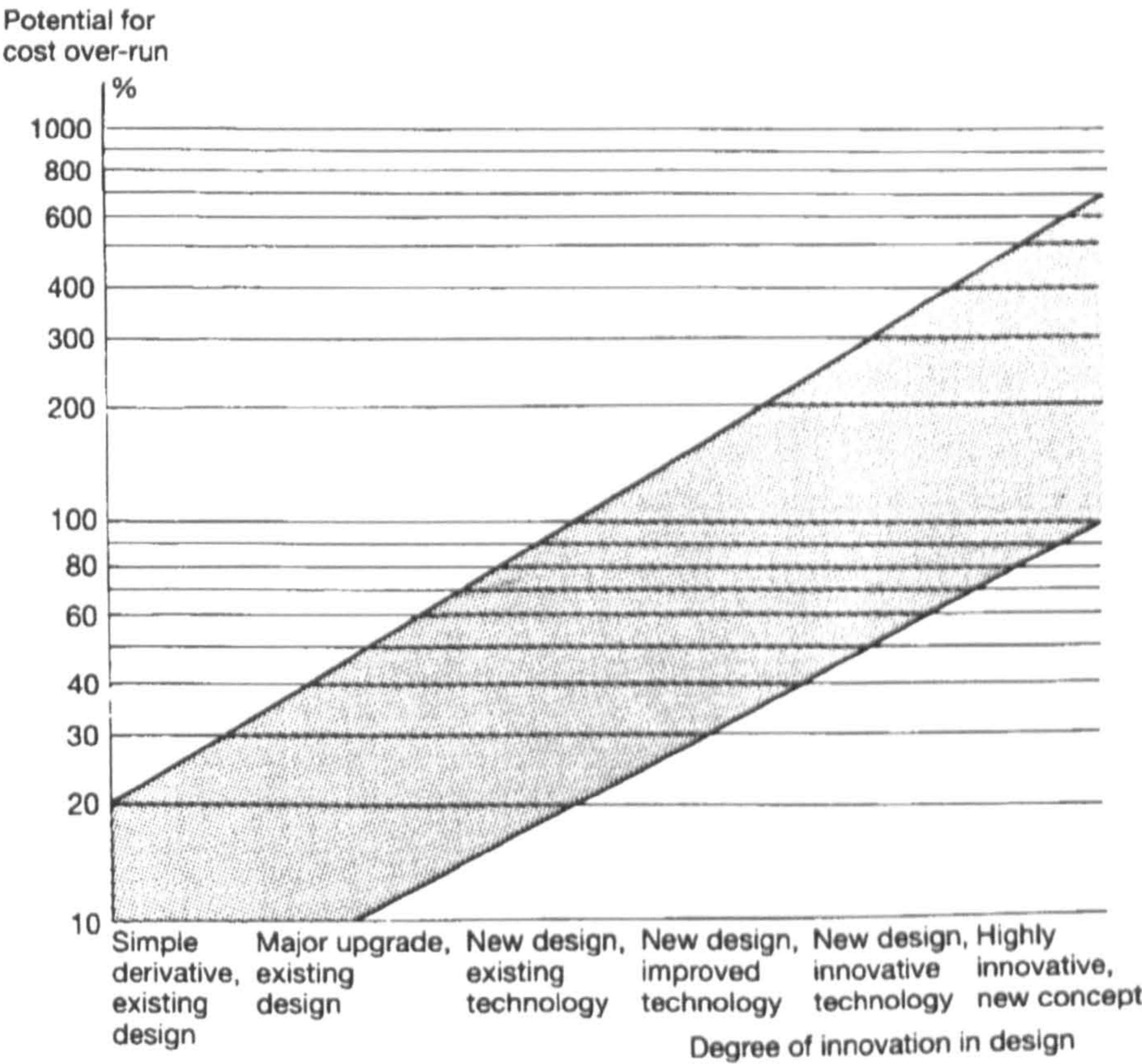


Figure 7.8. Degree of innovation vs potential for cost over run.

aircraft through reducing its life cycle costs. Typically, fuel and maintenance are key elements that can be reduced with the smart PMC airframe integration.

7.5.2 Operating Cost Implications Part 1 - Smart Airframe Maintenance

To maximise profit, airline operators must maximise aircraft utilization. To ensure safety, they must keep their aircraft in good condition. Military aircraft operators must ensure maximum readiness for war. To ensure mission effectiveness, military aircraft must also be kept in good condition. Both types of aircraft, therefore, undergo an approved maintenance schedule. Maintenance schedules vary from routine servicing before every take-off to major overhauls every few years.

Significant costs can be incurred in carrying out maintenance inspections. The more thorough the inspection, the higher the cost. Pertaining to commercial aircraft, the costs greatly increase if there is a need to temporarily remove the aircraft from service.

In general, for commercial aircraft, maintenance costs range from 10% to 25% of the total aircraft operating costs¹⁵². The actual value depending on the age of the aircraft. As civil aircraft can only make money when they are flying, the reduction of unnecessary dead time on the ground is now the objective of most schedulers.

Military aircraft operating costs, over a full life cycle, can be as much as 3 times the initial procurement costs. The major overhaul of a Tornado takes 13,400 man hours and is scheduled for shift working over 72 working days¹⁵³. Such major maintenance of the aircraft involves *strip down*, i.e. the removal of virtually all the components down to the primary structure.

The increasing utilization of today's fleet of both military and civil aircraft has resulted with increasing concerns over the structural integrity of such ageing aircraft¹⁵³. This has led to considerable time and effort being spent to ensure the safety of these vehicles, hence, a rise in inspection related costs. Such costs are expected to increase further as the ageing aircraft population continues.

Due to the difficulties encountered in detecting damage in PMC materials, it is anticipated that the current trend in the use of advanced composite materials may cause even further maintenance expense. Providing the PMC parts are accessible, PMC inspection techniques require expensive bulky equipment and highly experienced operators. Such methods, particular to complex assemblies, are also questionable¹⁵⁴.

Maintaining the airworthiness of aircraft, while at the same time keeping the maintenance costs low, is of prime concern to the operators. There is, therefore, considerable incentive to produce structures with built-in integrity monitoring or a damage assessment capability. Cost benefits are anticipated where maintenance costs can be reduced by continuous monitoring of the aircraft's condition, such that, unnecessary work is avoided¹⁵⁵. Such economic maintainability will be a selling point for new aircraft types.

Within both civil and military aircraft, a smart self-sensory system may replace regular maintenance with *as needed* maintenance and, therefore, extend the intervals between which the platform requires attention. Maintenance actions will be more efficient as the system will be able to alert ground personnel of the condition of a potential damage site².

Regarding civil aircraft, the largest benefit of reducing aircraft downtime is the resulting increased aircraft utilization. This leads to more available revenue earning days, hence, more profit.

As introduced in **Chapter 1**, PMC materials inherent with NDE can ensure that fabricated PMC components are of adequate quality prior to in-service entry. Once in-service, they

could provide guidelines for appropriate platform operating conditions, therefore, ensuring that the conditions do not degrade the component, or global platform's, performance. The system will have the potential to monitor the structural loads accurately and enable precise determination of the remaining life, thereby, extracting the maximum safe use of the airframe². Clearly, the more functions the sensory function can perform, the greater its cost effectiveness.

If damage is found early enough, repairs are kept minimal. If you wait until the damage is more advanced, the repair bill can be significantly higher.

Parts which are currently retired based on having been exposed to an analytical lifetime of structural loading will not have to be discarded until they exhibit threshold levels of damage accumulation¹¹.

Costs associated with a smart sensory system will be a major factor governing its implementation. A nonexclusive list of factors to consider when determining the total cost of smart sensing is as follows; (i) sensor development, (ii) sensor manufacturing, (iii) installation on or within the structural host (iv) signal interrogation requirements, (v) personnel training, (vi) inspection duration and frequency (vii) reliability and (viii) duration for disassembly/assembly.

Regarding signal interrogation requirements, the system will generate a lot of data that will have to be processed and presented in a simplified way. This, in turn, will require software and hardware that will undoubtedly increase the purchase cost

Vulnerability to handling and combat damage will be a significant consideration in integrating functional elements with structures, especially outer skin structures exposed to ground activity. Maintenance induced damage is a tough requirement on composite structures in general, and the induced damage may be much more difficult and expensive to repair when the structure performs many functions and must be functionally restored as well as structurally repaired.

7.5.3 Operating Cost Implications Part 2 - Smart Jet Engine Maintenance

Figure 7.9 shows how the overall cost of owning the Rolls-Royce RB-199 after-burning turbofan military aero-engine is split over a typical service life of 6000 operational flight hours¹⁵⁶. It can be seen that the cumulative costs of maintenance, including infrastructure, over a 20-25 year life cycle, amount to approximately 3.5 times the initial purchase cost.

The fan and compressor stages require 26.5% of these maintenance costs and foreign object damage, FOD, accounts for up to 50% of the fan and

