

Measurement of the Through-Thickness Strength of Composites

A Thesis submitted to the Department of
Engineering Science of Oxford University in
partial fulfilment of the requirements for the
degree of Doctor of Philosophy

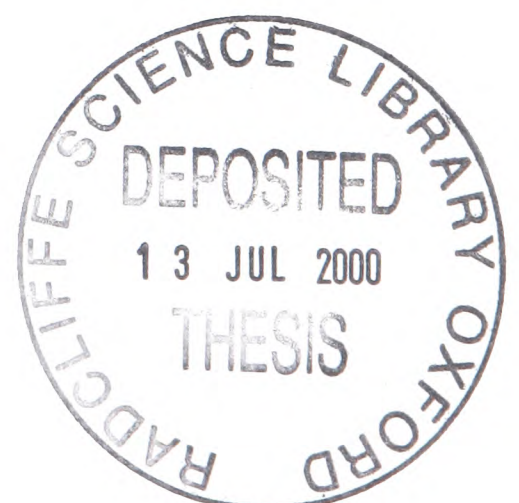
by

Shinro Taniguchi

St. Catherine's College

Oxford

Michaelmas Term 1998



Abstract

Shinro Taniguchi

D.Phil.

St. Catherine's College

Michaelmas Term 1998

Measurement of the Through-Thickness Strength of Composites

This research deals with the mechanical characterisation of thick composite laminates in the through-thickness direction. Three independent glass fibre/epoxy laminate configurations, namely cross, quasi-isotropic, and woven, plies were investigated. Six specimen configurations, of which two were developed herein, were employed in order to determine the strength behaviour of these three laminate configurations when subjected to interlaminar shear and interlaminar tensile stresses in isolation and in combination. The stress and strain distributions were estimated using the ABAQUS FEA package. The strain distribution obtained thereby was verified experimentally via Moiré interferometry which records the exact strain field at the test section. A two dimensional failure envelope is defined for each laminate configuration using the experimentally obtained data. All three independent laminate configurations exhibited almost identical failure envelopes. The woven laminate exhibited superior interlaminar shear strength when subjected to combined interlaminar shear and interlaminar tensile stresses, whereas the cross-ply laminate exhibited superior interlaminar tensile strength when subjected to the same combination of stress. The characteristics of the quasi-isotropic laminate were similar to that of the cross-ply laminate. A partial three dimensional failure envelope was also defined for the materials tested.

Preface

This thesis is an account of work which was carried out by the author at the Department of Engineering Science at the University of Oxford, under the supervision of Professor C. Ruiz. No part of this thesis has been submitted for a degree at any other university. The research described here is original, although the work of others has been drawn upon freely with due acknowledgements in the text.

Acknowledgements

The author would like to acknowledge the financial support of the Oxford-Kobe Scholarship given by Kobe Steel. The author wishes to thank Professor C. Ruiz for his supervision of this project. The assistance given by Mr P. Hardy, Mr P. Webb, Mr N. Warland, Mr R. Duffin in the University Technology Centre (UTC) for Solid Mechanics for manufacturing the test rigs was greatly appreciated. Thanks also to Mr. R. Moore in the grinding workshop for manufacturing all of the test specimens. I would also like to thank my colleagues of the UTC Solid Mechanics group for useful discussions and help. Finally, the author would like to recognise the encouragement and help of Dr S.P. O'Connell and the unwavering support of his family during the preparation of this thesis.

I thank you all.

S. Taniguchi

Oxford, November 1998

Nomenclature

The Evolution of Composites

σ	Interlaminar tensile stress in the through-thickness direction
τ	Interlaminar shear stress

Anisotropic Failure Theories

σ_1	In-plane stress in the fibre direction
σ_2	In-plane stress in the transverse direction
σ_3	Interlaminar stress in the through-thickness direction
τ_{12}	In-plane shear stress
τ_{31}, τ_{23}	Interlaminar shear stresses
X_c	In-plane compressive strength in the fibre direction
X_t	In-plane tensile strength in the fibre direction
Y_c	In-plane compressive strength in the transverse direction
Y_t	In-plane tensile strength in the transverse direction
Z_c	Interlaminar compressive strength

in the through-thickness direction

Z_t	Interlaminar tensile strength in the through-thickness direction
Q	In-plane shear strength
R, S	Interlaminar shear strengths
ϵ_1	In-plane strain in the fibre direction
ϵ_2	In-plane strain in the transverse direction
ϵ_3	Interlaminar strain in the through-thickness direction
γ_{12}	In-plane shear strain
γ_{13}, γ_{23}	Interlaminar shear strains
ϵ_1^c	In-plane maximum compressive strain in the fibre direction
ϵ_1^t	In-plane maximum tensile strain in the fibre direction
ϵ_2^c	In-plane maximum compressive strain in the transverse direction
ϵ_2^t	In-plane maximum tensile strain in the transverse direction
ϵ_3^c	Interlaminar maximum compressive strain in the through-thickness direction

ε_3^t	Interlaminar maximum tensile strain in the through-thickness direction		oblique specimen
Γ_{12}	In-plane maximum shear strain	σ_{max}	Maximum interlaminar direct stress
Γ_{13}, Γ_{23}	Interlaminar maximum shear strains	σ_{min}	Minimum interlaminar direct stress
F, G, H, L, M, N	Material strength parameters	τ_{max}	Maximum interlaminar shear stress
$i, j, k = 1, \dots, 6$	Notation for stress tensor quantity	τ_{min}	Minimum interlaminar shear stress
σ_i, σ_j	Stress tensor parameters	σ_1, σ_2	Interlaminar direct stresses
F_i, F_{ii}, F_{ij}	Strength tensor parameters	τ_{12}	Interlaminar shear stress
G	Strain energy release rate	α	An angle in the centrally notched beam specimen
G_c	Critical strain energy release rate	M	Bending moments
G_I	Mode I critical strain energy release rate	P	End forces
G_{II}	Mode II critical strain energy release rate	M_e	End moments
P	Applied load	d	An offset distance from the loading axis
The Design and Manufacture of Specimens		a	The inner radius of a plain curved beam specimen
		b	The outer radius of a plain curved beam specimen
		r	A radius in a plain curved beam specimen
		σ_r	Radial stress in a curved segment
		σ_θ	Circumferential stress in a curved segment
		$\tau_{r\theta}$	Shear stress in a curved segment
		Measurement of Material Response	
L	The length of a specimen	$i, j = 1, \dots, 3$	
b	The breadth of a specimen		
t	The thickness of a specimen		
φ	An angle of the waist in a wasted curved beam specimen		
θ	An oblique angle to the loading direction in a notched rectangular		

	Notation for the principal material direction	E_m	Matrix Young's modulus
1	Denotes the through-thickness direction	ν_{LT}	In-plane Poisson's ratio of unidirectional laminate
2	Denotes the 0° fibre direction	ν_{fLT}	Fibre Poisson's ratio
3	Denotes the 90° fibre direction	ν_m	Matrix Poisson's ratio
E_{11}	Young's modulus in the through-thickness direction	G_{LT}	In-plane shear modulus of unidirectional laminate
E_{22}	Young's modulus in the 0° fibre direction	G_{fLT}	Fibre shear modulus
E_{33}	Young's modulus in the 90° fibre direction	G_m	Matrix shear modulus
ν_{12}, ν_{31}	Poisson's ratios in the through-thickness direction	c, e	Applied load points for the Iosipescu test configuration
ν_{23}	In-plane Poisson's ratio	P	Applied forces
G_{12}, G_{13}	Shear moduli in the through-thickness direction	Analysis of Laminates	
G_{23}	In-plane shear modulus	U	Ultimate force
ρ_c	Density of composite	w	The distance between two notches of an Iosipescu specimen
ρ_f	Density of fibre	h	The thickness of an Iosipescu specimen
ρ_m	Density of matrix	σ_x	Applied stress to a notched rectangular oblique specimen
V_f, ν_f	Fibre volume fractions of composite	σ_L	In-plane stress in the notched rectangular oblique specimen
E_L	Young's modulus of unidirectional laminate in the longitudinal direction	σ_T	Interlaminar tensile stress in the notched rectangular oblique specimen
E_T	Young's modulus of unidirectional laminate in the transverse direction	τ_{LT}	Interlaminar shear stress in the notched rectangular oblique specimen
E_f	Fibre Young's modulus	θ	An oblique angle to the loading

direction in the notched
rectangular oblique
specimen

τ^* Ultimate shear strength
 ζ Interlaminar interaction coefficient
 σ_t^i Interlaminar tensile stress
 τ_t^i Interlaminar shear stress

Moiré Interferometry

x, y Reference geometric coordinate
 u Displacements in the x direction
 y Displacement in the y direction
 $M1, M2, M3$
Adjustable mirrors for a laser
optical equipment
 $A1, A2$
Incident beam pair for u
displacement fields
 $B1, B2$
Incident beam pair for v
displacement fields
 n Phase shifted interferogram
 I Intensity of an interferogram
 g Background intensity
 s Local contrast
 λ Phase to be determined
 δ Phase shift

Correlation and Interpretation of
the Results

σ Direct stress
 τ Shear stress
 σ^* Ultimate strength

General Abbreviations

DCB Double cantilever beam
ENF End notched flexure
ELS End load split
FE Finite element
FEA Finite element analysis
SEM Scanning electron microscope
913UD
Cross-ply laminate made of the 913
epoxy system
914NCF
Quasi-isotropic laminate made of
the 914 epoxy system
916W
Woven laminate made of the 916
epoxy system
IO Iosipescu specimen configuration
CNB Centrally notched beam specimen
configuration
CNB3 Centrally notched beam specimen
made from 913UD
CNB4 Centrally notched beam specimen
made from 914NCF
CNB6 Centrally notched beam specimen

made from 916W		configuration
<i>IO3</i>	Iosipescu specimen made from 913UD	<i>PCB3</i> Plain curved beam specimen made from 913UD
<i>IO4</i>	Iosipescu specimen made from 914NCF	<i>PCB4</i> Plain curved beam specimen made from 914NCF
<i>IO6</i>	Iosipescu specimen made from 916W	<i>PCB6</i> Plain curved beam specimen made from 916W
<i>NCB</i>	Notched curved beam specimen configuration	
<i>NCB3</i>	Notched curved beam specimen made from 913UD	
<i>NCB4</i>	Notched curved beam specimen made from 914NCF	
<i>NCB6</i>	Notched curved beam specimen made from 916W	
<i>NRP</i>	Notched rectangular prism specimen configuration	
<i>NRP3</i>	Notched rectangular prism specimen made from 913UD	
<i>NRP4</i>	Notched rectangular prism specimen made from 914NCF	
<i>NRP6</i>	Notched rectangular prism specimen made from 916W	
<i>ORP</i>	Oblique rectangular prism specimen configuration	
<i>ORP3</i>	Oblique rectangular prism specimen made from 913UD	
<i>ORP4</i>	Oblique rectangular prism specimen made from 914NCF	
<i>ORP6</i>	Oblique rectangular prism specimen made from 916W	
<i>PCB</i>	Plain curved beam specimen	

Contents

Chapter 1 The Evolution of Composites

1.1 Introduction 15

1.2 Applications 16

1.2.1 Civil Structures 16

1.2.2 Sports Equipment 16

1.2.3 Automotive 17

1.2.4 Industrial 17

1.2.5 Aerospace 18

1.3 Drawbacks Associated with the Use of Fibrous Composites 19

1.4 Research Objectives and Thesis Structure 21

References 22

Chapter 2 Anisotropic Failure Theories

2.1 Introduction 24

2.2 Synopsis of Failure Criteria 25

2.3 Commonly Employed Failure Criteria 26

2.3.1 The Maximum Stress Theory 27

2.3.2 The Maximum Strain Theory 28

2.3.3 The Tsai-Hill Theory	28
2.3.4 The Tsai-Wu Theory	29
2.4 The Fracture Mechanics Approach	31
2.4.1 Determination of Strain Energy Release Rate	32
2.5 The Empirical Development of the Failure Envelopes	34
2.6 The Applicability of the Various Failure Theories	34
2.7 Summary	35
References	37

Chapter 3 The Design of the Specimens

3.1 Introduction	41
3.2 The Design of Test Specimens	42
3.2.1 General Considerations	42
3.3 Current Testing Techniques	43
3.3.1 The Iosipescu Specimen	43
3.3.2 The Cylindrical Specimen	44
3.3.3 The Notched Rectangular Prism Specimen	45
3.3.4 The Hybrid Specimen	45
3.3.5 The Centrally Notched Beam Specimen	46
3.3.6 The Curved Beam Specimen (C-Shape)	47
3.3.7 The Four Point Bending Curved Beam Specimen (Ω -Shape)	47
3.3.8 The Waisted Curved Beam Specimen	48
3.3.9 The Hoop Specimen (U-shape)	48
3.3.10 The Rectangular Beam Specimen	49

3.3.11 The Rectangular Specimen with a Hole	50
3.4 Summary	50
3.5 The Specimens Selected for this Investigation	51
3.5.1 The Iosipescu Specimen	52
3.5.2 The Notched Rectangular Prism Specimen	52
3.5.3 The Centrally Notched Beam Specimen	54
3.5.4 The Plain Curved Beam Specimen	55
3.5.5 The Notched Curved Beam Specimen	56
3.5.6 General Abbreviations for the Specimens Selected	57
3.6 Materials Selection	58
3.7 Laminate Configurations	59
3.8 The Manufacture of the Laminates and Specimens	61
3.8.1 The Iosipescu Specimen	63
3.8.2 The Notched Rectangular Prism Specimen	64
3.8.3 The Centrally Notched Beam Specimen	66
3.8.4 The Plain and Notched Curved Beam Specimen	67
References	67

Chapter 4 Measurement of Material Response

4.1 Introduction	71
4.2 The Elastic Properties of the Laminates	72
4.2.1 Fibre Volume Fractions	73
4.2.2 The Manufacture of the Specimens	74
4.2.3 Experimental Procedure	76

4.2.4 Results and Discussion 77

4.3 The Iosipescu Specimen 81

4.3.1 Results and Discussion 81

4.4 The Centrally Notched Beam Specimen 91

4.4.1 Results and Discussion 92

4.5 The Plain and Notched Curved Beam Specimens 101

4.5.1 Results and Discussion 108

4.6 The Notched and Oblique Rectangular Prism Specimens 111

4.6.1 Results and Discussion 112

References 127

Chapter 5 Analysis of Laminates

5.1 Introduction 128

5.2 The Modelling Methodology Used for the Finite Element Analysis 129

5.3 The Iosipescu Specimen 130

5.3.1 Results and Discussion 131

5.4 The Plain Curved Beam Specimen 132

5.4.1 The Mechanics and Validation of the Sub-Modelling Technique 133

5.4.2 Results and Discussion 137

5.5 The Notched Curved Beam Specimen 140

5.5.1 Results and Discussion 140

5.6 The Centrally Notched Beam Specimen 145

5.6.1 Results and Discussion 145

5.7 The Notched and Oblique Rectangular Prism Specimens 156

5.7.1 Results and Discussion 159

5.8 The Accuracy of the Finite Element Models 168

References 170

Chapter 6 Moiré Interferometry

6.1 Introduction 171

6.2 Description of the Apparatus 172

6.3 Analysis Procedure 174

6.4 Specimen Preparation 176

6.5 Results and Discussion 177

6.5.1 The Plain Curved Beam Specimen 177

6.5.2 The Notched Curved Beam Specimen 179

6.5.3 The Centrally Notched Curved Beam Specimen 181

References 184

Chapter 7 Correlation and Interpretation of the Results

7.1 Introduction 185

7.2 Analysis of The Failure Surface 186

7.2.1 The Iosipescu Specimen 187

7.2.2 The Plain Curved Beam Specimen 187

7.2.3 The Notched Curved Beam Specimen 188

7.2.4 The Centrally Notched Beam Specimen 189

7.3 Correlation of the FEA Results with those in the Available Literature 189

7.3.1 The Interlaminar Shear Strength 190

7.3.2 The Interlaminar Tensile Strength 190

7.5 An Evaluation of the Test Techniques Employed 191

7.5 Establishing Failure Maps 192

7.5.1 The Two Dimensional Failure Envelope 193

7.5.2 The Three Dimensional Failure Envelope 198

References 202

Chapter 8 Conclusions and Further Work

8.1 Epilogue 204

8.2 Conclusions 206

8.2 Further Work 208

References 209

Appendices

Appendix A 210

Appendix B 212

Appendix C 213

Appendix D 214

Chapter 1

The Evolution of Composites

1.1 Introduction

Fibrous composite materials were used in Egypt as early as 4000 B.C. in the form of laminated writing materials made from the papyrus plant. Ancient Egyptians also made containers of coarse fibres drawn from heat-softened glass. A French scientist Reaumur [1.1] considered the potential of forming fine glass fibres as early as the 18th century. In 1920 Griffith [1.2] first measured the tensile strength of glass rods and glass fibres of different diameters. He discovered that as the rods and fibres became thinner, they became stronger. It was not until 1939 that continuous glass fibres were produced commercially [1.3]. At that time, the main use of these fibres was for high temperature electrical applications. In the late 1950's boron fibre was first produced and followed by carbon fibre in the early 1960s. The development of these new high performance fibres triggered a surge in the evolution of high strength fibres such as aramid, alumina, silicon carbide, alumina-boron-silica, boron nitride, titanium diboride, silica,

quartz, alumina/silica, and polyolefin etc..

Composite applications in the past twenty five years have primarily been in areas such as aerospace and sports equipment where the strength-to-weight ratio is critical. More recently the number and range of applications have grown to include the civil, automotive, and industrial fields. The use of composites is likely to expand further as analytical techniques and production methods continue to develop.

1.2 Applications

There follows a number of examples of current composite applications in use presently. However, this represents only a fraction of the possibilities as the true potential of composite materials is only beginning to be tapped.

1.2.1 Civil Structures

Composites are now often used in civil structures where corrosion is a dominant design consideration, e.g. the chemical industry and off-shore oil platforms [1.4]. Composites are also in use in lightweight overhead walkways, lighting, communication poles and the retractable roofs for stadia. Other benefits are the reduced maintenance and erection costs and the possibility of architectural enhancements. However, the use of composites in structures such as houses and buildings has lagged behind applications in other areas as strength to weight ratio is not as critical in static structures.

1.2.2 Sports Equipment

Advanced composites are used in sports equipment primarily to improve performance through lighter weight and improved tailorability. In the area of recreation, low weight is often

the key factor, however reduced maintenance and increased corrosion resistance are also major benefits associated with the selection of composites. Examples of sports and recreational equipment made from composites include tennis rackets, golf clubs, helmets, bicycles, skis, hockey sticks, fishing rods, boat hulls, windsurfing boards, surfboards, sails, shoes, canoes, racing shells, paddles, speed boats, scuba diving tanks and racing cars.

Cellular composites are being used in the construction of artificial playing surfaces and indoor and outdoor running lanes whereby the stiffness and damping properties of the surface may be optimised in order to minimise injury [1.5].

1.2.3 Automotive

The reason for selecting composites in automotive applications include higher strength-to-weight ratio and greater durability (improved corrosion resistance and wear resistance). Typical applications are drive shafts, fan blades and shrouds, springs, bumpers, interior panels, tyres, brake shoes, clutch plates, gaskets, hoses, and belts etc.. The advantages of composites is most evident in components like drive shafts yielding improved damping characteristics and a higher shaft whirl speed [1.6].

1.2.4 Industrial

Composites are used throughout industry in order to reduce costs by prolonging life expectancy as well as by improving performance. Examples of durable applications include hoses, drive and conveyer belts, tear and puncture resistant fabrics, rotor vanes, mandrels, ropes, cables, ladders and truck booms etc.. Ladders and truck booms are being made with glass fibres and resin matrices in order to minimise the possibility of injury due to electrical discharge. An exceptional example is the use of the fibreglass sucker rod in the oil industry

which has resulted in longer life and improved pumping efficiency [1.7].

1.2.5 Aerospace

The development and application of advanced fibrous composites was pursued primarily because of the potential for stronger structures with no weight penalty. The first applications in the early 1960s were in aerospace structures where weight critically affects fuel consumption, performance, and payload. Aircraft, spacecraft, satellites, space telescopes, the space shuttle, the space station, missiles, booster rockets and helicopters etc. are typical examples of the impact which composites materials have had in the industry.

The Boeing 757 and 767 were among the first commercial airliners to make extensive use of composites. The Boeing 757 has 1350 kg of composite parts in the body, wing and interior. About 30 percent of the external surface area of the Boeing 767 consists of composites [1.8]. While composites are being used conservatively in large airliners being applied only to the secondary structures, more sophisticated aircraft, such as aerobatic planes, the space shuttle and fighter planes, have composites in the primary structures. The space shuttle has many structural components made from composites such as carbon/epoxy, aramid¹/epoxy and carbon/carbon etc.. Aircraft such as the Stealth Fighter and the experimental forward-swept wing X-29 fighter would not have been possible without the use of advanced composites. Although the concept of a forward-swept wing for improved manoeuvrability is not new, conventional aluminium alloy structures could not withstand the aerodynamic forces acting on such a wing, so the implementation of the concept had to wait for the advent of very high strength-to-weight ratio materials.

¹ Kevlar

1.3 Drawbacks Associated with the Use of Fibrous Composites

Fibrous composites are becoming more and more the material of choice for designers for a variety of reasons which include low weight, high stiffness, high strength, electrical conductivity (or non-conductivity), low thermal expansion, low or high heat transfer coefficient, corrosion resistance, and retention of properties at high operating temperatures. Some of these advantages have already been discussed in Section 1.2. Fibrous composites seem to have the potential of fulfilling all of the requirements a designer might have of a material and, as a consequence, they continue to replace conventional materials in more and more common applications. However, while the ‘in-plane’ fibre dominated properties have been improved rapidly since the 1960s, no significant improvements have been made to the weaker matrix dominated properties in the through-thickness direction. This is the Achilles’ heel of fibrous composites with respect to current design.

Controlling Property	Problem
Interlaminar Shear Strength	• Failure induced by shear in beams under flexural loading. • Premature torsional failure.
Through-Thickness Strength	• Failure induced by through-thickness tensile fracture of curved beams in flexure. • Shock waves propagated by normal impacts.
Combination of above two	• Premature crippling failure in compression. • Failure in adherends in structural bonded joint. • Failure of laminate due to free edge effects [1.9].

Table 1.1 Overview of Problems Created by the Weak Matrix-Dominated Properties.

In the case of thin laminated composite skins, which are widely used in aircraft and spacecraft structures, the poor strength in the through-thickness direction is less important. This

is because the thin composite laminate represents only part of the structure and is under uniform in-plane stress, i.e. the stress in the through-thickness direction is negligible. For example, in sandwich structures or stiffened panels, the stresses due to flexure are distributed such that the thin laminate skins are subjected to in-plane stresses whereas the core and the stiffeners may experience three dimensional stresses [1.10]. The biggest problem with using these structures is the complexity associated with manufacture. The joining of dissimilar materials via adhesive bonding or mechanical fasteners is also fraught with difficulties, e.g. stress concentrations, consistency between joints and fatigue resistance. These problems lead to the use of increased safety factors and consequently have a detrimental effect on the cost effectiveness of composites in comparison with conventional materials.

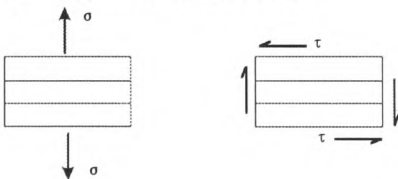
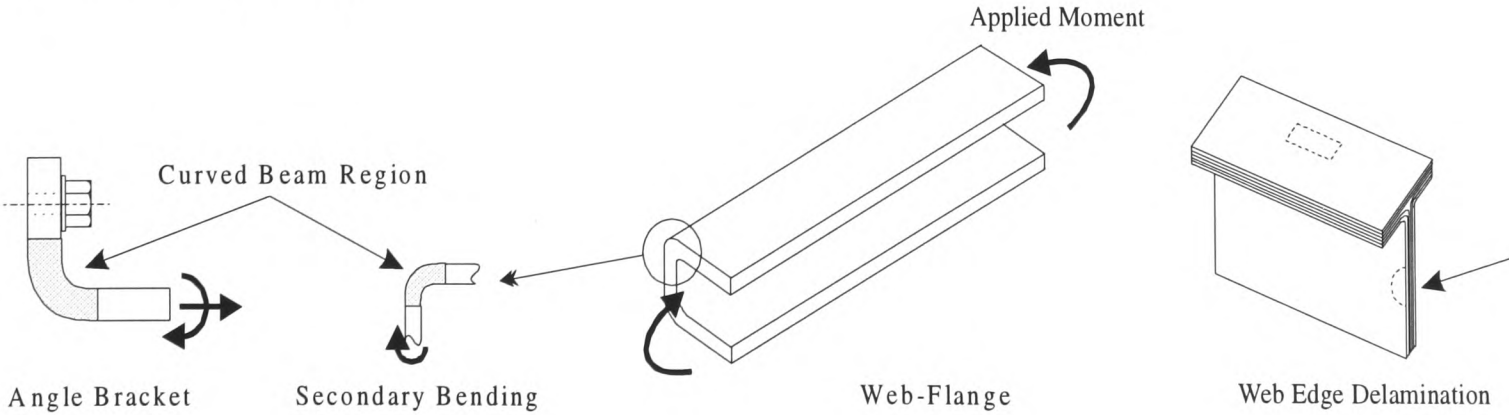
Sources
1) Manufacturing Defects
2) Out-of-plane loads
i) Applied loads giving rise to interlaminar stresses

ii) Laminate geometry


Table 1.2 Sources of Delamination.

The use of thick laminated composites to form structural components (e.g. angle brackets, co-curved webs and frames etc.) is becoming common practice and is replacing sandwich structures and stiffened planes. The catastrophic failure of such structural elements when subjected to a combination of in-plane and out-of-plane stresses is normally associated with the complex progression of ply cracking, fibre failure and delamination. The out-of-plane stresses comprise a normal stress and two shear stresses and are all designated interlaminar stresses. A general overview of the problems created by the matrix dominated properties, which arise in a wide variety of design features, are presented in Tables 1.1 and 1.2 [1.11]. It is therefore important that an understanding of the failure mechanism of thick laminated composites is obtained in order to aid the structural designer in optimising such components with respect to their interlaminar strengths.

1.4 Research Objectives and Thesis Structure

The primary objective of this investigation is to develop a testing methodology which will allow characterisation of the interlaminar behaviour of thick composites. The secondary objective is to develop an experimentally validated method for predicting the three dimensional mechanical behaviour of thick composite laminates. The tertiary objective is to define parts of the failure envelope in stress space using this new technique.

A literature review of existing failure theories for composite laminae and laminates is presented in Chapter 2. The development of experimental methodologies for characterising the behaviour of thick composites in the through-thickness direction for the material configurations employed in this investigation are presented in Chapter 3. In Chapter 4 these experimental procedures are presented in conjunction with the associated results. A detailed study of the failure surfaces was also conducted using a scanning electron microscope in order to establish

the exact mode of failure. Chapter 5 describes the methodologies employed in the numerical analyses of the specific specimen geometries using heterogeneous anisotropic material properties. The experimental verification of these numerical analyses via Moiré interferometry is presented in Chapter 6. Finally, Chapters 7 and 8 are dedicated to the discussion of the results and any conclusions which may be drawn.

References

- 1.1 Hartman D.R., Greenwood M.E. and Miller D.M., High Strength Glass Fibres, Owens-Corning Fibreglass Corporation, Toledo, 1994.
- 1.2 Griffith A.A., The Phenomena of Rupture and Flow in Solids, Philosophical Transactions of the Royal Society, 221A, 1920, p163-198.
- 1.3 Knox C.E., Fibreglass Reinforcement, Ed. Lubin G., Van Nostrand Reinhold Co., New York, 1982.
- 1.5 Morgan R.J. and Allred R.E., International Encyclopaedia of Composites, Vol.1, Ed. Lee S.M., VCH Publishers, New York, 1989, p37-56.
- 1.4 Easterling K.E., Advanced Materials for Sports Equipment, Chapman & Hall, London, 1993.
- 1.6 Riggs J.P., International Encyclopaedia of Composites, Vol.1, Ed. Lee S.M., VCH Publishers, New York, 1989, p197-225.
- 1.7 Lubin G., Handbook of Composites, Van Nostand Reinhold Co, New York, 1982.
- 1.8 Gibson R.F., Principles of Composite Material Mechanics, McGraw-Hill Inc. New York, 1994.
- 1.9 Paul P.C., Saff C.R., Sanger K.B., Mahler M.A., Kan H.P. and Kautz E.F., Analysis and Test Techniques for Composite Structures Subjected to Out of Plane Loads,

Composite Materials Testing and Design, Vol.10, Ed. Grimes G.C., ASTM STP 1120, 1992, p238-252.

- 1.10 Beaumont P.W.R., The Failure of Fibre Composites: An Overview, Journal of Strain Analysis, Vol.24, No.4, 1989, p1-17.
- 1.11 Herakovich C.T., Edge Effects and Delamination Failures, Journal of Strain Analysis, Vol.24, No.4, 1989, p57-64.

Chapter 2

Anisotropic Failure Theories

2.1 Introduction

In order for the use of composite materials in structural components to continue to expand, three key areas must continue to develop. The first is the flexibility, cost and ease of manufacture. Second is the development of definitive test procedures for establishing relevant material properties and confirming component performance. Finally, there is the need for wider availability of data and design methods. Most of the available strength information is based upon the uniaxial states of stress, whereas practical applications invariably involve more complex stress fields. Structural composite components usually consist of several anisotropic plies bonded together at differing orientations and thus, unlike isotropic materials, their strength is direction dependent. Furthermore, failure of a small number of plies in a laminated structure does not necessarily imply failure of the structure as a whole as the load path may change in order to go through other supporting plies.

Numerous macroscopic failure criteria have already been proposed for fibrous composites. In this chapter a synopsis of the various composite failure criteria is presented and is followed by a brief review of those failure criteria which are more commonly employed. Since strength theories are conceived primarily to predict the onset, and not the mode, of failure, the macroscopic view point will predominate. Another approach which has been employed is based upon fracture mechanics (microscopic view point) and a brief review of this method is also presented.

2.2 Synopsis of Failure Criteria

Particularly for prototype design purposes, it is important that a reliable strength prediction method be available for various combinations of normal and shear loading. The ideal failure criteria should be flexible enough to allow for the wide range of possible constituent materials and laminate configurations and yet require minimal laminate input data. Preferably, the acquisition of this data should require only relatively simple testing such as can be conducted in any normally equipped materials laboratory. To this end it would be desirable to have a reliable strength theory based upon a small number of laminate tests.

The Von Mises [2.1] energy of distortion or octahedral shear stress yield theory has significantly influenced the basis of many existing composite strength criteria. Hill [2.2] generalised the Von Mises' formulation to include anisotropy. Marin [2.3] subsequently suggested a formulation similar to Hill's by presupposing coincident directions of principal stress and material. In 1965, Hill's theory was adapted by Azzi [2.4] and Tsai [2.5] as a strength criterion for composites. Hoffman [2.6] altered Hill's criterion to allow for unequal tensile and compressive strengths by introducing linear stress terms in a functional form. Norris' extension of the distortion-energy theory [2.7] was modified by Fischer [2.8] to predict

the strength of glass reinforced composites.

A variation of the maximum-stress theory for unidirectional composites was proposed by Stowell and Liu [2.9] in which the failure stress of the fibres was taken as the limiting strength of the lamina in the fibre direction, while the limiting transverse and shear stresses are equal to those of the matrix material. As pointed out by Kelly and Davis [2.10] utilisation of bulk matrix properties for transverse and shear stresses assumes there to be no fibre-matrix interaction.

The St. Venant's maximum strain-theory of isotropic mechanics [2.11] was extended to include anisotropy by Petit and Waddoups [2.12]. A strength criterion based on the strain energy to failure has also been proposed by Sandhu [2.13]. Though most strength predictions use a linear approximation to the stress-strain relationship in the lamination analysis, those by Sandhu and Petit-Waddoups allow for non-linear lamina material behaviour.

Gol'denblat and Kopnov [2.14] and Ashkenazi [2.15] appear to have initiated attempts to develop a strength theory which is invariant with respect to the coordinate system. Malmeister [2.16], Tsai and Wu [2.17] and Tennyson *et al.* [2.18] also made subsequent efforts to this end.

The composite strength theories discussed above are based on lamina material properties and a lamina failure criterion. Puppo and Evensen [2.19] postulated an anisotropic strength theory applicable directly to the laminate as a whole not just the individual laminae. Wu and Scheublein [2.20] also proposed a direct laminate strength theory using interaction factors and the concept of principal axes of strength.

2.3 Commonly Employed Failure Criteria

Although considerable effort has been devoted to the formulation of composite strength

theories and also to their correlation with test data, no single theory has emerged which is completely satisfactory. Of all of the failure theories available, four criteria are considered representative and are the most widely employed, namely, the Maximum Stress Theory, the Maximum Strain Theory, the Tsai-Hill Theory and the Tsai-Wu Theory. A brief review of these theories and the associated equations follows.

2.3.1 The Maximum Stress Theory

The maximum stress theory for orthotropic laminae was first suggested by Jenkins [2.21]. This criterion assumes that failure occurs whenever any one component of stress reaches its limiting value and is independent of the values of all other components of stress. Thus, this theory predicts that, in order to avoid failure, the following set of inequalities must be satisfied:

$$\begin{aligned} X_c < \sigma_1 < X_t \\ Y_c < \sigma_2 < Y_t \\ Z_c < \sigma_3 < Z_t \\ |\tau_{12}| < Q \\ |\tau_{31}| < R \\ |\tau_{23}| < S \end{aligned} \tag{2.1}$$

where X is the ultimate normal stress in the fibre direction, Y and Z are the ultimate normal stresses in the two transverse directions, and Q , R and S are the ultimate shear stresses. The subscripts t and c denote tension and compression respectively. According to this theory the failure surface is planar and rectangular. The theory does not account for possible interactions between the stress components.

Theoretical failure surfaces agree well with this theory when the applied stress is along, or normal to, the fibre direction. However, due to the lack of allowance for stress interaction in this theory, agreement is poor when the applied stress is biaxial.

2.3.2 The Maximum Strain Theory

This theory is identical to the maximum stress theory but considers strain in place of stress. Thus, in order to avoid failure the following set of inequalities must be satisfied:

$$\begin{aligned}\varepsilon_1^c &< \varepsilon_1 < \varepsilon_1^t \\ \varepsilon_2^c &< \varepsilon_2 < \varepsilon_2^t \\ \varepsilon_3^c &< \varepsilon_3 < \varepsilon_3^t \\ |\gamma_{12}| &< \Gamma_{12} \\ |\gamma_{13}| &< \Gamma_{13} \\ |\gamma_{23}| &< \Gamma_{23}\end{aligned}\tag{2.2}$$

where the maximum allowable normal strains have been denoted with superscripts t and c to denote tension and compression respectively and Γ_{ij} denotes maximum allowable shear strains. Unlike the maximum stress theory, the maximum strain theory does account for interactions between stress components to a small degree through the effect of Poisson's ratio.

2.3.3 The Tsai-Hill Theory

This is a quadratic failure theory which attempts to provide better correlation between theory and experiment via inclusion of all of the components of stress in an equation representing the failure envelope. This theory is based upon the mathematical premise that a second-order curve has more parameters with which to fit experimental data than does a straight line and is not based upon the physics of the failure mechanism. While this may provide better correlation between theory and experiment to a certain extent, it is limited in that the sign of the normal stress components must be known if the positive and negative strengths are different, which is usually the case with composites. The failure surface given by the equation:

$$\begin{aligned}(G+H)\sigma_1^2 + (F+H)\sigma_2^2 + (F+G)\sigma_3^2 - \\ 2H\sigma_1\sigma_2 - 2G\sigma_1\sigma_3 - 2F\sigma_2\sigma_3 + \\ 2L\tau_{23}^2 + 2M\tau_{13}^2 + 2N\tau_{12}^2 = 1\end{aligned}\tag{2.3}$$

where F , G , H , L , M and N are material strength parameters which are expressed in terms of the failure stresses for one-dimensional loading. For pure shear loading, $\tau_{12} \neq 0$. If the corresponding shear strength is S , then eqⁿ. 2.3 yields:

$$2N = \frac{1}{S^2} \quad (2.4)$$

Similarly, if we consider pure shear loading in the directions τ_{23} and τ_{13} with the associated ultimate values of strength being Q and R respectively, then eqⁿ. 2.3 yields:

$$2L = \frac{1}{Q^2} \quad (2.5)$$

$$2M = \frac{1}{R^2} \quad (2.6)$$

The remaining three strength parameters, F , G and H , are determined from the simultaneous set of equations obtained from eqⁿ. 2.3 when the three individual boundary conditions are considered in which all stresses are zero with the exception of σ_1 , σ_2 and σ_3 respectively. In these instances eqⁿ. 2.3 yields:

$$2F = -\frac{1}{X^2} + \frac{1}{Y^2} + \frac{1}{Z^2} \quad (2.7)$$

$$2G = \frac{1}{X^2} - \frac{1}{Y^2} + \frac{1}{Z^2} \quad (2.8)$$

$$2H = \frac{1}{X^2} + \frac{1}{Y^2} - \frac{1}{Z^2} \quad (2.9)$$

2.3.4 The Tsai-Wu Theory

This is a development of the Tsai-Hill theory in which a complete quadratic stress tensor polynomial is adopted and includes all linear terms. Failure criteria based upon polynomials of strength tensors represent an attempt to mathematically overcome one of the shortcomings of a quadratic theory, i.e. to account for the differences in tensile and

compressive strengths. Additionally, these equations have the advantage of being scalar.

The Tsai-Wu criterion assumes that there exists a scalar function $f(\sigma_k)$ of the form

$$f(\sigma_k) = F_i \sigma_i + F_{ij} \sigma_i \sigma_j \quad (2.10)$$

where the contracted notation is used; and $i, j, k = 1, 2, \dots, 6$; F_i and F_{ij} are stress tensor quantities of strength parameters. Failure corresponds to the condition:

$$F_{ii} F_{jj} - F_{ij}^2 \geq 1 \quad (2.11)$$

where repeated indices are not summations for this equation.