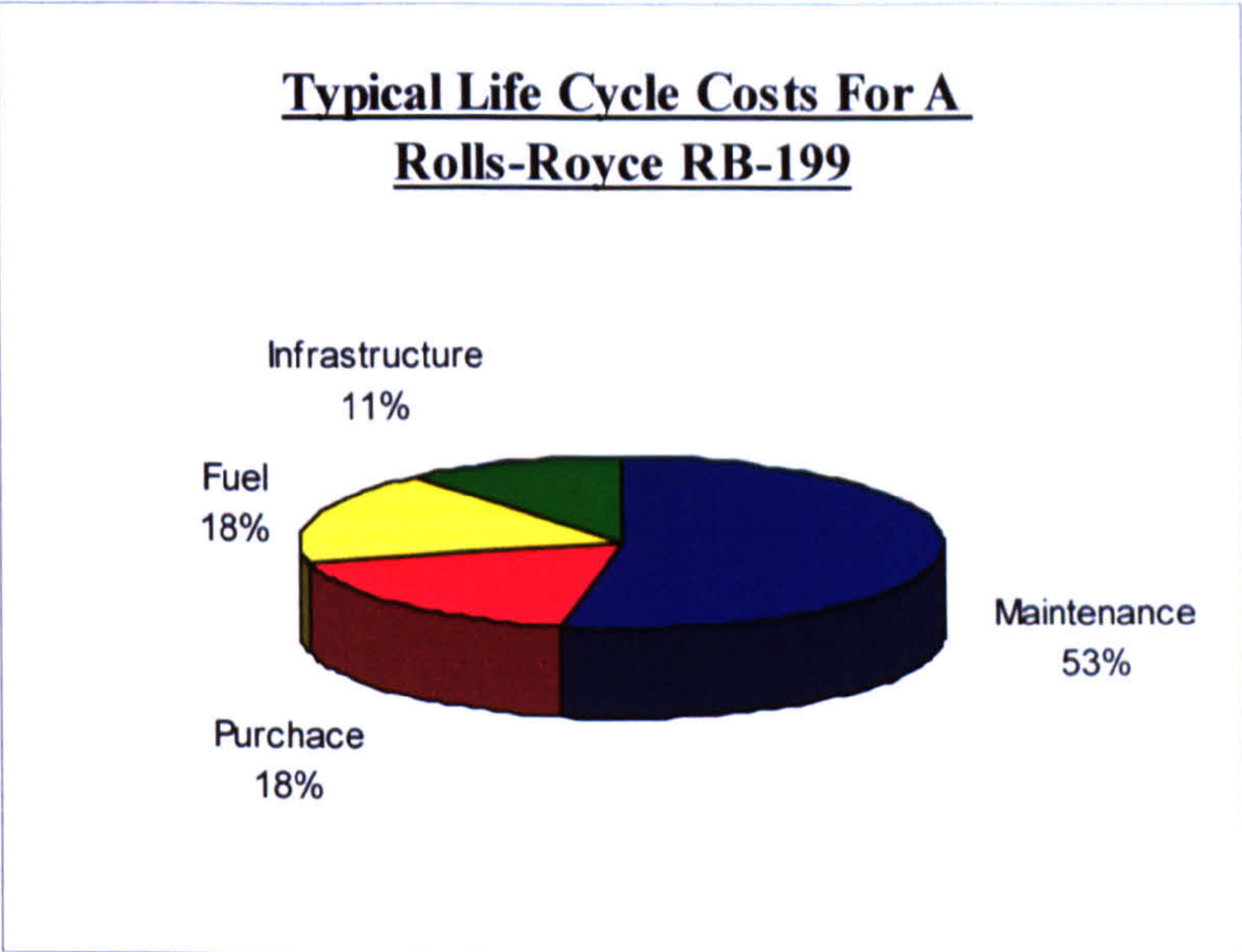


Figure 7.9. Life-cycle costs for an RB-199 aero-engine.

compressor stage repairs. Even with the latest engine designs, FOD issues will still necessitate scheduled maintenance inspections for the foreseeable future.

Within military and commercial aero-engines of the future, it is anticipated that the desired trend in the use of advanced composite materials will undoubtedly cause even further maintenance expense.

Erosion and damage accumulation is of particular concern for polymer matrix composites. For carbon/epoxy blades/vanes, as an example, the rate of erosion, or initiation of damage, could be monitored in real-time by the use of *moderately* distributed embedded optical fibre sensors. Erosion, or damage-in-progress, may be detected by the changes in a *to-be-defined* component parameter with respect to the corresponding base-line value for the *healthy* component.

The method, requiring a computerised diagnostic system, could supply in-flight information to the cockpit, or store the in-flight information for later retrieval by the ground personnel. This way a complete real time knowledge of various parts of the engine is obtained. The information may help the pilots in making decisions about the mission. For the ground personnel, the information could ensure appropriate maintenance intervention, thus, minimising maintenance induced DOC.

As with airframe maintenance, the inclusion of a self-sensory capability within aero-engines will affect the three main cost areas throughout the product life cycle, these being; (1) Research and Development, (2) Production and (3) In-service Operation and Support. The integration of self-sensory capability will undoubtedly require additional expenditure on R&D and Production. The hope is that substantial savings gained through O&S reductions would exceed additional costs associated with the former, thereby, making the product more market competitive.

With regards expenditure on In-service Operations and Support, a well documented study performed on the T-38 wing box demonstrated that as much as \$14.4 million could be saved on the cost of fleet inspections per year when calculating the savings due to manpower and personnel reductions. Savings were based on 420 flight hours per aircraft per year, for a 720 aircraft fleet¹⁵⁷.

Preliminary studies indicate savings can also be achieved on the F-18 aircraft. Savings in excess of \$35 million per year were estimated for 1000 aircraft that fly, on average, 33 hours per month¹⁵⁷. These figures do not include the rare but plausible saving due to the avoidance of a catastrophic structural failure causing the loss of an aircraft.

7.6 And Finally !

The introduction of smart PMC materials within aerospace platforms will be a slow process. First, the benefits of having it on the aircraft must substantially outweigh all the additional costs pertaining to its integration².

Smart PMC integration will first be associated with slightly loaded structural units. With the satisfactory accumulation of in-service data and system operating characteristics, smart PMC materials may well find their way into load-bearing components designed from static strength conditions. As operating experience continues, they could find their way into primary load-bearing structures whose strength is determined by fatigue and longevity.

This process will probably be over a 15-20 year period. With the exception of spacecraft, without a doubt, the first smart PMC applications will focus on aircraft health monitoring, whereby, substantial maintenance and inspection cost alleviation is anticipated.

In general, the smart PMC concept is fairly innovative. This, therefore, implies a step into the unknown and carries with it the risk that any research work pertaining to the subject will not make a satisfactory return for the amount invested.

Regarding high technology aerospace companies, failure to invest may result in the eventual demise of the organisation. To stand still is to go backwards as there will always be rivals who are prepared to invest in innovation, thus, ensuring their better future.

Chapter 8

Conclusions & Further Work

Smart polymeric-matrix composite plates/panels that are able to respond to detrimental changes in their operating conditions have been studied in this research. Such response being, for example, the tendency of the plates/panels to realise their optimal flat configuration when under the influence of both a mechanical and a thermal load.

Piezoelectric, electrostrictive, magnetostrictive, electrorheological fluids and shape memory materials have frequently been proposed as the actuation mechanism for smart/adaptive structures. Of these, nickel-titanium, Ni-Ti, shape memory alloys generate the highest level of force for a specific actuator volume. Such a force being generated by the prevention of the shape memory recovery phenomenon. On this basis, this material was selected for the undertaken PhD research.

Carbon/epoxy unidirectional prepreg was the employed structural host material. Its selection was based on its widespread acceptance within the aerospace community for high performance parts and the ease of integration and consolidation of the chosen Ni-Ti SMA constituent.

Throughout this research programme the author has primarily been concerned with the buckling behaviour and post-buckling displacement alleviation of carbon/epoxy plate and panel specimens. Such specimens featuring embedded pre-treated Ni-Ti shape memory alloy actuators at pre-defined locations.

The research intention was to demonstrate a sufficiently enhanced adaptive post-buckling structural capability. Realization of this intention has been achieved, though, the enhanced adaptive post-buckling response depends on a variety of parameters.

Two Ni-Ti SMA actuator compositions were purchased and tested throughout this research programme. The alloys have nominal compositions of 49.8-50.0 & 50.0-50.2 at.% Ni and were annealed at 500°C for one hour. Both 0.3mm & 0.4mm diameter SMA actuators have been considered for structural control purposes.

Post-buckling displacement alleviation has been accomplished by two means;

- The first approach, which is referred to as *control strategy 1*, utilizes 0.4mm diameter SMA actuators fed through either PTFE tubes or polyimide micro-tubes, located on the specimens neutral plane, after specimen fabrication. The actuators are fixed to an external boundary. For the actuators, initially pre-strained by 6%, the external constraints prevent shape memory recovery, therefore, upon activation, significant stresses accumulate within the actuator that can be employed to achieve the desired structural adaptation. Nine laminated plate configurations have been considered.
- The second approach, referred to as *control strategy 2*, features 0.3mm diameter SMA actuators, pre-strained by 6%, embedded and consolidated to the host structure at a desired location within the host laminate. Their position will be as far off the neutral plane as possible, therefore, for a given level of SMA activation, recovery forces, equilibrated by the surrounding matrix, invoke the maximum permissible bending moment. The imposed bending moment permits structural adaptation. Two laminated panel specimens have been considered.

SMA actuation is met through an inherent solid-solid phase transition. Such phase transition necessitates a temperature rise. Whilst this can be met through the application of an electrical power source, it may also be provided for by the operating environment. The former, requiring a large cumbersome power supply unit, has been employed within this research investigation. The use of the latter stimulus, however, may allow such smart SMA/PMC's to be exploited within high temperature components of next generation aerospace vehicles.

Calculation of the appropriate power level necessary to transform the SMA actuators into complete austenite, therefore, inducing maximum recovery stress, requires the experimental determination of the phase transformation temperatures. Within this study, the most important transformation temperature has been A_f .

- The stress free A_f for the 49.8-50.0 at.% Ni alloy is $\approx 68^\circ\text{C}$. For the 50.0-50.2 at.% Ni alloy, it is $\approx 32^\circ\text{C}$.
- Depending on the stress and stress influence coefficient, the aforementioned values can rise by an amount anywhere within the range of $50 \rightarrow 150^\circ\text{C}$.
- When embedding these Ni-Ti alloys within PMC hosts, due to the hosts thermal constraints, only partial austenitic phase transition will be achieved. This is especially so when embedding the 49.8-50.0 at.% Ni alloy. This is undesirable as we fail to utilize the alloys full capability.
- Within this investigation, therefore, much use has been made of the 50.0-50.2 at.% Ni alloy.

The ability of the actuating material to operate within the host composite is, amongst others, dependent on the integrity of the SMA. Prior to integrating SMA actuators within the composite laminates, the effect of SMA preparation and composite manufacturing on the shape memory performance was determined. In particular to *control strategy 2*, SMA preparation entailed the assessment of;

- The affect that the high temperature curing process has on actuator performance.
- The affect that the abrasive action, associated with sandblasting, has on the actuator performance. Sandblasting enhances the mechanical interaction between the SMA and the matrix material.

These two assessments were made by the comparison of their recovery force measurements with that of a plain actuator. This was achieved by cycling constrained samples through their transformation temperature range and monitoring the associated recovery force. The plain actuator refers to SMA material with initial pre-strain only. For both control strategies varied levels of SMA pre-strain have been considered.

When subjecting the trained pre-treated 49.8-50.0 at.% Ni alloy wire actuators to an on-off type step electrical power input, the following is observed;

- The maximum recovery force is generated during the first cycle. Subsequent cycles show a decrease in the recorded recovery force. The greatest reduction occurs during the first

few cycles. After 15-20 cycles the recovery force becomes more stable. Such behaviour being characteristic of nickel-titanium.