The reduced form of the scalar function $f(\sigma_k)$ for an orthotropic material is:

$$\begin{aligned} f(\sigma_k) = & F_1 \sigma_1 + F_2 \sigma_2 + F_3 \sigma_3 + 2F_{12} \sigma_1 \sigma_2 + 2F_{13} \sigma_1 \sigma_3 + 2F_{23} \sigma_2 \sigma_3 \\ & + F_{11} \sigma_1^2 + F_{22} \sigma_2^2 + F_{33} \sigma_3^2 + F_{44} \sigma_4^2 + F_{55} \sigma_5^2 + F_{66} \sigma_6^2 \end{aligned} \quad (2.12)$$

The strength tensors F_i and F_{ij} can be written in terms of the ultimate strengths $X_t, X_c, Y_t, Y_c, Z_t, Z_c, Q, R$ and S as previously defined. For tensile failure in the σ_1 direction, eqⁿ. 2.12 reduces to:

$$F_1 X_t + F_{11} X_t^2 = 1 \quad (2.13)$$

For compressive failure in the σ_1 direction, eqⁿ. 2.12 reduces to:

$$F_1 X_c + F_{11} X_c^2 = 1 \quad (2.14)$$

Simultaneous solution of eqⁿs. 2.13 and 2.14 yields:

$$F_1 = \frac{1}{X_t} + \frac{1}{X_c} \quad (2.15)$$

$$F_{11} = -\frac{1}{X_t X_c} \quad (2.16)$$

Similarly, the other uniaxial stress states $\sigma_2, \sigma_3, \sigma_6$ (τ_{12}), σ_5 (τ_{13}), or σ_4 (τ_{23}) yield:

$$F_2 = \frac{1}{Y_t} + \frac{1}{Y_c} \quad (2.17)$$

$$F_{22} = -\frac{1}{Y_t Y_c} \quad (2.18)$$

$$F_3 = \frac{1}{Z_t} + \frac{1}{Z_c} \quad (2.19)$$

$$F_{33} = -\frac{1}{Z_t Z_c} \quad (2.20)$$

$$F_{44} = \frac{1}{Q^2} \quad (2.21)$$

$$F_{55} = \frac{1}{R^2} \quad (2.22)$$

$$F_{66} = \frac{1}{S^2} \quad (2.23)$$

The remaining coefficients in eqⁿ. 2.12, i.e. F_{12} , F_{13} and F_{23} , correspond to coupling terms between normal components of stress. These coefficients can be determined via tests in which the biaxial load cases, i.e. $\sigma_1 = \sigma_2$, $\sigma_1 = \sigma_3$ and $\sigma_2 = \sigma_3$ are considered independently. However, it is difficult to conduct a test in which a specimen fails as a result of an uniformly biaxial state of stress. Narayanaswami *et al.* [2.22] demonstrated that these interaction coefficients are small and can be neglected.

2.4 The Fracture Mechanics Approach

The strength of material based failure theories have been discussed in Section 2.3. In this section a different approach based on microscopic failure mechanisms is reviewed. The presence of interlaminar stress is the primary cause of delamination in laminated fibrous composites. Delamination occurs when the interlaminar stress exceeds the interlaminar strength. A fracture mechanics based failure criterion offers a method of predicting the initiation and growth of delamination in a component with stress singularities, i.e. matrix cracks, material defects, edges and other discontinuities. Using a fracture mechanics based failure criterion, a value of strain energy release rate, G , has to be determined at every potential

delamination source. When G reaches the interlaminar fracture toughness of the composite, an interlaminar crack will be initiated and will subsequently lead to delamination. This limiting value of G is defined as the critical value, G_c . In the following sections the subscripts I and II denote mode I (crack opening) and mode II (crack shearing) failure.

2.4.1 Determination of Strain Energy Release Rate

The Double Cantilever Beam (DCB) shown in Figure 2.1 is a standard method used to determine G_I . The beam is loaded via a tab fixed by adhesive to the material adjacent to the crack. The End Notched Flexure (ENF) and the End Load Split (ELS) methods (Figures 2.2 and 2.3) are commonly used to determine G_{II} . ENF uses three point bending whilst ENS has one end of the beam built-in with the load applied at the other end. In both the ENF and the ELS methods the cracks extend through shear.

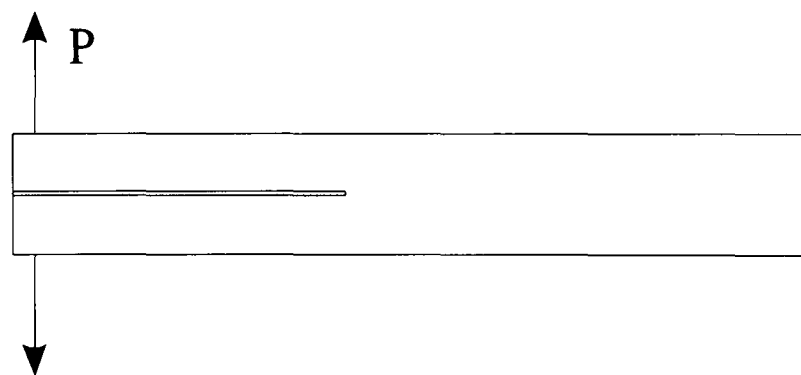


Figure 2.1 A Schematic Illustration of Double Cantilever Beam Specimen.

The ELS method is known to be more sensitive than the ENF method with respect to the loading conditions. In DCB, ENF and ENS specimens, an artificial defect, i.e. a film insert or a precrack, is used to simulate the existence of crack. The variations in the favoured approaches for creating the starter crack among the standard organisations were reported in a review of

current test protocols by Brunner *et al.* [2.23].

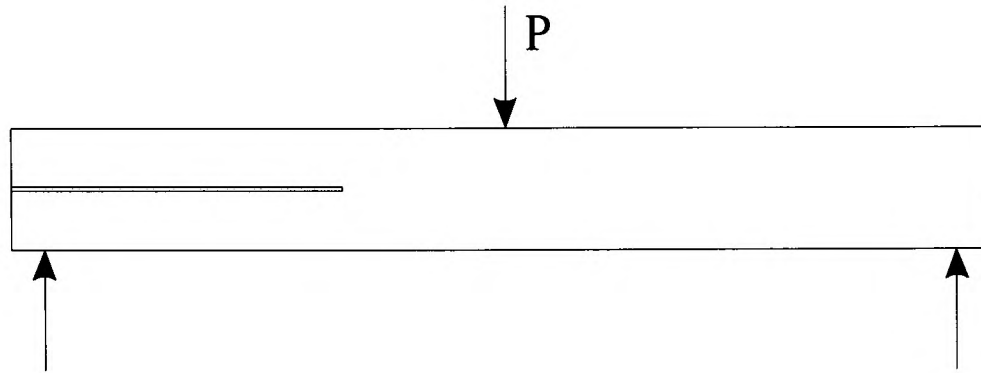


Figure 2.2 A Schematic Illustration of End Notched Flexure Specimen.

Film inserts are favoured by the American Society for Testing and Materials (ASTM) whereas both film inserts and precracks are favoured by the European Structural Integrity Society (ESIS) and the Japanese Industrial Standardization (JIS).

Extensive round robin testing conducted by Davies *et al.* [2.24], Davies [2.25], Hojo *et al.* [2.26], and O'Brien *et al.* [2.27] has highlighted technical problems, e.g. the G_c values obtained exhibited a large scatter and could only be used as an indication of the strength of the material but not as accurate and well defined values for design.

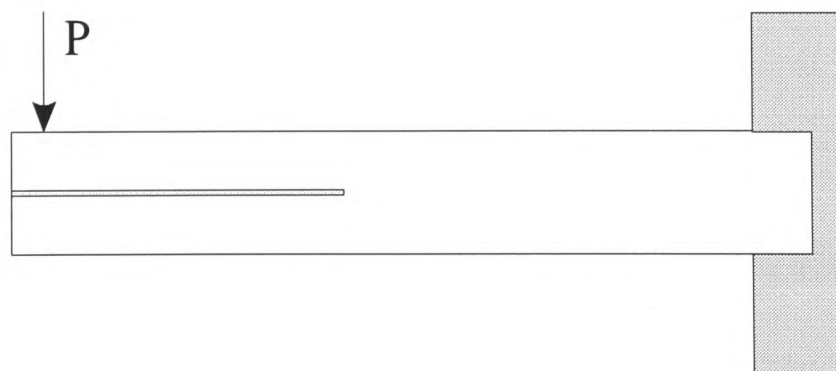


Figure 2.3 A Schematic Illustration of End Load Split Specimen.

2.5 The Empirical Development of the Failure Envelopes

Hognestad [2.28] attempted to develop parts of a failure surface in three dimensional stress space using the interlaminar tensile stress, interlaminar shear stress and in-plane stress. He discovered that the in-plane tensile stress offsets the in-plane normal stresses and thus increasing the through-thickness strength. This work was followed by Cui [2.29] who increased the number of data points with more experimentation. These data points were concentrated in the in-plane compressive stress - interlaminar tensile stress plane and in the interlaminar shear stress - interlaminar tensile stress plane. He found that the data points in the interlaminar shear stress - interlaminar tensile stress plane were sensitive to the experimental errors.

2.6 The Applicability of the Various Failure Theories

Almost all of the failure theories presented herein are based upon the assumption of homogeneity and a linear stress-strain relationship up to the point of failure. All of these theories can be expressed in terms of the ultimate strengths referred to the principal material axes. However, it has been demonstrated by Rowlands [2.30] that the applicability of any of these theories is dependent upon the deviation of those conditions being considered from the assumptions which each theory makes with respect to loading and lamina orientation; more specifically, the laminae should be unidirectional (transversely isotropic) or orthotropic and the loading should act in such a way so as to induce a plane state of stress.

The maximum stress and strain theories are most applicable when brittle behaviour is predominant, i.e. tensile loading in the principal direction. Of these two theories, only the maximum strain theory considers a small amount of stress interaction via Poisson's ratio which is used to obtain the normal stress components. These theories, though conceptually simple, do not allow accurate failure prediction for combined stress states.

The Tsai-Hill and Tsai-Wu theories are most applicable when ductile behaviour under shear or compressive loading is predominant. The Tsai-Hill theory, though based on Hill's theory for ductile anisotropic materials, represents an adaptation which improves the predictive accuracy for the more brittle heterogeneous composites via a form of "curve fitting". However, the theory remains incapable of allowing for differing tensile and compressive strengths whereas the Tsai-Wu theory uses an additional coefficient to allow distinction between tensile and compressive strengths and represents a more reliable form of "curve fitting". This theory also uses a "stress interaction coefficient" which, though an approximation can be made, may be more precisely determined via specimen tests in which failure occurs under an equally biaxial state of stress.

The fracture mechanics approach is most applicable when brittle behaviour is predominant. It allows for different modes of failure both in isolation and in combination. Despite its successful application to homogeneous materials, the technical problems associated with the accurate quantification of delamination toughness raises doubt about its applicability to composite design.

The empirical development of failure envelopes is most applicable for both brittle and ductile behaviours and allows interaction between the in-plane stresses and interlaminar stresses. However, this approach is still at the early development stage and there is lack of the quality and quantity of data points especially for interlaminar stresses. Additionally, the characterisation of interlaminar mechanical behaviour of composites requires the development of definitive test procedures for accurately quantifying the relevant material properties.

2.7 Summary

With the exception of the fracture mechanics approach, all current strength failure

criteria tend to be phenomenological and empirical in nature. Also, no mathematical representation of strength exists for a general composite and loading configuration. The analyses predict the onset, but not the mode, of failure. As with isotropic criteria, composite strength theories are predicted on the basis of limiting stress, strain or energy. Most of the existing analyses assume a laminate stress-strain relationships based upon lamina material properties and use lamination theory in order to evaluate the stresses and strains in the various plies.

The greatest deficiencies of current composite strength analyses (theoretical and experimental) are as follows;

1. The lack of a simple but representative lamination theory which realistically accounts for interlaminar stresses.
2. The quality and quantity of experimental data.

A failure criterion or envelope based on the interlaminar stress is required as a design tool in order to ease the design and optimisation process for composite structures.

The empirical establishment of a failure envelope which accounts for stress interactions at failure requires a great deal of experimental data corresponding to a variety of stress states. More specifically, at least a six dimensional stress space is required in order to express the three in-plane stresses and the three interlaminar stresses. As discussed in Section 1.3, the failure of thick laminates is strongly influenced by interlaminar stresses. The interlaminar tensile stress causes peeling (Mode I) and the interlaminar shear stress causes tearing (Mode II or III). In isolation or in combination, these two interlaminar stress types lead to catastrophic delamination. Another important factor is the effect of in-plane tensile stress in offsetting the in-plane normal stress and thus increasing the through-thickness strength. It was therefore decided that the interlaminar tensile, the interlaminar shear and the in-plane tensile stresses

should be the main variables for establishing some parts of the failure envelope in this investigation.

References

- 2.1 Timosenko S.P., History of Strength of Materials, McGraw-Hill, New York, 1953.
- 2.2 Hill R., A Theory of the Yielding and Plastic Flow of Anisotropic Metals, The Mathematical Theory of Plasticity, Oxford University Press, London, 1950.
- 2.3 Marin J., Theory of Strength for Combined Stresses and Non-Isotropic Materials, Journal of Aeronautical Science, Vol.24, No.4, 1957, p324-331.
- 2.4 Azzi V.D. and Tsai S.W., Anisotropic Strength of Composites, Experimental Mechanics, Vol.5, No.9, 1965, p283-288.
- 2.5 Tsai S.W., Strength Theories of Filamentary Structures, Fundamental Aspects of Fibre Reinforced Plastic Composites, Ed. Schwartz R.T. and Schwartz H.S., Wiley Interscience, New York, 1968.
- 2.6 Hoffman O., The Brittle Strength of Orthotropic Materials, Journal of Composite Materials, Vol.1, 1967, p200-206.
- 2.7 Norris C.B., The Elastic Theory of Wood Failure, Transactions of the ASME, Vol.61, 1939, p259-261.
- 2.8 Fischer L., Optimization of Orthotropic Laminates, Engineering for Industry, Vol.83, No.3, Series B, 1967, p399-402.
- 2.9 Stowell E.Z. and Liu T.S., On the Mechanical Behaviour of Fibre-Reinforced Crystalline Materials, Journal of Mechanics and Physics of Solids, Vol.9, 1961, p242-260.
- 2.10 Higdon A., Ohlsen E.H., Stiles W.B., Weese J.A. and Riley W.F., Mechanics of

Materials, 3rd edition, John Wiley & Sons, 1976.

- 2.11 Kelly A. and Davis G.J., On the Theory of Laminated Anisotropic Shell and Plates, Journal of Aerospace Science, Vol.29, 1965, p969-975.
- 2.12 Petit P.H. and Waddoups M.E., A method of Predicting the Nonlinear Behaviour of Laminated Composites, Journal of Composite Materials, Vol.3, 1969, p2-9.
- 2.13 Sandhu R.S., Ultimate Strength Analysis of Symmetric Laminates, Journal of Applied Mechanics, Vol.28, 1974, p402-408.
- 2.14 Gol'denblat I.I. and Kopnov V.A., Strength of Glass-Reinforced Plastics in the Complex Stress State, Polymer Mechanics, Vol.1, No.2, 1965, p54-59.
- 2.15 Ashkenazi E.K., Problems of the Anisotropy of Strength, Polymer Mechanics, Vol.1, No.2, 1959, p60-70.
- 2.16 Malmeister A.K., Geometry of Theories of Strength, Polymer Mechanics, Vol.2, No.4, 1966, p324-331.
- 2.17 Tsai S.W. and Wu E.M., A General Theory of Strength for Anisotropic Materials, Journal of Composite Materials, Vol.5, 1971, p58-80.
- 2.18 Tennyson R.C., MacDonald D. and Nanyaro A.P., Evaluation of the Tensor Polynomial Failure Criterion for Composite Materials, Journal of Composite Materials, Vol.12, 1978, p63-75.
- 2.19 Puppo A.H. and Evensen H.A., Strength of Anisotropic Materials under Combined Stresses, American Institute of Aeronautics and Astronautics Journal, Vol.10, No.4, 1972, p468-474.
- 2.20 Wu E.M. and Scheublen J.K., Laminated Strength - A Direct Characterization Procedure, Composite Materials, ASTM STP 546, American Society for Testing and Materials, 1974, p188-206.

- 2.21 Jenkins C.F., Report on Materials of Construction Used in Aircraft and Aircraft Engines, Great Britain Aeronautical Research Committee, 1920.
- 2.22 Narayanaswami R. and Adelman H.M., Evaluation of the Tensor Polynomial and Hoffman Strength Theories for Composite Materials, Journal of Composite Materials, Vol.11, 1997, p366-377.
- 2.23 Brunner A.J., Flüeler P., Davies P., Blackman B.R.K. and Williams J.G., Determination of the Delamination Resistance of Fibre Reinforced Composites: Current Scope of Test Protocols and Future Potential, Proceedings, ECCM-7, Seventh European Conference on Composite Materials, Vol.2, 1996, p3-8.
- 2.24 Davies P., Kaush H.H., Williams J.G., Kinloch A.J., Charalambides M.N., Pavan A. *et al.*, Round-Robin Interlaminar Fracture Testing of Carbon-Fibre-Reinforced Epoxy and PEEK Composites, Composites Science and Technology, Vol.43, 1992, p129-136.
- 2.25 Davies P., Moulin C., Kausch H.H. and Fischer M., Measurement of G_{Ic} and G_{IIc} in Carbon/Epoxy Composites, Composite Science and Technology, Vol.39, 1992, p1-13.
- 2.26 Hojo M., Kageyama K. and Tanaka K., Prestandardization Study on Mode I Interlaminar Fracture Toughness Test for CFRP in Japan, Composites Vol.26, 1995, p243-255.
- 2.27 O'Brien T.K. and Martin R.H., Round Robin Testing for Mode I Interlaminar Fracture Toughness of Composite Materials, Journal of Composite Technology and Research, JCTRER, Vol.15, No.4, 1993, p269-281.
- 2.28 Hognestad G., Analysis of Composite Flange Connections and Determination of Interlaminar Strength, D.Phil. Thesis, Oxford University, 1993.
- 3.29 Cui G.Y., Establishment of a Through-Thickness Failure Criterion for Laminated Composites, Rolls-Royce University Technology Centre for Solid Mechanics Report,

No.33, Nov 1992.

2.30 Rowlands R.E., Handbook of Composites, Vol.3, 1985, Elsevier Science Publishers.

Chapter 3

The Design and Manufacture of the Specimens

3.1 Introduction

In this chapter the design and implementation of the various existing methods for the mechanical testing of composite materials in the through-thickness direction is discussed. In Chapter 2 the emphasis was on the evaluation of various failure theories for the mechanical behaviour of composite materials, the validation and implementation of which relies entirely upon the availability of experimentally obtained mechanical properties data. Additionally, some aspects of the mechanical behaviour of composites are of such complexity that the feasibility of an accurate failure theory is questionable. In view of these facts, an empirical approach may be the only practicable solution.

Owing to the complex characteristics of composites, i.e. anisotropy, coupling effects and the variety of possible failure modes, it has been shown that the mechanical test methods employed for conventional metallic materials are usually not adequate. Thus, the development

and evaluation of new test methods for composite materials has been, and continues to be, a priority in order to enable the experimental establishment of the mechanical properties.

3.2 The Design of Test Specimens

In this investigation the engineering characteristics of interest are the interlaminar shear and the interlaminar tensile strengths both in isolation and in combination. As discussed in Chapter 2 these are the governing factors in the failure of thick laminated composites. The effect of the in-plane normal stress on the interlaminar strength is also of interest.

3.2.1 General Considerations

Ideally, specimens for the determination of the mechanical properties of a material require two main features: a) uniform stress distribution across the failure cross section and b) uniaxial stress. In order to achieve these conditions, the test section needs to be far enough from the grips to avoid stress concentrations which would lead to premature failure. Thus, specimens for characterising the in-plane properties have high aspect ratios, L/t and L/b , where L , b and t are the length, breadth and thickness respectively. In most cases loading is transmitted to the specimen via inter-surface frictional shear between the specimen and some relatively soft tabs which act to minimise stress concentrations as far as is practicable. Additionally, for the most meaningful property determination, it is essential to test specimens of the same fibre volume fraction, thickness and fabrication method as the material being used in the application of interest.

When measuring out-of-plane properties it is impossible to achieve high aspect ratios as the manufacturing process limits the length of the specimen. This necessitates the use of grips resulting in a load path which is complex and is normally associated with high stress

concentrations in the sensitive interlaminar direction. These difficulties account for the shortage of publications, research work, test standards, and the availability of mechanical properties data in the through-thickness direction. In view of these problems, a literature review was conducted in order to identify the optimum specimen configuration for use in this investigation.

3.3 Current Testing Techniques

Standards and test methodologies for deriving the interlaminar shear modulus and strength have previously been established [3.1 - 3.3] though no such protocols exist for characterising the interlaminar tensile strength. Recently, the National Physical Laboratory (NPL) published a review of existing methods with a view to the establishment of a standard protocol for the measurement of interlaminar tensile strength. It was concluded that none of the existing methods satisfied the requirements proposed by the NPL [3.4].

There have been several investigations which have attempted to tackle interlaminar tensile strength, both in isolation and in combination, with interlaminar shear strength. These may be divided into two groups:

1. *DIRECT METHODS*, These methods create a relatively uniform stress field at the failure section.
2. *INDIRECT METHODS*, These methods usually utilise bending to create a dramatically non-uniform stress distribution at the failure section.

3.3.1 The Iosipescu Specimen

The most frequently used tests for interlaminar shear strength are the notched tension coupon test and the short beam shear test [3.5] illustrated in Figure 3.1. Both of these tests have

been criticised for having stress fields too nonuniform to provide accurate strength measurements [3.6]. The Iosipescu shear test, which was first proposed for use with metals, has more recently been successfully adapted for use with composites by Walrath *et al.* [3.7]. The induced moments vanish at the midsection to produce a state of pure shear in the region of minimum cross sectional area. The Iosipescu specimen (Figure 3.2) yields the more uniform stress field [3.2 -3.3].

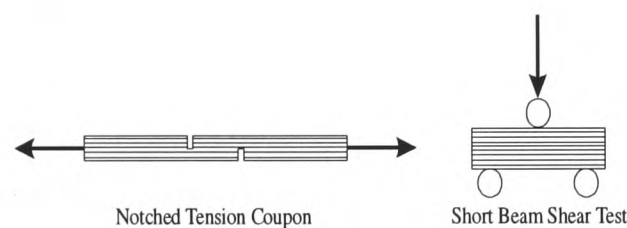


Figure 3.1 Standard Tests for Interlaminar Shear Strength [3.5].

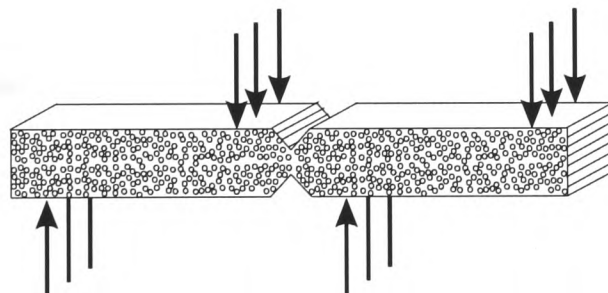


Figure 3.2 Typical Iosipescu Specimen Load Configuration.

3.3.2 The Cylindrical Specimen

This comprises a cylindrical specimen (Figure 3.3) which is ground along its length such that the diameter at the centre is significantly smaller than that at both ends to which aluminium tabs are bonded [3.8, 3.9]. Two problems were reported with this specimen type, i.e. difficulties in machining the notch profile and frequent failure at the adhesive layer between the specimen and the aluminium tabs. Large scatter in the results was also reported and

attributed to specimen failure at the discontinuity where the notch radius starts.

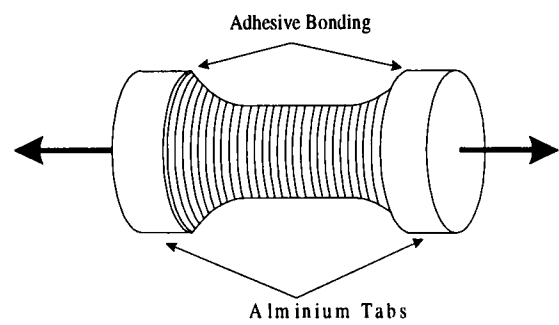


Figure 3.3 The Cylindrical Specimen.

3.3.3 The Notched Rectangular Prism Specimen

The loading mechanism for this specimen type is similar to that of the cylindrical specimen (Figure 3.4). There have been several studies published using this basic geometry with differing notch profiles and waisting configurations (width only or width and depth) [3.10, 3.11]. Similarly to the cylindrical specimen, the rectangular specimen was reported to fail frequently at the adhesive layer between the specimen and the metallic tab, or at the start of the notch radius. However, in contrast to the cylindrical specimen, machining difficulties were not reported.

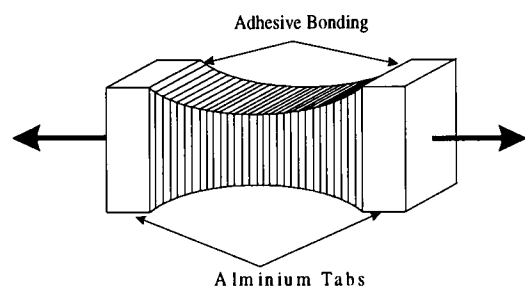


Figure 3.4 The Notched Rectangular Specimen.

3.3.4 The Hybrid Specimen

This comprises a flat rectangular specimen bonded to two aluminium strips which are

notched centrally to localise failure (Figure 3.5). Shear load is transmitted through these aluminium strips to the specimen [3.12]. The main problem with this test is the complex stress distribution through the aluminium strips and the specimen requiring lengthy and complex analysis. Additionally, only a unidirectional laminate configuration can be used for this test as the load transfer by shear through the aluminium strips requires a large shear area which, in turn, requires a long specimen.

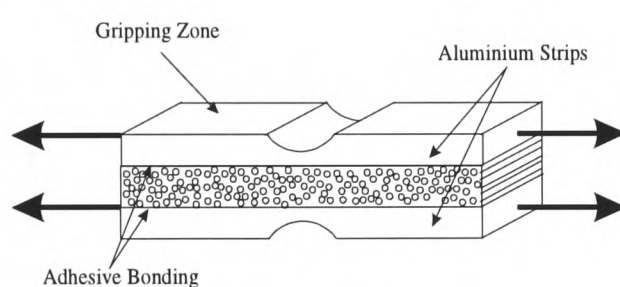


Figure 3.5 The Hybrid Specimen.

3.3.5 The Centrally Notched Beam Specimen

This comprises a beam with a central notch loaded under four-point bending (Figure 3.6) [3.13]. The presence of the notch induces a combination of interlaminar shear and interlaminar tensile stresses to the surface of the notch. Various combinations of these stresses are achieved by altering the size and shape of the notch. A low level of scatter associated with the delamination location was reported.

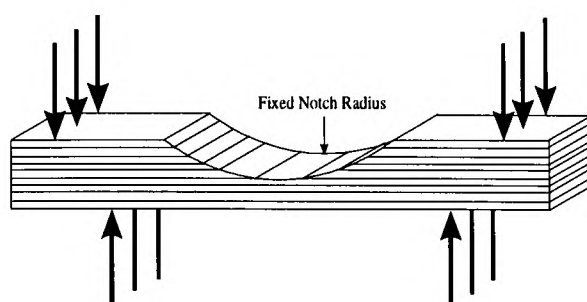


Figure 3.6 The Centrally Notched Beam Specimen.

3.3.6 The Curved Beam Specimen (C-Shape)

At the apex of a curved beam, loaded in tension, zero interlaminar tensile stress results at the free surfaces and reaches a maximum at some location through the thickness (Figure 3.7) [3.14 - 3.16]. The curved beam test is sensitive to the presence of voids, resin pockets and resin-rich interlayers. These defects typically stem from difficulties in achieving a uniform pressure distribution over the curved region of the beam whilst curing. Researchers have reported two distinct sets of data from curved beam tests:

1. A set with higher strength where the inherent flaws appear in the microstructure only.
2. A set with lower strength where voids and other defects are present in the macrostructure as observed by post-failure inspection of polished sections.

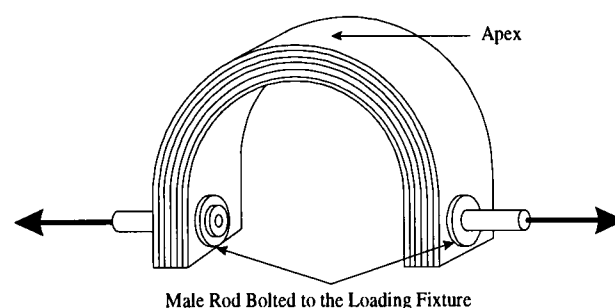


Figure 3.7 The Curved Beam Specimen.

3.3.7 The Four Point Bending Curved Beam Specimen (Ω -Shape)

This specimen geometry (Figure 3.8) induces a similar stress distribution at the apex as the C-shape specimen, although the specimen is loaded under four-point bending [3.17]. The main objective of this alternative loading mechanism was to avoid the premature failures at the steel tube insertions which were reported in the C-shape specimen tests. Similar results to the C-shaped specimen tests were reported with less prevalent premature failure. However, it was reported that manufacturing the profile with a uniform section thickness is difficult, especially

at the region A.

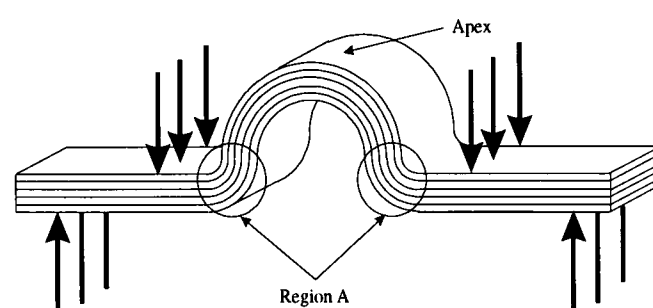


Figure 3.8 The Four Point Bending Curved Beam Specimen.

3.3.8 The Waisted Curved Beam Specimen

This is a derivative of the C-shape curved beam specimen with the addition of a reduction in width located at an angle ϕ from the loading axis (Figure 3.9). The waisting localises failure and in the waisted section both interlaminar shear and interlaminar tensile stresses are present [3.18]. Altering the angle of the waist location varies the ratio between interlaminar shear and interlaminar tensile stresses. The large scatter present in the results was attributed to the presence of manufacturing defects.

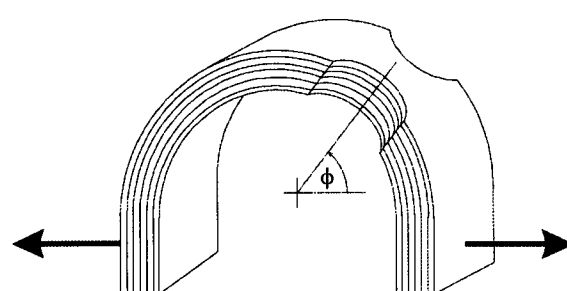


Figure 3.9 The Waisted Curved Beam Specimen.

3.3.9 The Hoop Specimen (U-shape)

When a U-shape specimen (Figure 3.10) is loaded in three-point bending, both interlaminar shear and interlaminar tensile stresses arise in the curved section [3.19]. Changing

the geometry of the specimen alters the ratio between the stresses in these two directions. The main problem with this test configuration is the complex boundary condition at the contact of the PTFE sliding tabs which requires lengthy analysis.

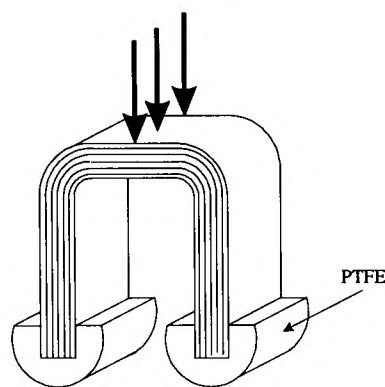


Figure 3.10 The Hoop Specimen.

3.3.10 The Rectangular Beam Specimen

Under four-point flexural loading, the tensile failure stress of a rectangular beam specimen (Figure 3.11) was determined by measurement of the strain at failure in the tensile zone along the beam [3.20]. This method limits the laminate configuration to be transversely isotropic in order to achieve the necessary high aspect ratio, L/t , where L and t are the length and thickness of the specimen respectively.

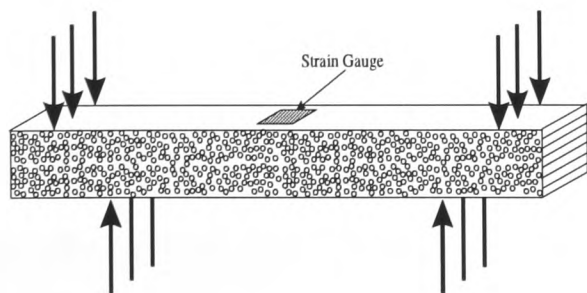


Figure 3.11 The Rectangular Beam Specimen.

3.3.11 The Rectangular Specimen with a Hole

This comprises a rectangular specimen with a through hole across the width (Figure 3.12) which is loaded uniaxially in compression [3.21, 3.12]. The use of this specimen geometry for interlaminar tensile strength is based upon analytical evidence of the presence of interlaminar tensile stress at the hole surface where the tangent is perpendicular to the loading direction. The advantage of this method is the simplicity of direct loading without special fixtures, though significant scatter in the results has been reported.

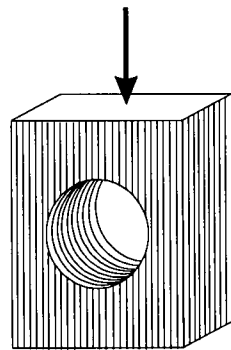


Figure 3.12 The Rectangular Specimen with a Hole.

3.4 Summary

Current specimen designs for characterising the interlaminar shear and interlaminar tensile strength, both in isolation and in combination, have been reviewed. Many of these have complex geometry requiring special moulding techniques or a series of grinding processes in order to achieve the desired shape. Ideally, the manufacturing process should be kept simple in order to minimise the possibility of defects stemming from these processes, i.e. machining damage and misalignment etc.. It should also be noted that the inconsistencies associated with the structural integrity of a given plate and any variations in the fibre volume fraction between specimen batches both contribute to scatter in the experimental results.

The short beam shear test and the notched tension and Iosipescu coupons are standardised methods with straight forward manufacturing processes and loading mechanisms. However, the question remains, with regard to the resulting non-uniform shear stress fields, as to the reliability of the short beam shear test and notched tension coupon tests.

As simple tensile test coupons, the cylindrical and notched rectangular prism specimens are more practical than the hybrid specimen which involves lengthy and detailed analysis and is also limited to a unidirectional lay-up configuration. Both cylindrical and notched rectangular specimens suffer problems associated with the discontinuity of the notch profile which causes premature failure at that point. Furthermore, grinding a short thin cylindrical shape (the specimen length is limited by the thickness of the parent plates) is difficult as the manufacturing processes induce vibrations which lead to breakages.

Despite a simple loading mechanism and manufacturing process, the largest scatter of results occurs with the rectangular specimen with a hole and is consequently impractical for use in its current form. Of all of the curved beam specimens, the C-shape specimen is the simplest to manufacture although care must be taken to prevent failure at the steel inserts.

For a study of the interlaminar stresses in combination, the waisted C-shape curved beam is more practical than the U-shape curved beam as the associated analysis is simpler. Additionally, if the plain C-shape specimen is being used, the waisted C-shape specimens may be manufactured from the same batch of material. However, the scatter associated with the waisted C-shape specimen is high.

3.5 The Specimens Selected for this Investigation

Subsequent to the literature review and a detailed study of current specimen designs, both direct and indirect methods were selected for this study for the characterisation of the

interlaminar mechanical properties. The final decision was driven by the following criteria:

1. Simple manufacturing processes in order to minimise the defect density.
2. Maximum utilisation of a single laminated plate for the various types of specimens in order to minimise variations in the results associated with the material constituents.
3. A simple loading mechanism.

For direct methods, the Iosipescu and Notched Rectangular Prism specimens were selected whilst the Centrally Notched Beam, Plain Curved Beam and Notched Curved Beam specimens were selected for the indirect methods. The Notched Curved Beam Specimen configuration is a new method and is described in Section 3.5.5. Each method lends itself well to simple loading conditions, i.e. simple tension, compression, shear, and flexure, using a standard testing machine.

3.5.1 The Iosipescu Specimen

The Iosipescu shear configuration was chosen for characterising the interlaminar shear properties as it has a more uniform stress field at the test section than the other methods. The UTC test configuration was employed in preference to the standard procedure [3.5] because this configuration facilitates improved control over specimen alignment as the load is applied [3.22]. An additional advantage is that the V-notched beam geometry has already been studied extensively and numerous analytical solutions are available for calculating the exact shear properties which include various correction factors.

3.5.2 The Notched Rectangular Prism Specimen

Previous investigators [3.23] have demonstrated that specimens machined in the through-thickness direction from laminates of thickness less than 20 mm provide reliable

strength determination. Therefore, short length Notched Rectangular Prism Specimens were selected and an attempt was made to resolve the aforementioned problems associated with the load path between the test rig and the specimen and the stress concentration around the notch profile. In order to determine the optimum notch geometry, an intensive FEA was conducted and is presented in Chapter 5. It has been demonstrated by Cui [3.18] that external dimensions of 6 mm (width) $\times$ 5 mm (breadth) for tensile specimens yield results which are independent of scale and thus these dimensions were adopted as the geometrical basis for a model.

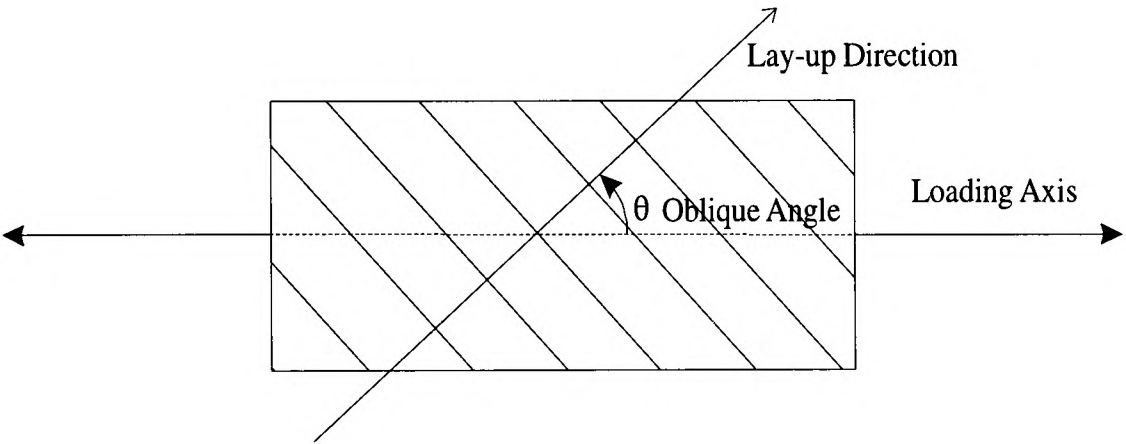


Figure 3.13 A Schematic Diagram of Oblique Loading.

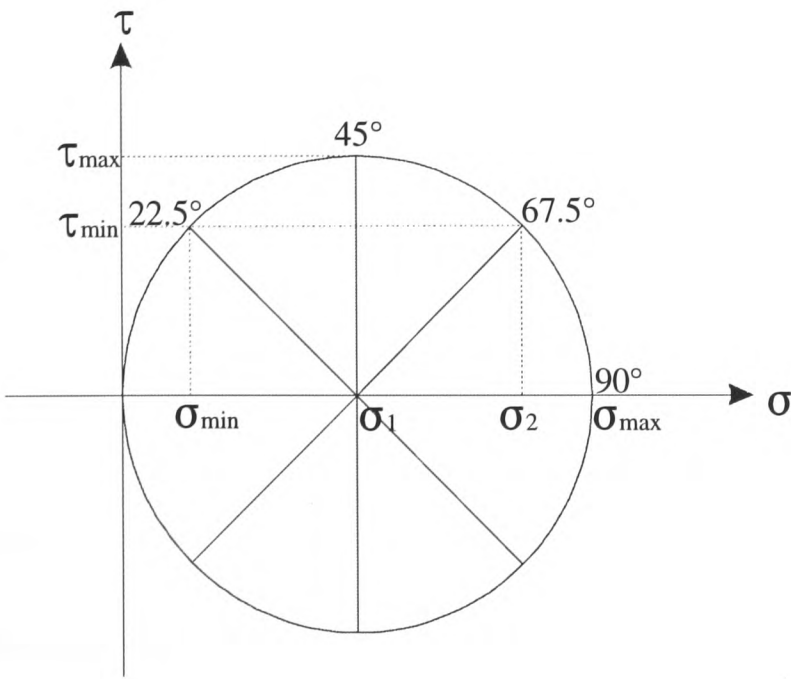


Figure 3.14 Local Stress Components due to an Oblique Angle.

The Notched Rectangular Prism Specimen was also used for testing combined stress under pure tensile loading, i.e. interlaminar shear and interlaminar tensile stresses. The material co-ordinates of this specimen have an oblique angle, θ , to the load axis as shown in Figure 3.13. By controlling this oblique angle, different stress ratios between interlaminar tension and interlaminar shear could be obtained. Using a simple Mohr’s circle analysis, shown in Figure 3.14, four different oblique angles were chosen in order to produce sufficient variations in the stress ratio. Table 3.1 summarises combinations of the interlaminar tension to interlaminar shear ratio.

Oblique Angle, θ	Interlaminar Tensile Stress	Interlaminar Shear Stress
22.5°	$\sigma_{\min}$	$\tau_{\min}$
45°	σ_1	$\tau_{\max}$
67.5°	σ_2	$\tau_{\min}$
90°	$\sigma_{\max}$	0

Table 3.1 Possible Combinations of Interlaminar Stresses.

3.5.3 The Centrally Notched Beam Specimen

The main benefit of this configuration is that the combination of stresses along the notch surface may be controlled by varying the notch depth without having to alter the notch radius. When a uniform beam is subjected to four point bending, the stress across the depth varies linearly from compressive at the top surface to tensile at the bottom surface. However, when a notch is introduced at the bottom surface, the stress distribution through the specimen depth becomes non-linear. More specifically, the presence of the notch induces interlaminar tensile, interlaminar shear and tensile in-plane stresses along the notch surface (Figure 3.15).

In order to obtain an exact picture of the stress distribution along the notch surface, a numerical analysis was conducted, the details of which are presented in Chapter 5. It has been established by Cui [3.18] that a 70 mm notch radius and test thicknesses, t , of 2, 3 and 4 mm are adequate in offering various combinations of stress and were, therefore, adopted for this study.

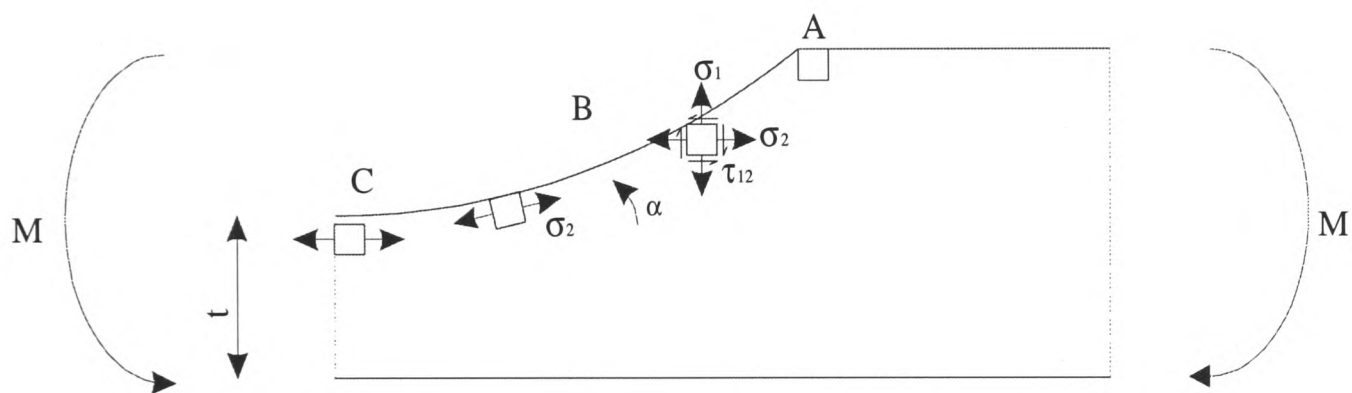


Figure 3.15 The Stress Distribution Along the Notch.

3.5.4 The Plain Curved Beam Specimen

This specimen geometry resembles that of many parts currently being used on actual structures, eg. webs, spars and flanges etc.. The test section of the curved beam specimen gives a better representation of composite structures than a flat tensile specimen and hence may provide more representative strength data for use in failure prediction. Figure 3.16 shows the geometry of the composite curved beam used in interlaminar tensile tests of thick composite materials. Because finite areas are required for the load application points, both ends of the curved beam must be extended slightly. The curved beam specimen consists of a semicircular curved region with straight regions at both ends, thus, under the application of the end forces, P , the loading axis will be offset by distance, d , from the centre of the specimen curvature. Therefore, the loading on the curved beam specimen is the sum of the end forces, P , and the

end moments, $M_e = Pd$. The circumferential stress is tensile at the inner radius and compressive at the outer radius.

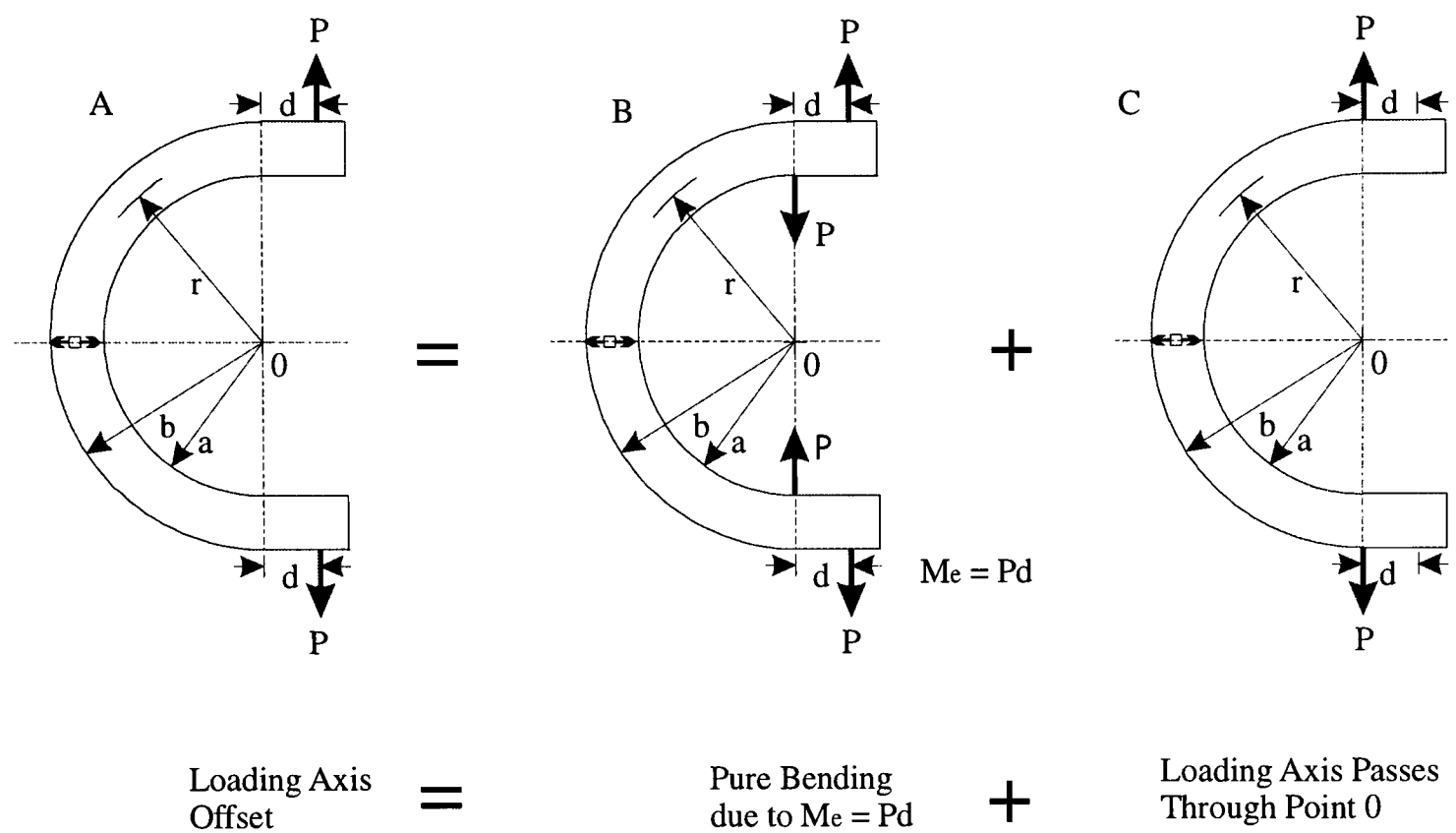


Figure 3.16 The Bending of Curved Beams by End Moments and Forces.

3.5.5 The Notched Curved Beam Specimen

The thickness of the Plain Curved Beam specimen, shown in Figure 3.7, was reduced at an angle, ϕ , from the apex in order to localise failure, Figure 3.17. This is a new method, which allows the investigation of interlaminar tensile and shear stresses in combination at the test section. The combination of stresses is shown in Figure 3.18. Initially the notch radius was selected to be one third of the mean thickness. Alteration of angular location of the notch whilst maintaining its radius yields variations in the interlaminar tensile to interlaminar shear stress ratio. The thickness of the specimen was reduced by only 30% in order to avoid ‘hinging’ at the reduced section. It was not possible to calculate an exact stress distribution in the specimen

analytically due to the complexity of the geometry, therefore, an FEA was conducted, the details of which are presented in the Chapter 5.

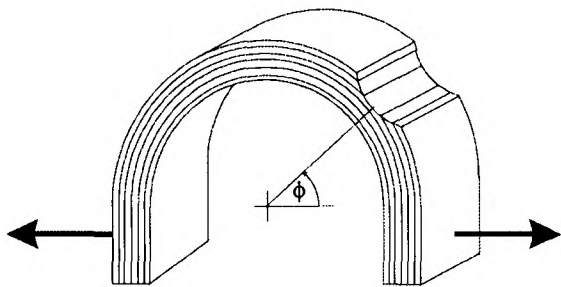


Figure 3.17 The Notched Curved Beam Specimen.

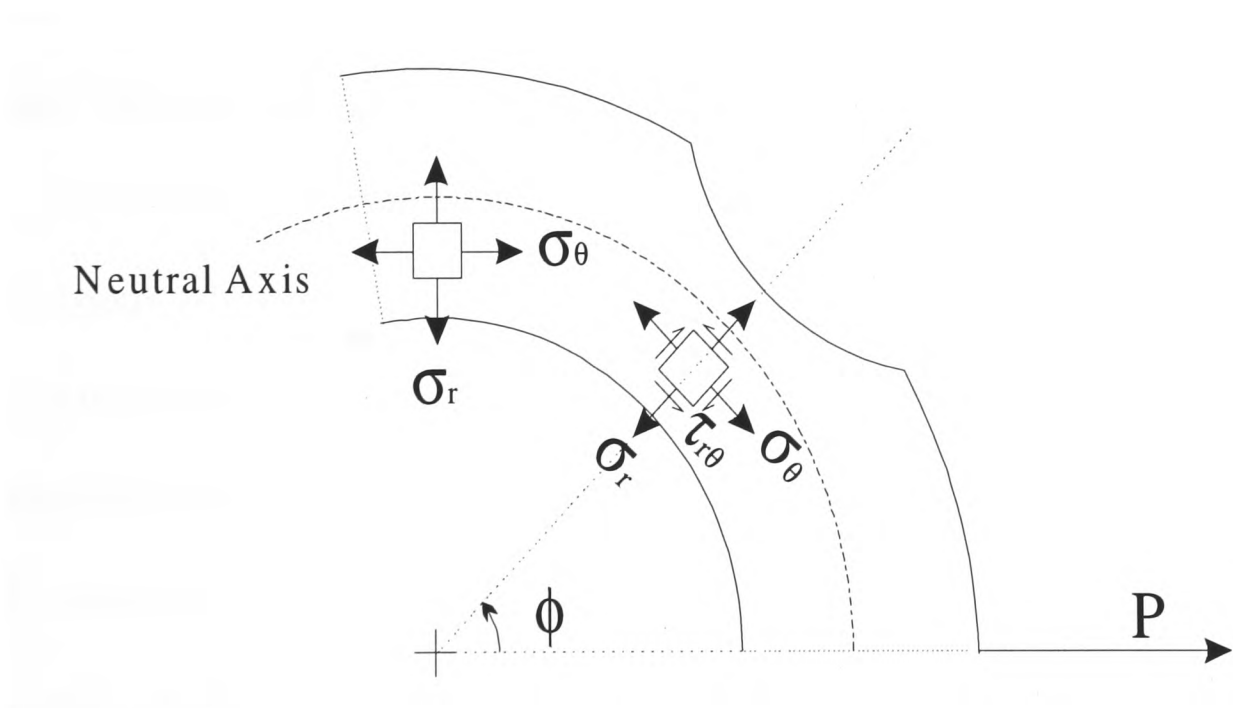


Figure 3.18 The Stress Distribution at Angle ϕ in a Curved Segment.

3.5.6 General Abbreviations for the Specimens Selected

Table 3.2 summarises the specimen types selected for this investigation and shows the abbreviations used for future reference.

Specimen Type	Abbreviation
Centrally Notched Beam Specimen	CNB
Iosipescu Specimen	IO
Plain Curved Beam Specimen	PCB
Notched Curved Beam Specimen	NCB
Notched Rectangular Prism Specimen	NRP

Table 3.2 General Abbreviations for the Specimen Types.

3.6 Materials Selection

Carbon fibre composites are widely used in applications requiring a high strength-to-weight ratio. There is great need for further experimental data regarding the through-thickness strength and the development of associated failure criteria. The characterisation of the various modes of failure of carbon fibre laminates is difficult because the features of the fracture surface can barely be seen, even under a microscope. A further difficulty is that, unlike glass fibre composites, the material is not opaque and thus the initiation and extent of failure can not be readily observed. In view of these difficulties, glass fibre laminates were considered to be more suitable for this investigation. It should be noted that the modes of failure remain identical for both materials as the fibre architecture is the same in both types of laminate, therefore the same failure criteria apply. The following commercially available materials were selected to ensure that the information obtained from this investigation would be useful to designers:

1. Ciba-Geigy¹ Fibredux 913 Glass/Epoxy unidirectional fibre prepreg (913UD).

In order to produce better comparison with woven composites, the cross-ply lay-up

¹ Hexcel Composite

configuration was chosen for unidirectional laminates. Free edge effects, which are known to influence the results, are unavoidable and, therefore, must be tolerated.

2. Ciba-Geigy Fibredux 914 Epoxy sheet and Non-Crimp Fabric (914NCF).

This is a new non-crimp fabric which yields improvements in interlaminar strength under a combination of interlaminar shear and interlaminar tensile stresses. A non-crimp fabric is a unidirectional fibre mat laid up in a $0^\circ/-45^\circ/+45^\circ/90^\circ$ orientation and stitched together crudely to maintain the relative orientations.

3. Ciba-Geigy Fibredux 916 Glass/Epoxy Woven prepreg (916W).

This material shows potential improvements with respect to the interlaminar tensile strength due to the complex fibre interactions introduced by the plain weave pattern.

3.7 Laminate Configurations

It is important to select a laminate configuration which is representative of those commonly employed in industry. To this end, a laminate thickness of 5 mm was chosen as a good average thickness of widely used structural composite components, i.e. flanges, webs and spars etc. [3.25, 3.26]. A 13 mm thick laminate was selected for the production of thicker laminated composite components, i.e. angle brackets and leading edges etc. [3.27, 3.28]. It should be noted that the 13 mm thickness represents the maximum value attainable via the use of a normal curing cycle, i.e. one which has been proven effective by composite manufacturers.

In order to reduce scatter in the results due to quality differences between specimen batches, a flat panel size of 200 mm $\times$ 150 mm was selected for the 5 mm laminate thickness and was used to produce the Centrally Notched Beam and Iosipescu specimens. Panel sizes of 150 mm $\times$ 150 mm for 913UD and 914NCF, and 220 mm $\times$ 220 mm for 916W, were selected for the 13 mm laminate thickness and were used to produce the Notched Rectangular Prism

specimens.

Using a curved plate, previous researchers [3.15, 3.18] found that when the dimensions of the inner radii and the width were 15.9 mm and 10 mm respectively, the system became independent of scale. A rectangular template size of 230 mm × 105 mm was selected such that the Plain Curved Beam and Notched Curved Beam specimens could be produced from the same parent plate. This avoids fluctuations in the results due to differences in quality between specimen batches.

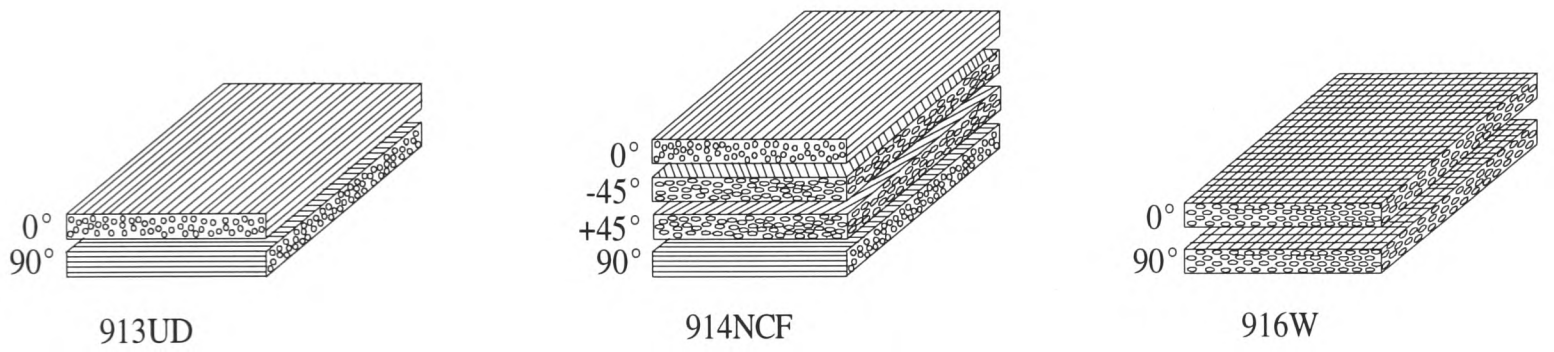


Figure 3.19 A Schematic Illustration of the Chosen Material Configurations.

The lay-up configurations of the laminates manufactured for this investigation are summarised in Figure 3.19. For the 5 mm thick flat and curved plates, 913UD was laid up in a $[(0^\circ/\overline{90^\circ})_{39}]_s$ orientation, 39 plies being required to produce the finished thickness. Similarly, 916W was laid up in a $[(0^\circ/\overline{90^\circ})_{25}]_s$ orientation with the warp direction between similarly oriented layers being maintained in an attempt to reduce anisotropy since the strength in the warp direction is higher than that in the weft direction [3.29]. Finally, Fibredux 914 epoxy sheet and non-crimp fabric were used to produce a quasi-isotropic laminate panel which was constructed using 8 glass mats and 6 resin sheets in a $[(0^\circ/-45^\circ/+45^\circ/90^\circ)_8]_s$ orientation. For the 13 mm plates, 913UD was laid up in a $[(0^\circ/\overline{90^\circ})_{111}]_s$ orientation, 111 plies being required to produce a finished thickness of 13.1 mm. Similarly, 916W was laid up in a $[(0^\circ/\overline{90^\circ})_{71}]_s$

orientation, 71 plies being required to produce a finished thickness of approximately 13.8 mm. Finally, 914NCF was laid up in a $[(0^\circ/-45^\circ/+45^\circ/90^\circ)_{22}]_s$ orientation with 22 glass mats and 16 resin sheets being required to produce a finished thickness of 14.1 mm.

3.8 The Manufacture of the Laminates and Specimens

Fibredux 913 and Fibredux 916 prepregs were stored in drum form in a freezer at -17 °C in accordance with the instructions provided by Ciba-Geigy. Laminae were cut to the template size from these drums, however, due to its brittle nature when frozen, the Fibredux 914 epoxy sheet drum had to be defrosted before cutting. The previously cut sheets of material were then frozen again before the laying up process began and were kept cool whilst this process was conducted. This avoided problems associated with the adhesive nature of the prepreg which attracts dust and other particulates.

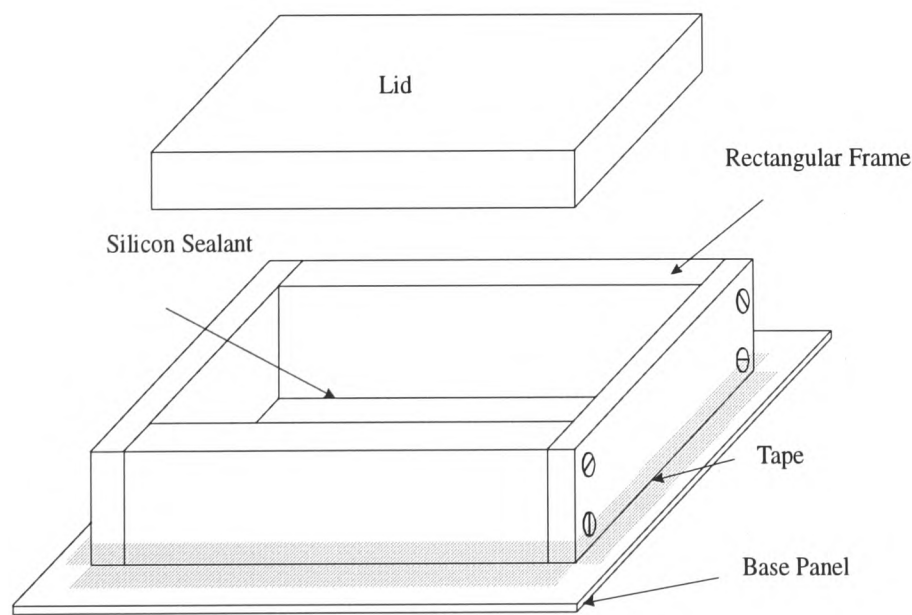


Figure 3.20 The Mould Configuration for Rectangular Plates.

Prior to the installation of the ply stack, the frame and the base of the mould were wrapped with PTFE coated cloth using heat resistant sellotape and were subsequently attached together with

silicon sealant to avoid resin leakage at the interface. The ply stack, with bleed cloths placed on top, was then placed in a rectangular frame (Figure 3.20), the function of which was to prevent the thick laminate from skewing in the horizontal direction when subjected to the high pressures applied during the curing process.

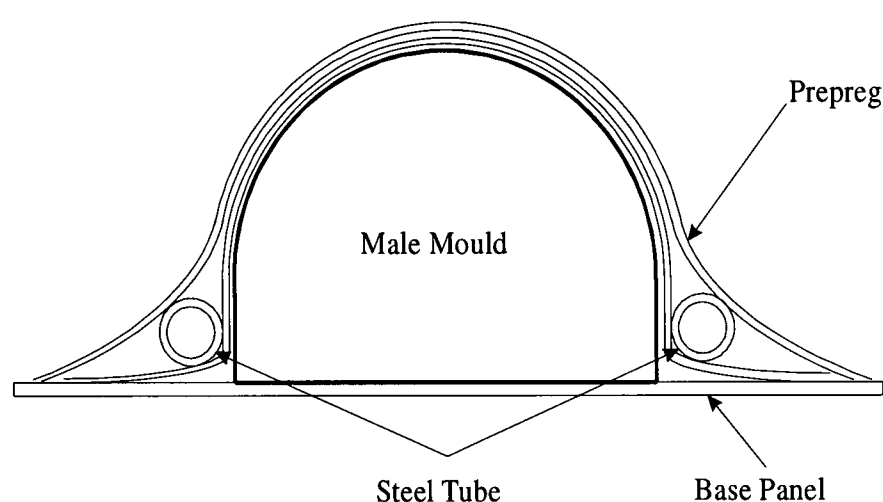


Figure 3.21 The Mould Configuration for Curved Plates.

The curved plates (C-section) were fabricated from prepreg utilising a male mould for both the plain and notched curved beam specimens. A steel tube was placed between the plies at each side of the male mould (Figure 3.21). The most suitable position for the steel tubes, in order to avoid premature failure at the steel insert, was found to be one third of the total plies required for the finished laminate. The plies were periodically debulked during the lay-up process using vacuum compaction and the bleed cloths were then placed on the final ply stack in order to control the fibre volume fraction.

Finally, the entire panel was wrapped with Kapton film in order to avoid accidental resin leakage blocking the vacuum pump prior to curing. All panels were vacuum bagged and cured in an autoclave oven in accordance with the specifications of the manufacturer (Appendix A). Also in accordance with the instructions provided by the manufacturer, 913UD

and 916W did not undergo a postcure cycle, only 914NCF was postcured at 190 °C for 4 hours.

After manufacture, each panel was ultrasonic C-scanned to ascertain the panel quality. Defects were detected along the edges of all of the panels manufactured and hence, in order to eliminate these defects, 10 - 15 mm of material was removed from each edge using a diamond grinding wheel cooled with water. The use of ultrasonic C-scans in detecting macroscopic defects in curved laminated components is extremely difficult and thus the C-specimen batches were not C-scanned. However, after grinding, the quality of the curved beam specimens was ascertained by inspection with a low power microscope.

3.8.1 The Iosipescu Specimen

The Iosipescu specimen was miniaturised in order to allow its manufacture from the same panel as the centrally notched beam specimens. The dimensions used were 9.5 mm square by 5 mm thick. The thickness was carefully controlled to provide good surface flatness. 10.5 - 11 mm wide strips were cut from the panel and subsequently the width was reduced to 9.5 mm using a 38A60 grit grinding wheel spinning at 2500 rpm with CIMSTAR BF404 cooling fluid. As recommended by a previous study at the UTC [3.1, 3.22] a 90° V-notch was chosen. These V-notches were ground into both sides of each bar with a 38A100 grit grinding wheel rotating at 2500 rpm with CIMSTAR BF404 cooling fluid. These notched bars were then cut into 10.5 - 11 mm wide pieces using a diamond wheel. The aforementioned procedure was repeated to reduce the width and again grind V-notches into both the remaining plain sides to complete each specimen.

The notch roots were inspected using a low power microscope and found to have a fillet radius of approximately 0.25 mm. The surface condition of the V-notch was also inspected by the same microscope at a greater magnification and, as a result, a few specimens were

discarded due to the identification of damage caused during the grinding process.

Both ends of the V-notched specimens were bonded to the loading rig to make up a complete Iosipescu specimen. This was done using Ecobond 45, a 2 part epoxy adhesive with a low temperature curing cycle.

3.8.2 The Notched Rectangular Prism Specimen

All of the 13 mm plates were surface ground with a 38A60 grit grinding wheel using CIMSTAR BF404 cooling fluid prior to specimen manufacture. Square section strips were then cut from the plate using a circular diamond wheel and water coolant. These strips were then mounted in a special fixture at oblique angles of 22.5°, 45°, and 67.5° respectively (Figure 3.22). This allowed smaller section strip to be generated with the laminates fixed in these orientations. The grinding was performed using a 38A100 grit grinding wheel spinning at 2500 rpm with CIMSTAR BF404 cooling fluid. This oblique angled strip was cut into approximately 6.5 mm (width) × 5.5 mm (breadth) × 12 mm (length) pieces.

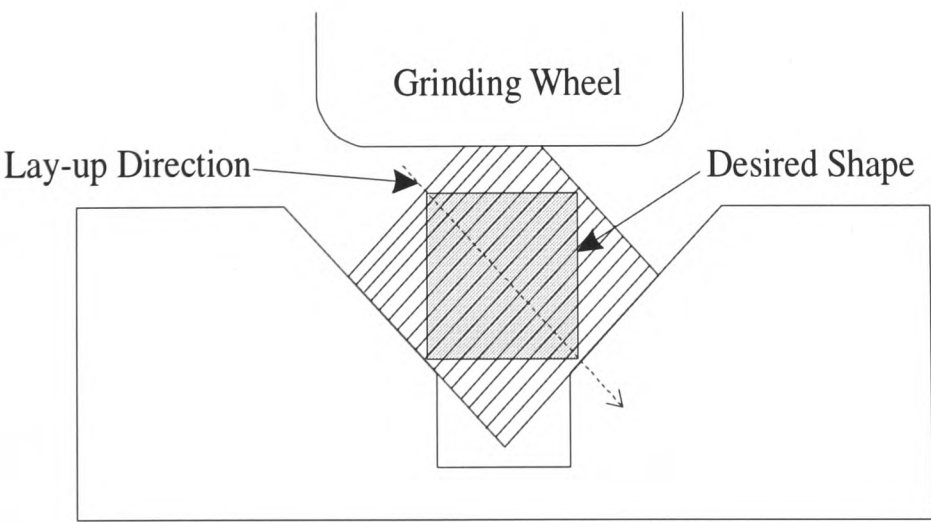


Figure 3.22 The Fixture for Grinding the 45° Orientation.

The width was ground using a 38A100 grit grinding wheel with CIMSTAR BF404 cooling

fluid in order to give a polished surface. After a series of FE analyses, the notch profile shown in Figure 3.23 was chosen as it yielded the least stress concentration factor at 1.09. This profile consisted of a 2 mm straight section and a notch radius of 49 mm. The straight section at the middle is needed for strain gauging. A 38A100 grit grinding wheel was profiled via a pantograph system attached to the grinding machine. Finally, the notch was ground on both sides of the specimens using this profiled grinding wheel. After these processes, all of the specimens were quality assessed using a low power microscope and any specimens with noticeable macroscopic damage were discarded. However, at least 5 specimens for each material and each angle were prepared.

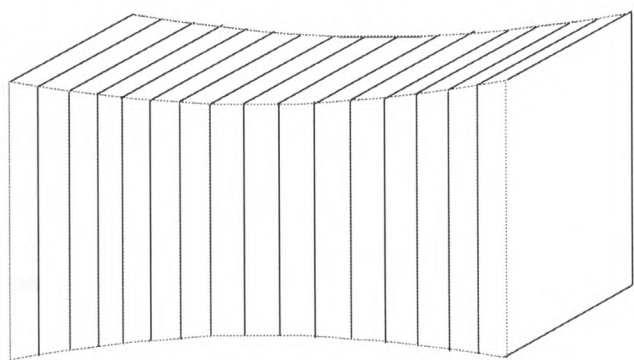


Figure 3.23 A 417 % Scale of the Chosen Notch Configuration.

A specimen was then attached at both ends to a loading fixture using 3M Scotch-Weld 9323 (a two part epoxy) as shown in Figure 3.24. The fixture overlaps the specimens on all four sides in order to provide extra strength. It can be seen from Figure 3.24 that the notch profile produces a pocket at the interface between the specimen and the loading fixture. Filling this pocket with adhesive was thought to reduce the stress concentration at the specimen ends as a previous researcher has found that the stress peaks along the edges and at the corners could be reduced by chamfering the adherends and filling the resulting gaps with extra resin or

adhesive [3.6]. For the purpose of keeping good alignment a slotted fixture produced from PTFE was used to cure the adhesive. The completed specimens were cured using this fixture in a preheated oven at 60°C for 2 hours.

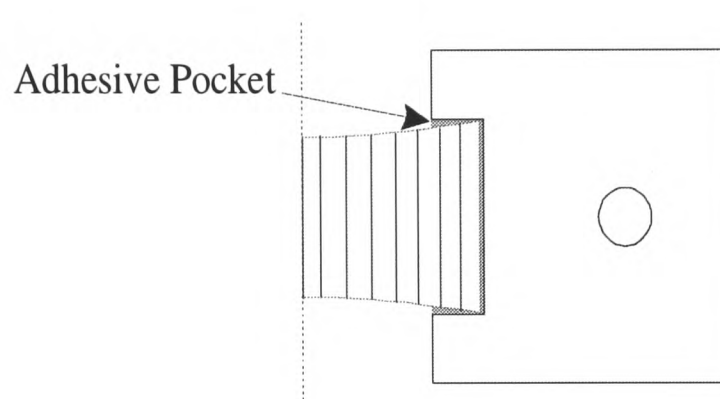


Figure 3.24 The Loading Fixture Configuration.

3.8.3 The Centrally Notched Beam Specimen

All of the 5 mm panels were ground to provide flat surfaces using a 38A60 grit grinding wheel rotating at 2500 rpm with CIMSTAR BF404 cooling fluid. The panels were cut into strips 148 mm long and approximately 11.5 - 12 mm wide using a circular diamond saw with water coolant. The width of each strip was then reduced to 10 mm using a 38A60 grit grinding wheel spinning at 2500 rpm with CIMSTAR BF404 cooling fluid in order to give a highly polished surface and square edges which would, in turn, reduce the risk of failure due to surface flaws. The notches were ground using a 38A100 grit water cooled grinding wheel rotating at 3600 rpm. Initially, when machining the 916W specimen with a notch radius of 69.9 mm, the grinding process was found to generate excessive amounts of heat resulting in clogging of the wheel grit. This problem was remedied by reducing the notch radius to 68.9 mm and increasing the flow rate of the cooling fluid. In total, forty five specimens (5 for each test section thickness and each material) were prepared and inspected with a low power microscope to maintain

consistent quality.

3.8.4 The Plain and Notched Curved Beam Specimen

Using the previously manufactured curved plates, 11.5 - 12 mm wide specimens were cut using a diamond wheel cooled with water. A 38A60 grit grinding wheel rotating at 2500 rpm cooled by CIMSTAR BF404 fluid was used to reduce the width to 10 mm. This process yielded a highly polished surface with square edges. A notch radius of 4.79 mm was then ground into the Notched Curved Beam specimen using a 38A100 grit water cooled grinding wheel spinning at 3600 rpm. This notch was located at 45° from the apex of the curved beam and reduced the thickness by 30%. 5 specimens were prepared for each type of the curved beam specimen. A low power microscope was used to confirm macroscopic quality.

References

- 3.1 Xing Y.M., Poon C.Y. and Ruiz C., A Whole-Field Strain Analysis of the Iosipescu Specimen and Evaluation of Experimental Errors, Composite Science and Technology, Vol.47, 1993, p251-259.
- 3.2 Grédiac M., Pierron F. and Vautrin A., The Iosipescu In-Plane Shear Test Applied to Composites: A New Approach Based on Displacement Field Processing, Composite Science and Technology, Vol.51, 1994, p409-417.
- 3.3 Zhou G., Green E.R. and Morrison C., In-Plane and Interlaminar Properties of Carbon/Epoxy Laminates, Composite Science and Technology, Vol.55, 1995, p187-193.
- 3.4 Broughton W.R. and Sims G.D., An Overview of Through-Thickness Test Methods for Polymer Matrix Composites, National Physical Laboratory Report, DMM(A)148, 1994.

- 3.5 ASTM Standards and Literature References for Composites Materials, 2nd Edition, American Society for Testing and Materials, Philadelphia, 1990.
- 3.6 Harris A. and Orringer O., Investigation of Angle-Ply Delamination Specimen for Interlaminar Strength Test, Journal of Composite Materials, Vol.12, 1978, p285-299.
- 3.7 Walrath D.E. and Adams D.F., The Iosipescu Shear Test as Applied to Composite Materials, Experimental Mechanics, Vol.23, No.1, 1983, p105-110.
- 3.8 Tarnopolskii Y.M. and Kinics T., Static Test Methods for Composites, Ed. Lubin G., Van Nostrand Reinhold Co., New York, 1985.
- 3.9 Hallet S., Small Specimen Impact Testing and Modelling of Carbon Fibre T300/914, D.Phil Thesis, University of Oxford, 1997.
- 3.10 O'Brien T.K. and Salperkar S.A., Scale Effects on the Transverse Tensile Strength of Graphite/Epoxy Composites, Composite Materials: Testing and Design (Eleventh Volume), ASTM STP 1206, American Society for Testing and Materials, 1993, p23-52.
- 3.11 Mespoulet S., Hodgkinson J.M., Matthews F.L., Hitchings D. and Robinson P., A Novel Test Method to Determine the Through-Thickness Tensile Properties of Long Fibre Reinforced Composites, Proceedings, ECCM-7, Seventh European Conference on Composite Materials, Vol.2, 1996, p131-137.
- 3.12 Ishai O., Strengthening of Composite Materials in the Third Dimension, Annual Report, Technion Research and Development Foundation Ltd., Private Communication, 1995.
- 3.13 Ruiz C., Cui G.Y. and Lewin R.E., Through-Thickness Strength of Fibre-Reinforced Polymer Matrix Composite Laminates, Proceedings, ECCM-7, Seventh European Conference on Composite Materials, Vol.2, 1996, p139-144.
- 3.14 Hiel C.C., Smich M. and Chappell D.P., A Curved Beam Test Specimen for

Determining the Interlaminar Tensile Strength of a Laminated Composite, *Journal of Composite Materials*, Vol.25, 1991, p854-868.

- 3.15 Hognestad G., Interlaminar Strength Determination using C-Shaped Specimens, Rolls-Royce University Technology Centre for Solid Mechanics Report, No.25, May 1992.
- 3.16 Jackson W.C. and Martin R.H., An Interlaminar Tension Strength Specimen, NASA Technical Memorandum 107623, AVSCOM Technical Report 92-B-008, 1992.
- 3.17 Wisnom M.R. and Jones M.I., Size Effects in Interlaminar Tensile and Shear Strength of Unidirectional Glass Fibre-Epoxy, *Journal of Reinforced Plastics and Composites*, Vol.15, 1996, p2-15.
- 3.18 Cui G.Y., Experimental Study of the Through-Thickness Strength of Laminated Composites., D.Phil Thesis, University of Oxford, 1994.
- 3.19 Wisnom M.R. and Petrossian Z., Through-Thickness Failure Prediction for Fibre Reinforced Composites, *Proceedings, NAFEMS World Congress 97*, Vol.1, 1997, p148-159.
- 3.20 Ishai O. and Gandelsman M., Strengthening of Composite Materials in the Third Dimension, Annual Report, Technion Research and Development Foundation Ltd., Private Communication, 1996.
- 3.21 Cui G.Y., Establishment of a Through-Thickness Failure Criterion for Laminated Composites, Rolls-Royce University Technology Centre for Solid Mechanics Report, No.33, Nov 1992.
- 3.22 Hognestad G., Analysis of Composite Flange Connections and Determination of Interlaminar Strength, D.Phil Thesis, University of Oxford, 1993.
- 3.23 Kitching R., Tan A.L. and Abu-Mansur T.M.N., The Influence of Through-Thickness Properties on Glass Reinforced Plastics Laminated Structures, *Composite Structures*,

Vol.2, 1984, p105-116.

- 3.24 Hognestad G., Manufacture, Testing and Analysis of Composite Flanges, Rolls-Royce University Technology Centre for Solid Mechanics Report, No.43, Nov 1993.
- 3.25 Johnstone C.D., Testing of Bolted Composite Flanged Specimens, Rolls-Royce University Technology Centre for Solid Mechanics Report, No.54, Aug 1994.
- 3.26 Johnstone C.D., Testing of Tay Nose Spinners for Flange Strength, Rolls-Royce University Technology Centre for Solid Mechanics Report, No.62, April 1995.
- 3.27 Fernando S., Strength of Bolted Bracket Attachments in Composite Structures, University of Oxford, Forth Year Project Report, 1995.
- 3.28 Byun J.H. and Chow T.W., Modelling and Characterization of Textile Structural Composites: A Reveiw, Journal of Strain Analysis, Vol.24, No.4, 1989, p65-74.
- 3.29 Harding J., Saka K. and Taylor M.E.C., Behaviour of Fibre-Reinforced Composites Under Dynamic Tension, University of Oxford, Report No. OUEL 1654/86, 1986.