- The actual mechanism for recovery force degradation is associated with the internal defect formation, i.e. dislocations. These defects are not so common when subjecting the material to an incomplete phase cycle.
- For both 0.3 and 0.4mm diameter SMA specimens, the thermoset cycle has no compromising effects on the actuator recovery force.
- For the 0.4mm diameter alloy, the sandblasting variant is seen to be insignificant.
- When sandblasting the 0.3mm diameter alloy, the abrasive action degrades the recovery force. This degradation results when the entire SMA periphery is sandblasted.
- When such alloys are embedded and activated in PMC hosts, *control strategy 2*, most of the recovery force is transferred at the actuator ends. Therefore, only the ends require the enhanced mechanical interaction with the matrix.
- 0.3mm diameter alloy actuators, partially sandblasted over a 10mm region from both ends, show a modest decrease in the generated recovery force.
- It is likely that the observed lower recovery force levels are due to periphery material loss. Providing the sandblasting prevents interfacial decohesion, then, for the partially sandblasted actuators, the lower force levels are tolerable.

When subjecting the trained 50.0-50.2 at.% Ni alloy wire actuators to an on-off type step electrical power input, the following is observed;

- For a comparable power level per unit length, the recovery force levels are significantly higher than those of the previous alloy. The recovery force degrades with successive cycling, though, the rate of degradation is only seen to occur during the first few cycles.
- When deactivating and allowing the specimen to return to the ambient temperature, the specimen retains a partial recovery force. This remaining residual force was typical for all the 50.0-50.2 at.% Ni alloy specimens tested, regardless of diameter and the level of pre-strain. This is because the imposed operating energisation/temperature cycle prevents the formation of martensite upon cool down.
- When compared to the previous alloy, for both diametrical samples, a decrease in the initial level of pre-strain, i.e. from 6% to 2.5%, results with a relatively small drop in stabilised recovery force. It is well known that the recovery force fatigue life of Ni-Ti depends upon the initial level of pre-strain. Therefore, for this particular alloy, within the current imposed operating conditions, relatively small amounts of pre-strain can still induce a significant recovery force.
- The samples pre-strained by 6% exerted the greater levels of recovery force, therefore, this level of pre-strain was used for plate/panel adaptation.

Nickel-titanium actuators, should they be incorporated within aerospace structural platforms, will generally be expected to function consistently over a great number of cycles and, thus, knowledge of the degradation of the shape memory recovery response during cycling is of vital importance.

- For the 49.8-50.0 at.% Ni composition, both alloy diameters tested required 15-20 cycles prior to generating a pseudo-stable recovery force. The greatest initial recovery drop is seen when the alloys have a 3% pre-strain. For this particular alloy, with the operating constraints imposed for this programme of study, the recovery force is expected to continue to drop. This is especially true for actuators with the higher initial pre-strain levels.
- For the tests performed on the 50.0-50.2 at.% Ni alloy specimens, after the first few actuation cycles, the generated recovery forces stabilise to a consistent level. The consistent cyclic behaviour is attributed to the operating temperature cycle imposed upon the specimen. Activation and deactivation above the materials stressed martensitic start temperature subjects the material to an incomplete phase cycle. This prohibits the prolific formation of lattice defects which occur when completely cycling between austenite and martensite.
- As complete phase cycling degrades the recovery behaviour with each successive activation/deactivation, with knowledge of the alloy operating temperature, then, the alloy composition and heat treatment can be optimised to improve the cyclic response.

The completely fixed constraints within *control strategy 1* prevent SMA recovery. Within *control strategy 2*, spring-type constraints prevent complete shape memory recovery. The spring coefficient being dependent upon the panels in-plane stiffness. When under the influence of the external mechanical load and internal SMA recovery forces, the amount of strain imposed within each of the panel specimens will not be high enough to cause significant SMA shape recovery. Upon SMA energization, based on the assumption of finite strain recovery, the author has assumed the actuators behave as if they were completely fixed.

The processing cost of Ni-Ti is very high. This makes the Ni-Ti alloy selection to be the most cost ineffective. The requirements of this research are performance driven. The aims have been to demonstrate a concept. As the concept has shown significant potential, then, to achieve a desired market realization, issues related to cost effectiveness should be considered.

Further work should focus on;

- Reducing the costs of the actuating constituent. This could be realised with the utilization of Cu-Zn-Al, & Cu-Ni-Al shape memory alloys, though, an assessment of their performance must first be established.
- Electrical energization power requirements. When wishing to control structural response via an electrical input, further work should seek to reduce the required driving electronics of the power supply unit. The requirements are for a reduction in both mass and cost.

All specimens throughout this programme are constructed from Fiberdux 913C-TS unidirectional high tensile carbon/epoxy prepreg.

- The stacking sequences of the laminated plates of *control strategy 1* are $[0_2/90_2]_s$, $[0/\pm 45/90]_s$ & $[0_2/\pm 45]_s$.
- The stacking sequences of the laminated skin sections of the panels within *control strategy 2* are $[0_2/90_2]_s$ & $[0_2/\pm 45]_s$. The lay-ups in the blade sections of the stiffeners being $[0/90_2/0]_s$ & $[0/\pm 45/0]_s$ respectively.
- The plate and panel skin section dimensions are 300*310mm. The depth of the panel's blade stiffeners are 12.5mm. Two stiffeners are bonded to each panel skin section. They have a spacing of 200mm.

Within the framework of this investigation, the author has been concerned with embedding actuators within the aforementioned host laminates. As a result;

- Significant degradation in the global compressive performance of the fabricated specimens will result when locating the inclusions perpendicular to the fibres of both adjacent plies and the loading direction. Such behaviour being attributed to the initial prebuckled reinforcement local to the inclusion. The magnitude of this degradation will be dependent on the specimen geometry, specimen properties, inclusion diameter and inclusion volume fraction.
- In order to decrease the extent of the aforementioned structural degradation, embedded inclusions should be embedded parallel to both the loading direction and adjacent reinforcing fibres. The smaller circular cross-sections of an embedded inclusion parallel to the loading direction should reduce the load bearing ability of the laminate to a lesser extent than the relatively larger rectangular projected cross-section of the cylindrical inclusion embedded perpendicular to the load.

To minimise the property degradation of the host laminates considered within this study, then, when choosing the orientation in which to embed the SMA actuators, it should preferably conform to the latter statement. This may not always be attainable, however, or, from a structural control point of view, be desirable.

The authors main study is concerned with plate/panel post-buckling deflection removal and the associated post-buckled non-uniform stress alleviation, in which, the applied stresses are considerably lower than the hosts ultimate. Therefore, it has seemed suitable to orient the embedded inclusion in a manner best suited to achieve the desired structural response.

For *control strategy 1*, for the ease of manufacture and test, the wire actuators were always embedded perpendicular to the applied mechanical compressive loading direction. The 0.4mm diameter SMA wire, plus the tubing, represents quite a severe geometric inclusion. This is especially true, since these were embedded in approximately 1mm thick laminates.

- For the $[0_2/90_2]_s$ lay-up, placing the SMA/PTFE inclusion parallel to the neighbouring reinforcement is clearly advantageous, as such a configuration poses minimal reinforcement disruption.
- For the $[0_2/\pm 45]_s$ lay-up, placement of the SMA/PTFE inclusion at an angle of 45° to the reinforcement results with considerable reinforcement disruption and the formation of a lenticular resin rich region extending approximately 2mm from the inclusion interface.

- For all the specimens featuring SMA/PTFE inclusions, significant surface topology disruption is apparent.
- When utilizing the micro-tubes, for the $[0_2/90_2]$, lay-up, owing to the finite wall thickness, placing the SMA/polyimide inclusion parallel to the neighbouring reinforcement resulted with the most advantageous hybrid specimen as the inclusion is least intrusive to the host.
- Difficulty can arise, however, when trying to insert the actuators within the polyimide tubing after the manufacturing process.

When embedding such inclusions within PMC hosts, the local stress-state is significantly altered. For *control strategy 1*, an appreciation of this local stress-state was numerically attained for the $[0_2/\pm 45]$, and $[0/\pm 45/90]$, lay-ups in close proximity to the embedded inclusion.

Five numerical load cases were considered. These load cases examined the application of; (i) P_c , (ii) approximately $3P_c$, (iii) the thermal residual stress state at ambient conditions, (iv) P_c + the thermal residual stress state at ambient, and (v) approximately $3P_c$ + the thermal residual stress state at 100°C on the local stressed state. From which;

- The maximum tensile stress was observed within the $[0_2/\pm 45]$, lay-up and within the outer 0° directional plies, local to the embedded inclusion, for the third load case. This stress only amounted to 1.4% of the materials ultimate load carrying capability. Such a stress magnitude, therefore, is no cause for concern.
- The maximum compressive stress was observed within the $[0/\pm 45/90]$, lay-up, local to the embedded inclusion, for the fourth load case. At worst, this will be carried by the small amount of 90° directional plies present in close proximity to this region and amounts to 51.7% of the materials compressive load carrying capability. Such a value may have fatigue implications that should be assessed.

Owing to the advantageous orientation of the embedded inclusions within the specimens of *control strategy 2*, i.e., they are embedded parallel to the loading direction, the effect the inclusions have on the local stressed state has not been assessed. Further work should quantify such effects.

Control strategy 2 requires the adherence of the actuators to the host composite. On activation, restraint is imposed upon SMA strain recovery by the SMA/composite interface. Here, tensile axial stresses form within the actuator. Such stresses are equilibrated through interfacial shear stresses and result with a compressive load on the composite. This interfacial shear stress acts over a finite distance at the ends of the bonded actuators. The location of these internally generated interfacial shear stresses requires careful consideration so as not to degrade the mechanical performance of the compression loaded specimens.

- Preliminary calculations showed that SMA induced membrane stresses will have a domineering detrimental effect on the post-buckling response of plate specimens.
- The effect of constrained recovery, imposed on the embedded SMA actuators by the host, was conceived as having the desired response within skin loaded stiffened panels. Having an understanding of the plates bending-buckling response, a high volume fraction of

activated SMA material placed in an un-symmetric manner can alleviate the out-of-plane deflections.