Chapter 4

Measurement of Material Response

4.1 Introduction

The experiments detailed herein were conducted using an Instron tension/ compression testing machine, which is a standard screw driven loading machine equipped with a digital data acquisition system. The specimens were attached between a driven cross-member and a stationary anchor using a loading fixture. Furthermore, a load cell was built into the driven cross-member. Detailed descriptions of the fixtures used for each specimen type are presented later in this chapter.

Each test involved the extension or compression of the specimens until catastrophic failure occurred. The loads and displacements were recorded at a sampling rate of 50 data points/min and traces for these slow rates of strain are presented in the subsequent sections. This raw data was then converted from ASCII format into binary format using the Microsoft Excel computer spreadsheet program which offers good graphing facilities.

4.2 The Elastic Properties of the Laminates

The engineering properties of interest in this investigation are the elastic constants in the principal material directions. Figure 4.1 depicts the coordinate system used throughout this study. Nine independent elastic parameters are required for 3-D numerical analysis, namely:

$$E_{11}, E_{22}, E_{33}, \nu_{12}, \nu_{31}, \nu_{23}, G_{12}, G_{13}, G_{23}$$

where the subscript 1 denotes the through-thickness direction, subscript 2 denotes the 0° fibre direction and subscript 3 denotes the 90° fibre direction. It is important to note that, with regard to the Poisson's ratios, ν_{ij} , the subscript i refers to the direction of the applied stress whilst the subscript j refers to the direction of the associated lateral strain. Thus, for $\sigma_i \neq 0$, $i \neq j$, and $i, j = 1, 2, 3$:

$$\nu_{ij} = \frac{-\epsilon_j}{\epsilon_i} \quad (4.1)$$

The shear moduli, G_{ij} , refer to the shear stress-strain relationship in the $i - j$ plane. $G_{ij} = G_{ji}$ was assumed for the shear modulus corresponding to the shear stiffness of the associated plane.

The specially orthotropic material configuration reduces the number of independent elastic parameters to five, i.e. E_{11} , E_{22} , ν_{31} , ν_{23} , and G_{12} . These constants were determined from three non-destructive compression tests. Two specimens were loaded in the through-thickness and fibre directions to determine the elastic moduli E_{11} and E_{22} and the Poisson's ratios ν_{31} and ν_{23} . The third specimen was loaded in the off-axis direction to determine the out-of-plane shear modulus, G_{12} , as this method was found to provide more accurate results [4.1]. Finally, the constants ν_{12} and G_{23} were calculated using eqⁿs. 4.2 and 4.3 respectively.

$$\nu_{12} = \frac{E_{11}}{E_{22}} \nu_{21} \quad (4.2)$$

$$G_{23} = \frac{E_{22}}{2(1 + \nu_{23})} \quad (4.3)$$

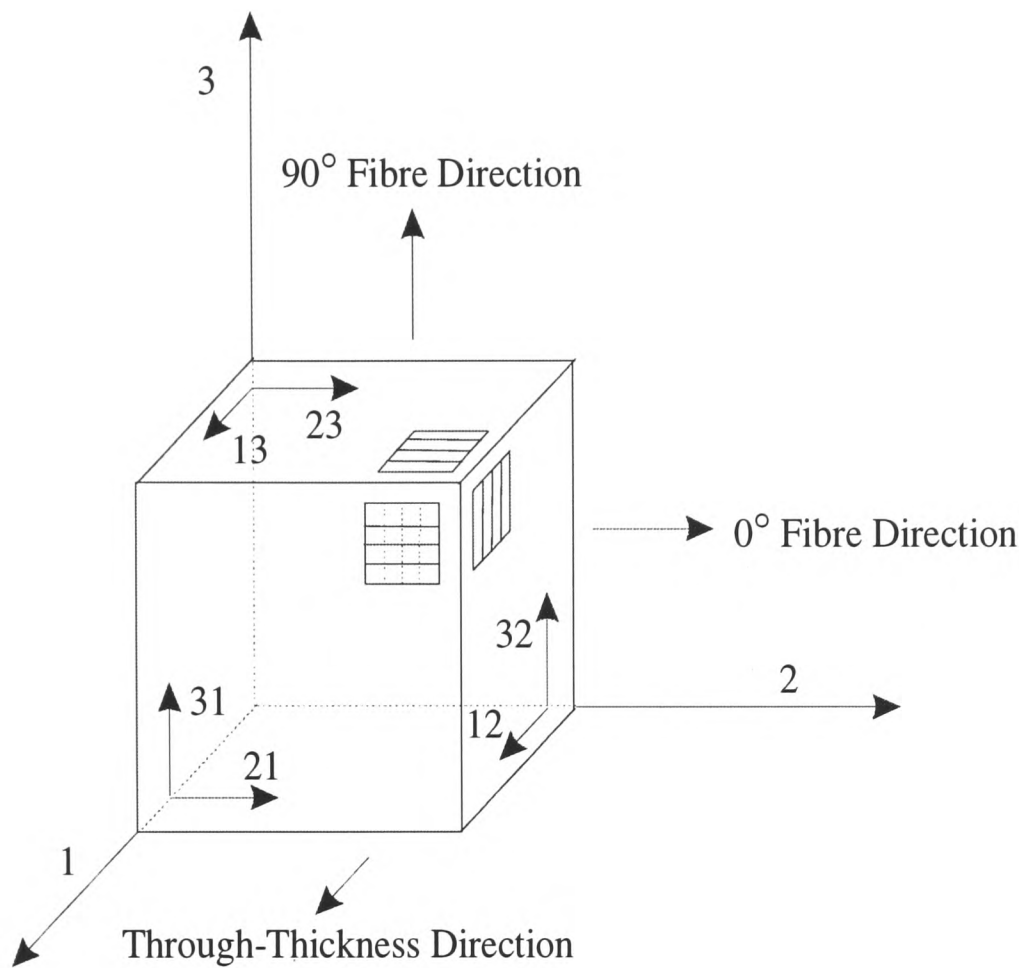


Figure 4.1 The Specially Orthotropic Material Coordinate System.

4.2.1 Fibre Volume Fractions

The volume fraction of the fibres in a composite is a very important parameter which controls, to a great extent, the mechanical properties of a laminate. There are several existing methods used to determine fibre volume fraction [4.2]. One method used in this investigation was to determine the density of the finished composite, ρ_c , and then calculate the fibre volume fraction knowing the density of the fibres, ρ_f and the matrix, ρ_m , (Table 4.1). On this basis, the fibre volume fraction is given by:

$$V_f = \frac{\rho_c - \rho_m}{\rho_f - \rho_m} \tag{4.4}$$

The mean dimensions of the previously manufactured plates were determined by measuring the thickness, width, and length in three locations with a vernier caliper and then

calculating the corresponding volume. For the curved plates, several small pieces were cut from tested specimens and a similar procedure was again conducted. The mass, which was measured using a weighing balance, was then divided by the calculated volume in order to obtain the mean laminate density. Using eqⁿ. 4.4, the fibre volume fractions for each manufactured plate were calculated and are tabulated in Table 4.2.

	E-Glass	913 Epoxy	914 Epoxy	916 Epoxy
Density, ρ (g/cm ³)	2.58	1.23	1.29	1.30
Poisson's Ratio, ν	0.22	N/A	0.410	0.375
Young's Modulus, E (GPa)	73.03	3.39	3.90	4.17
Shear Modulus, G (GPa)	30.11	N/A	1.40	1.52

N/A refers to parameters which were not available

Table 4.1 The Fibre and Epoxy Properties [4.3].

	913UD	914NCF	916W
5 mm Plate	0.62	0.57	0.49
13 mm Plate	0.61	0.57	0.50
Curved Plate	0.63	0.61	0.51

Table 4.2 The Fibre Volume Fraction for Each Plate.

4.2.2 The Manufacture of the Specimens

The type I and type II cubic specimens were roughly cut from the 13 mm thick plates using a water cooled circular diamond wheel and were subsequently finish ground using a 38A100 grit grinding wheel rotating at 2500 rpm with CIMSTAR BF404 cooling fluid to

provide a polished surface finish. For the off-axis specimen (type III), a strip of a square cross section was cut using a water cooled circular diamond wheel. An oblique angle was then generated using the same procedure as described in Section 3.8.2. This 45° off-axis strip was then cut and ground into cubic pieces using the same method as that described for the type I and type II specimens.

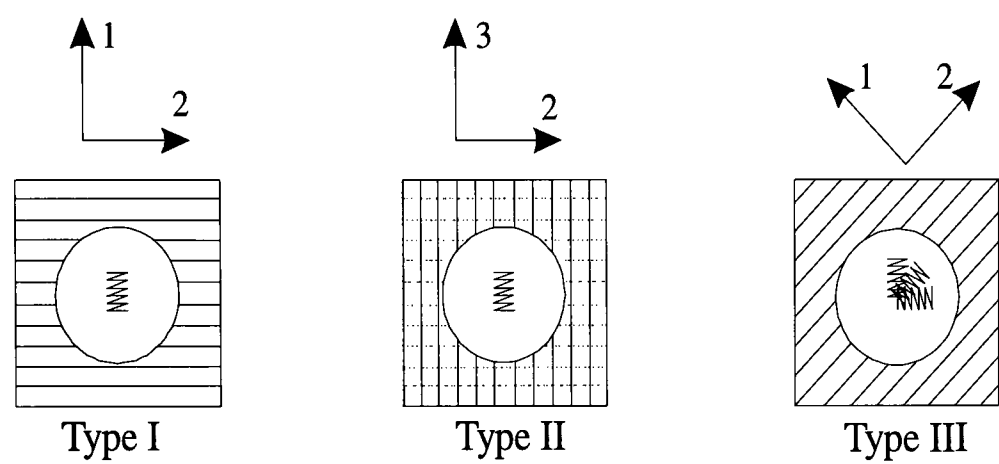


Figure 4.2 The Strain Gauge Orientations Relative to the Lay-Up Direction.

Two Kyowa KFC-2-D16-23 bi-directional strain gauges were attached in parallel on both sides of the type I and type II specimens using Kyowa strain gauge cement CC-33A. Thus, the differences between two strain gauges may be measured to ensure proper alignment of the applied load and to provide a better average. Figure 4.2 presents the orientation of the strain gauges relative to the laminate lay-up direction. For the type III specimen, two Kyowa KFG-1-120-D17-11 triple-strain-gauge rosettes were attached in parallel on both sides of the specimen, again using CC-33A. Since the strain gauges used were designed for steel, a dummy gauge was required for each measured gauge in order to compensate for thermal expansion mismatch which causes unpredictable zero reading shift in the output. These dummy gauges were attached to a scrap piece of the same material using the aforementioned procedure. It should be noted that the orientation of the dummy gauges, relative to the material lay-up direction,

were kept identical to that of the associated measured gauge.

4.2.3 Experimental Procedure

The elastic properties were obtained by means of compression tests. Figure 4.3 presents a schematic illustration of the test rig. Great care was taken to align the specimen with the load axis and, to this end, the centre of the specimen and the upper and lower parts of the rig were marked. The load was applied to the upper rig through a ball bearing to ensure the application of uniform pressure to the specimen. A thin PTFE film was inserted between the specimen surfaces and the mating surfaces of the rig in order to minimise edge effects due to friction. A subsequent improvement in alignment was achieved by examining the strain gauge readings resulting from the application of a small load and adjusting the specimen position until the two values became equal. Once the rig was set up, a small load within the elastic region was applied to the specimen at a rate of 0.1 mm/min. Since this test is non-destructive, it was repeated twice with each specimen. Additionally, the type I specimens were rotated through 90° in order to obtain the Young’s modulus in the fibre direction and facilitate a comparison with the type II specimens.

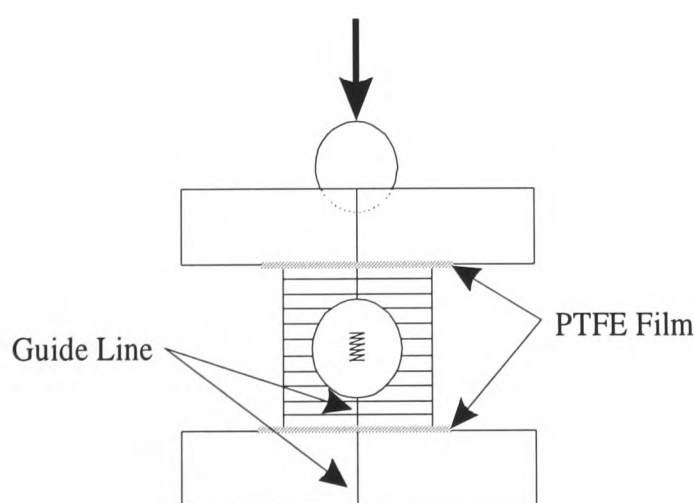


Figure 4.3 A schematic Illustration of a Simple Compression Test.

The output of the strain gauges formed the arms of Wheatstone bridge circuits. The out-of-balance voltages of the bridge circuits were amplified and stored on Datalab DL912s digital transient recorders. The calculation method for the strain is presented in Appendix B.

4.2.4 Results and Discussion

A calculation of the elastic properties using the rule of mixtures, eqⁿs. 4.5 - 4.8, was performed in order to compare the theoretical and experimental values obtained for the 13 mm plates (Table 4.3). The assumptions made for calculating elastic properties for cross ply, quasi-isotropic ply and woven ply configurations are explained in detail in the following sections. The longitudinal modulus, E_L , is given by:

$$E_L = E_f v_f + E_m (1 - v_f) \quad (4.5)$$

where E_f , v_f , and E_m are the fibre Young's modulus, fibre volume fraction and matrix Young's modulus respectively.

The transverse modulus, E_T , is given by:

$$\frac{1}{E_T} = \frac{v_f}{E_f} + \frac{(1 - v_f)}{E_m} \quad (4.6)$$

The Poisson's ratio, v_{LT} , is given by:

$$v_{LT} = v_{fLT} v_f + v_m (1 - v_f) \quad (4.7)$$

where, v_{fLT} and v_m are the fibre and matrix Poisson's ratios respectively.

The shear modulus, G_{LT} , is given by:

$$\frac{1}{G_{LT}} = \frac{v_f}{G_{fLT}} + \frac{(1 - v_f)}{G_m} \quad (4.8)$$

where, G_{fLT} and G_m are the fibre and matrix shear moduli respectively. The experimentally obtained elastic moduli are tabulated in Table 4.4 and the dependent elastic properties were

calculated using Eqⁿs. 4.2 and 4.3 (Table 4.5). The currently available manufacture’s material properties data are also presented in Table 4.6.

	913UD	914NCF	916W
Young’s Modulus, E_{33} (GPa)	24.63	20.80	21.39
Young’s Modulus, E_{11} (GPa)	7.92	8.47	7.88
Poisson’s Ratio, ν_{31}	N/A	0.201	0.199
Shear Modulus, G_{13} (GPa)	N/A	3.06	2.89

N/A refers to parameters which were not available

Table 4.3 The Theoretically Determined Elastic Properties.

	913UD	914NCF	916W
Young’s Modulus, E_{11} (GPa)	15.21	16.26	17.39
Young’s Modulus, E_{22} (GPa)	27.31	24.06	22.53
Young’s Modulus, E_{33} (GPa)	27.31	24.06	22.53
Poisson’s Ratio, ν_{31}	0.162	0.159	0.173
Poisson’s Ratio, ν_{23}	0.116	0.198	0.149
Shear Modulus, G_{12} (GPa)	4.14	4.80	4.45
Shear Modulus, G_{13} (GPa)	4.14	4.80	4.45

Table 4.4 The Experimentally Determined Elastic Properties.

	913UD	914NCF	916W
Poisson’s Ratio, ν_{12}	0.090	0.114	0.133
Shear Modulus, G_{23} (GPa)	12.07	10.04	9.8

Table 4.5 The Elastic Properties Determined Using Eqⁿs. 4.2 and 4.3.

	913UD	914NCF	916W
Fibre Configuration	Unidirectional	N/A	Plain Weave
Fibre Volume Fraction, V_f	0.65	N/A	N/A
Fibre Axial Modulus, E (GPa)	48	N/A	N/A
Transverse Modulus, E (GPa)	15	N/A	N/A
In-Plane Poisson's Ratio, ν	0.227	N/A	N/A
In-Plane Shear Modulus, G (GPa)	4.0	N/A	N/A

N/A refers to parameters which were not available

Table 4.6 The Manufacturer’s Data Sheets for the Elastic Properties [4.3].

The fibre volume fraction of each plate (Table 4.2) varies slightly in spite of the great care which was taken in an attempt to control this value. The fibre volume fraction of the curved plates are higher due to the fabrication process which used a male mould and allowed resin to flow under the high pressure, high temperature conditions.

a) 913UD Laminate Configuration

The simple rule of mixtures equation for the transverse modulus is known to grossly underestimate the actual value [4.4]. In view of this, a comparison was made between the values obtained by this method and those obtained both experimentally and via the manufacturer’s data and this comparison yielded good agreement. The in-plane shear modulus also agrees well with experiment, though the necessary data for the rule of mixtures estimation was not available. With a cross-ply fibre configuration, the transverse laminae have no contribution to the longitudinal stiffness. Thus we may consider an analogy between the cross-ply configuration and the unidirectional configuration with a 50 % difference fibre volume fraction. This simple analogy allows us to obtain a value for the axial modulus using the manufacturer’s data. The resulting value agrees well with that obtained experimentally (Table

4.3).

b) 914NCF Laminate Configuration

There was no data sheet available for this configuration in order to allow comparison with the experimental results. Thus, the data was estimated from the raw material properties using the rule of mixtures and assuming that the transverse stiffness does not contribute to the longitudinal stiffness and that both of the 45° fibres contribute 1/4 of the stiffness of the longitudinal fibres. The through-thickness modulus is slightly higher than that of 913UD (Table 4.1). This is reasonable as the elastic modulus of the 914 epoxy is slightly higher than that of the 913 epoxy. The in-plane shear modulus is also slightly higher than that of 913UD for the same reason.

c) 916W Laminate Configuration

Although this material was in a prepreg form, the manufacturer could not provide the mechanical properties data for use as a basis for comparison. Again, the through-thickness modulus is higher than that of 913UD. This can be explained by comparing the elastic moduli of the 916 and the 913 epoxies. In order to provide a basis for comparison the same assumption used in approximating the 913UD axial modulus was employed. The resulting value agrees well with that obtained experimentally.

There is a variance in the elastic properties of different plates due to small manufacturing discrepancies which, in turn, leads to differing values of fibre volume fraction between plates. It is also impractical to obtain separate values for the elastic properties of each plate due to the labourious nature of the process involved. The greatest difference in the fibre volume fraction was seen between the 914NCF plates and was 4 %. This bandwidth was considered to be tolerable and the FE analyses, presented in Chapter 5, use for all models, irrespective of plate type, the elastic moduli obtained from the 13 mm plates.

4.3 The Iosipescu Specimen (IO)

The asymmetric four point bending test rig is shown schematically in Figure 4.4. This configuration, developed at the UTC, provides improved control over specimen alignment, which is essential in order to keep the test section under pure shear. However, though this method has been successfully employed by previous researchers, its use in association with the miniaturised Iosipescu specimen, failed due to insufficient strength at the bonding interface. Details of the iterative modifications made to the experimental setup in an effort to resolve this problem are presented in Section 4.3.1. A ball bearing was used to apply the load to the upper rig in order to maintain proper alignment whilst rollers were used to guarantee that point loads were applied to the specimen. A cross head velocity of 0.5 mm/min was applied to each specimen until sudden failure occurred. A load vs. displacement curve was obtained during each test.

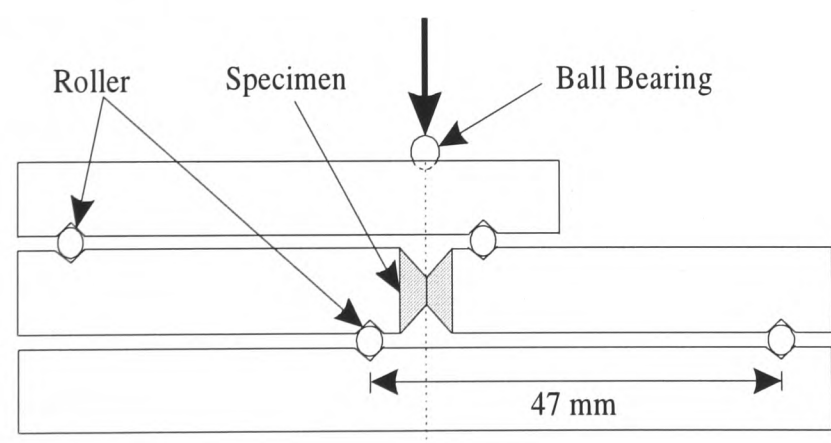


Figure 4.4 A Schematic Illustration of the Iosipescu Loading Configuration.

4.3.1 Results and Discussion

In the initial tests, only the 916W laminate was used as it was thought to have the strongest interlaminar shear strength because of the interaction between the layers due to the weave pattern. A first attempt was made using Iosipescu specimens consisting of a central test

section glued to steel shanks. Four different formulations of Ecobond 45 were tried. These formulations, tabulated in Table 4.7, are defined by the ratio, by weight, of epoxy to catalyst (hardener). The surface preparation was carefully conducted in order to achieve optimum bonding conditions. The bonding surface of the steel rig was roughened using 120 grit carborundum paper and the V-notched specimen surface was roughened using a needle file at angles of $\pm 45^\circ$ and subsequently finished using 180 grit carborundum paper. After roughening, both the steel and the V-notched specimen surfaces were cleaned using ethanol. Three specimens were prepared for each bonding formulation. The 4:5 formulation showed the strongest resistance to shear and bending stresses, however, all of the formulations prepared failed at the adhesive. It was thought possible that the poor bond strength was due to the presence of an unknown substance which could not be removed by ethanol. In view of this a sulphuric acid based etchant was applied to the steel surface in an effort to prepare a better bond surface. Any residual traces of the etchant were removed with the application of a neutraliser. Since previous tests showed the best bond performance to be obtained from the 4:5 formulation, its use was continued in testing for improvements in bond strength. Three specimens were prepared and tested but showed minimal improvement in bond strength.

The ratio of Ecobond 45 to Catalyst		
1	Flexible	2:1
2	Slightly-Rigid	1:1
3	Very-Rigid	4:5
4	Rigid	2:3

Table 4.7 The Adhesive Formulations Tried in the Iosipescu Tests.

A further iteration was conducted using a cyano-acrylate adhesive. Three specimens were prepared without roughening the surfaces as this yields the best performance from cyano-acrylate adhesives [4.5]. However, in comparing the results with those of the previous 2:1 formulation, which has comparable cured rigidity, little improvement was observed. Therefore, it was concluded that bonding composite materials to steel for this test configuration, i.e. small bonding surface area, is not practicable.

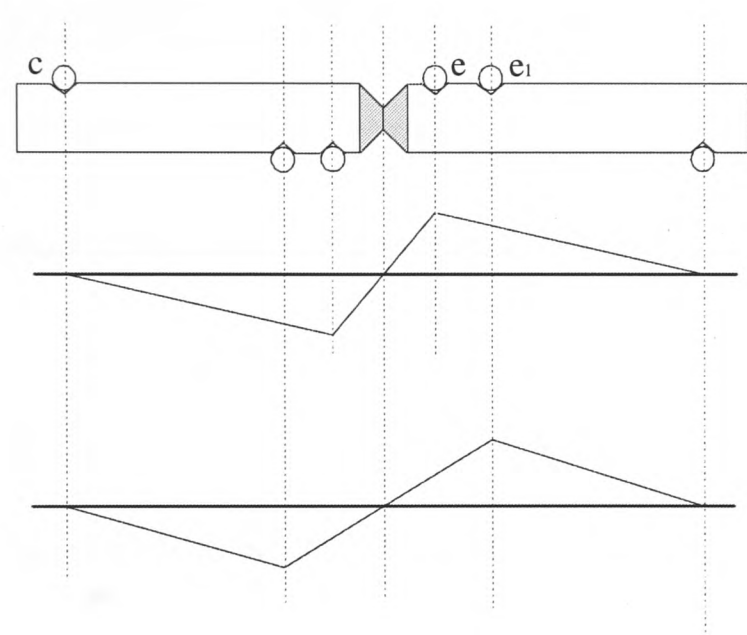


Figure 4.5 The Bending Moment Diagram for the Iosipescu Test.

The next iteration involved the replacement of the steel shanks with carbon/epoxy composite. The same loading geometry was used, shown in Figure 4.5, and resulted in a promising increase in bond strength. It was also thought possible that the failures were due to the proximity of the bonding surface to the maximum bending moment (Figure 4.5). The estimated bending stress at the bonding surface, using classical bending theory, is 2.48 MPa tensile. Thus the loading point e was moved to e_1 hence changing the gradient of the bending moment curve. The position of load point c remained constant throughout these tests. Though the results obtained from testing of five further specimens in this new configuration yielded the

highest bond strengths yet obtained, the magnitude remained insufficient to achieve shear failure of the specimen itself.

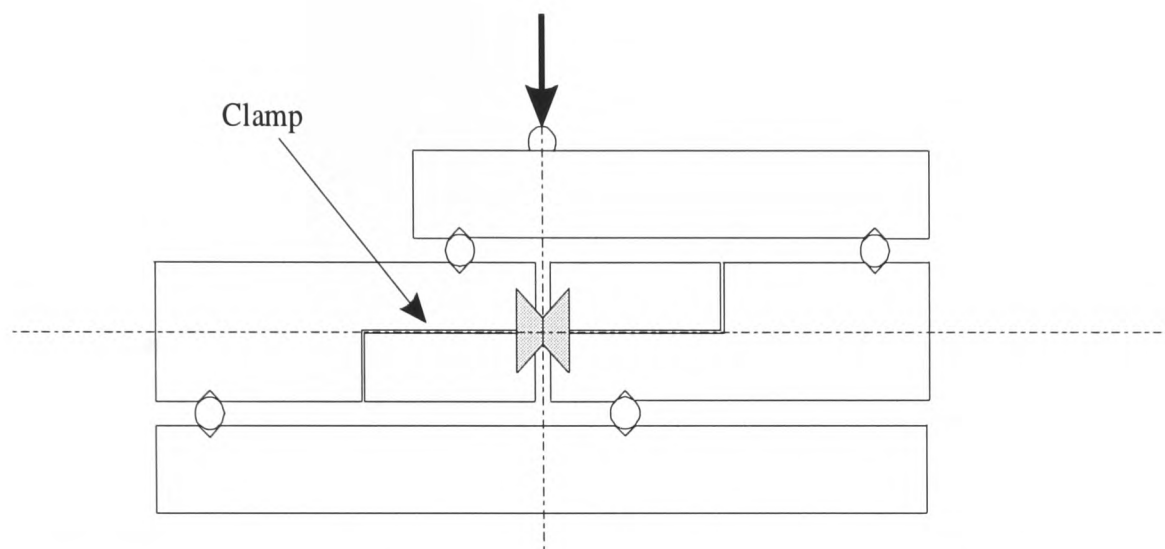


Figure 4.6 A Schematic Illustration of the New Dovetail Clamping Configuration.

In view of the aforementioned problems associated with bond strength, the use of adhesives was discontinued and a radical change was made to the method of specimen attachment. Figure 4.6 presents the new dovetail clamp configuration. It can be seen that the loading configuration and the geometry at the test section are identical to that of the previous tests. The V-notched test section was inserted into a dovetail clamp matching the specimen V-angle. The movable section of the clamp was secured with two bolts which were tightened sufficiently to prevent the specimen from moving. The validity of this design was based on a photoelastic analysis which demonstrates the absence of stress concentrations which might affect the stress distribution at the notch section (Figure 4.7). The experimental procedure of the photoelasticity analysis is presented in Appendix C. Figure 4.8 shows the isocromatic fringes observed using a standard Iosipescu configuration, i.e. a uniform beam with central V-notches. It can be seen that, at the centre section, the isocromatic fringe pattern of the clamped Iosipescu specimen is the same as that of the standard Iosipescu specimen.

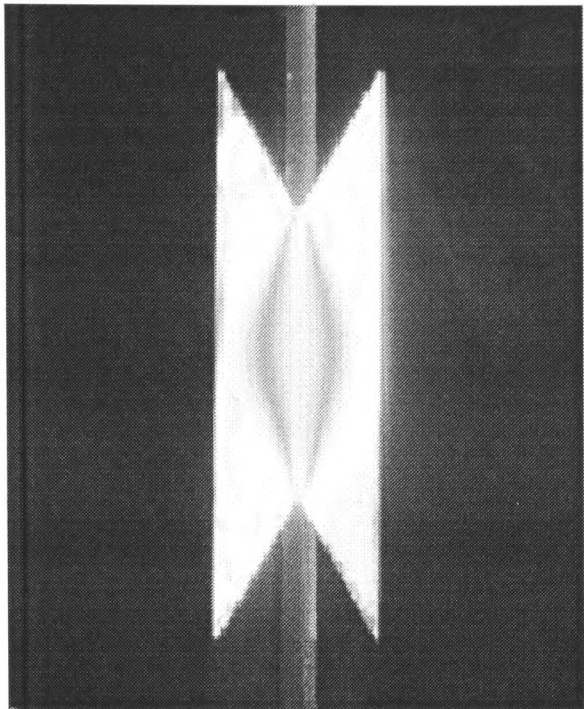


Figure 4.7 The Isocromatic Fringe Pattern for the Dovetail Clamped Iosipescu Specimen.

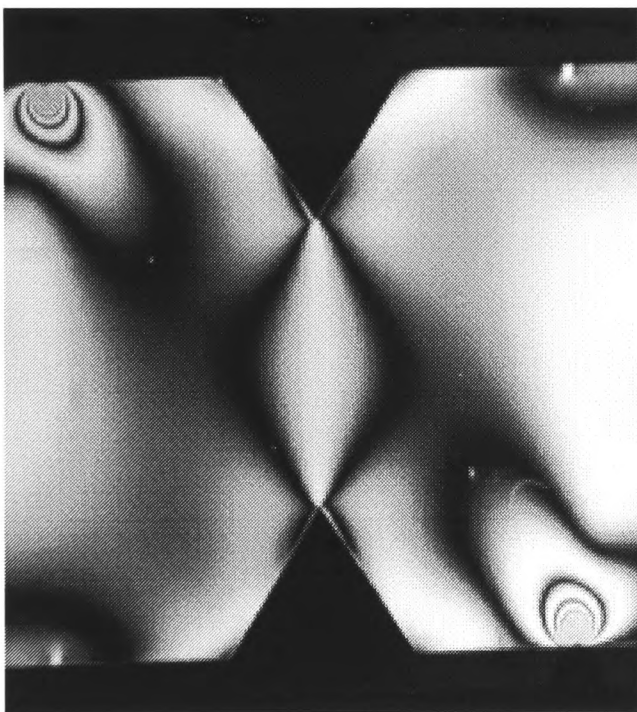


Figure 4.8 The Isocromatic Fringe Pattern for a Standard Iosipescu Specimen.

Five specimens for each material were loaded at a rate of 0.5 mm/min using these dovetail clamps until sudden failure occurred. The load vs. displacement curves are plotted in Figures 4.9 - 4.11 showing the brittle rupture characteristic. The corresponding failure loads for each specimen, at each cross sectional area and for each material are recorded in Tables 4.8 -

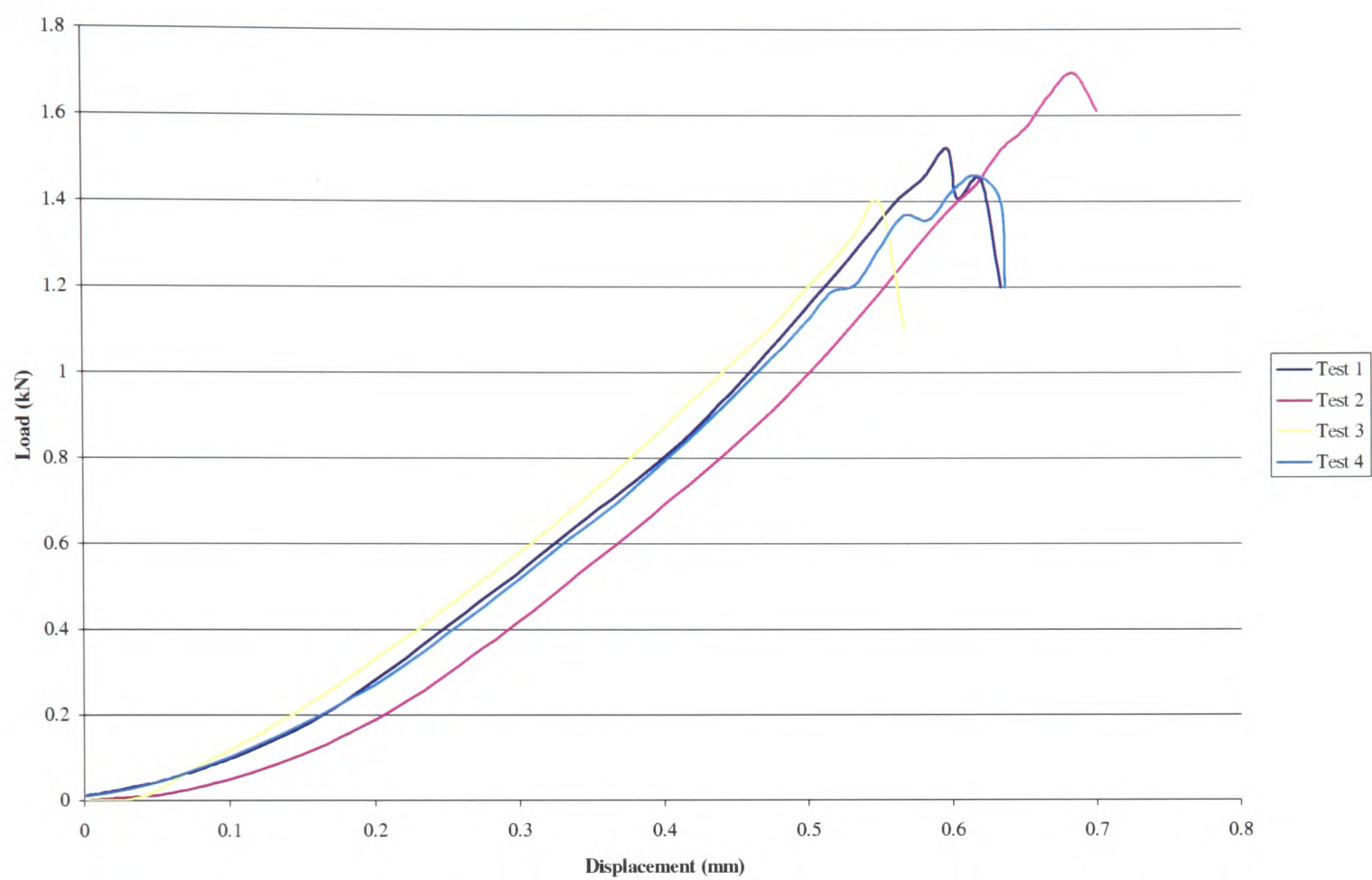


Figure 4.9 IO3, Load vs. Displacement Curve.

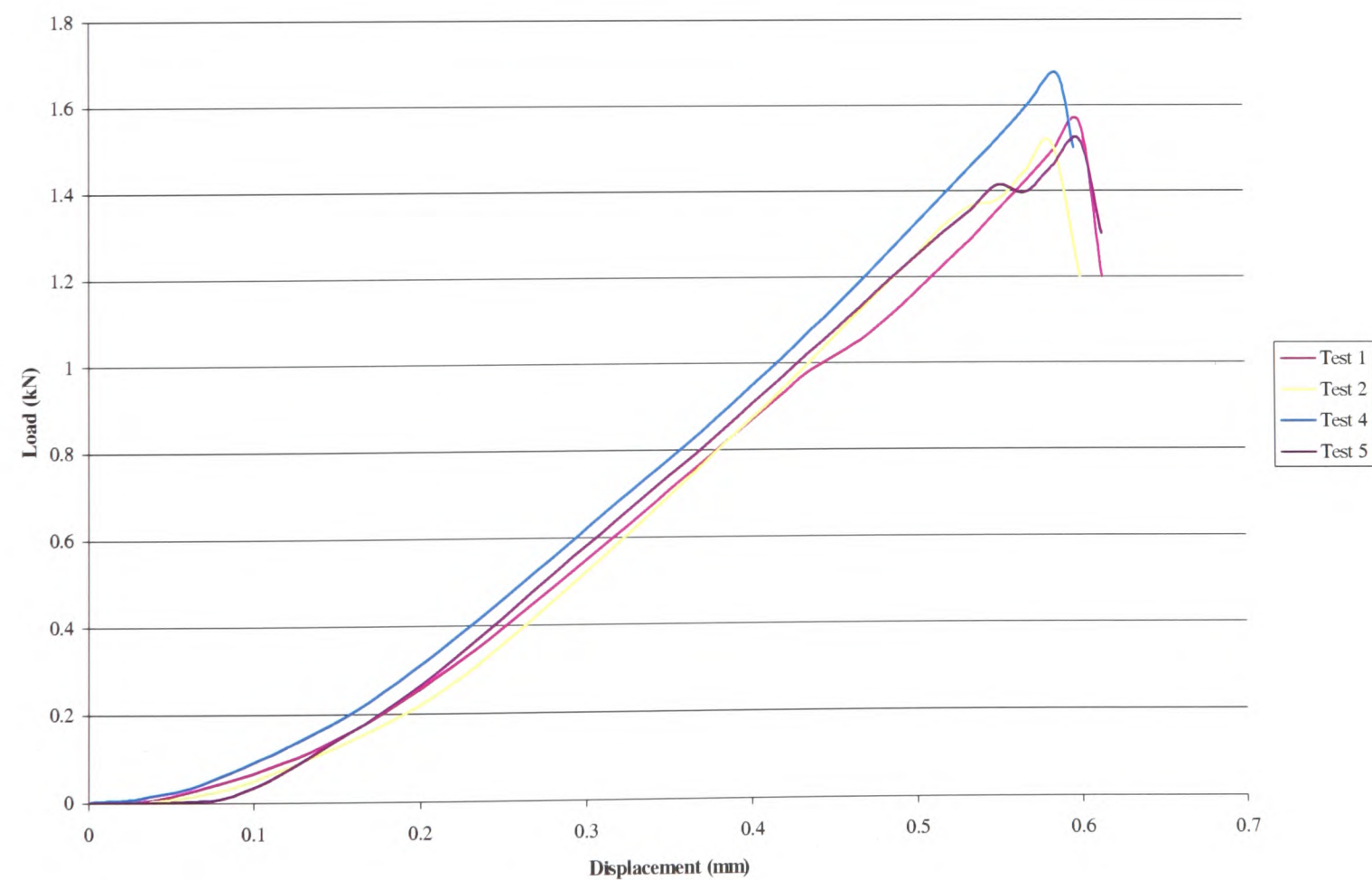


Figure 4.10 IO4, Load vs. Displacement Curve.