The direct inclusion of Ni-Ti within carbon/epoxy laminates necessitates the provision of a suitable insulating barrier to segregate the Ni-Ti and carbon electrical conductive constituents. It was achieved by sandwiching the actuators between woven glass fabric.

- Electrical insulation was only unobtrusively provided for when embedding the abraded sandwiched inclusion parallel to the reinforcement of the neighbouring plies.
- Although the SMA actuators are placed un-symmetrically within the specimens, the glass insulating weave is required to be placed skew symmetrically else warpage of the skin section will result as the specimen cools down from the curing temperature.

Electrical insulation would not be required if the actuators were energised by the operating environment as opposed to the electrical current.

Regarding the panel specimens, the constrained shape memory recovery forces were induced 100mm from the specimens loaded edges, whereby, the induced bending stresses have a more advantageous effect on the panel than the detrimental membrane stresses. This presented difficulties in terms of manufacturing. It required PTFE tubing to be located around the parts of the actuator required to regain the memorised shape. This local regain of the initial shape ensures that no local stresses will be transferred to the composite.

- When placing the SMA/PTFE inclusion parallel to the neighbouring ply reinforcement, the frictional forces, or mechanical hindrance, succumb to any shape memory recovery force. This results with the required shape recovery and the transference of recovery stress at the desired location.

The adaptability of the fabricated stiffened panel specimens depends on the stress transfer from the embedded SMA actuators to the surrounding composite. With activation of the embedded pre-strained SMA wires, interfacial shear stresses will be generated at the actuator/composite interface. The shear strength of this interface must be stronger than the SMA recovery stress or the interface will fail.

- Sandblasting the periphery of the actuators is the means provided for in this investigation to enhance the SMA/host bond strength.

An additional requirement for adaptation is that the SMA/host interface must be of sufficient quality to sustain an elevated temperature. Fibre pull-out tests were conducted in order to quantify the interface quality between 0.3mm diameter sandblasted SMA wires, with embedded lengths of 12.5mm and 25mm, embedded within the $[0_2/90_2]_s$ and $[0_2/\pm 45]_s$ carbon/epoxy lay-ups and at temperatures ranging from 22°C up to 108°C. Each embedded length of material was located between plies 1 & 2.

- When exposing the specimen to an elevated temperature, the trend of an increased pull-out force, required to fracture the interface, was witnessed. This trend was shown regardless of embedded length.

Owing to the nature of the matrix properties at elevated temperature, this trend was not expected.

When subjecting the specimens to an elevated temperature macroscale thermal residual stresses will decrease.

- For both the lay-ups considered, within the outer plies that host the embedded inclusion, a significant transverse tensile residual stress exists at room temperature.
- Again, within the two outer plies, for the $[0_2/90_2]_s$ lay-up, a compressive longitudinal residual stress exists at room temperature. For the $[0_2/\pm 45]_s$ lay-up, the longitudinal residual stress is tensile.
- While the compressive stresses will act in favour of adhesion, the low temperature tensile stresses are detrimental in nature.

Application of a tensile load to the SMA wire results with a local tensile loading within the neighbouring host. As the tensile thermal residual stresses were of greatest magnitude during the 22°C tests, the superposition of such stresses results with lower applied pull-out forces to fracture the interface. Alleviation of the tensile thermal residual stresses, during the higher temperature tests, necessitates higher pull-out forces to cause the same interface fracture.

It is expected that the interface shear strength would decrease sharply as the test temperature approaches the glass transition temperature of the constituent surrounding the SMA inclusion. Owing to the small deviation in the required pull-out force between the 85°C and 108°C, it is assumed that, with further increase in temperature, the interface quality may soon be on a significant downward trend.

- To generate useful recovery forces from the embedded 0.3mm diameter actuators, they will have to be subjected to temperatures between 80°C & 100°C. For the lay-ups chosen, it may be conceivable to say that such an operating temperature provides an optimal interface strength.

At the high operating temperatures proposed, viscoelastic effects around the interface region may become important. Future work should examine;

- The importance of such viscoelasticity with respect to required operating time scales.

This investigation has not involved the ultimate strength reduction or stiffness reduction local to the inclusions. This is because of the relatively low volume fraction of SMA employed and the magnitude of the applied compressive load required to cause the desired post-buckling response. Additional parameters, particularly related to the host composite material, need investigation. Such parameters include;

- Void growth and distribution surrounding the embedded wires.
- The detrimental effects on the overall mechanical properties of the hybrid system.

Such investigations will either qualify or impair the reliability of the envisioned adaptive hybrid systems.

To gain an appreciation for each of the specimens mechanical response, the buckling and post-buckling behaviour of the symmetrically laminated orthotropic and anisotropic plates and panels were numerically determined and experimentally investigated. This was then followed by the adaptive post-buckling investigation.

2-D Finite element linear buckling and structural non-linear numerical procedures were successfully employed to predict the specimens mechanical response to an in-plane uniaxial compressive load. Finite element analysis was employed as no closed form analytical solution exists that will enable the accurate prediction of the specimens non-linear anisotropic adaptive response. MSC/NASTRAN was employed for structural modelling purposes.

Linear buckling analysis was first conducted to ascertain the first two buckling loads and mode shapes. It is important to ascertain the second buckling value as energization of the SMA actuators at a load level above P_{c2} can cause modal instability. The experimental buckling load was obtained by the point of inflection method.

- Given the difficulties in determining the experimental buckling load, the experimental results have shown, in most cases, reasonable agreement with those predicted. There are, however, conspicuous exceptions.
- No buckling was witnessed for plate specimen configuration number 1. Instead, rapid out-of-plane deformation was recorded with the onset of loading. The exact cause of which was not ascertained.
- For the plate specimens of *control strategy 1*, on average, the experimental buckling loads were higher than those predicted. This may suggest an element of clamping in the fixtures. It was thoroughly assessed throughout the testing phase but could not be quantified. The most extreme discrepancies occurred for plate specimen configuration numbers 5 & 7 where the experimental buckling values, P_{imp} , are seen to be approximately 22% and 33%, respectively, higher than the predicted values of the finite element model.
- For the anisotropic panel, specimen configuration number 11, the linear buckling FEA significantly over estimated the buckling load. This is understandable as the analysis does not represent the real response which features quite significant non-linear bending and pseudo-buckling. For this configuration, the buckling load is better approximated by non-linear FEA.

Pertaining to the non-linear mechanical response devoid of shape memory adaptation, with the exception of the results close to the predicted buckling loads;

- The experimental results have shown the general trend of the behaviour predicted.
- For the two panel specimens, the numerical results show higher post-buckling stiffness curves. This is the likely cause of the continuous account of the glass insulting weave over the central 110mm of the specimen's FE representation. The glass weave is only located over discrete regions local to the constrained SMA actuators. The continuous nature will undoubtedly stiffen the structure.

Pertaining to the specimens adaptive response, subjecting the SMA wire actuators to an electrical stimulus results with resistive heating. Such heating imposes a non-uniform temperature profile over the laminates. The temperature profiles within each of the specimens have been numerically evaluated so that an account can be made of their effect within the non-linear analysis. These numerically determined thermal profiles have been experimentally verified.

Based on the assumption of a uniform temperature distribution in a direction parallel to the SMA wires, a 2-D model was used to predict the specimens cross-sectional temperature profile. The SMA orientation being normal to the 2-D representation. The model accounting for both forced and free convection. P3/Thermal was employed for the analysis.

For the actual tested specimens, however, the thermally uniform 2-D assumption will be unfound at the loading rig edge supports. Such supports acting as heat sinks.

- For *control strategy 1*, the error in the predicted temperature is predominant along the knife edge supports, whereas, for *control strategy 2*, the predicted temperature error is predominant along the loading platens.
- The effect of the *heat sink* boundary conditions will be a temperature profile that is more elevated at the centre of the plate than along the surrounding supported edges.
- Due to the formation of compressive membrane force intensities, spatial temperature gradients may induce out-of-plane plate/panel deformation.
- The extent of temperature error was not fully accounted for and is a proposal for future work.

For the activation strategies proposed, transient and steady-state temperature profiles have been numerically determined. Transient results were output at various time increments. From such results, relevant macroscale thermal stress and deformation profiles were computed.

- The first batch of fabricated plates contained a low volume fraction of SMA actuators centrally located within the specimens. Upon SMA energization, a highly non-uniform temperature profile forms over the plate surface.
- In an attempt to create a more uniform temperature profile, a higher volume fraction of SMA wires, with greater distribution, were located throughout the laminate. Here, the transient response exhibits a more uniform temperature distribution, though, its non-uniformity is still conspicuous with the major non-uniformity at the plates loaded edges.

The thermal response will have two domineering effects on each specimen. One, it will affect the macroscale thermal stresses within the composite layers and two, it will influence the material properties.

Regarding the macroscale thermal stresses, the stress distribution over the surface area of the specimens entails compressive as well as tensile stresses, equilibrium being maintained. They arise even when the plate edges are unrestrained and, therefore, free to deform in-plane. The compressive stresses are particularly important to ascertain as they can rise to such an extent that they initiate buckling and/or promote post-buckling.

- The magnitudes of such force intensities vary as a result of the lay-up's differing global coefficient of thermal expansion.
- A more even distribution of energized SMA actuators results with a significant drop in the compressive force intensities local to the central region of the specimens.

The matrix dominated material properties of the laminated specimens are sensitive to changes in temperature.

- T_g for the Fiberdux 913 is 131°C. To ensure that the matrix remains in the glassy state and that the associated temperature dependent properties are not significantly degraded, the manufactured specimens are not thermally loaded above 100°C.

It was the intention to account for the matrix-dominated property degradation in our non-linear finite element analysis. Within MSC/NASTRAN, the use of the PCOMP property cards, assigned to the 2-dimensional CQUAD4 elements, do not, however, allow for material temperature dependency. The material temperature dependency can only be accounted for when using numerically expensive solid elements that reference appropriate solid element property cards.

- Good experimental and numerical agreement, however, can be achieved when ignoring the effects of temperature on the material properties.

Within this study, owing to the large percentage of zero directional plies parallel to the loading direction within each of the manufactured plates and within the current operating time scales, viscoelastic or creep effects of the specimens can be ignored.

Over the temperature range encountered in our experiments, the temperature dependency of the listed thermal properties has also been ignored. It is possible, however, that the thermal conductivity will actually increase slightly with elevated temperature.

Further work should quantify the affects of material temperature dependency and viscoelasticity.