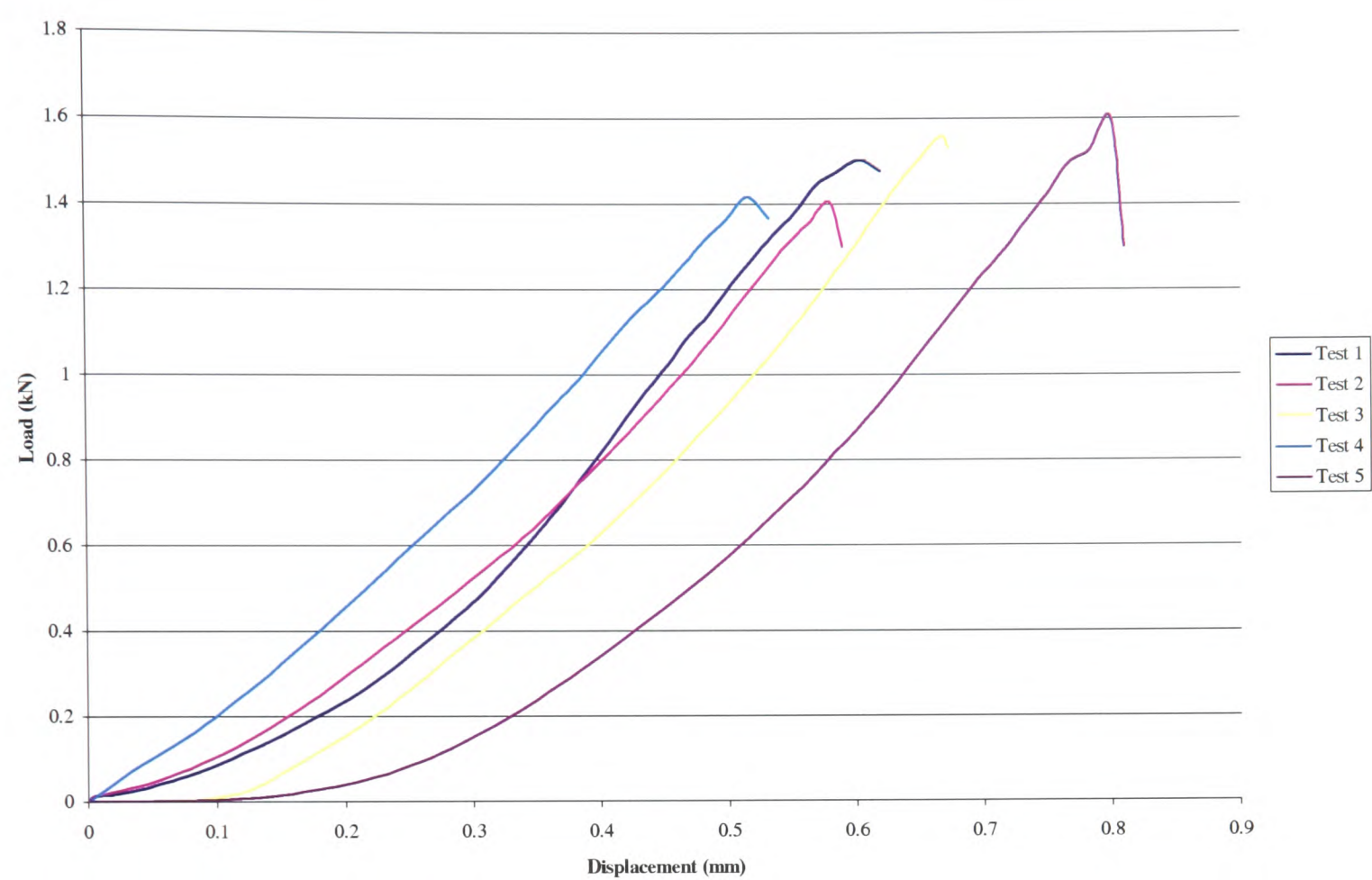


Figure 4.11 IO6, Load vs. Displacement Curve.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	X-Area a (mm ²)	Failure Load P (kN)	Comments
1	39	UD	0°/90°	5.01	23.14	1.48	
2	39	UD	0°/90°	5.00	22.94	1.70	
3	39	UD	0°/90°	5.01	23.47	1.57	
4	39	UD	0°/90°	5.00	20.43	1.46	
5	39	UD	0°/90°	5.01	-	0.76	Failure due to inherent damage

Standard Deviation of Failure Load = 0.142

Table 4.8 913UD, Iosipescu Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	X-Area a (mm ²)	Failure Load P (kN)	Comments
1	8	NCF	0°/-45°/+45°/90°	5.01	29.65	1.70	
2	8	NCF	0°/-45°/+45°/90°	5.01	28.19	1.57	
3	8	NCF	0°/-45°/+45°/90°	5.01	-	0.64	Failure due to inherent damage
4	8	NCF	0°/-45°/+45°/90°	5.01	25.5	1.67	
5	8	NCF	0°/-45°/+45°/90°	5.01	26.05	1.54	

Standard Deviation of Failure Load = 0.080

Table 4.9 914NCF, Iosipescu Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness <i>t</i> (mm)	X-Area <i>a</i> (mm ²)	Failure Load <i>P</i> (kN)	Comments
1	25	Woven	0°/90°	4.99	22.66	1.50	Audible cracking
2	25	Woven	0°/90°	5.00	21.95	1.40	
3	25	Woven	0°/90°	5.02	22.54	1.56	
4	25	Woven	0°/90°	5.03	20.69	1.42	
5	25	Woven	0°/90°	5.01	23.04	1.60	

Standard Deviation of Failure Load = 0.086

Table 4.10 916W, Iosipescu Specimen.

4.10. It should be emphasised that none of the specimens failed in the region of the clamps and that the results showed good consistency, as demonstrated by the low standard deviation value for each material.

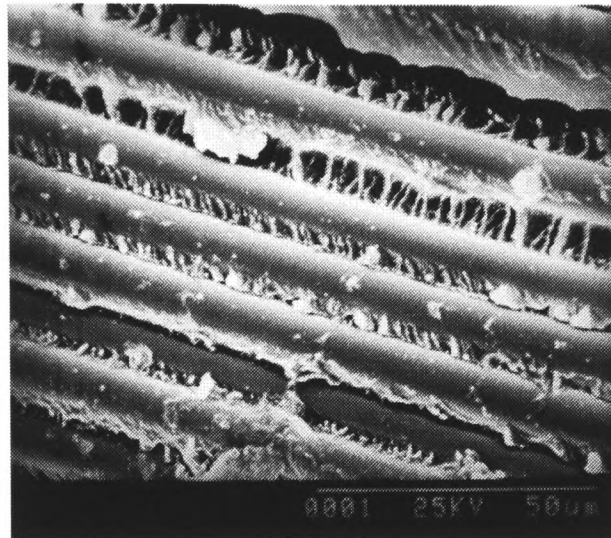


Figure 4.12 IO3, A Fractograph of Matrix Failure due to Pure Shear.



Figure 4.13 IO4, A Fractograph of Matrix Failure due to Pure Shear.

The fracture surfaces of the specimens were carefully examined using a scanning electron microscope (SEM). In order to prevent charging, the surfaces were coated with gold prior to the SEM examination. In the figure titles, IO3, IO4 and IO6 denote the 913UD, 914NCF and 916W material types respectively. In Figures 4.12 and 4.13 the fractographic

analysis showed that the 913UD and 914NCF specimens, when under pure shear, failed predominantly due to matrix cracking. The 916W specimen, Figure 4.14, showed both matrix failure and fibre distortion. The fractographic analysis is discussed in more detail in Chapter 7.



Figure 4.14 IO6, A Fractograph of Matrix Failure due to Pure Shear.

4.4 The Centrally Notched Beam Specimen (CNB)

Figure 4.15 shows a schematic illustration of the rig used to perform the simple four point bending tests. The load was applied to the upper loading beam via a ball bearing. Rollers were employed in order to ensure that point loads were applied to the specimen irrespective of deformation. The centre of the specimens were marked before each test to facilitate alignment with the load axis. Twenty seven specimens (three specimens for each of three thicknesses and three materials) were loaded such that the notched surface was subjected to tension at a rate of 0.5 mm/min until a sudden decrease in the stiffness was detected. A load vs. displacement plot was recorded for each test. Since the delamination of the specimens occurred catastrophically, the test itself was recorded using a video camera focussed on the notch radius zone in order to provide a visual record for later analysis.

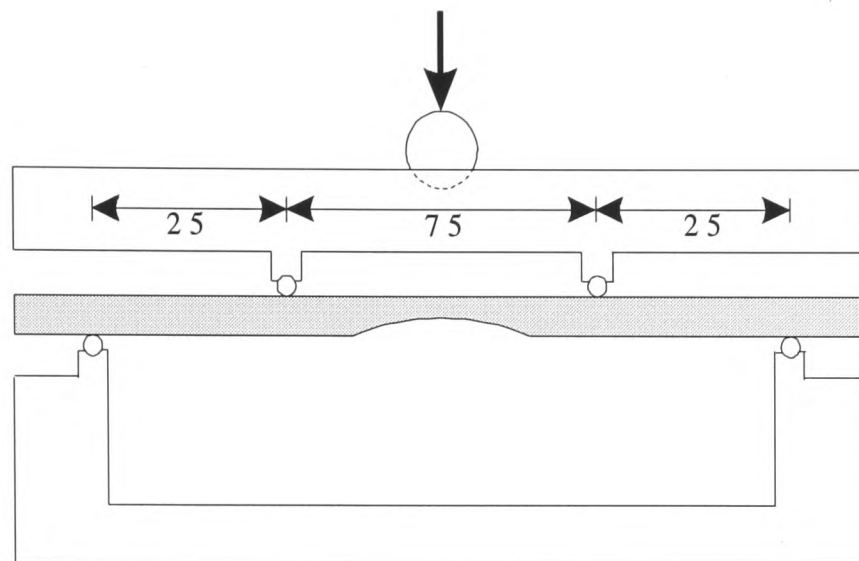


Figure 4.15 A Schematic Illustration of the Simple Four Point Bending Test.

4.4.1 Results and Discussion

Before catastrophic delamination occurred, the audible sound of cracking was observed. This sound implied the onset of specimen failure. Also present was the visible development of crazing in the notch surface of the specimen. The load vs. displacement plots are presented in Figures 4.16 - 4.23. The position of delamination and the corresponding failure load for each specimen, at each thickness and for each material, are presented in Tables 4.11 - 4.13. The delamination position was identified by ply number, counted from the surface opposite to the notched surface.

Because of fluctuations in the thickness, the notch depth, relative to the specimen thickness, was not constant. Thus the root of the notches coincided with 0° plies for some specimens and 90° plies for others. This gave rise to different delamination positions. More specifically, specimens ground up to a 90° ply showed delamination propagation between that, and the adjacent ply. Specimens ground up to a 0° ply showed fibre distortion of the 0° ply and then delamination propagation between that and the adjacent 90° ply.

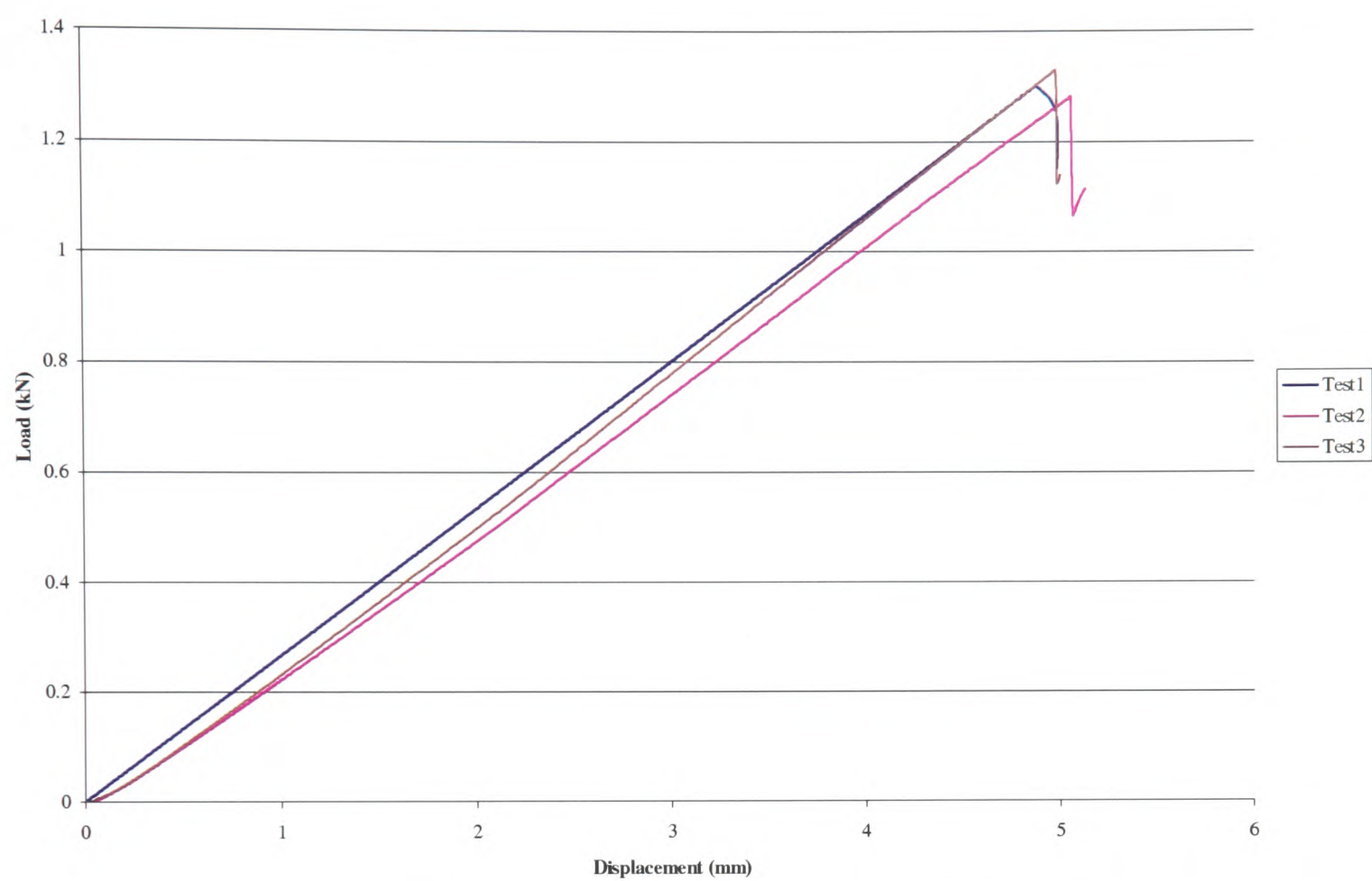


Figure 4.16 CNB3, t = 4, Load vs. Displacement Curve.

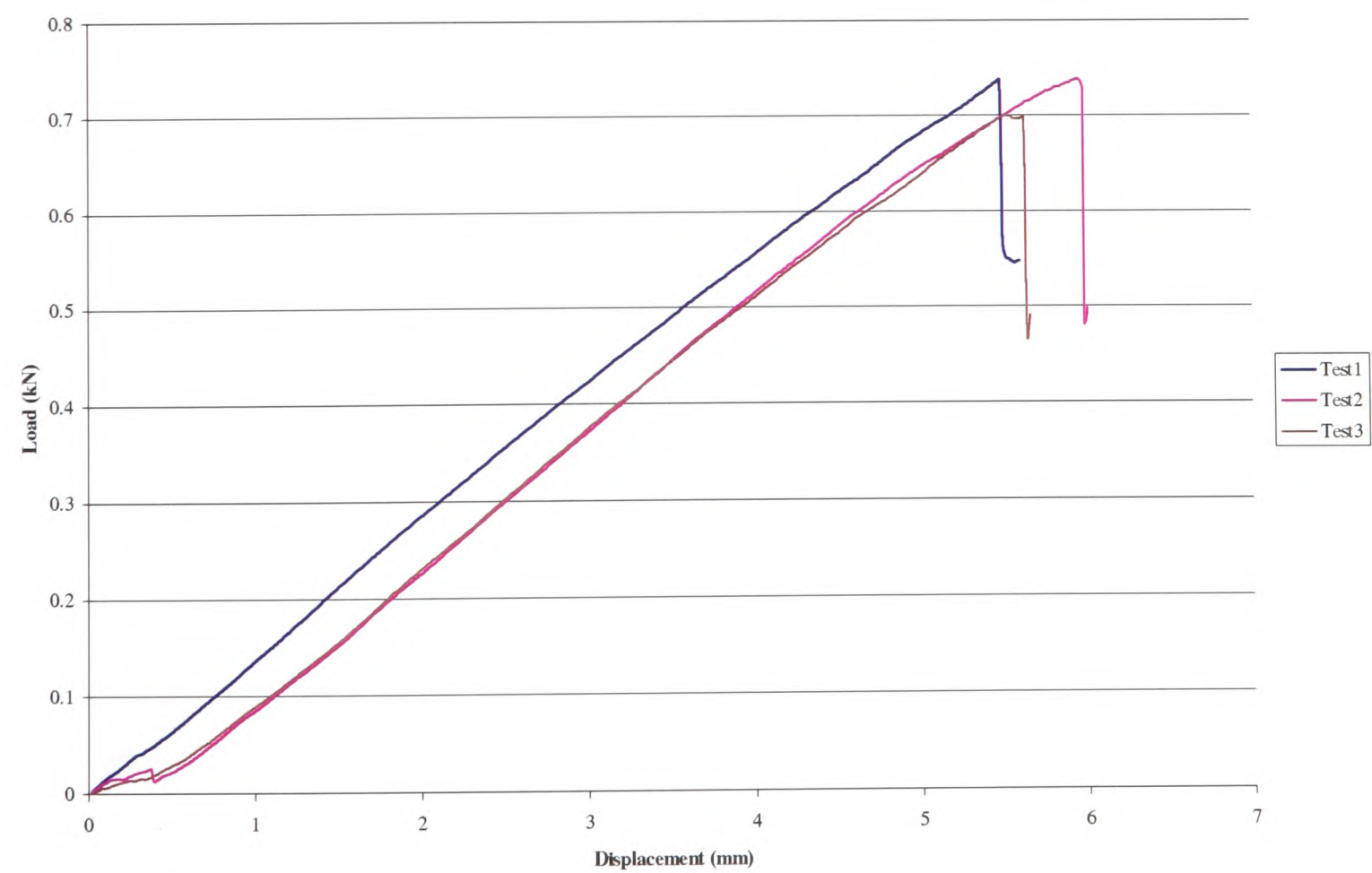


Figure 4.17 CNB3, t=3, Load vs. Displacement Curve.

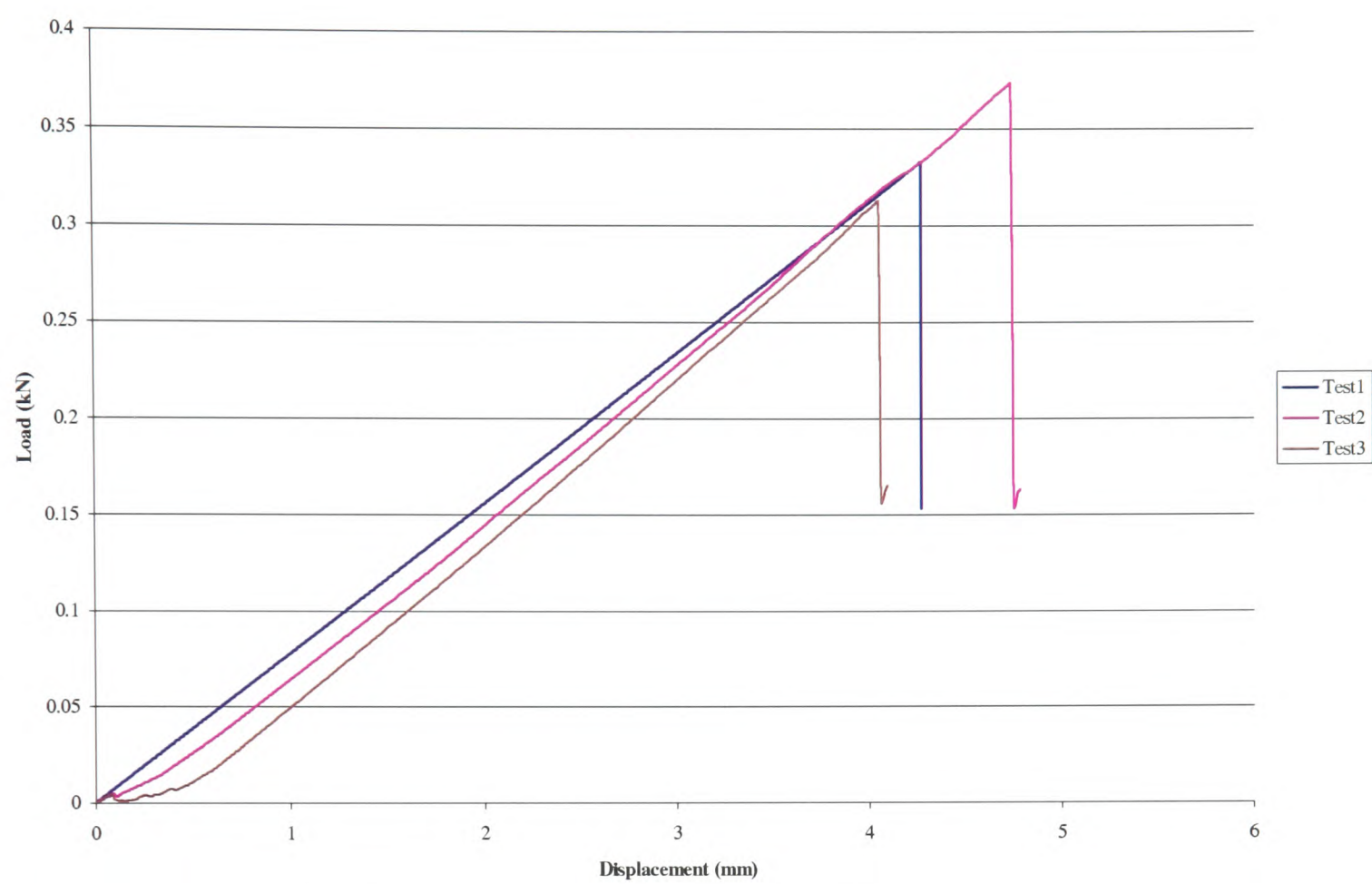


Figure 4.18 CNB3, t = 2, Load vs. Displacement Curve.

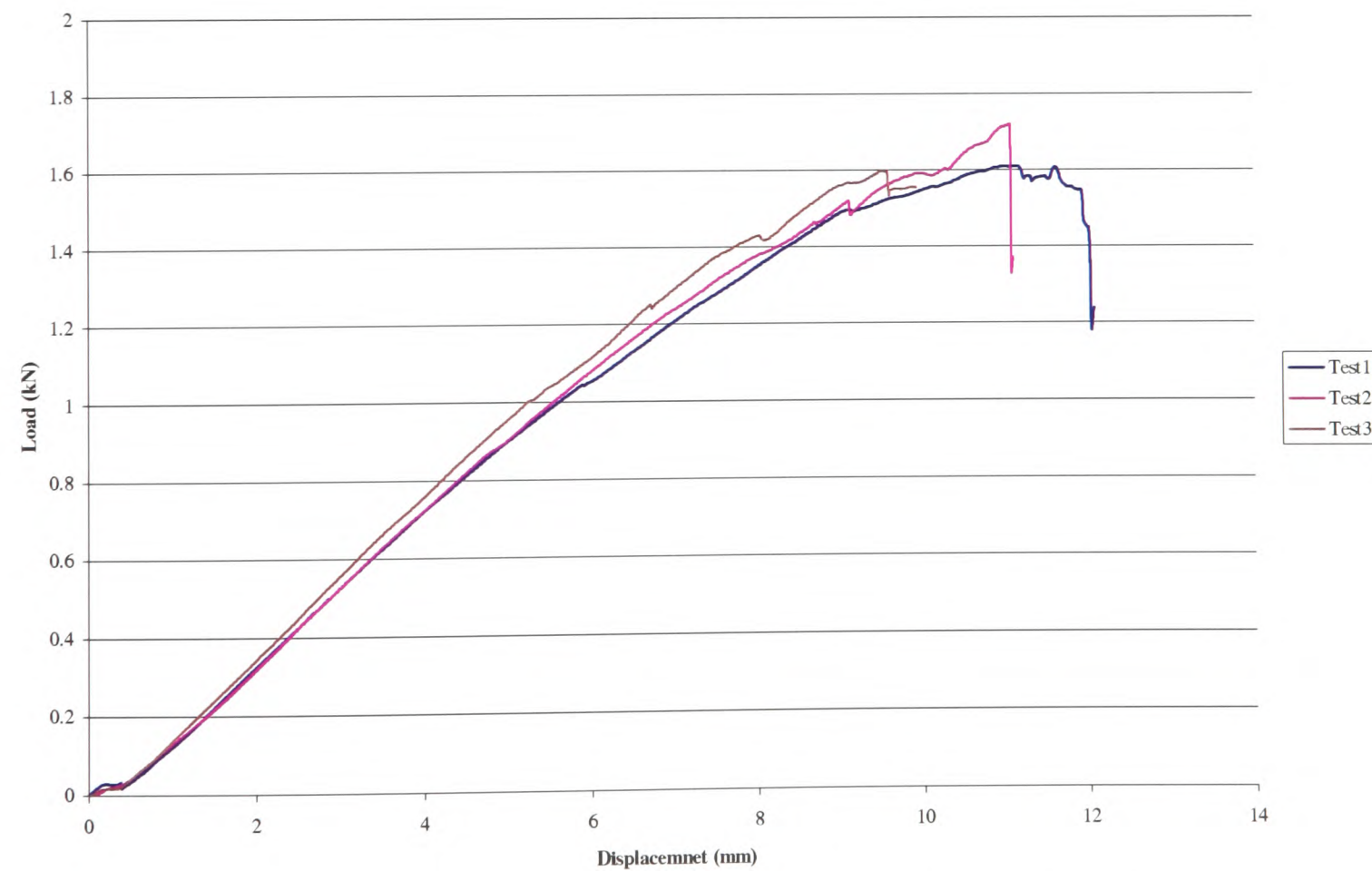


Figure 4.19 CNB4, t = 4, Load vs. Displacement Curve.

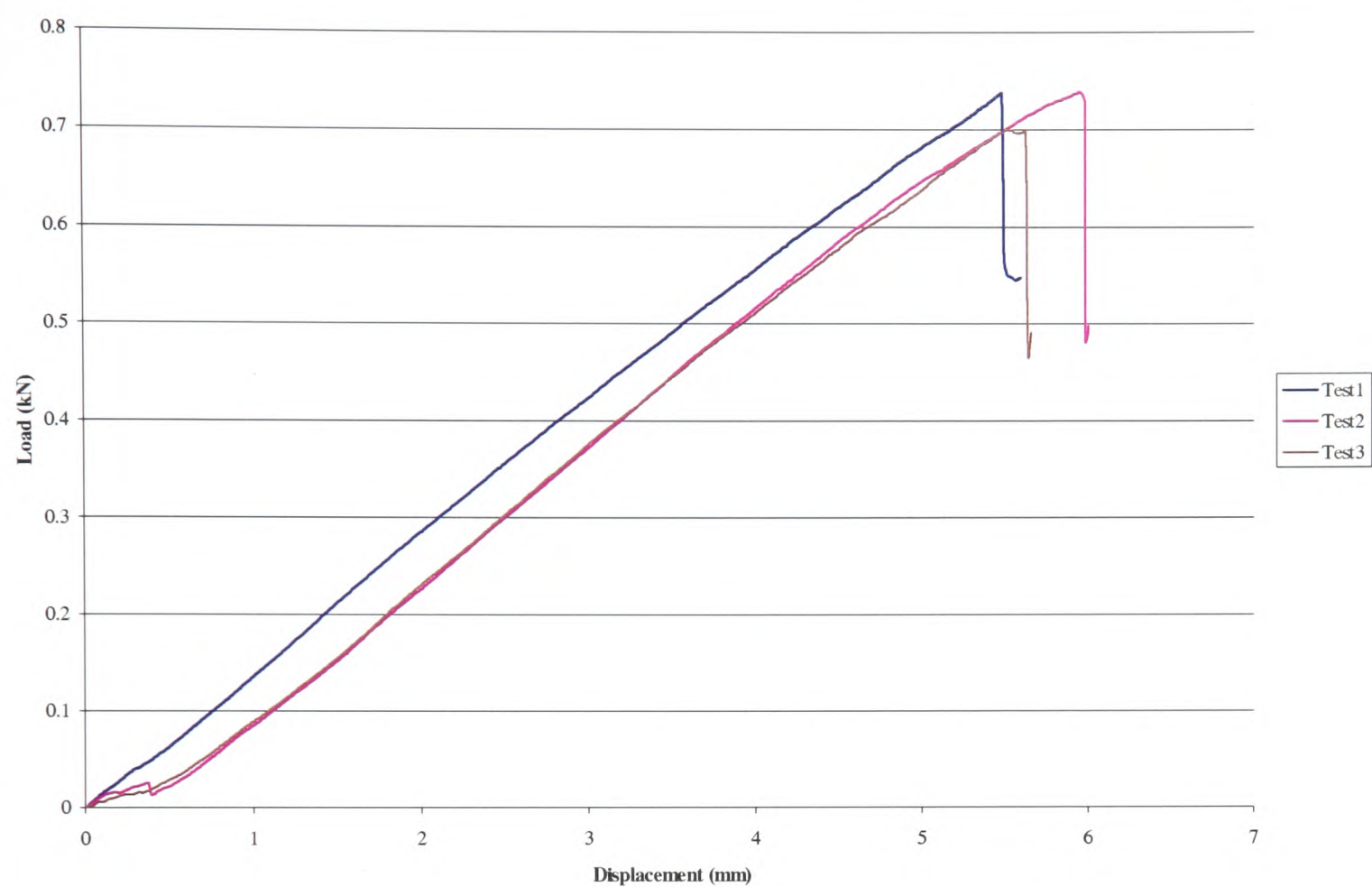


Figure 4.20 CNB4, t = 3, Load vs. Displacement Curve.

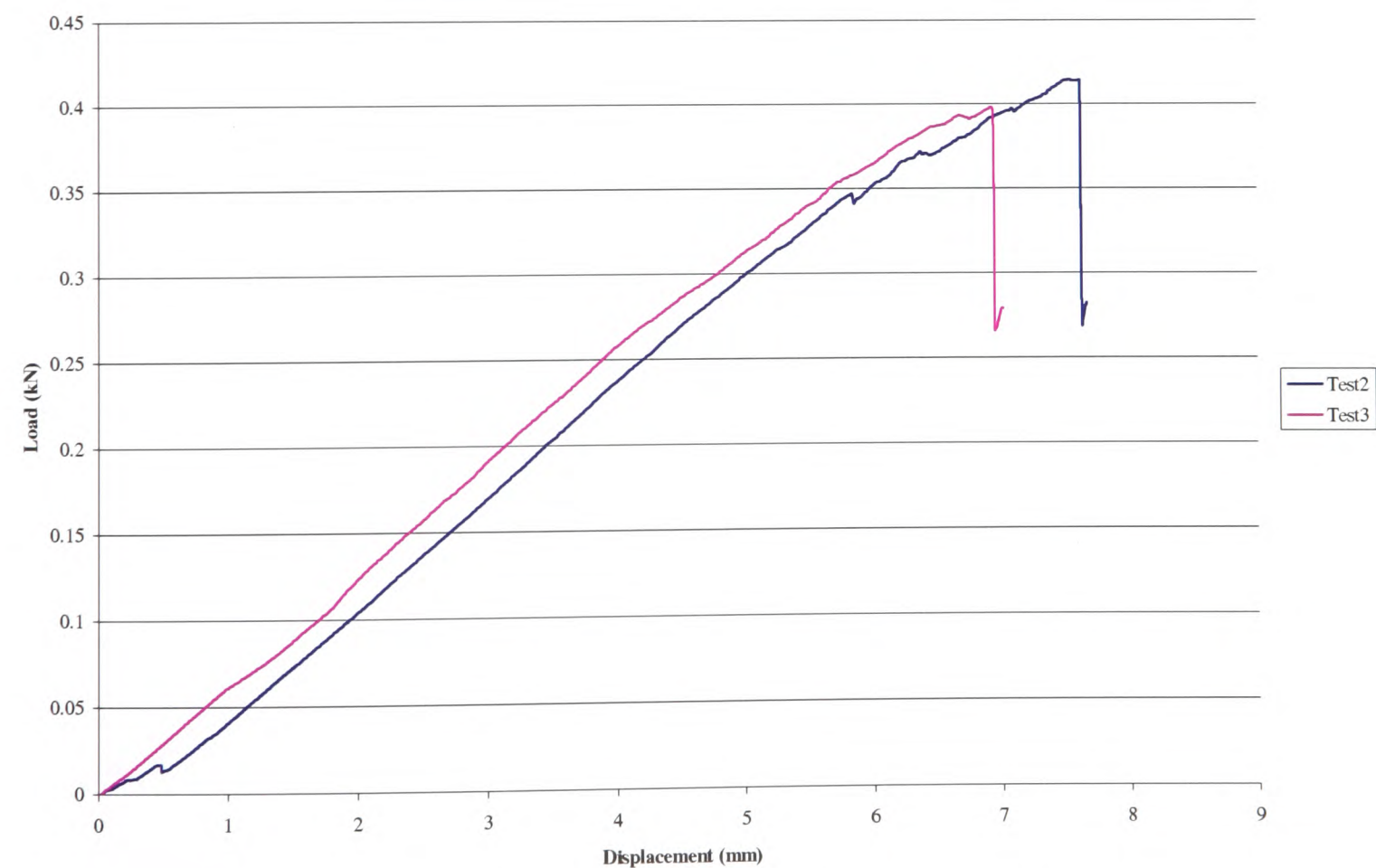


Figure 4.21 CNB4, t=2, Load vs. Displacement Curve.

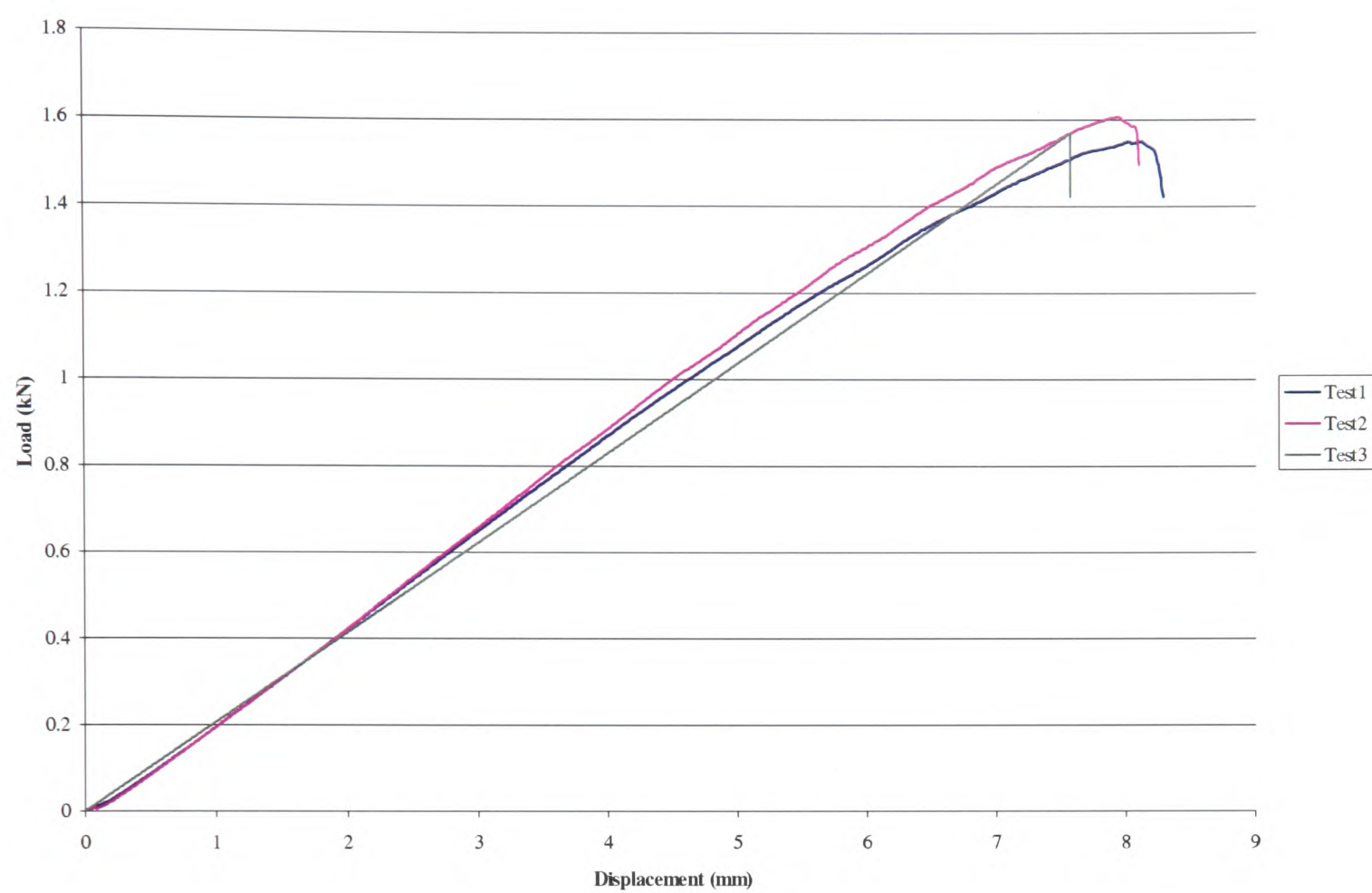


Figure 4.22 CNB6, t = 4, Load vs. Displacement Curve.