Throughout the tests conducted, the control was a simple on-off type action. The on-action was sent once the loading state reached the prescribed value. When wishing to control SMA/PMC specimens electrically, further work should also seek to reduce the thermal effects within the host specimen. This could be accomplished with;

- The provision of an electrical pulse to power the SMA material. The electrical pulse will be on for shorter periods of time, therefore, minimising the amount of thermal energy propagating through the host.
- The utilization of higher thermally conductive carbon reinforcement. A high thermal conductivity is desirable as this will help reduce local hot spots, thus, alleviating high stress gradients. This may, however, have cost implications.

Regarding the specimens adaptive post-buckling response, **Table 8.1** tabulates key results that were first presented in **Chapter 6**. Within **Table 8.1** the experimental readings, the FEA prediction and their associated percentage difference are presented at three earlier defined characteristic adaptive post-buckling phases. *Phase 1* is associated with the specimens mechanical loading to approximately $3P_c$, *phase 2* commences with SMA energization within

Specimen Configuration	Lay-up	Phase 1 Results				Phase 2 Results				Phase 3 Results				Phase 2 EPBDA ^z	Phase 3 EPBDA ^z	Load kN	Number of SMA Wires
		Exp. mm	FEA mm	diff. ^φ %	Exp. mm	FEA mm	diff. ^φ %	Exp. mm	FEA mm	diff. ^φ %	Exp. mm	FEA mm	diff. ^φ %				
1	[0 ₂ /90 ₂] _s	-2.79	-2.60	-7.3	-1.67	-1.58	-5.7	-1.84	-1.75	-5.1	-1.84	-1.75	-5.1	40.1	34.1	1.9	10 ^α
2	[0 ₂ /±45] _s	-2.24	-2.47	+9.3	-1.14	-0.70	-62.9	-2.45	-2.11	-16.1	-2.45	-2.11	-16.1	49.1	+9.4	1.9	10 ^α
3	[0/±45/90] _s	-2.93	-3.04	+3.6	-1.95	-1.98	+1.5	-2.11	-2.23	+5.4	-2.11	-2.23	+5.4	33.2	28.0	2.5	12 ^α
4	[0 ₂ /90 ₂] _s	-3.01	-2.66	-13.2	-1.02	-0.80	-27.5	-1.14	-0.96	-18.7	-1.14	-0.96	-18.7	66.1	62.1	1.9	12
4 ^β	[0 ₂ /90 ₂] _s	-	-	-	-1.38	-1.45	+4.8	-1.69	-2.03	+16.7	-1.69	-2.03	+16.7	54.2	43.9	1.9-2.5	12
5	[0 ₂ /±45] _s	-2.10	-2.54	+17.3	-0.38	-0.54	+29.6	-1.27	-1.37	+7.3	-1.27	-1.37	+7.3	74.3	41.3	1.9	12
6 ^γ	[0/±45/90] _s	-2.71	-3.23	+16.1	-1.17	-	-	-	-	-	-	-	-	56.8	-	2.5	14
7	[0 ₂ /90 ₂] _s	-2.93	-2.80	-4.6	-1.40	-1.27	-10.2	-1.58	-1.31	-20.6	-1.58	-1.31	-20.6	52.2	46.1	1.9	16
7 ^β	[0 ₂ /90 ₂] _s	-	-	-	-1.90	-2.13	+10.8	-2.56	-2.80	+8.6	-2.56	-2.80	+8.6	35.2	12.6	1.9-2.87	16
8	[0 ₂ /90 ₂] _s	-2.81	-2.66	-8.6	-0.80	-0.57	-40.4	-0.86	-0.57	-50.9	-0.86	-0.57	-50.9	71.5	69.4	1.9	18
8 ^β	[0 ₂ /90 ₂] _s	-	-	-	-1.72	-1.87	+8.0	-2.32	-2.56	+9.4	-2.32	-2.56	+9.4	38.8	17.4	1.9-2.91	18
9	[0/±45/90] _s	-2.66	-2.40	-10.8	-0.80	-0.98	+18.4	-0.90	-0.90	+9.1	-0.90	-0.90	+9.1	69.9	66.2	1.9	17
10	[0 ₂ /90 ₂] _s	1.25	1.11	-12.6	1.07	0.87	-23.0	1.39	1.46	+4.8	1.39	1.46	+4.8	14.4	+11.2	2.1	23
11	[0 ₂ /±45] _s	1.66	1.48	-17.6	1.61	1.41	-14.2	1.82	1.36	-33.8	1.82	1.36	-33.8	3.1	+9.6	2.5	23

Table 8.1. *Experimental and finite element non-adaptive and adaptive post-buckling results.*

^α Alloy composition = 49.8-50.0 at.% Ni. The un-cited actuators have the higher at.% Ni alloy composition.
^β Specimens tested in displacement control. The un-cited specimens were tested in load control.
^z EPBDA = Experimental post-buckled deflection alleviation.
^φ The percentage difference between FEA and experimental results is obtained from the equation; 100*{(FEA-Exp.)/FEA}.
^γ The occurenc of modal instability precluded substantive testing.

the loaded specimens and *phase 3* is associated with the energised SMA/PMC response to the thermal energy propagation until steady-state conditions prevail.

For each specimen, the percentage reduction in the experimentally monitored post-buckling out-of-plane-deflection at the end of *phases 2 & 3* are also presented.

- For *control strategy 1*, i.e. specimens 1-9, activation of constrained pre-strained SMA wire actuators, located on the laminated plates neutral plane, can result with a significant plate post-buckled deflection alleviation while under the influence of an external compressive load approximately three times the critical buckling value.
- While not as effective as *control strategy 1*, the concept behind *control strategy 2* has been shown to work, specimen configurations 10 & 11. Its efficient, or optimal, utilization, however, has yet to be demonstrated.
- For all the specimen configurations, the resulting SMA restoration forces act to pull the plate back to the flat configuration. This is true even for low SMA volume fractions. In some cases, however, this will result with post-buckled instability, specimen configuration number 6.
- During *phase 3*, the increase in lateral deformation is attributed to both the degradation in the matrix dominated material properties associated with exposure to the elevated temperature and to the non-uniform thermal stressed state set-up across the plate. At the end of *phase 3*, the plate deflection tends to its constant value as the temperature profile within the laminate approaches the steady-state.
- It is not entirely the N_y membrane force distribution that causes the proliferate deflection increase. Predominantly, it is the resulting self-equilibrating thermal N_x compressive force intensity.
- The stability of the adapted shape is dependent upon the laminate stacking sequence. Due to the elevated temperatures required for SMA transition, the stacking sequence chosen should be such that temperature effects have minimal influence on the structural performance.
- During *phase 3*, the proliferate deflection behaviour associated with the anisotropic lay-up, specimen configurations 2 & 5, is attributed to the global coefficient of thermal expansion. Such prominent affects could be overcome by increasing and uniformly distributing the SMA volume fraction.
- When loading the specimens in displacement control, with SMA energization, the out-of-plane displacement alleviation is not so prominent.
- When testing in displacement control, a rise in the recorded load results with fixed cross head restraint against the lateral force imposed on the post-buckled plate by the actuators and the specimens thermal response.

- Actual recovery force measurements, for a particular test environment, can provide a reasonable means of predicting the adaptive structural behaviour. Specimen configuration number 11 being an exception.
- The actual power level applied to energise the actuators falls short of the amount required for complete phase transition, therefore, only partial actuation results. None the less, significant control authority is evident. Increasing the power level further, enabling greater restoration forces, will subject parts of the laminate to a temperature in excess of 100°C. Not what is desirable for such a host material.
- Obviously, utilizing the higher recovery forces for the higher at.% Ni alloy actuators and the slightly higher volume fraction results with greater post-buckled displacement alleviation.
- The remaining partial recovery force, post-cycle 1, for the 50.0-50.2 at.% Ni alloy will provide specimen lateral restraint even when the power is removed.
- Restoration recovery forces not only reduce the peak displacement amplitude, they also alleviate high stress levels at the boundary supports that are typical to post-buckled plate configurations. The tendency of the adaptive plate is to redistribute the loading back towards the plates central region, such that, a more uniform stressed state exists.
- In particular to the anisotropic lay-up, specimen numbers 2 & 5, as the specimens thermal response, attributed to SMA energization, tends to the steady-state, the resulting thermally induced stresses act to significantly increase the lateral deformation such that the resulting thermal and mechanical in-plane force intensity profile once again exists in a state of high non-uniformity. No benefit is associated with the electrical energization of the parsimonious distribution of SMA actuators, i.e. specimen configuration number 2.
- Application of the thermal influence only, associated with SMA actuation, to the mechanically post-buckled plate specimen can result with a significant increase in the peak out-of-plane displacement.
- SMA utilization would certainly be of benefit when such components are subjected to the thermal environment by means other than electrical energization. As an example, heating, associated with skin friction, may be sufficient to drive the actuators through their phase transition such that they exert stabilising recovery forces on the skin sections of high speed aircraft.
- Such strategies may be employed to improve the specimens anticipated life by alleviating critical stresses through the enforcing of a more uniform load path. Such load uniformity is characteristic of the pre-buckled configuration.
- Post-buckling alleviation may also be advantageous for wing skin sections where buckling may not be permissible for aerodynamic performance reasons.
- The strict requirement for carefully clamped SMA structural boundary conditions may hinder the implementation of *control strategy 1* within conventional airframe design. The

internal anchoring of the embedded SMA wires against the natural boundary conditions of the host structure, *control strategy 2*, is likely to be more acceptable.

As with earlier citations in this thesis, the findings of this investigation can only highlight the potential for smart polymeric-matrix composite structures within the aeronautics community. The SMA/PMC adaptive post-buckling concepts do require further work. This is particularly true for *control strategy 2*. Further work should thoroughly assess;

- The interaction of the cooler stiffener and the warmer skin. Due to time constraints, it has not been assessed.
- The required SMA volume fraction that would significantly alleviate the post-buckled deformations.

For both control strategies, further work could encompass;

- Testing the specimen in an environmental chamber, therefore, omitting the necessary electrical energization and alleviating the effects of the non-uniform temperature profile.
- The utilization of a numerical procedure that can efficiently account for the temperature dependent properties of the host polymeric-matrix composite.
- Additional parameters such as the number of plies, i.e. 16 plies, and the fixtures employed, i.e. clamped boundary conditions.