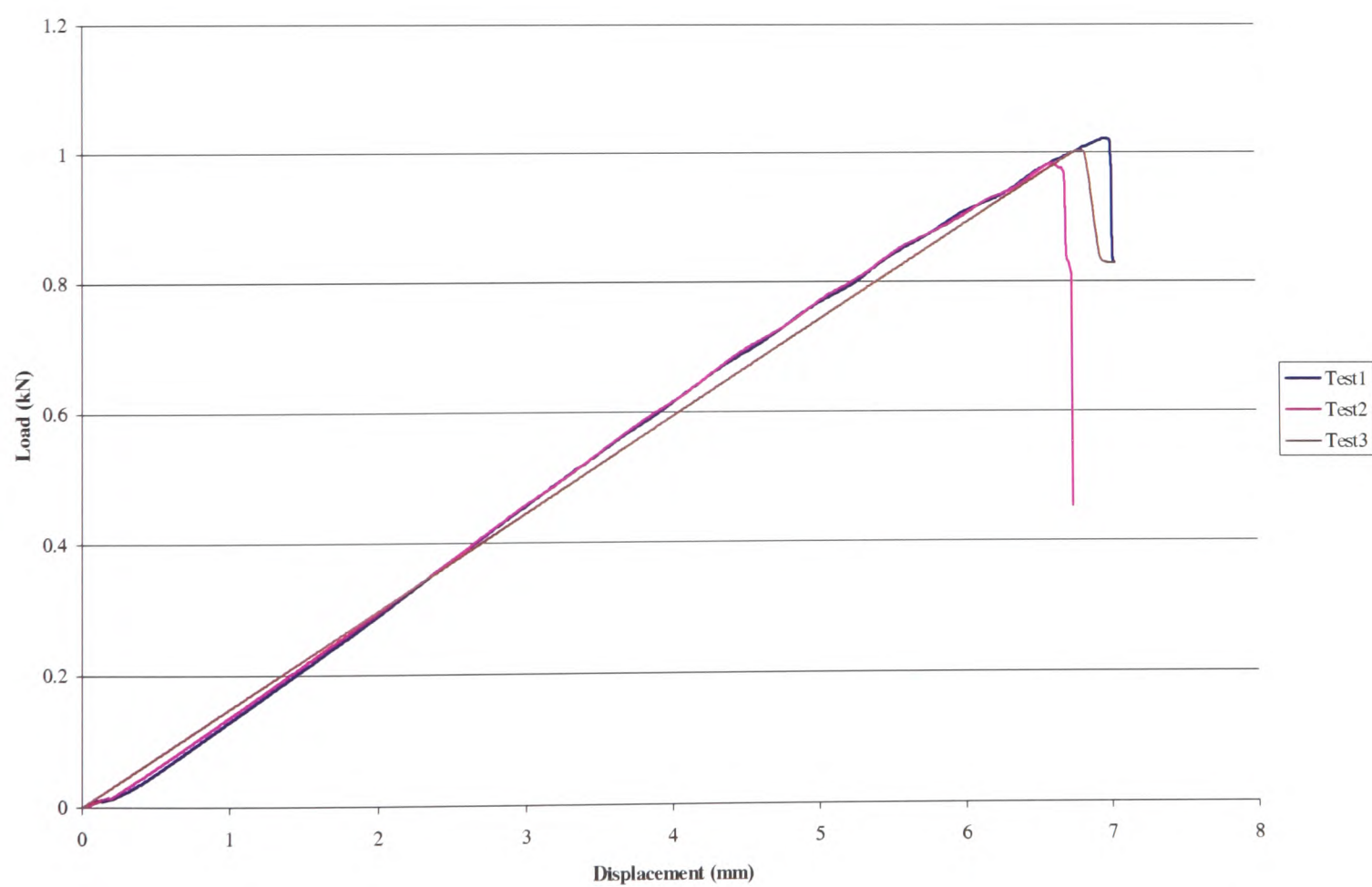


Figure 4.23 CNB6, t = 3, Load vs. Displacement Curve.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Depth h (mm)	Width b (mm)	Failure Load (kN)	Delamination position (ply)	Comments
1	39	UD	0°/90°	2	5.00	9.80	0.35	16-18	Delamination along 17
2	39	UD	0°/90°	2	4.76	9.88	0.37	16-18	Delamination along 17
3	39	UD	0°/90°	2	4.52	10.04	0.31	18-20	Delamination along 19
1	39	UD	0°/90°	3	5.00	10.04	0.8	24-26	Delamination along 25
2	39	UD	0°/90°	3	5.01	10.06	0.84	24-26	Delamination along 25
3	39	UD	0°/90°	3	5.09	10.04	0.76	22-24	Delamination along 23
1	39	UD	0°/90°	4	4.97	9.85	1.32	30-32	Delamination along 31
2	39	UD	0°/90°	4	4.82	9.86	1.26	32-34	Delamination along 33
3	39	UD	0°/90°	4	5.00	9.85	1.30	30-32	Delamination along 31

Standard Deviation of Failure Load for Each Tests

t = 2, 0.0306
t = 3, 0.0400
t = 4, 0.0451

Table 4.11 913UD, Centrally Notched Beam Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Depth d (mm)	Width b (mm)	Failure Load (kN)	Delamination Position (ply)	Comments
1	32	NCF	0°/-45°/+45°/90°	2	4.62	9.98	-	-	Did not fail
2	32	NCF	0°/-45°/+45°/90°	2	4.60	9.94	0.41	16-17	Crack initiated around 0.38kN
3	32	NCF	0°/-45°/+45°/90°	2	4.65	9.93	0.39	13-14	Delamination along 13
1	32	NCF	0°/-45°/+45°/90°	3	4.57	10.00	0.72	24-25	Delamination along 25
2	32	NCF	0°/-45°/+45°/90°	3	4.62	9.99	0.72	25-26	Delamination along 25
3	32	NCF	0°/-45°/+45°/90°	3	4.60	9.99	0.69	24-25	Delamination along 25
1	32	NCF	0°/-45°/+45°/90°	4	4.56	10.00	1.58	27-28-29	Delamination along 27, 29
2	32	NCF	0°/-45°/+45°/90°	4	4.56	9.97	1.60	28-29	Delamination along 29
3	32	NCF	0°/-45°/+45°/90°	4	4.56	9.99	1.57	28-29	Delamination along 29

Standard Deviation of Failure Load for Each Tests

t = 2, 0.0144
t = 3, 0.0268
t = 4, 0.0650

Table 4.12 914NCF, Centrally Notched Beam Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Delamination position (ply)	Comments
1	19	Woven	0°/90°	2	4.21	9.90	-	-	Did not fail
2	19	Woven	0°/90°	2	4.50	9.96	-	-	Did not fail
3	19	Woven	0°/90°	2	4.32	9.92	-	-	Did not fail
1	19	Woven	0°/90°	3	4.43	9.97	1.05	22-24	Delamination along 23
2	19	Woven	0°/90°	3	4.58	9.93	1.00	22-24	Delamination along 23
3	19	Woven	0°/90°	3	4.50	9.96	0.96	22-24	Delamination along 23
1	19	Woven	0°/90°	4	4.73	10.00	1.55	28-30	Delamination along 29
2	19	Woven	0°/90°	4	4.50	9.99	1.52	28-30	Delamination along 29
3	19	Woven	0°/90°	4	4.60	9.92	1.58	28-30	Fibre distortion at 26

Standard Deviation of Failure Load for Each Tests

t = 2,
t = 3, 0.0451
t = 4, 0.0300

Table 4.13 916W, Centrally Notched Beam Specimen.



Figure 4.24 CNB3, A Fractograph of Mixed Mode Failure.



Figure 4.25 CNB4, A Fractograph of Mixed Mode Failure.

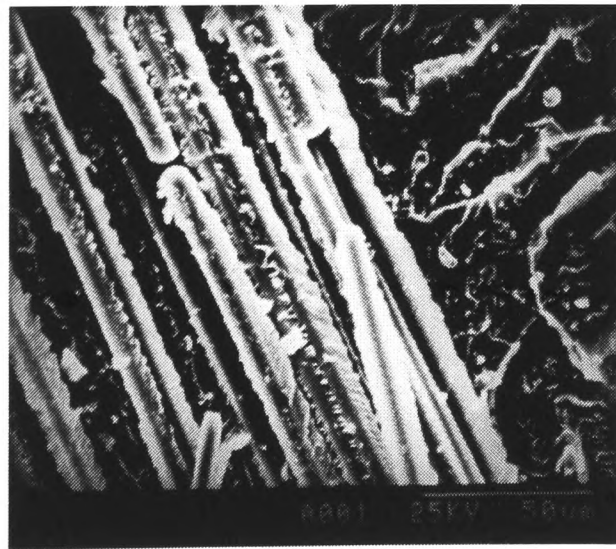


Figure 4.26 CNB6, A Fractograph of Mixed Mode Failure.

Consequently, all three materials showed similar modes of failure. The matrix failure and the interfacial debonding of the fibres may be observed in the fractographic analyses, shown in Figures 4.24 - 4.26. In the figure titles, CNB3, CNB4 and CNB6 denote the 913UD, 914NCF and 916W material types respectively.

4.5 The Plain (PCB) and Notched (NCB) Curved Beam Specimens

A schematic illustration of the load configuration is shown in Figure 4.27. The upper part of the rig was attached via a universal joint to a cross member into which was built a 5kN load cell. The use of a universal joint guarantees axial alignment of the specimens along the load direction and thus eliminates bending effects. Three PCB and three NCB specimens with a uniform width of 10 mm were monotonically loaded to failure at room temperature, the deflection being increased at a rate of 0.5 mm/min until delamination occurred.

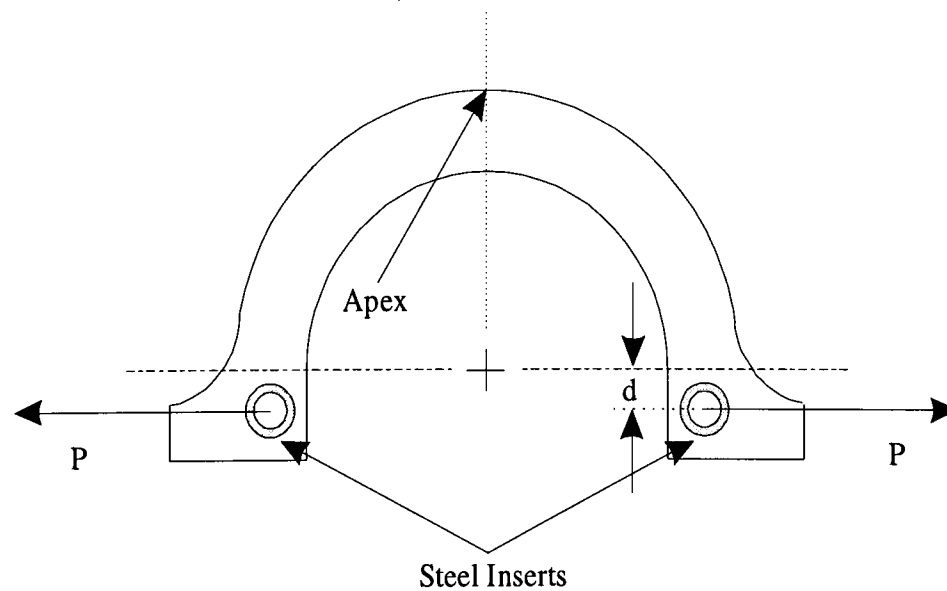


Figure 4.27 A Schematic Illustration of the Plain Curved Beam Test Configuration.

Since delamination occurs catastrophically, failure was recorded using a video camera with maximum zoom local to the test section to facilitate visual inspection at the onset of

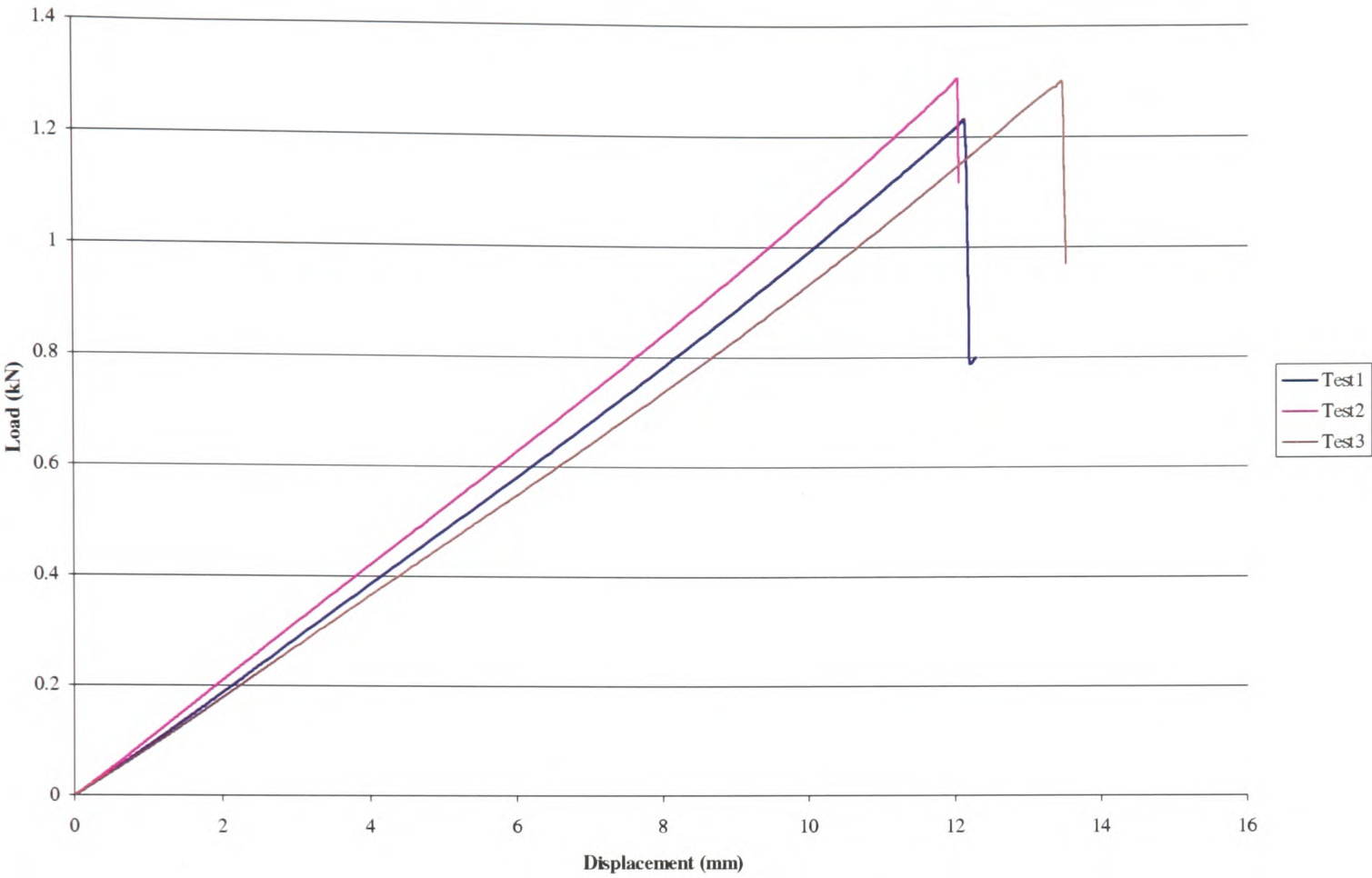


Figure 4.28 PCB3, Load vs. Displacement Curve.

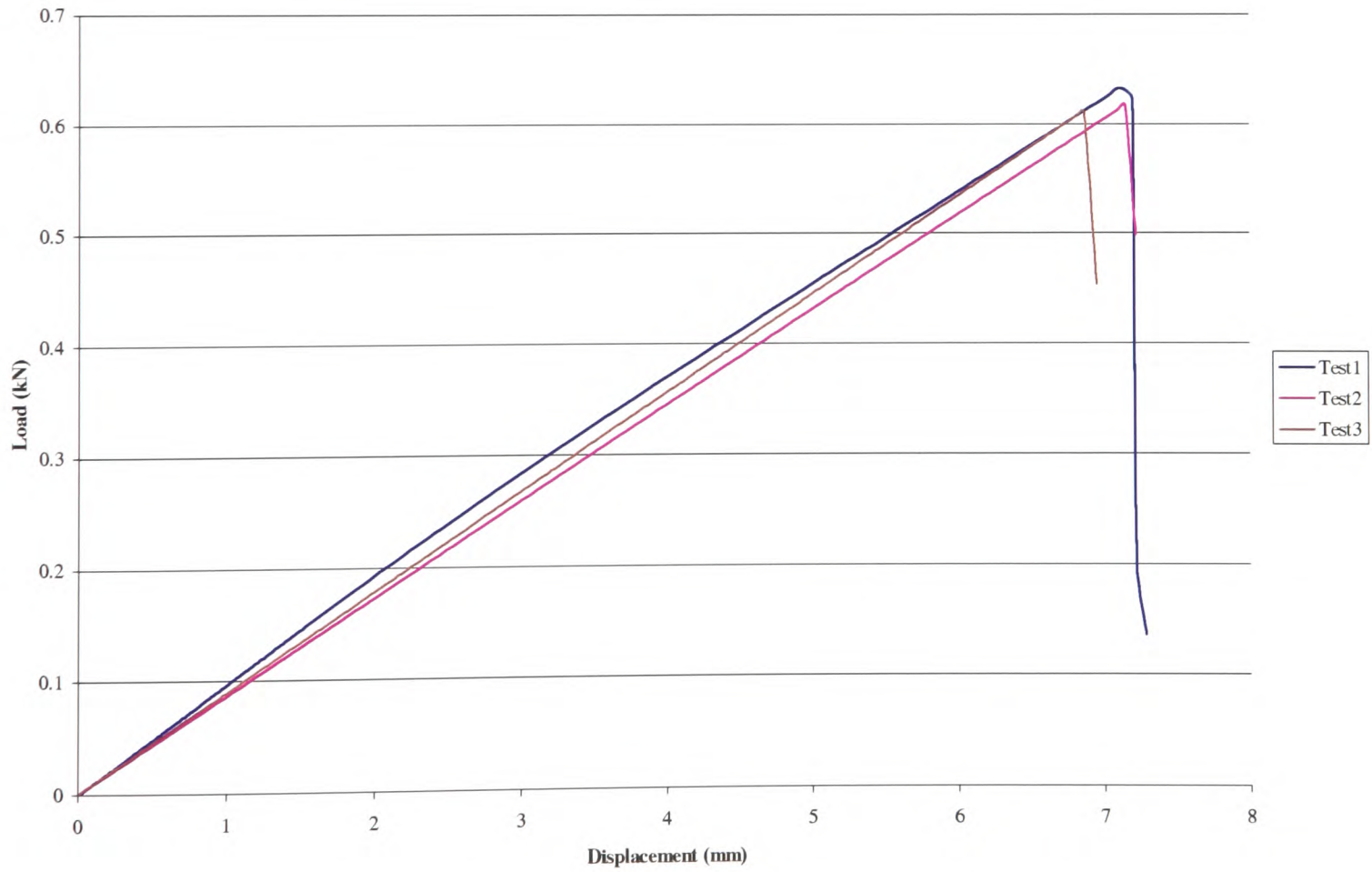


Figure 4.29 PCB4, Load vs. Displacement Curve.

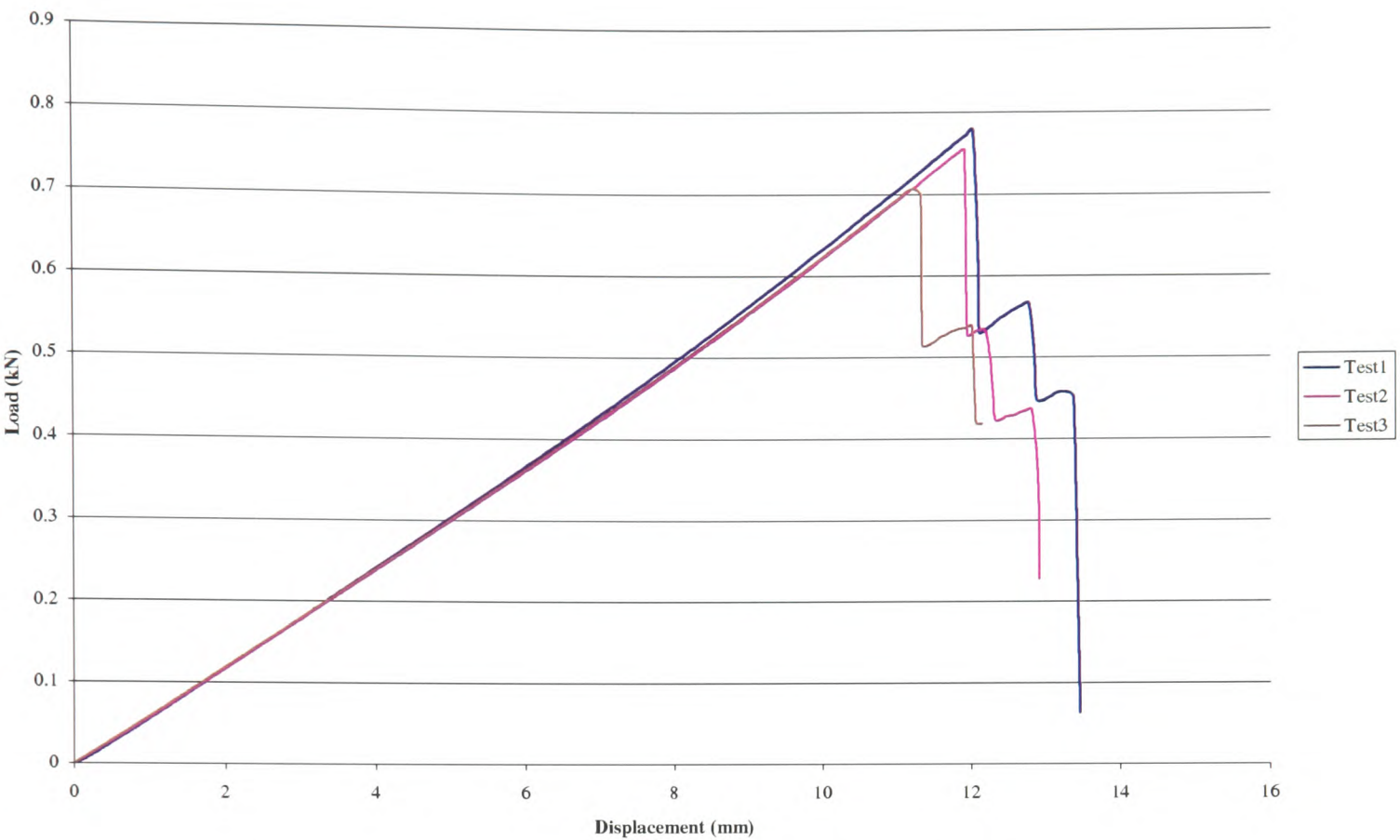


Figure 4.30 PCB6, Load vs. Displacement Curve.

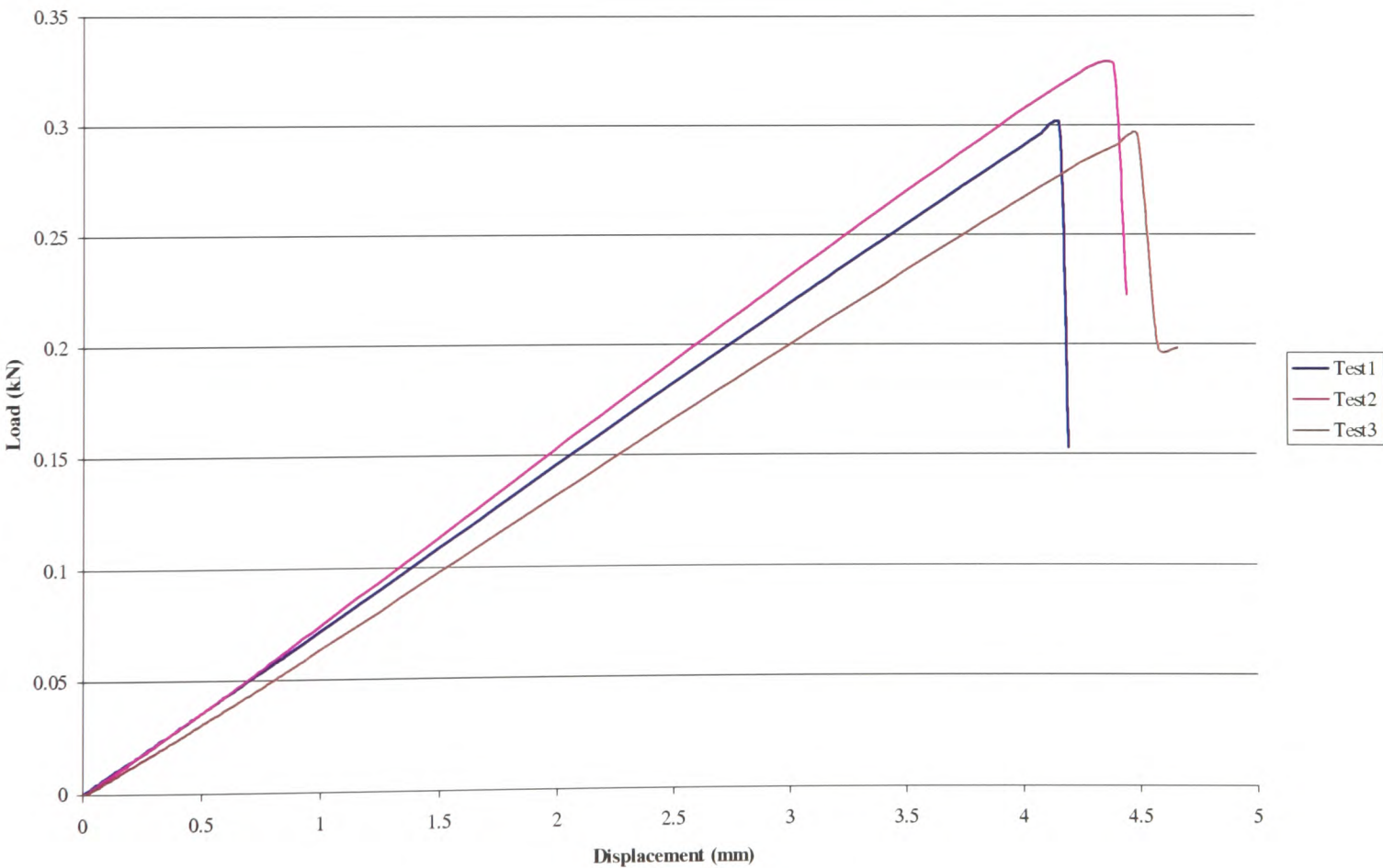


Figure 4.31 NCB3, Load vs. Displacement Curve.

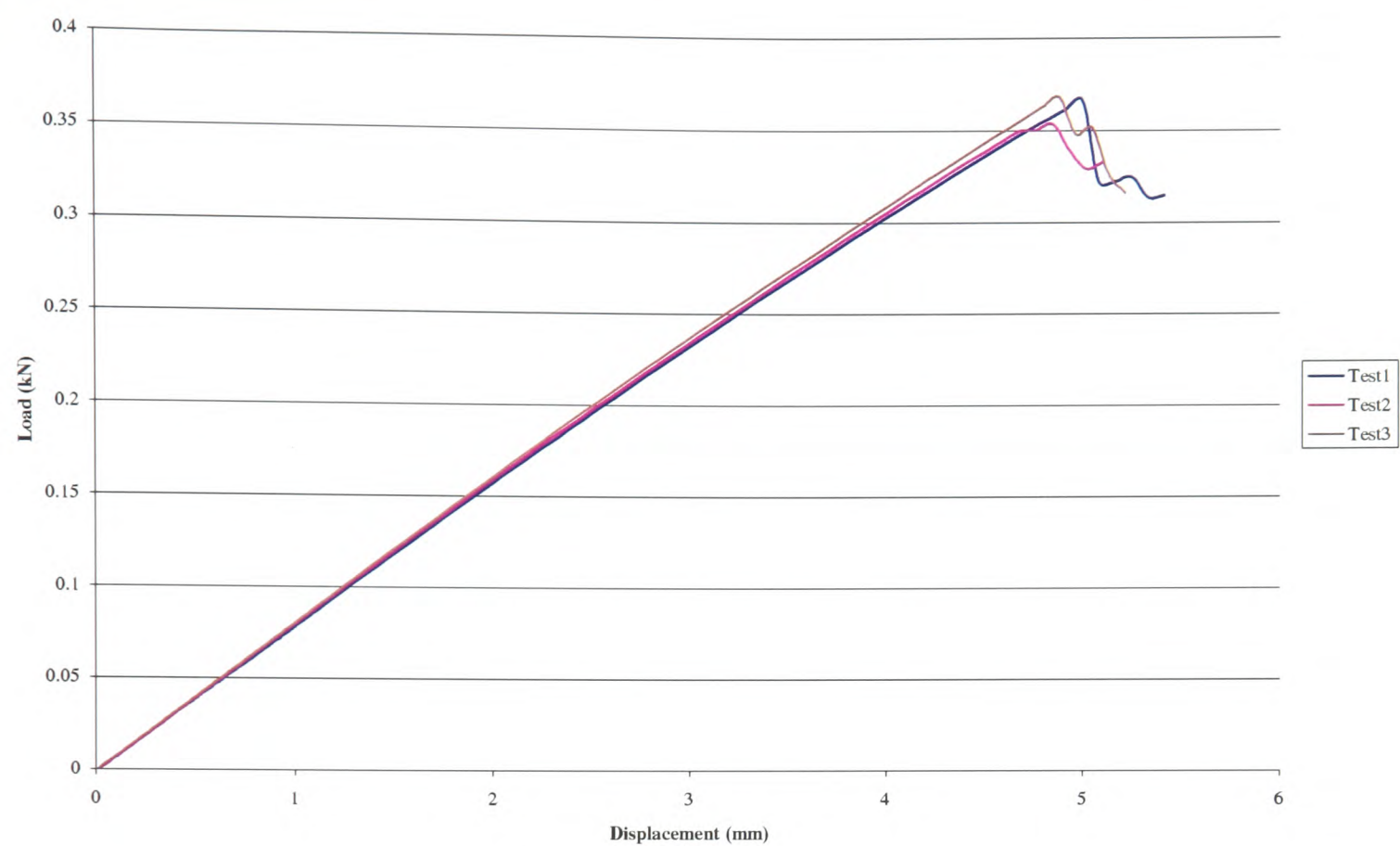


Figure 4.32 NCB4, Load vs. Displacement Curve.

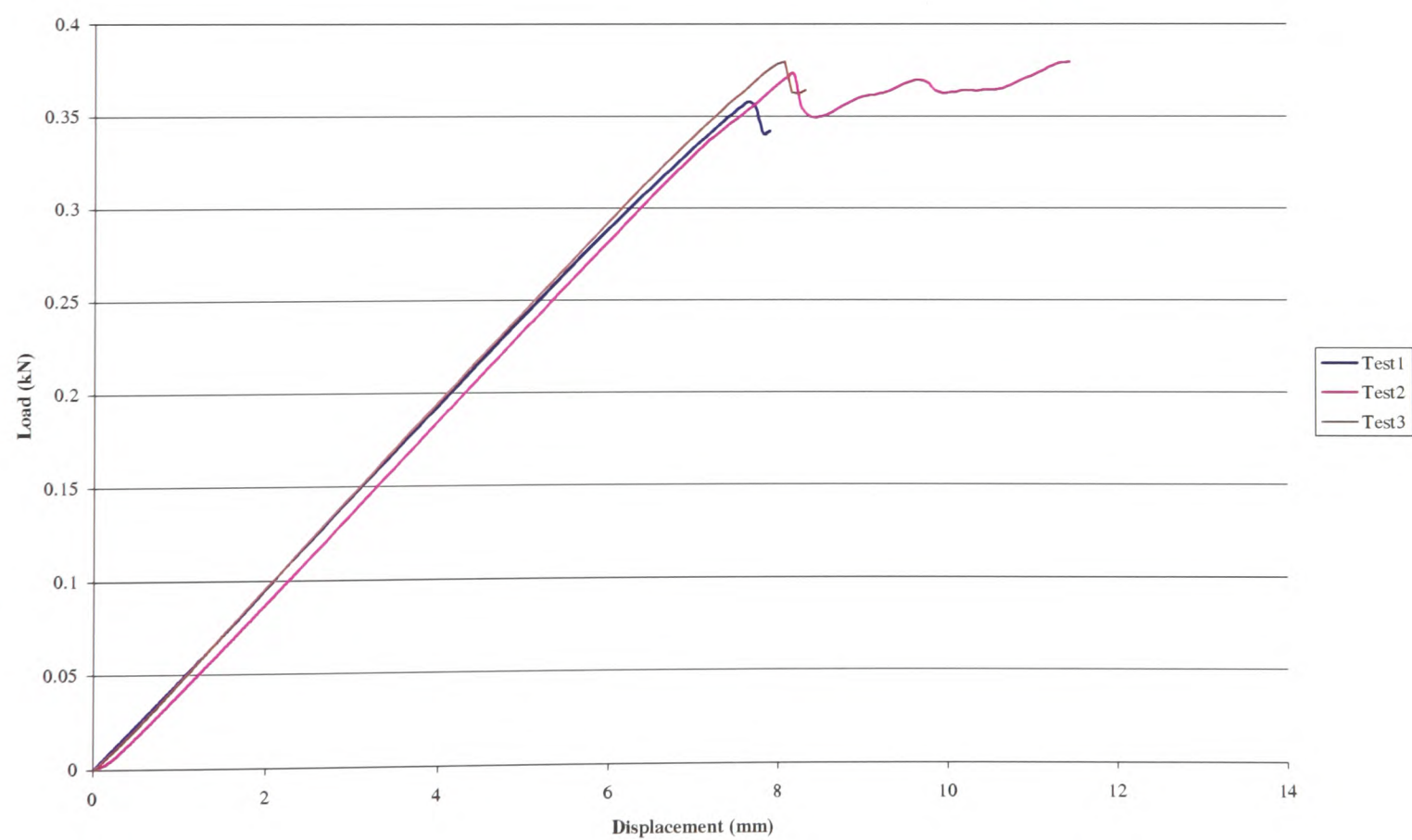


Figure 4.33 NCB6, Load vs. Displacement Curve.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Delamination position (ply)	Comments
1	39	UD	0°/90°	-	4.56	10.04	1.21	34-36	No fibre distortion
2	39	UD	0°/90°	-	4.66	10.00	1.28	36-38	No fibre distortion
3	39	UD	0°/90°	-	4.41	10.05	1.28	36-38	No fibre distortion

Standard Deviation of Failure Load = 0.039

Table 4.14 913UD, Plain Curved Beam Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Delamination position (ply)	Comments
1	39	UD	0°/90°	2.21	4.42	9.94	0.29	22-26	No fibre distortion
2	39	UD	0°/90°	2.49	4.51	9.99	0.32	24-26	No fibre distortion
3	39	UD	0°/90°	2.36	4.60	9.98	0.29	26-28	No fibre distortion

Standard Deviation of Failure Load = 0.0175

Table 4.15 913UD, Notched Curved Beam Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Depth d (mm)	Width b (mm)	Failure Load (kN)	Delamination Position (ply)	Comments
1	32	NCF	0°/-45°/+45°/90°	-	4.44	10.09	0.62	10-12	Delamination along 3, 12
2	32	NCF	0°/-45°/+45°/90°	-	4.52	9.99	0.62	4-8,10-12	Delamination along 5, 11
3	32	NCF	0°/-45°/+45°/90°	-	4.49	10.05	0.61	10-12	Delamination along 11

Standard Deviation of Failure Load = 0.0058

Table 4.16 914NCF, Plain Curved Beam Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Depth d (mm)	Width b (mm)	Failure Load (kN)	Delamination Position (ply)	Comments
1	32	NCF	0°/-45°/+45°/90°	2.97	4.91	9.84	0.36	28-29	Delamination along 29
2	32	NCF	0°/-45°/+45°/90°	2.96	4.40	9.89	0.36	26-27	Delamination along 26
3	32	NCF	0°/-45°/+45°/90°	3.04	4.42	9.95	0.35	28-29	Delamination along 29

Standard Deviation of Failure Load = 0.0087

Table 4.17 914NCF, Notched Curved Beam Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Delamination position (ply)	Comments
1	19	Woven	0°/90°	-	3.45	9.97	0.76	-	Fibre distortion from 1 to 8
2	19	Woven	0°/90°	-	3.40	9.84	0.74	-	Fibre distortion from 1 to 8
3	19	Woven	0°/90°	-	3.42	9.96	0.69	-	Fibre distortion from 1 to 8

Standard Deviation of Failure Load = 0.0356

Table 4.18 916W, Plain Curved Beam Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Delamination position (ply)	Comments
1	19	Woven	0°/90°	2.38	3.46	10.00	0.37	18-20	No fibre distortion
2	19	Woven	0°/90°	2.64	3.49	9.95	0.37	22-24	No fibre distortion
3	19	Woven	0°/90°	2.39	3.45	10.01	0.35	24-26	No fibre distortion

Standard Deviation of Failure Load = 0.0117

Table 4.19 916W, Notched Curved Beam Specimen.

delamination and to allow the applied load at the point of delamination to be determined precisely. A load vs. displacement plot was recorded for each test.

4.5.1 Results and Discussion

The load vs. displacement plots are shown in Figures 4.28 - 4.33. In Tables 4.14 - 4.19, the failure loads for each material and the corresponding position of delamination for each specimen, with and without notches, are tabulated. The delamination location was identified by ply number, counted from the inner radius of the specimen toward the outer radius. Fluctuation between results was small as can be seen from the standard deviation for both specimen configurations. Therefore further experiments, for the purpose of obtaining a better average, were considered unnecessary.

a) 913UD

It should be emphasised that all of the failures occurred within a 90° ply, i.e. the odd number plies. The cracks propagated across the 90° ply toward the adjacent 0° ply and the delamination then continued to propagate between these plies.



Figure 4.34 PCB3, A Fractograph of Matrix Failure.

The matrix failure and the interfacial debonding of the fibres may be observed in the fractographic analysis, shown in Figure 4.34.

Due to the non-uniform thickness of the curved beam specimens, the notch depth for the NCB specimens varied, leading to variations in the delamination positions. In all of the NCB specimens, the delamination initiated at the root of the notch and then propagated between this and the adjacent ply. The matrix failure and the interfacial debonding of the fibres may be observed in the fractographic analysis, shown in Figure 4.35.

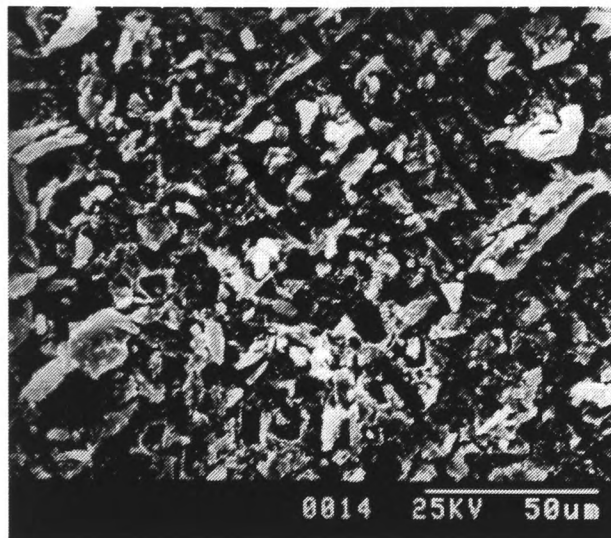


Figure 4.35 NCB3, A Fractograph of Fibre Debonding and Matrix Failure.

b) 914NCF

All of the PCB specimens failed due to delamination at approximately one third of the thickness from the inner radius. The delamination occurred in one of two ways: a) initiation within a 90° ply, moving to the adjacent 45° ply and continuing between these two plies, b) initiation within a 45° ply, moving to the adjacent 90° ply and continuing between these two plies. The matrix failures and the interfacial debonding of the fibres may be observed in the fractographic analyses, shown in Figure 4.36. In all of the 914NCF NCB specimens, the modes of failure were found to be identical to that of the 913UD NCB specimens. Since the 914NCF

laminates were laid-up using unidirectional fibre laminae, a similar mode of failure to that of the 913UD laminate was observed, as might be expected. The matrix cracking and the interfacial debonding of the fibres may be observed in the fractographic analysis, shown in Figure 4.37.

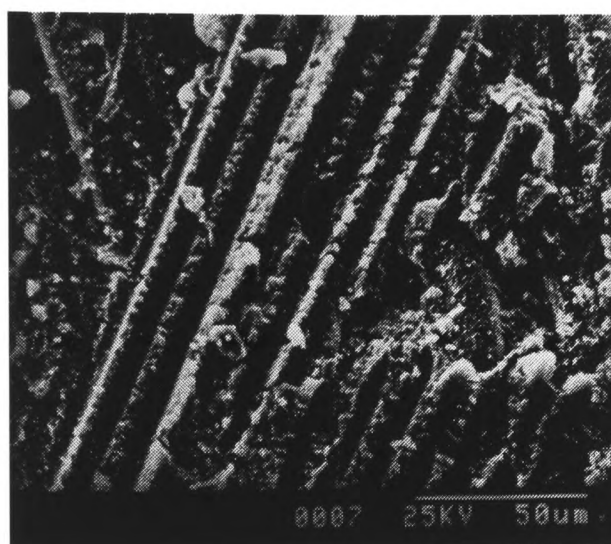


Figure 4.36 PCB4, A Fractograph of Matrix Failure.

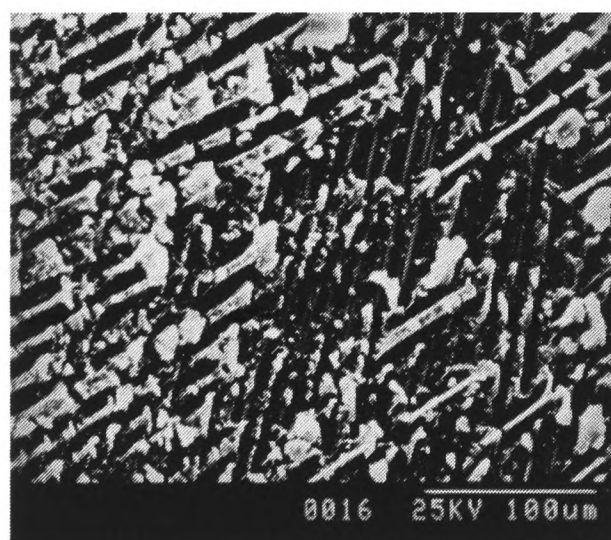


Figure 4.37 NCB4, A Fractograph of Fibre Debonding and Matrix Failure.

c) 916W

All of the PCB specimens failed due to fibre distortion which caused the specimens to hinge at the apex. This demonstrated that reinforcement via weaving in the through-thickness direction may be effective in improving the interlaminar tensile strength, although the strength

in this direction could not be obtained using this experimental method.

Visual inspection using a low power microscope revealed that, unlike the PCB specimens, the NCB specimens failed due to delamination which propagated between the ply at the root of the notch, where the crack initiated, and the adjacent ply. The 916W NCB specimens failed in an identical mode to that of 913UD NCB specimens. Unlike the 913UD laminates, the exact delamination path for the 916W laminates was not clear. The matrix failure and the interfacial debonding of the fibres are presented in the fractographic analyses, shown in Figure 4.38.

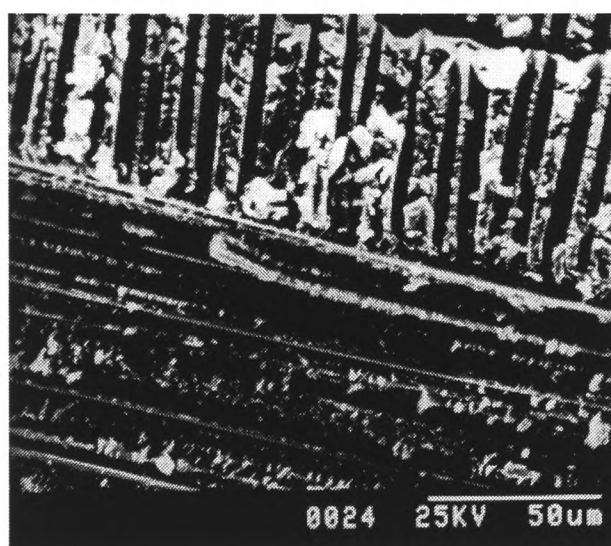


Figure 4.38 NCB6, A Fractograph of Fibre Debonding and Matrix Failure.

4.6 The Notched (NRP) and Oblique (ORP) Rectangular Prism Specimens

A schematic illustration of the loading configuration is shown in Figure 4.39. Load was applied to the upper rig at a rate of 0.5 mm/min via a universal joint until rupture occurred. A load vs. displacement plot was recorded for each test.

A major problem associated with interlaminar tensile test is the achievement of good alignment between the specimens and the loading axis. Even minor misalignment can cause significant local stress concentrations due to bending. There are a number of causes of

misalignment including machining errors, off-set of the upper and lower attachments to the testing machine and misalignment during bonding of the specimen to the loading fixtures. In order to maximise the reliability of the results, four strain gauges, Tokyo Sokki FLA-2-11, were attached to four sides of three specimens such that the difference between opposing strain gauge measurements could be used to quantify and eliminate the aforementioned misalignment. Although significant misalignment was not found, it was thought that the probability of a misalignment in the area of the notch, shown in Figure 4.39, was higher than elsewhere. Thus, for each specimen batch the first two specimens had strain gauges attached to both sides of the notch.

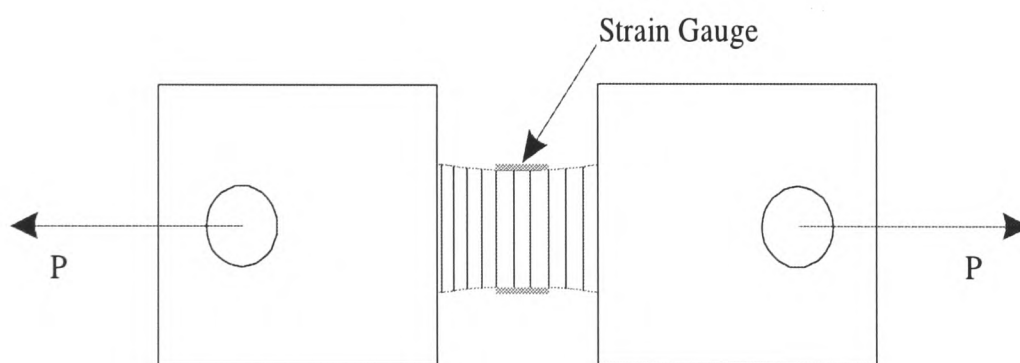


Figure 4.39 A Schematic Illustration of the Simple Tension Test.

4.6.1 Results and Discussion

Initially, the 916W material was used to test the reproducibility of the specimen configuration. It was found that, for the 90° specimens, the initial results showed good consistency. However, the 22.5° , 45° and 67.5° specimens produced large scatter in the results. The 22.5° specimen produced the greatest scatter, equating to a 50 % difference between the results. This was due to the large length of the failure surface for a given specimen length as indicated in Figure 4.40 which shows typical failure surface locations for the oblique ply

configurations. It may clearly be seen that, at this specimen thickness, the failure surface of the 22.5° specimen lies in the region where the aluminum tabs are bonded. The 45° and 67.5° specimens also suffered from a similar problem. In view of this, the specimen geometry was reconfigured for 22.5°, 45° and 67.5° specimens. Since it was not possible to extend the length of specimen due to the manufacturing limitations associated with the thickness of the parent plate, it was decided to reduce the thickness of the specimen from 5 mm to 3 mm as shown in Figure 4.41. This thickness enabled the failure surface of the 22.5° specimen to terminate outside the region of the loading tabs. Numerical analyses, presented in Chapter 5, revealed that, even with flat rectangular geometry, there is a stress concentration due to the oblique angle of the material lay-up direction. This acts so as to localise the failure and negates the need to waist the specimen. Thus, a plain rectangular prism of 12 mm (length) × 6 mm (width) × 3 mm (thickness) was selected. This specimen configuration will be referred to as ‘Oblique Rectangular Prism’ (ORP) and the original specimen geometry (Figure 3.24) will be referred to as ‘Notched Rectangular Prism (NRP)’. The same loading fixture configuration as shown in Figure 3.25 was employed with the addition of chamfering at the inside edges of the aluminium tabs in an attempt to minimise the aforementioned stress concentration at the specimen ends.

The audible sound of cracking was observed before catastrophic delamination occurred. This sound pre-empted specimen failure although, unlike the CNB specimens, the visible development of crazing in the surface was not present. Each load vs. displacement plot is presented in Figures 4.42 - 4.53, all of which exhibit a brittle fracture characteristic. It can be seen from Figures 4.46 - 4.49, 4.52 and 4.53 that there is a ‘knee’ in the curves. This effect was due to the strain hardening of the adhesive layer at the end fixtures. The failure loads for each specimen, at each oblique angle and for each material, are presented in Tables 4.20 - 4.31. The failure location of each specimen was also recorded using a low power microscope. The

reference point for the failure location was taken as the nearest edge of the aluminium tab. This record will subsequently be compared with stress concentrations obtained by FEA in Chapter 5.

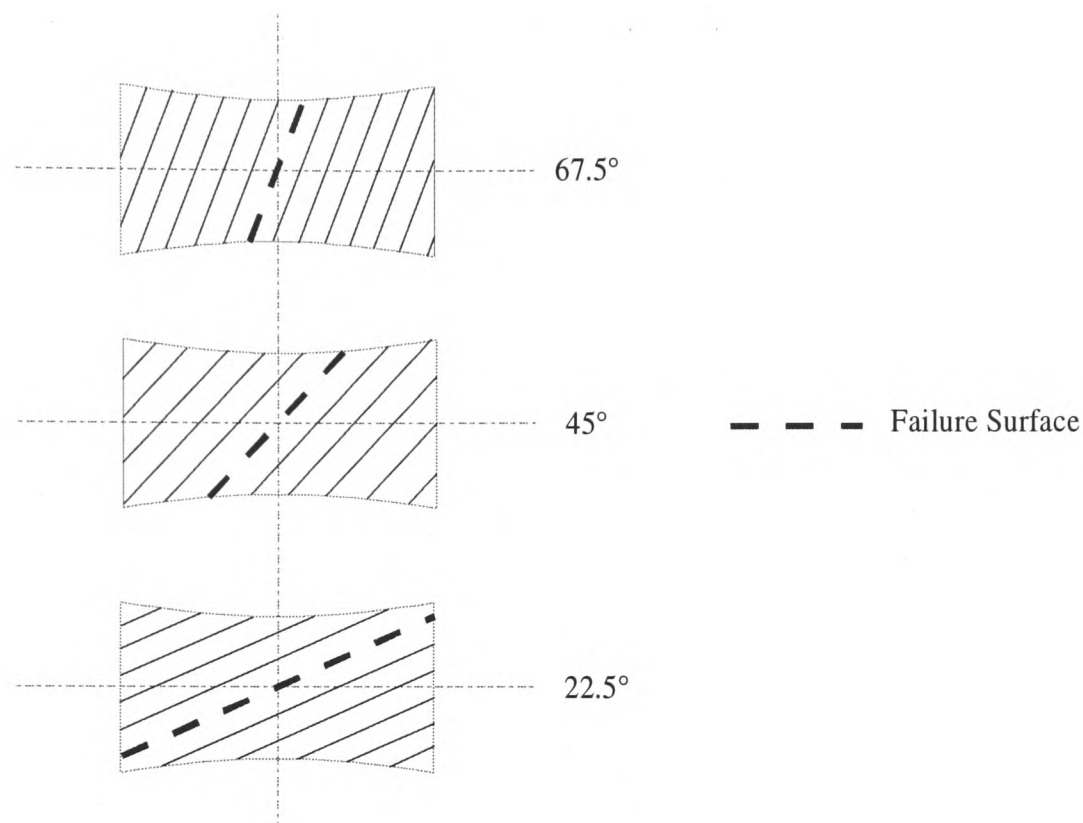


Figure 4.40 The Average Failure Surface Location Observed From the Initial Tests.

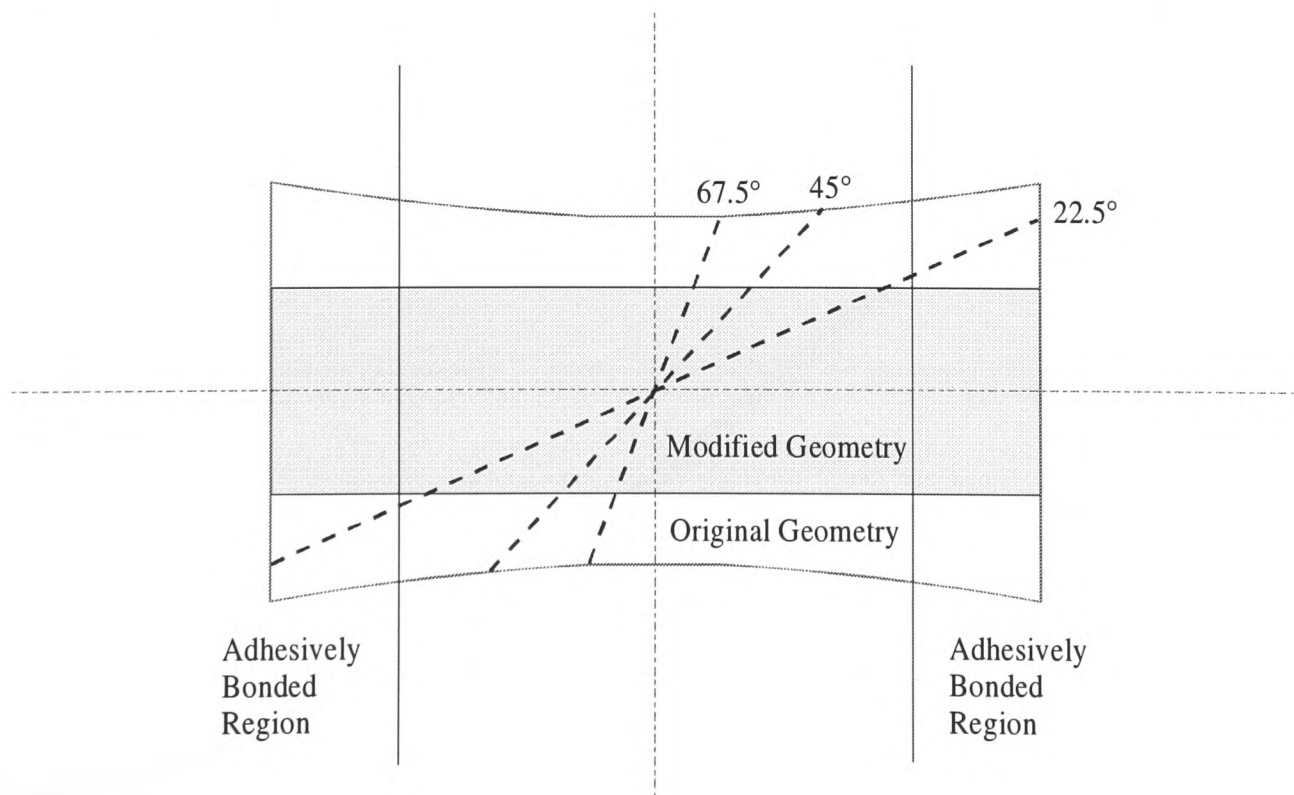


Figure 4.41 The Modified Geometry for the Oblique Rectangular Prism Specimen.

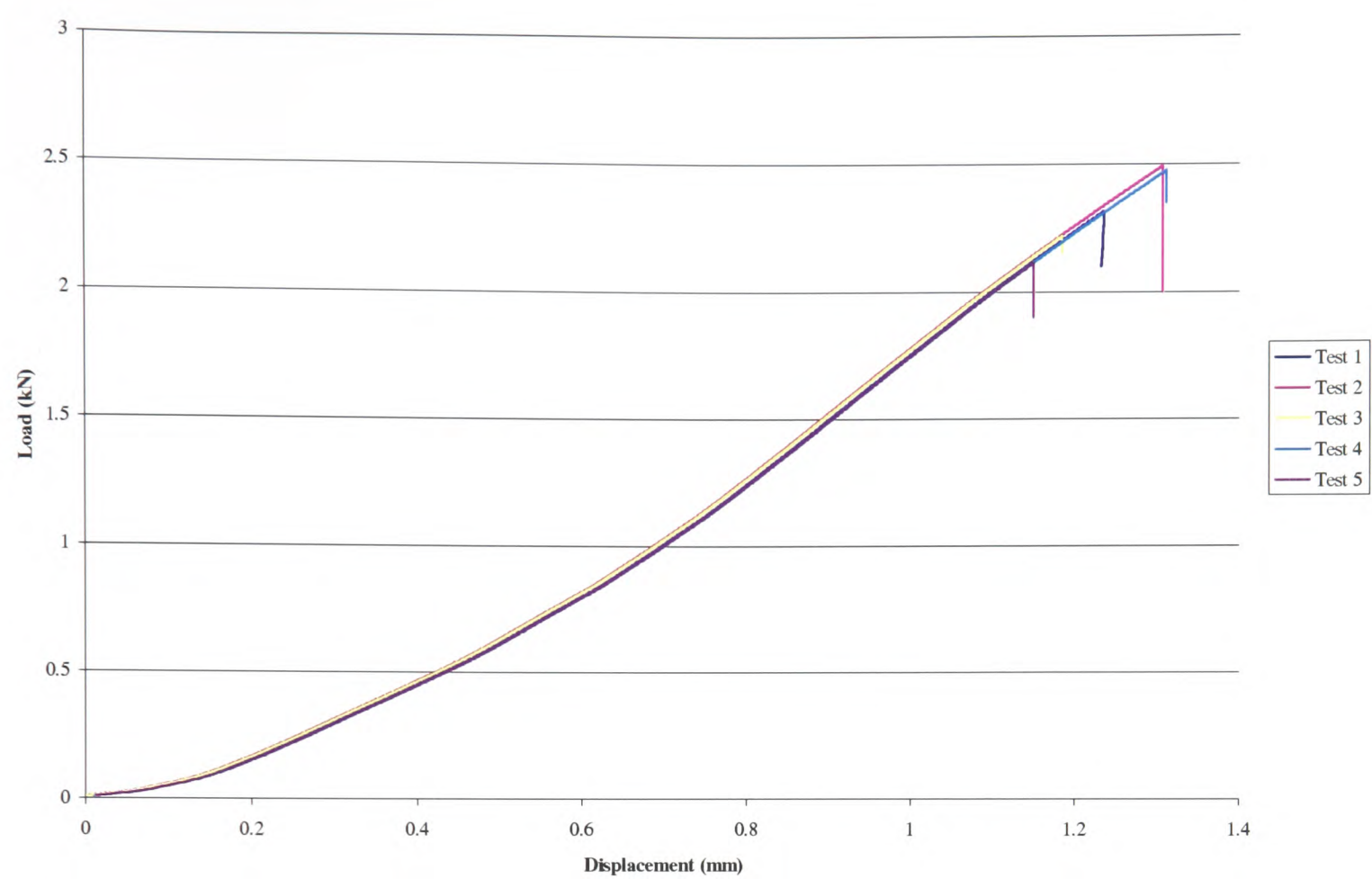


Figure 4.42 NRP3, $\theta=90^\circ$, Load vs. Displacement Curve.

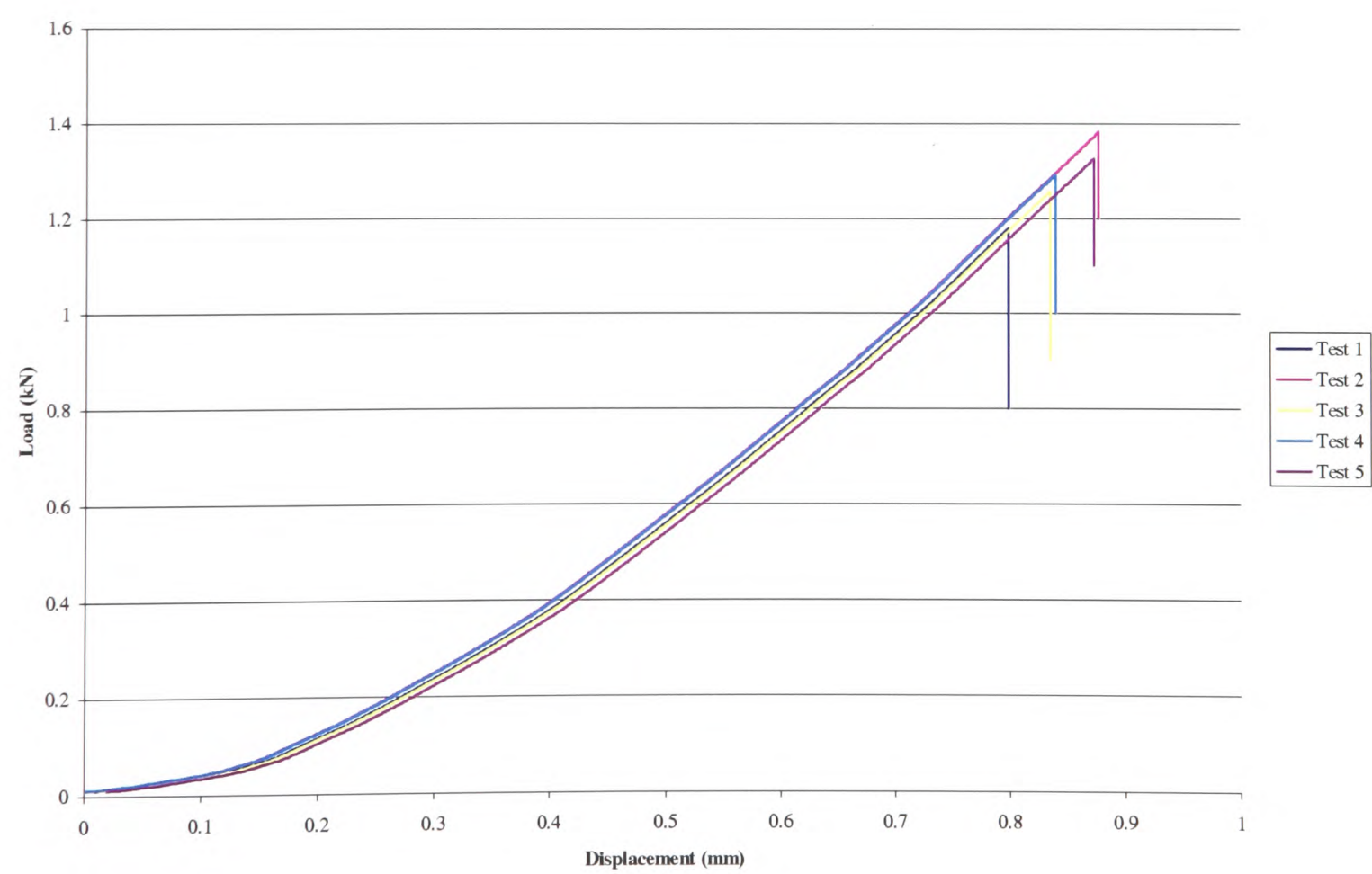


Figure 4.43 ORP3, $\theta=67.5^\circ$, Load vs. displacement Curve.

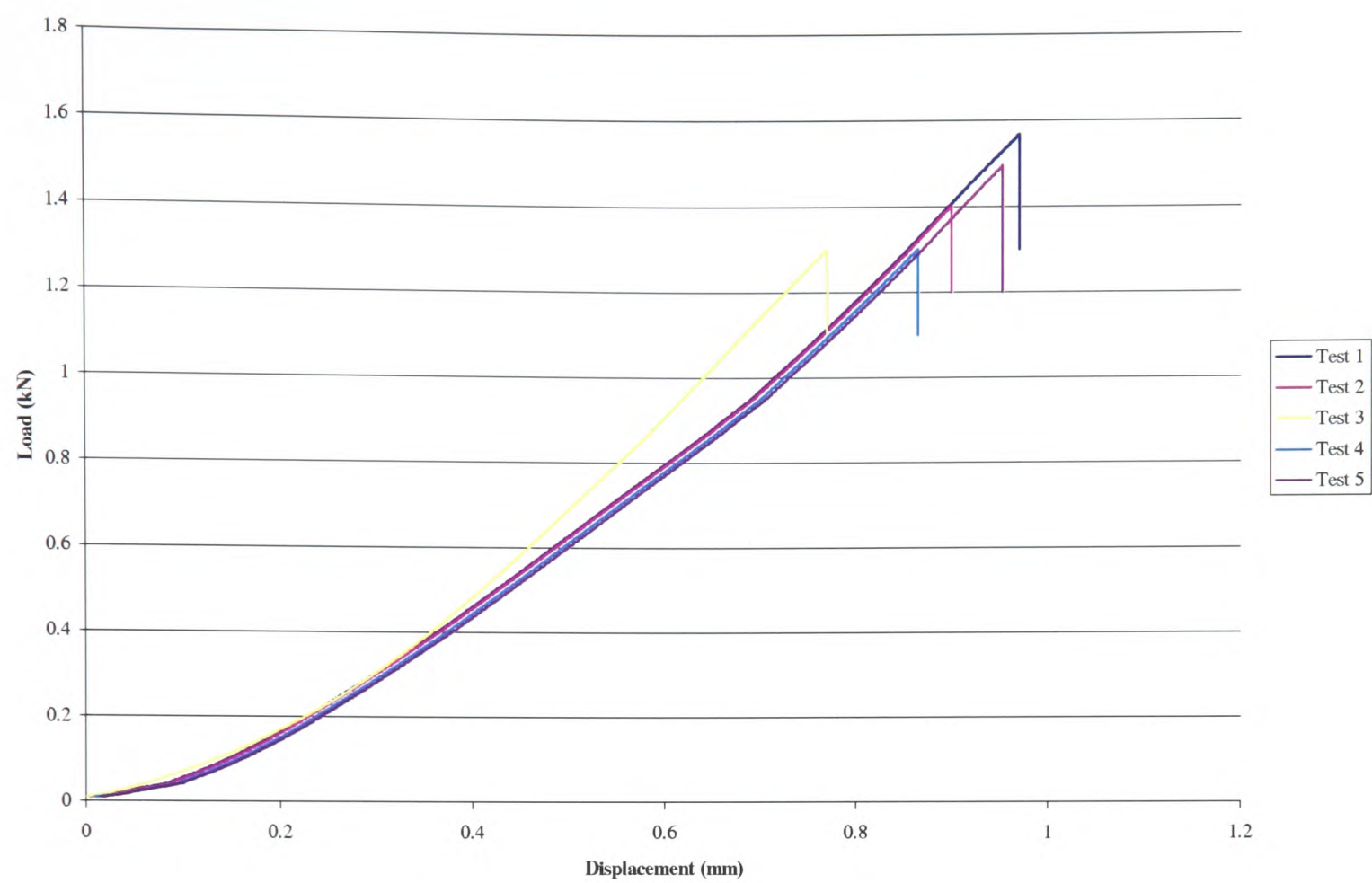


Figure 4.44 ORP3, $\theta=45^\circ$, Load vs. Displacement Curve.

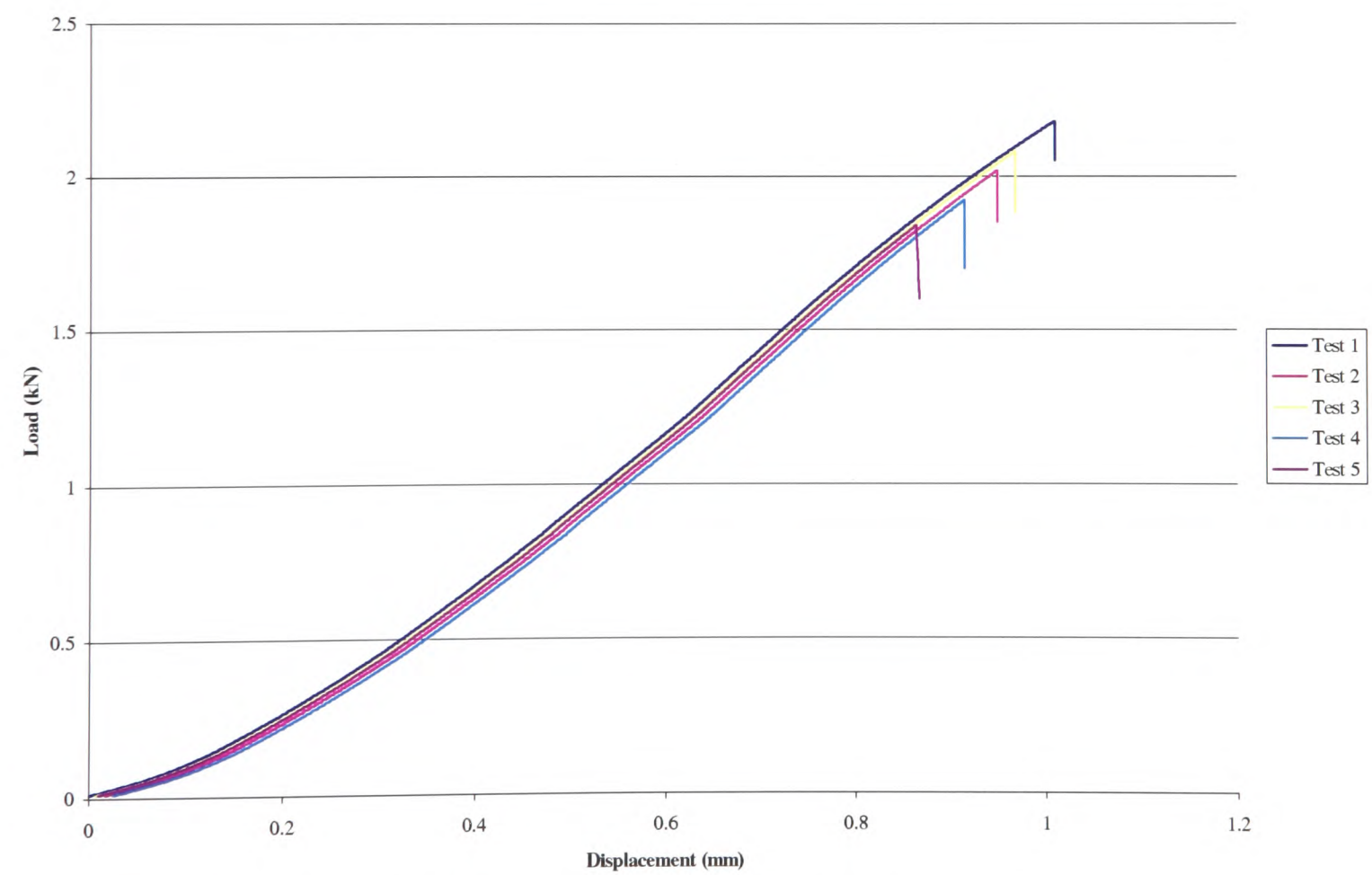


Figure 4.45 ORP3, $\theta=22.5^\circ$, Load vs. Displacement Curve.

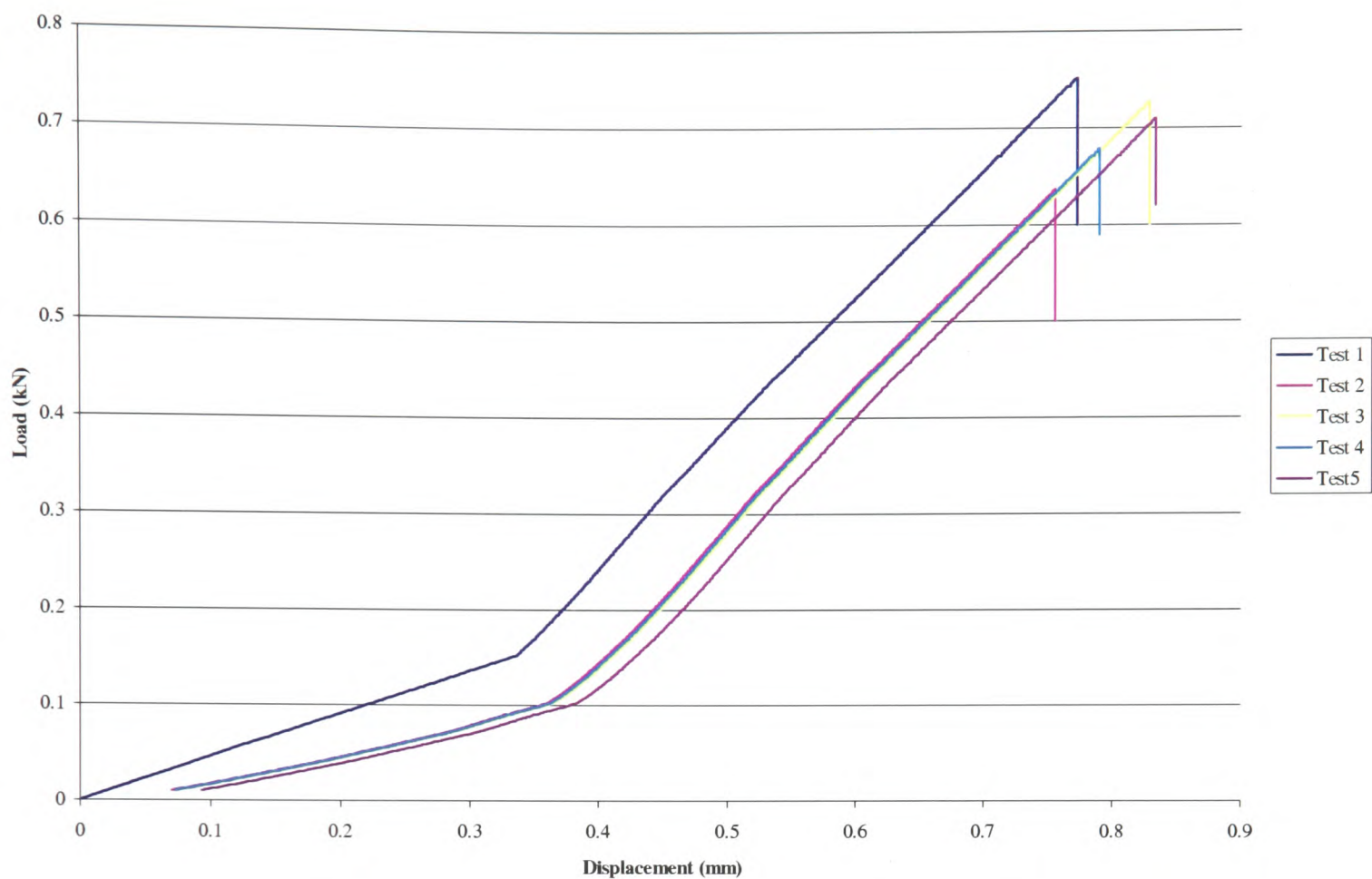


Figure 4.46 NRP4, $\theta=90^\circ$, Load vs. Displacement.

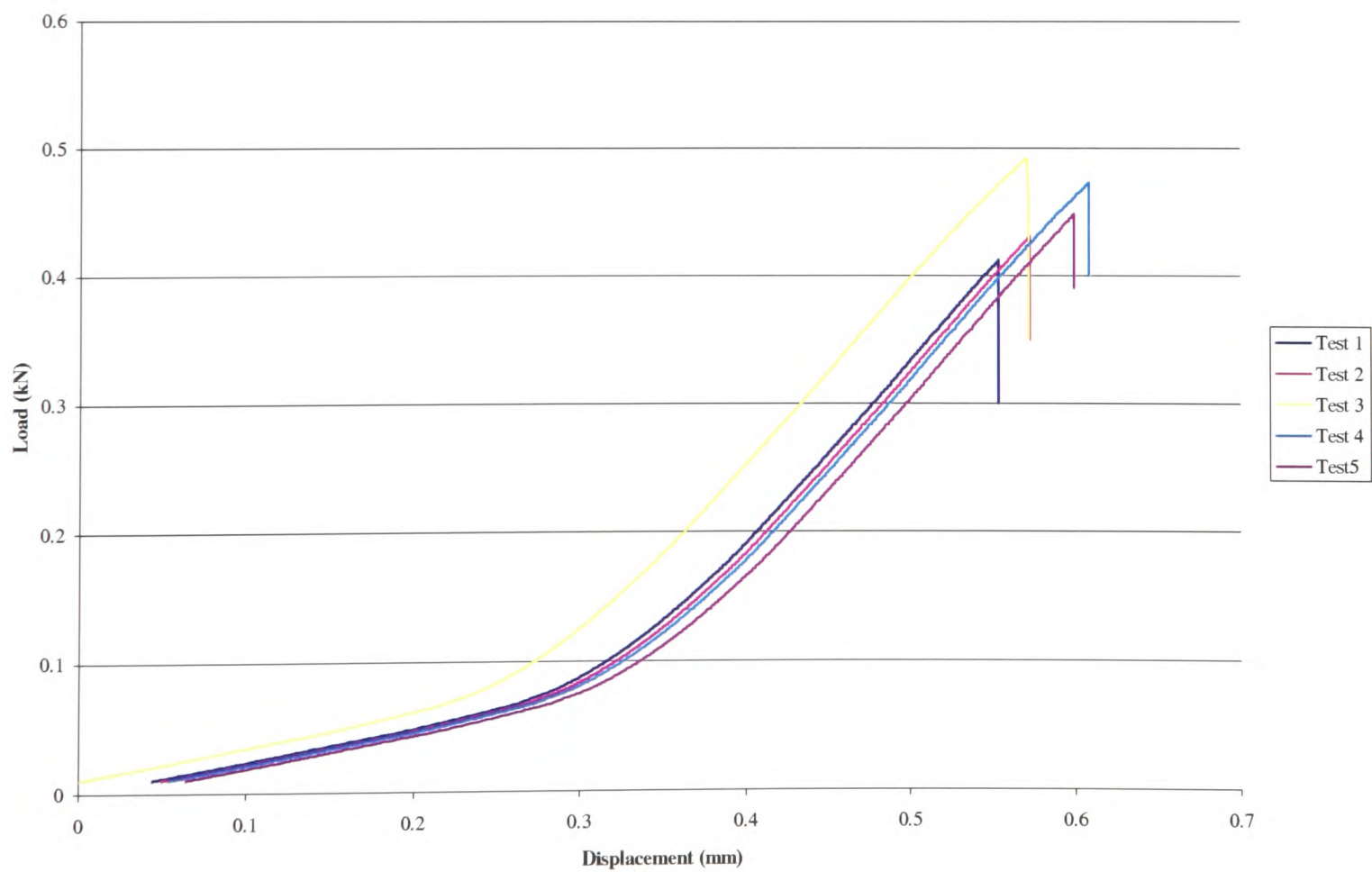


Figure 4.47 ORP4, $\theta=67.5^\circ$, Load vs. Displacement Curve.