It is the need for future aerospace structures to have increased performance, efficiency and reliability which has boosted research in passive and smart polymeric-matrix composites. It is foreseen that such structures will be lighter in weight and, therefore, consume less energy to operate.

The performance benefits of smart polymeric-matrix composites materials, however, must carefully be assessed against issues of technical risk, producability, maintainability, reliability, and, of course, cost. Improved performance must be at an affordable price.

The utilization of smart PMC materials will probably be too expensive for the mass PMC customers. They will first be incorporated into components that require the highest level of performance, i.e. high performance aircraft or science based space missions.

It is the second generation supersonic civil aircraft market that could best exploit SMA/PMC components within its design. To be economically and environmentally successful, a second generation supersonic civil aircraft will require exceptionally high structural efficiency. This will only be realised with innovative design and widespread utilization of advanced PMC materials. Foreseen structural applications for the use of SMA/PMC's are the wing skins, control surface skins, vertical & horizontal tail sections and a major portion of the fuselage.

The operating environment at supersonic cruise conditions can provide the necessary energy required for SMA phase transition, therefore, enabling the structure to tailor itself so that it is best suited to operate in the mechanical and thermal environment. Such self-adaptation eliminates the need for the electrical input power stimulus which, in turn, would make savings on mass and maintenance of its necessary control system.

This class of SMA/PMC material could be classed as environmentally responsive. Such materials and structures can respond to environmental changes, i.e. temperature, and, thus,

autonomously adjust themselves toward their optimum operating condition. The system may be completely devoid of any analogue or digital sensing interrogation techniques, therefore, minimising the complexity of the system. Such minimal complexity adding weight in their favour.

One final note - It is the author's own impartial view that such smart, adaptive or environmentally responsive shape memory alloy/polymeric-matrix composites will find their way into future aeronautical and space systems where the enhanced performance characteristics will greatly outweigh the procurement cost implications.

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Appendix A

A Shape Memory Alloy Constitutive Equation

A.1 Introduction

Within this appendix, a one-dimensional constitutive law that represents the physical behaviour of an SMA actuator, in particular, an SMA wire, is summarised. During the early stages of this research programme, it was initially thought that the employment of such an SMA constitutive law within the adaptive post-buckling modelling would be required. The data obtained during actual specimen testing, **Chapters 2 & 6**, however, provided adequate input for the finite element analysis. SMA constitutive modelling, therefore, was not called upon.

It is proposed that future work should employ such constitutive modelling as this can eliminate the need for a *costly* test programme. This appendix is presented as it provides reference to literature in the public domain essential to SMA modelling in which the reader may wish to obtain.

A variety of constitutive models have been developed with the majority aimed at describing the one-dimensional material behaviour. The model presented here is able to quantitatively describe the behaviour of the SMA fully constrained recovery and controlled recovery response^{158,159,160}.

The model described presents the SMA in a very simple form, therefore, making it easy to use in engineering design¹⁶⁰. The material parameters required by this model can be calculated or measured using standard material testing and apparatus.

While it is stated that the computed results using such a constitutive equation are in agreement with the experimental data¹⁵⁸, it should be noted that no single model exists that is able to describe accurately the shape memory performance of a real SMA material. The major complicating factors that prevent accurate modelling are the material's dependency on composition, processing, microstructure and the thermo-mechanical history.

A.2 The Governing SMA constitutive equation

The stress, strain, temperature and martensite fraction can provide a complete set of variables for the SMA modelling¹⁶¹. The most simple form of the one-dimensional SMA constitutive equation is given by^{160,162};

$$\bar{\sigma} - \bar{\sigma}_0 = D(\bar{\epsilon} - \bar{\epsilon}_0) + \theta(T - T_0) + \Omega(\xi - \xi_0) \quad (\text{A.1})$$

where D is the one-dimensional Young's modulus, θ is the thermo-elastic tensor, Ω is the transformation tensor, ξ is the martensitic fraction and the variables with subscript "o" denote the initial conditions. As SMA's generally involve large deformations, the Green strain, $\bar{\epsilon}$, and Piola-Kirchoff stress, $\bar{\sigma}$, are employed.

The transformation tensor, Ω , is associated with SMA volume change with phase transformation¹⁵⁸. It is related to the maximum recoverable strain limit, ϵ_L , or *domain width*,

Section 2.4. The recovery strain limit can be determined by tensile testing at a temperature $T < A_s$, **Figure 2.2**. The limit strain of the plateau where $\xi=1$, is the maximum strain that can be recovered on heating after unloading the specimen. Knowing ε_L and D , then, the transformation tensor, Ω , can be calculated using the equation;

$$\Omega = -D_M * \varepsilon_L \quad (A.2)$$

The thermo-elastic tensor, θ , is related to the thermal expansion coefficient¹⁶³ and can be measured directly from a restrained recovery test at a temperature above A_s ⁵⁶. The measured parameter of stress, over the required temperature range, allows for the determination of θ . θ is determined from;

$$\theta = \sigma / (A_s - T) \quad (A.3)$$

θ generally has a value approximately equal to 0.1 MPa/deg C¹⁶⁰. The value is different for martensite and austenite but is much smaller than the other material constants such that it may be assumed constant¹⁵⁸.

During SMA phase transition, the Young's modulus of the material varies significantly. A reasonable assumption for the modulus variation is suggested as¹⁶⁴;

$$D = D_A + \xi(D_M - D_A) \quad (A.4)$$

where D_M is the Young's modulus when the SMA is 100% martensitic and D_A is the Young's modulus when the SMA is 100% austenitic. Equation (A.4) is derived from a rule of mixtures type approach¹⁶⁴.

The martensitic fraction, ξ , is the quantity of the SMA material in the martensite phase. $\xi=0$ indicates that the material is fully austenitic, while $\xi=1$ indicates that the material is fully martensitic. The martensite fraction is known to change as a function of both stress, σ , and temperature, T . A variety of laws have been proposed to predict the martensite fraction, $\xi(\sigma, T)$. Such laws include the use of linear, exponential or cosine functions. The empirically based cosine law is presented as an example^{160,162};

During the martensite to austenite transformation;

$$\xi = \frac{\xi_M}{2} \{ \cos[a_A(T - A_s) + b_A\sigma] + 1 \} \quad (A.5)$$

where

$$a_A = \frac{\pi}{A_f - A_s}, \quad b_A = -\frac{a_A}{C_A} \quad (A.6)$$

Similarly, during the austenite to martensite transformation;

$$\xi = \frac{1 - \xi_A}{2} \cos[a_M(T - M_f) + b_M\sigma] + \frac{1 + \xi_A}{2} \quad (A.7)$$

where

$$a_M = \frac{\pi}{M_s - M_f}, \quad b_M = -\frac{a_M}{C_M} \quad (A.8)$$

In these equations, C_A and C_M represent material constants of the SMA material which determine the influence of stress on transition temperatures¹⁶², **Section 2.5.2**.

In describing the constrained or recovery response of SMA materials, incremental constitutive equations are used¹⁶². Such incremental constitutive equations are different for cooling and heating. The following discussions, pertaining the SMA constrained and recovery response, is devoted to heating.

A.3 Constrained Recovery

For constrained recovery, i.e. *control strategy 1*, there is no change in strain. The recovery stress, $\bar{\sigma}'$, is a function of temperature and martensite fraction only. The constitutive equation (A.1), therefore, becomes;

$$\bar{\sigma}' - \bar{\sigma}'_0 = \theta(T - T_0) + \Omega(\xi - \xi_0) \quad (A.9)$$

The initial temperature $T_0 = T_M$ and can be obtained through manipulation of equation (A.5) such that;

$$T_M = M_f + \frac{\cos^{-1}[2(\xi_M - 0.5)]}{a_M} \quad (A.10)$$

Within the temperature range $T_M < A_s$, there is no new austenite. The constrained recovery stress temperature relation, equation (A.9), is linear and given by;

$$\bar{\sigma}' - \bar{\sigma}'_0 = \theta(T - T_M) \quad (A.11)$$

The initial stress $\bar{\sigma}'_0$ is assumed to be zero at $T_0 = T_M$. If stress is present then the austenite start temperature will rise, **Section 2.5.2**. The new austenitic start temperature can be described as follows¹⁶⁰;

$$A_s^m = \frac{C_A A_s - \theta T_M}{C_A - \theta} \quad (A.12)$$

Substituting A_s^m into equation (A.11) yields;

$$\bar{\sigma}'_{A_s} = \theta(A_s^m - T_M) \quad (A.13)$$

If the ambient temperature is higher than A_s^m , then, there is new austenite and the constitutive relation has the form of equation (A.9) where the initial conditions are¹⁶⁰;

$$\bar{\sigma}_o = \bar{\sigma}_{As} \quad T_0 = A_s^m \quad \xi_0 = \xi_M \quad (\text{A.14})$$

Substituting these initial conditions into equation (A.9) and with algebraic manipulation yields the constrained recovery response as¹⁶⁰;

$$\bar{\sigma}' - \bar{\sigma}_{As} = \theta(T - A_s^m) + \frac{\Omega}{2} \frac{\bar{\epsilon}_{res}}{\bar{\epsilon}_L} \left(\cos \left[a_A(T - A_s) + b_A \bar{\sigma}' \right] - 1 \right) \quad (\text{A.15})$$

The above stress-temperature relation is not explicit, for a given temperature, iteration is required for convergence. It is applicable at temperatures below M_d and the complete austenite finish temperature.

A.4 Controlled Recovery

Controlled recovery is defined as the shape memory recovery process in which some recovery strain is allowed during the heating cycle. When the SMA actuators are embedded within the composite panel specimen and constrained to the host epoxy, *control strategy 2*, the amount of strain recovery is dependent upon the stiffness of the surrounding host.

The controlled recovery process is as follows. When the pre-strained SMA wires are heated, the recovery force, generated by the shape memory effect, “pulls” on the panel such that it stores energy, much like pulling on a spring. When the SMA actuators are cooled, the potential energy stored in the panel is released to strain the SMA wires back to their initial position. This results with a deformation-reformation cycle.

The SMA/host, *control strategy 2*, can best be described by this bias spring behaviour. An important parameter is the *bias spring*, or stiffness constant¹⁶⁵.

Consider the spring/SMA system as shown in **Figure A.1**. If the spring constant is k , the length of SMA wire L and the SMA wire cross-sectional area is s , then the stress-strain relation for the spring is described by;

$$\bar{\sigma}' - \bar{\sigma}_o = \frac{kL}{s} (\bar{\epsilon}_{res} - \bar{\epsilon}') \quad (\text{A.16})$$

The greater the value of kL/s , the higher the resulting recovery stress. The controlled recovery stress will approach the restrained recovery stress which corresponds to an infinite spring constant, k . Also the larger kL/s , the higher the temperature that will be required to finish the transformation due to the increase in stress¹⁶⁵.

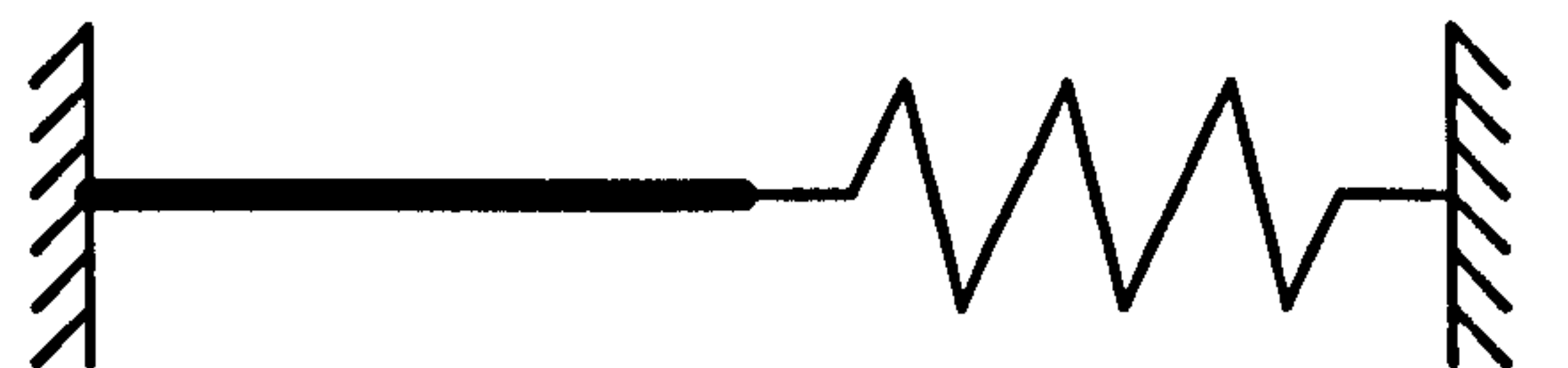


Figure A.1 Simplified SMA force actuator.

Substituting equation (A.16) into the constitutive equation, equation (A.1), yields¹⁶⁵;

$$\left(1 + s \frac{D}{kL} \right) (\bar{\sigma}' - \bar{\sigma}_o) = \theta(T - T_0) + \Omega(\xi - \xi_0) \quad (\text{A.17})$$

this equation being handled in the same way as constrained recovery, **Section A.3**¹⁶⁰.

Appendix B

Carbon/Epoxy Mechanical Property Validation

B.1 Introduction

This appendix is concerned with the experimental measurement of some basic composite mechanical properties that are employed in the present analysis. The tensile method for the measurement of unidirectional lamina properties such as strength, stiffness and Poisson's ratio's for a particular unidirectional lay-up have been employed.

Composite suppliers provide data sheets for their materials. These data sheets, however, should be verified for the cure cycle used. Such verification generally being the mechanical determination of E_{11} , E_{22} , ν_{12} and ν_{21} . Tension tests only have been considered as they are easier and cheaper than compression tests. It should be pointed out that tension and compression experiments usually give slightly different results because the composite stiffness results from different mechanisms. In the tension case the fibres are stretched, whereas in compression, the fibres have a tendency to bend.

Method of test for the longitudinal tensile strength and modulus of unidirectional fibre reinforced plastics (Method 300) and Method of test for the transverse tensile strength and modulus of unidirectional fibre reinforced plastics (Method 301) were employed, a descriptive account of these tests is provided for within the published literature¹⁶⁶.

B.2 Results

The specimens were prepared in accordance with the previously cited guidelines. Aluminium end tabs were employed for load transference. Uni-axial CEA-06-240UZ-120 strain gauges, purchased from Measurements Group Inc., were employed for monitoring purposes. The specimens were mounted in an Instron Universal Testing Machine with self-aligning jaws.

Typical longitudinal stress-strain curves for the two lay-ups considered are presented in **Figures B.1 & B.3**. The results show the typical fibre-dominated linearity for the longitudinal strain response of the $[0]_8$ lay-up and a slight non-linearity in the transverse strain response for the $[90]_{16}$ lay-up. Corresponding transverse-longitudinal strain behaviour are shown in **Figures B.2 & B.4**. The reported material properties, indicated on the appropriate figures, are in accordance with the values published by the manufacturer, **Table 3.1**.

All specimens were loaded to ultimate failure. As expected, upon failure, the unidirectional tensile specimens gauge exploded violently with a loud bang. The failure was characteristically brittle with excessive longitudinal splitting. Such longitudinal splits running along the length and through the thickness of the sample right up to the end tabs. Debris and fibre strands were emitted at the time of failure.

The failure of the transverse specimens occurred, in a brittle manner, at one location. The failed specimen having a failure crack parallel to the reinforcement such that the failed specimen comprised of two parts. Two of the specimens failed at the tag, the remaining specimens failing approximately one tag length away from the strain gauge location.

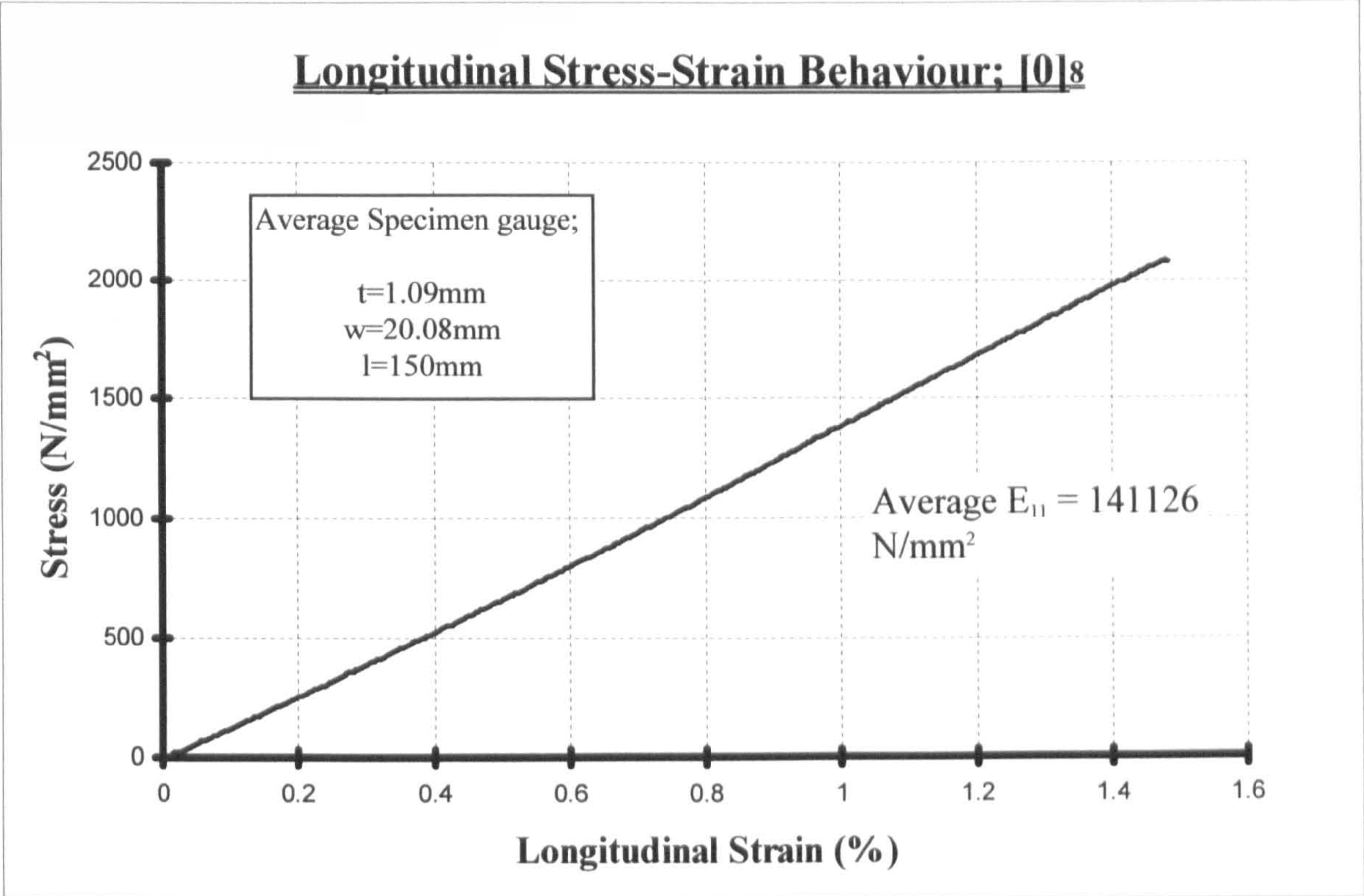


Figure B.1.

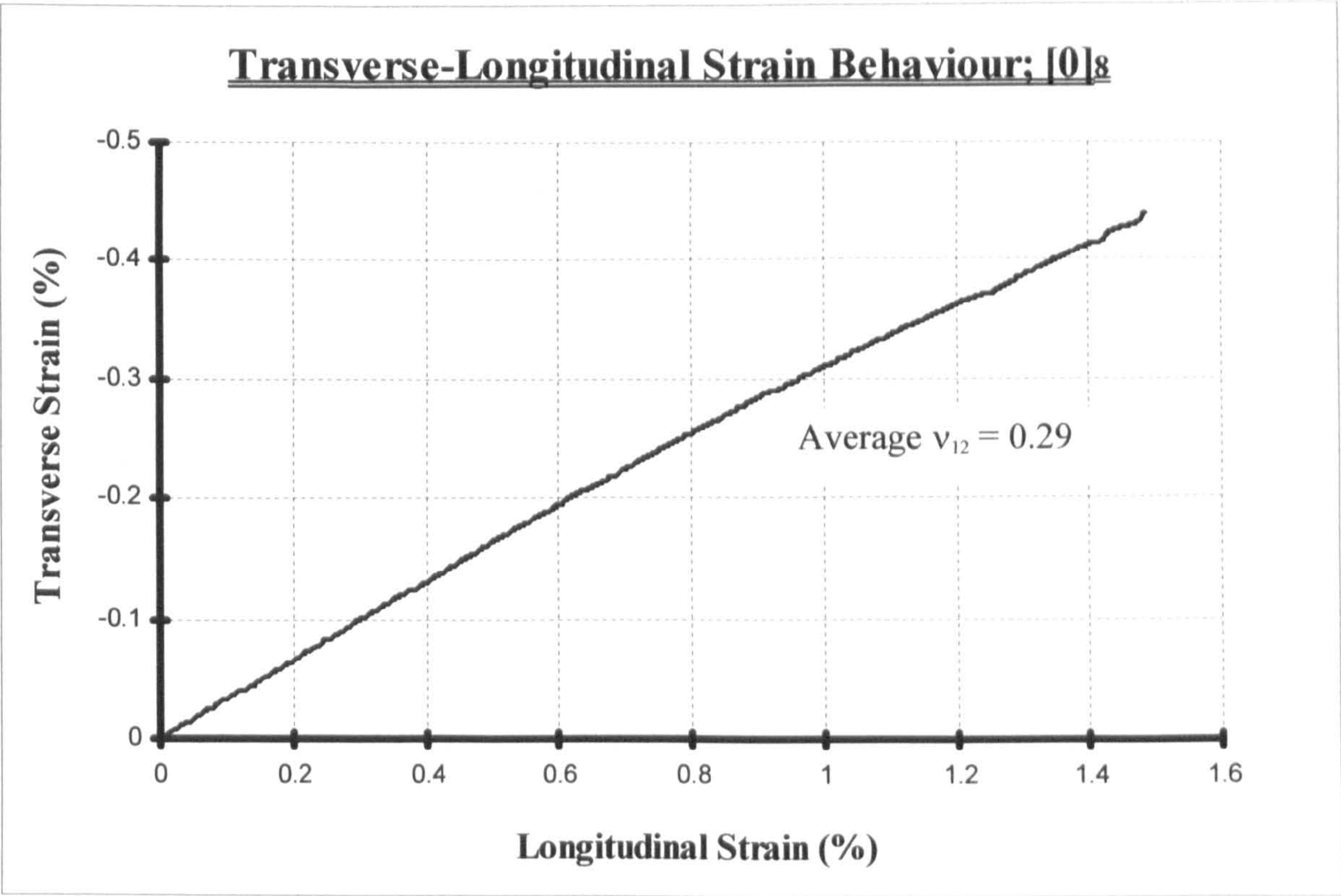


Figure B.2.

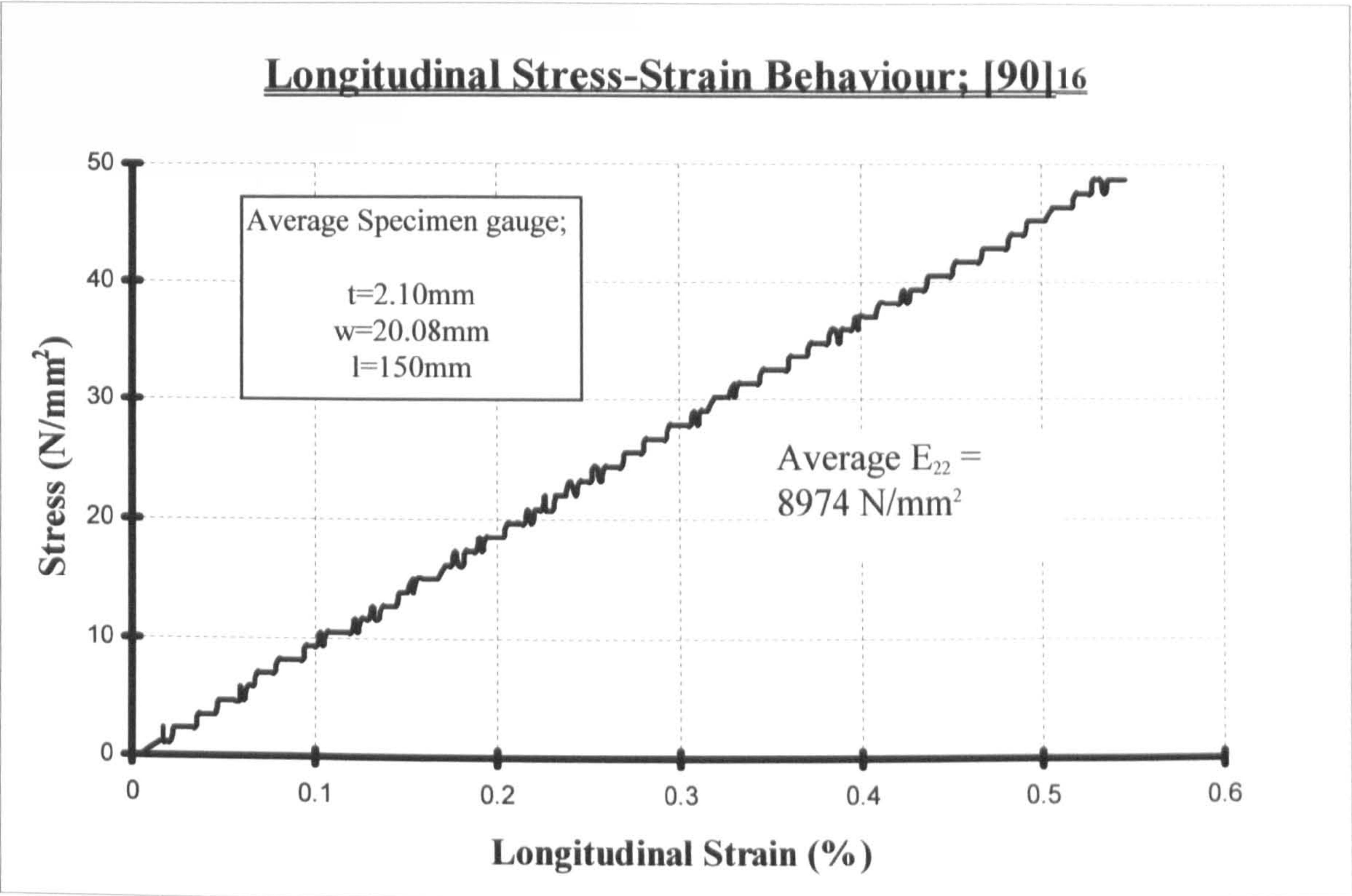


Figure B.3.

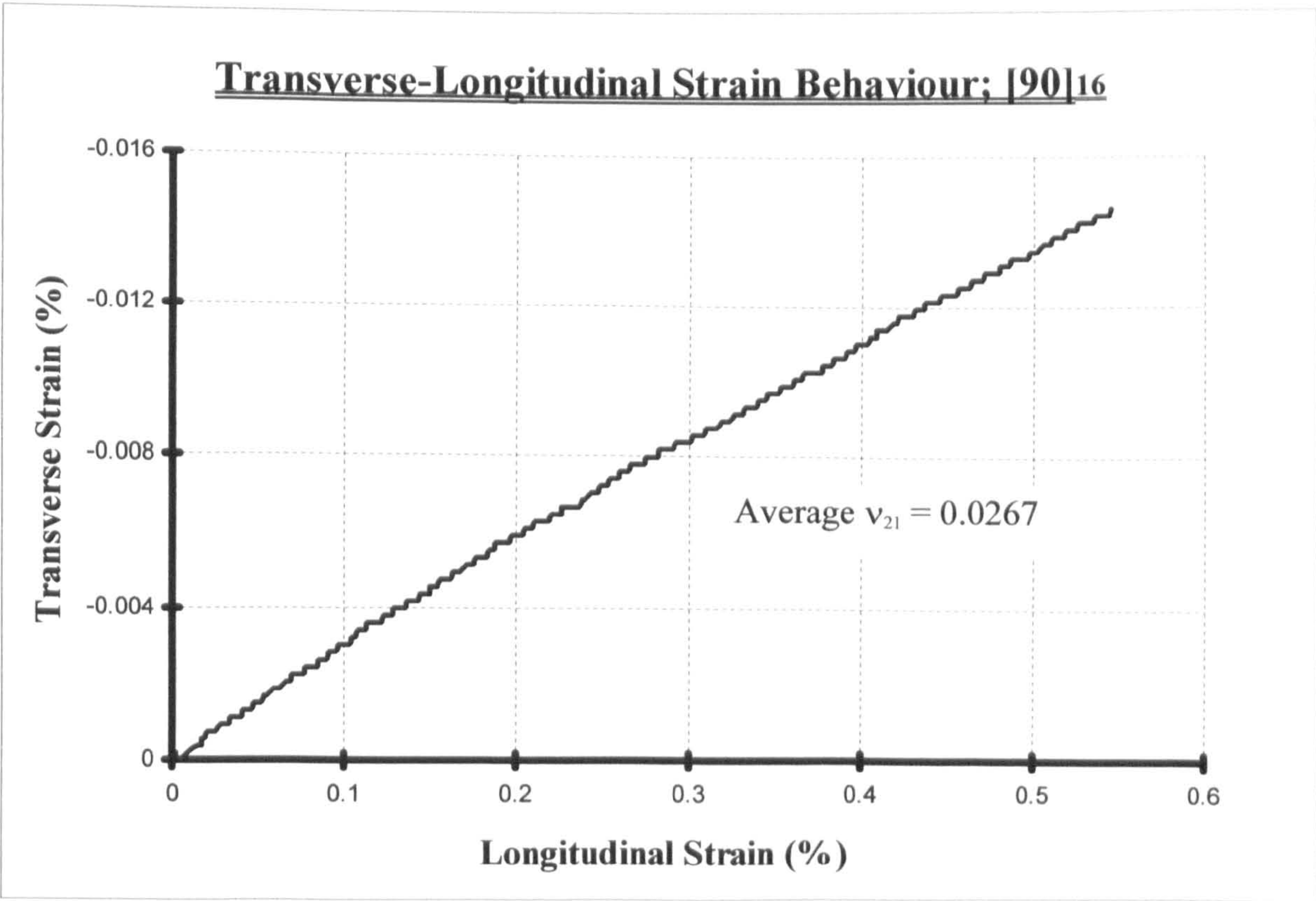


Figure B.4.