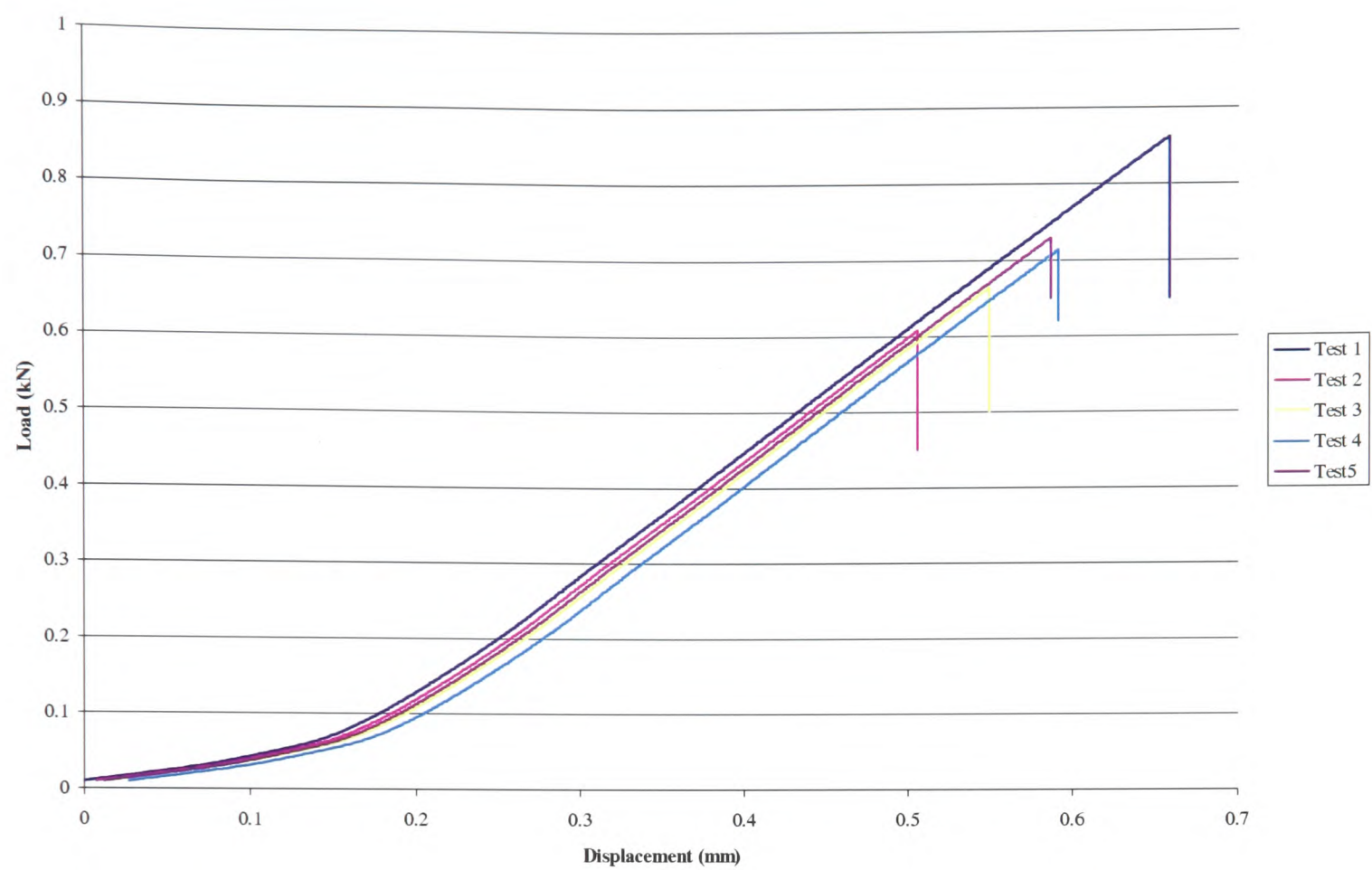


Figure 4.48 ORP4, $\theta=45^\circ$, Load vs. Displacement Curve.

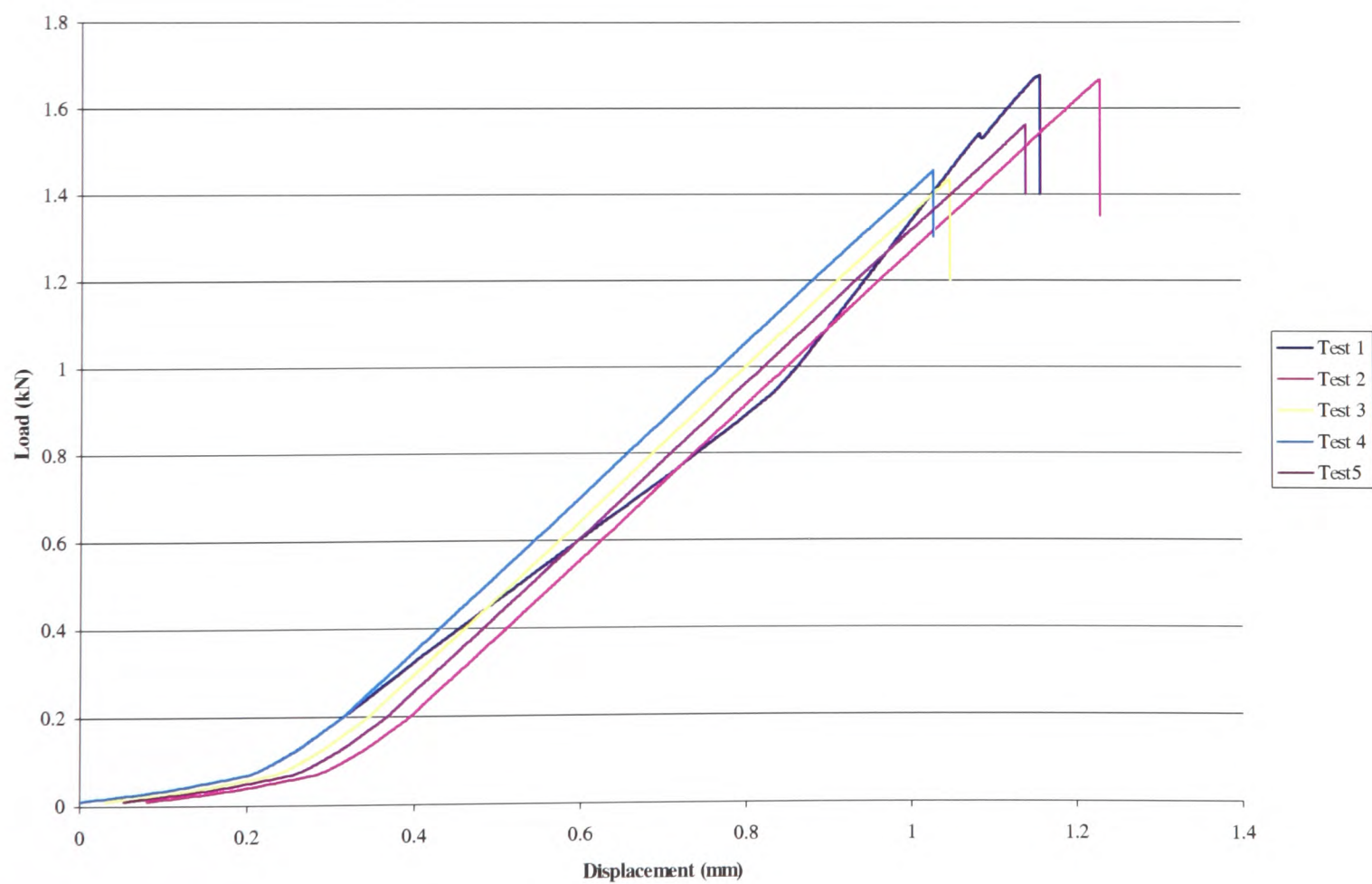


Figure 4.49 ORP4, $\theta=22.5^\circ$, Load vs. Displacement Curve.

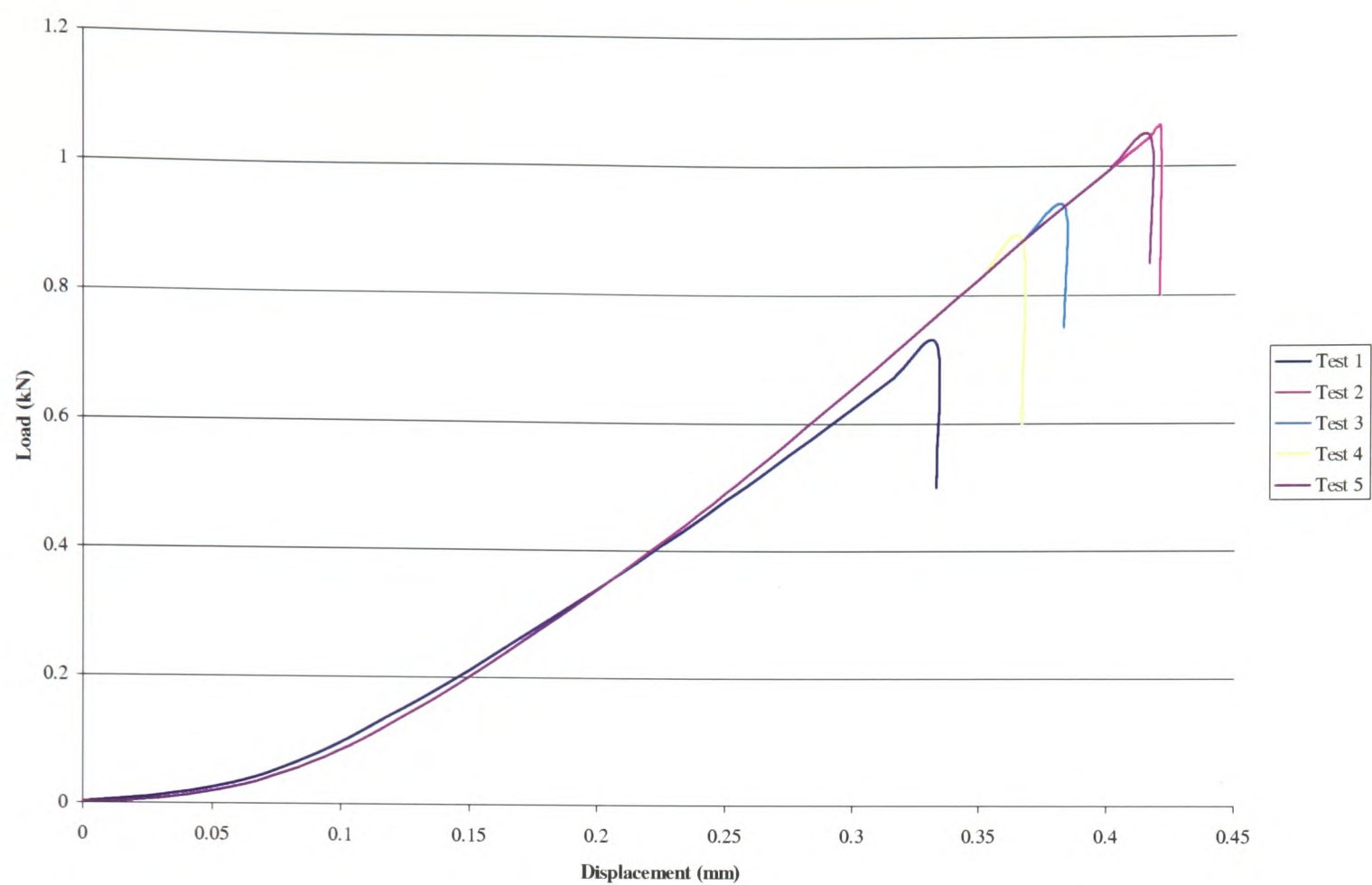


Figure 4.50 NRP6, $\theta=90^\circ$, Load vs. Displacement Curve.

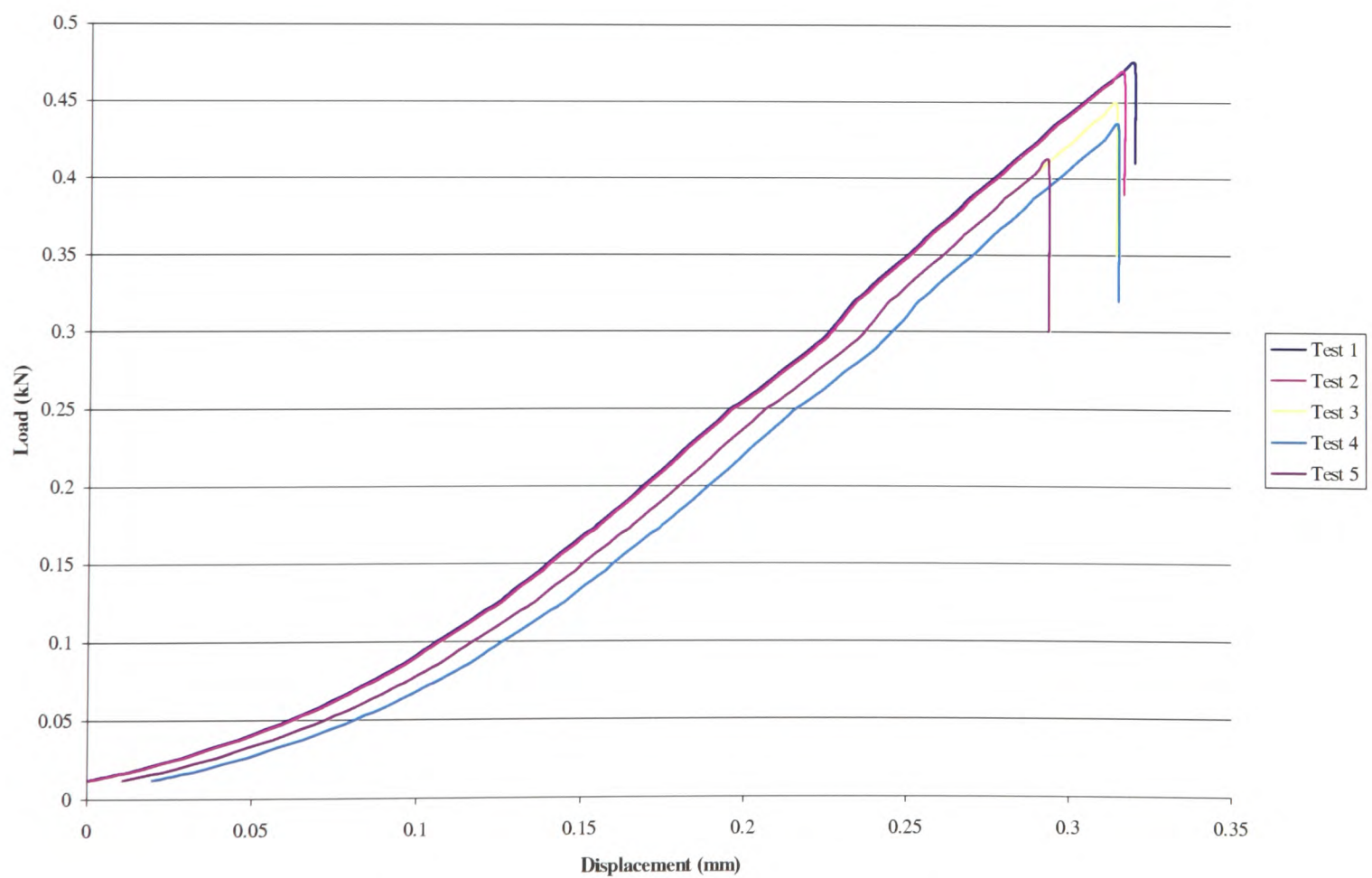


Figure 4.51 ORP6, $\theta=67.5^\circ$, Load vs. Displacement Curve.

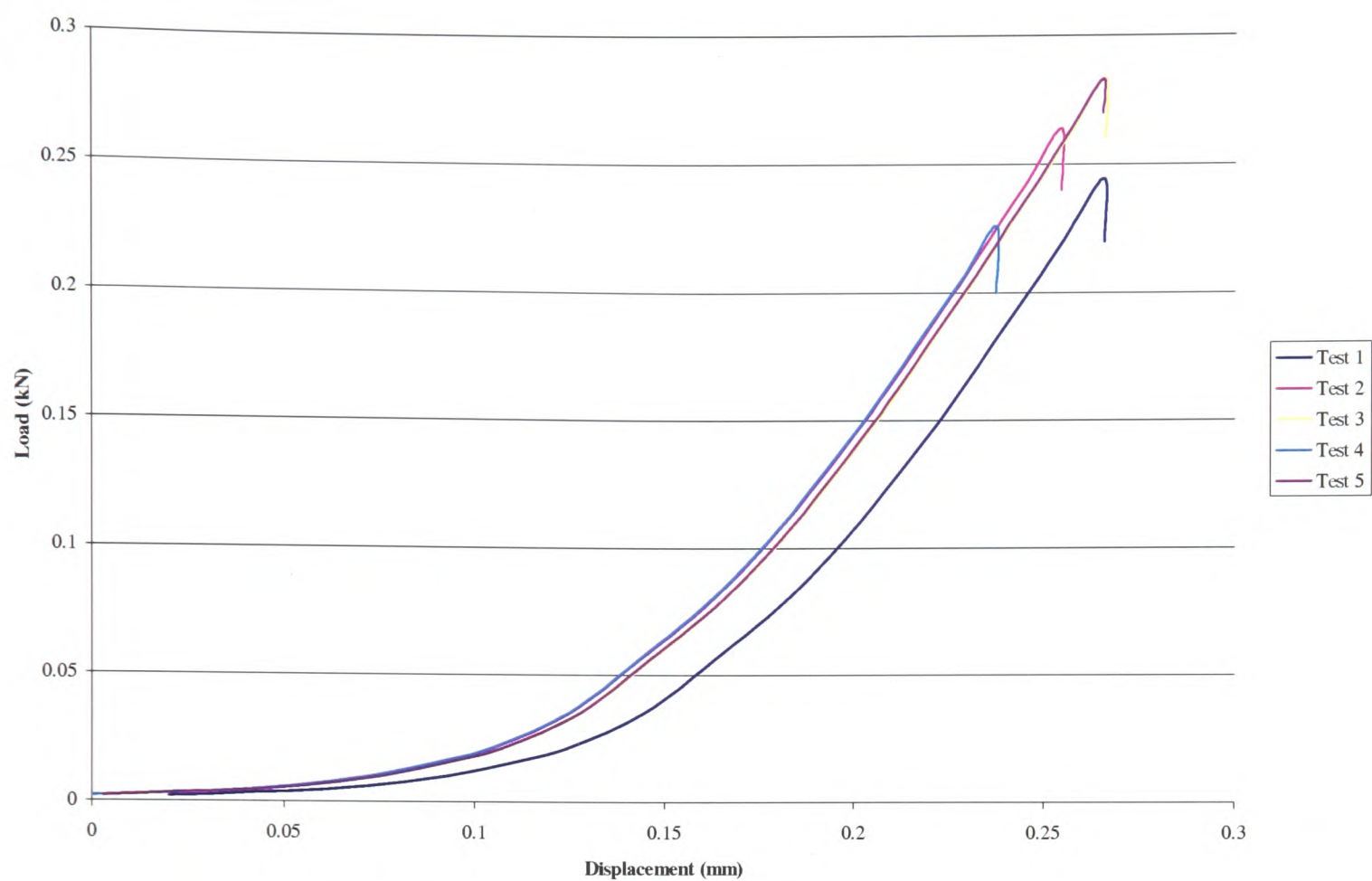


Figure 4.52 ORP6, $\theta=45^\circ$, Load vs. Displacement Curve.

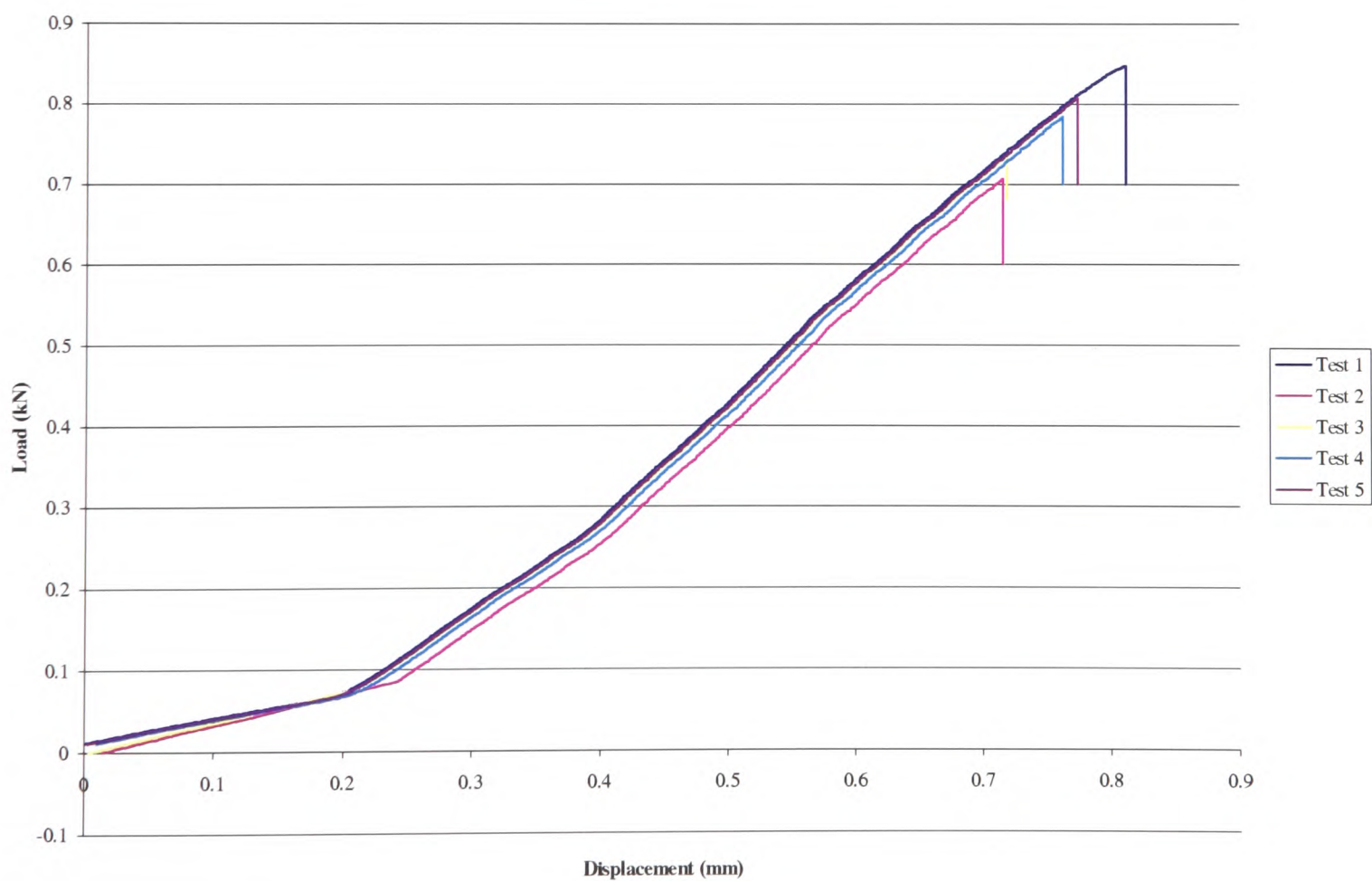


Figure 4.53 ORP6, $\theta=22.5^\circ$, Load vs. Displacement Curve.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle ϕ (°)	Comments
1	111	UD	0°/90°	5.04	12.01	5.99	2.32	90	Failed at the test section
2	111	UD	0°/90°	5.06	11.99	5.99	2.49	90	Failed at the test section
3	111	UD	0°/90°	5.02	12.00	6.01	2.22	90	Failed near the loading tab
4	111	UD	0°/90°	5.04	11.99	6.00	2.47	90	Failed at the test section
5	111	UD	0°/90°	5.03	12.02	6.01	2.11	90	Failed at where the notch starts

Standard Deviation of Failure Load = 0.162

Table 4.20 913UD No.tched Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle ϕ (°)	Comments
1	111	UD	0°/90°	3.02	12.00	6.00	1.18	67.5	Failed 0.3 mm from the edge
2	111	UD	0°/90°	3.06	12.02	6.05	1.38	67.5	Failed 1.1 mm from the edge
3	111	UD	0°/90°	3.04	12.01	6.00	1.26	67.5	Failed 1.9 mm from the edge
4	111	UD	0°/90°	3.05	12.01	6.04	1.29	67.5	Failed 0.5 mm from the edge
5	111	UD	0°/90°	3.04	12.02	6.02	1.32	67.5	Failed 0.9 mm from the edge

Standard Deviation of Failure Load = 0.074

Table 4.21 913UD Oblique Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle ϕ (°)	Comments
1	111	UD	0°/90°	3.00	12.01	6.08	1.57	45	Failed 1.6 mm from the edge
2	111	UD	0°/90°	3.02	12.04	6.1	1.40	45	Failed 3.1 mm from the edge
3	111	UD	0°/90°	3.03	12.01	6.09	1.30	45	Failed 5.5 mm from the edge
4	111	UD	0°/90°	3.01	12.02	6.05	1.30	45	Failed 4.0 mm from the edge
5	111	UD	0°/90°	3.00	12.01	6.06	1.50	45	Failed 2.5 mm from the edge

Standard Deviation of Failure Load = 0.120

Table 4.22 913UD Oblique Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle ϕ (°)	Comments
1	111	UD	0°/90°	3.01	12.01	6.04	2.18	22.5	Failed 0.2 mm from the edge
2	111	UD	0°/90°	3.00	12.03	6.03	2.02	22.5	Failed 0.5 mm from the edge
3	111	UD	0°/90°	2.99	12.02	6.01	2.08	22.5	Failed 0.4 mm from the edge
4	111	UD	0°/90°	3.00	12.00	6.00	1.92	22.5	Failed 0.4 mm from the edge
5	111	UD	0°/90°	3.01	11.99	6.02	1.84	22.5	Failed 0.5 mm from the edge

Standard Deviation of Failure Load = 0.133

Table 4.23 913UD Oblique Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle φ (°)	Comments
1	88	NCF	0°/-45°/+45°/90°	5.05	12.00	6.00	0.75	90	Failed at the test section
2	88	NCF	0°/-45°/+45°/90°	5.09	12.01	5.99	0.64	90	Failed along the notch
3	88	NCF	0°/-45°/+45°/90°	5.03	11.99	5.98	0.73	90	Failed at the test section
4	88	NCF	0°/-45°/+45°/90°	5.04	12.00	6.00	0.68	90	Failed along the notch
5	88	NCF	0°/-45°/+45°/90°	5.02	12.01	5.99	0.71	90	Failed at the test section

Standard Deviation of Failure Load = 0.043

Table 4.24 914NCF No.tched Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle φ (°)	Comments
1	88	NCF	0°/-45°/+45°/90°	3.02	11.99	6.04	0.41	67.5	Failed 2.1 mm from the edge
2	88	NCF	0°/-45°/+45°/90°	3.03	12.01	6.02	0.43	67.5	Failed 1.8 mm from the edge
3	88	NCF	0°/-45°/+45°/90°	3.02	12.01	6.02	0.49	67.5	Failed 1.1 mm from the edge
4	88	NCF	0°/-45°/+45°/90°	3.00	12.00	6.03	0.47	67.5	Failed 0.8 mm from the edge
5	88	NCF	0°/-45°/+45°/90°	3.01	12.00	6.01	0.45	67.5	Failed 1.9 mm from the edge

Standard Deviation of Failure Load = 0.031

Table 4.25 913UD Oblique Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle ϕ (°)	Comments
1	88	NCF	0°/-45°/+45°/90°	3.03	12.00	6.04	0.86	45	Failed 4.2 mm from the edge
2	88	NCF	0°/-45°/+45°/90°	3.05	12.01	6.05	0.61	45	Failed 3.6 mm from the edge
3	88	NCF	0°/-45°/+45°/90°	3.03	11.99	6.05	0.66	45	Failed 1.3 mm from the edge
4	88	NCF	0°/-45°/+45°/90°	3.02	12.00	6.03	0.71	45	Failed 3.2 mm from the edge
5	88	NCF	0°/-45°/+45°/90°	3.01	12.01	6.04	0.73	45	Failed 2.8 mm from the edge

Standard Deviation of Failure Load = 0.094

Table 4.26 914NCF Oblique Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle ϕ (°)	Comments
1	88	NCF	0°/-45°/+45°/90°	3.03	12.01	6.04	1.68	22.5	Failed 0.7 mm from the edge
2	88	NCF	0°/-45°/+45°/90°	3.05	12.00	6.05	1.66	22.5	Failed 0.5 mm from the edge
3	88	NCF	0°/-45°/+45°/90°	3.03	11.99	6.03	1.44	22.5	Failed 0.6 mm from the edge
4	88	NCF	0°/-45°/+45°/90°	3.04	12.03	6.03	1.45	22.5	Failed 0.5 mm from the edge
5	88	NCF	0°/-45°/+45°/90°	3.02	12.02	6.09	1.56	22.5	Failed 0.6 mm from the edge

Standard Deviation of Failure Load = 0.113

Table 4.27 914NCF Oblique Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle ϕ (°)	Comments
1	71	Woven	0°/90°	5.04	12.01	6.03	0.72	90	Failed at where the notch starts
2	71	Woven	0°/90°	5.05	12.05	6.03	1.06	90	Failed at the test section
3	71	Woven	0°/90°	5.02	12.00	6.05	0.94	90	Failed at the test section
4	71	Woven	0°/90°	5.04	12.03	6.02	0.88	90	Failed near the loading tab
5	71	Woven	0°/90°	5.03	12.04	6.04	1.05	90	Failed at the test section

Standard Deviation of Failure Load = 0.139

Table 4.28 916W No.tched Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle ϕ (°)	Comments
1	71	Woven	0°/90°	3.01	12.01	6.09	0.47	67.5	Failed 0.9 mm from the edge
2	71	Woven	0°/90°	3.07	12.00	6.06	0.47	67.5	Failed 1.3 mm from the edge
3	71	Woven	0°/90°	3.02	12.00	5.99	0.45	67.5	Failed 1.8 mm from the edge
4	71	Woven	0°/90°	3.05	12.01	6.01	0.43	67.5	Failed 1.5 mm from the edge
5	71	Woven	0°/90°	3.04	12.03	6.01	0.41	67.5	Failed 1.6 mm from the edge

Standard Deviation of Failure Load = 0.026

Table 4.29 916W Oblique Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle ϕ (°)	Comments
1	71	Woven	0°/90°	3.03	12.01	6.00	0.24	45	Failed 4.6 mm from the edge
2	71	Woven	0°/90°	3.02	12.00	6.05	0.26	45	Failed 2.9 mm from the edge
3	71	Woven	0°/90°	3.06	12.02	6.00	0.28	45	Failed 2.3 mm from the edge
4	71	Woven	0°/90°	3.04	12.02	6.04	0.23	45	Failed 3.2 mm from the edge
5	71	Woven	0°/90°	3.05	12.01	6.03	0.28	45	Failed 3.9 mm from the edge

Standard Deviation of Failure Load = 0.023

Table 4.30 916W Oblique Rectangular Prism Specimen.

Test No.	No. of Layers	Fabric Type	Fabric Orientation	Thickness t (mm)	Height h (mm)	Width b (mm)	Failure Load (kN)	Oblique Angle ϕ (°)	Comments
1	71	Woven	0°/90°	3.00	12.00	6.08	0.85	22.5	Failed 0.5 mm from the edge
2	71	Woven	0°/90°	3.02	11.99	6.07	0.71	22.5	Failed 0.6 mm from the edge
3	71	Woven	0°/90°	3.03	12.00	6.01	0.73	22.5	Failed 0.4 mm from the edge
4	71	Woven	0°/90°	3.01	12.01	6.04	0.78	22.5	Failed 0.6 mm from the edge
5	71	Woven	0°/90°	3.02	12.02	6.03	0.81	22.5	Failed 0.6 mm from the edge

Standard Deviation of Failure Load = 0.057

Table 4.31 916W Oblique Rectangular Prism Specimen.

All of the NRP specimens exhibited two types of failure, i.e. failure at the centre of the specimen and failure at some point along the notch profile. It should be noted that only two of the specimens (913UD and 916W) failed at the start of the notch radius, a result which showed good consistency, as demonstrated by the low standard deviation value for each material.

The results for all of the ORP specimens also showed good consistency. Of all of the ORP specimens, the 45° specimen configuration showed the largest variation in failure location followed by the 67.5° specimen configuration. This variation was expected in the 67.5° specimen because of the shorter length of the failure surface which is more easily formed across the specimen thickness, a characteristic which is explained in more detail in Section 5.7.1.

References

- 4.1 Pindera M.J. and Herakovich K.T., Shear Characterization of Unidirectional Composites with Off-Axis Test, *Experimental Mechanics*, Vol.26, No.1, 1986, p103-112.
- 4.2 Agrawal B.D. and Broutman L.J., *Analysis and Performance of Fibre Composites*, John Wiley & Sons Inc., New York, 1990.
- 4.3 Bonded Structures Ciba-Geigy Plastics, Information Sheet No. FTA 46f, 1991.
- 4.4 Gibson R.F., *Principles of Composite Materials Mechanics*, McGraw-Hill Inc. New York, 1994.
- 4.5 Lees W.A., *Adhesives in Engineering Design*, The Design Council, London, 1984.

Chapter 5

Analysis of Laminates

5.1 Introduction

In Chapter 4 it had been demonstrated that fibrous composites exhibit a wide range of behaviour depending upon the properties of the fibre and matrix constituents, the fibre orientation of the laminae, and the type of loading. In this chapter the theoretical stress distributions within the various types of notched specimens are obtained either analytically or numerically. The solutions for most of the specimens, i.e. the Centrally Notched Beam, Plain Curved Beam, Notched Curved Beam, Notched Rectangular Prism and Oblique Rectangular Prism, are obtained numerically with the exception of the Iosipescu specimen for which an analytical analysis is presented. The use of sophisticated numerical procedures, in conjunction with experimentally determined boundary and initial conditions, yielded the complete states of stress, strain, and displacement for the given constitutive relations, boundary conditions and initial conditions.

5.2 The Modelling Methodology Used for the Finite Element Analysis

The material was assumed to be orthotropic such that the laminate could be considered an orthotropic homogeneous continuum in which the elastic contributions from fibres and matrix were represented by averaged bulk properties. The previously obtained elastic properties (Table 4.5) were used as input. Additionally, a linear elastic response was assumed as most of the specimens exhibited a linear load vs. displacement relationship up to the ultimate strength at which point sudden failure occurred (brittle type failure characteristic). The process of modelling was divided into two stages:

1. Modelling the specimen geometry and creating a suitable mesh for use in an FEA utilising the PATRAN 3 computer software package.
2. Debugging the data input file by iterative use of the ABAQUS FEA software package.

The computational efficiency of an FE model in ABAQUS was improved by reducing the bandwidth of the global matrix created during each analysis. The bandwidth of the FE model created within PATRAN 3 was reduced using the Cuthill-McKee method within the function *optimise* which is specific to PATRAN 3. This procedure reduced the bandwidth by optimising and reconfiguring the node and element numbers.

In the ABAQUS FEA, 20 noded hexahedral elements were used as these solid elements have been shown to improve the accuracy of the solution over that obtained using lower order elements [5.1]. The extra nodes on the elements also allow the boundaries of the hexahedral elements to be distorted from that of a plain parallelepiped and facilitated a more accurate representation of the curved boundaries along the notch.

In the following sections only the final mesh is presented, though in order to reach these optimal meshes several iterations were required. Since the dimensions of the specimens differed between material batches, all of the models were parameterised so that slight variations

in the geometry could easily be achieved. The stress distributions for each material were obtained by altering the specimen dimensions and the engineering constants in the ABAQUS input data file. All the models were deformed by applying a displacement at the loading points until the reaction force at these loading points coincided with the failure load observed in the experiments. It should be noted that, in the stress distributions which follow, the magnitude of the contour levels were expressed in MPa for all models.

5.3 The Iosipescu Specimen

It is known that the shear stress distribution in the region between the two notch roots is not perfectly uniform. A previous study using the UTC specimen, with a fillet radius of approximately 0.25 mm at the notch root, showed that the maximum shear stress was approximately 4 % higher than the mean level [5.2]. The Iosipescu specimens used in this study also had a fillet radius of approximately 0.25 mm (Section 3.7.1). When 32 ply carbon/epoxy (ACG T700/LTM45) laminates in three different lay-up configurations, i.e. unidirectional, cross-ply and quasi-isotropic, were tested to obtain the in-plane and interlaminar shear properties, it was reported [5.3] that eqⁿ. 5.1 produced good agreement with the shear strength values obtained by other researchers.

$$\tau = \frac{U}{wh} \quad (5.1)$$

where U , w and h are the ultimate force, distance between the two notches and thickness of the specimen respectively. Therefore, it was assumed that the average shear stress could be defined conventionally by the ratio of the ultimate force, U , as measured by the load cell, to the cross sectional area between the two notches.

5.3.1 Results and Discussion

The interlaminar shear strengths obtained from each test along with the final averages are presented in Table 5.1. It was thought that 916W would yield the greatest strength due to interactions within the complex fibre architecture (plain weave). It is clear from Tables 5.1 that this was not the case. If the fibre volume fraction is considered to be one of the dominant differences between 913UD and 916W, then this unexpected result can be explained. From Table 4.2 we see that the fibre volume fractions of the 5 mm plate made from 913UD and 916W were 0.62 and 0.49 respectively. This difference is indicative of how closely the fibre layers were packed for each material type.

Test No.	913UD	914NCF	916W
1	63.9	57.3	66.1
2	74.1	55.6	63.7
3	67.1	-	69.2
4	71.4	65.4	68.6
5	-	59.1	69.4
Average	69.2	59.6	67.5

Table 5.1 The Interlaminar Shear Strength (MPa) of Various Laminates.

Figure 5.1 shows the representative volume elements (RVE) for 913UD and 916W. It can be seen from the schematic diagram that, owing to the crimped architecture, the fibre layer in a plain weave lamina is thicker than that for a unidirectional lamina. As the fibre volume fraction increases, the amount of fibre interaction with the adjacent layers increases due to the decrease in the thickness of the matrix layer. Hence, for identical fibre volume fractions, we would

expect the interlaminar shear strength of 916W to be greater than that of 913UD and not the misleading result obtained in Table 5.1 due to the inconsistent fibre volume fractions.

A reason for the low strength of 914NCF was because of the symmetric lay-up configuration $[(0^\circ/-45^\circ/+45^\circ/90^\circ)_8]_s$ which places the mid plane of the laminate between two 90° unidirectional laminae which contribute nothing to the interlaminar shear strength. Using carbon/epoxy laminates, Zhou *et al.* [5.3] found that the interlaminar shear strength of cross-ply laminates was 23 % greater than that of unidirectional laminates. This supports the conclusion that the cross-ply laminate configuration is stronger in this respect.

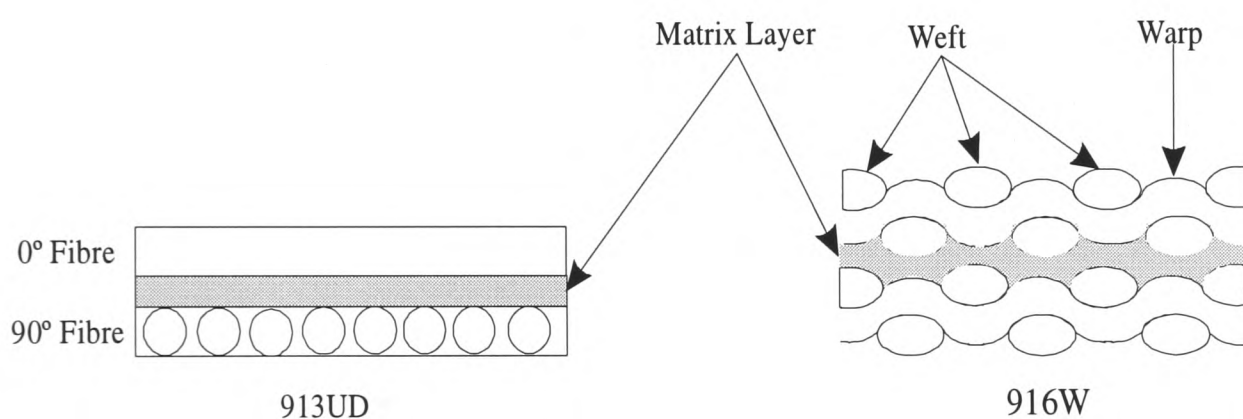


Figure 5.1 The Representative Volume Elements for 913UD and 916W.

5.4 The Plain Curved Beam Specimen (PCB)

In this model a technique called ‘sub-modelling’ was used to divide the analysis into either two or three stages depending upon material type. Sub-modelling is the technique of studying part of a model with a refined mesh, based upon interpolation of the solution from an initial, global model, onto the nodes of the boundary of the sub-model [5.1].

The specimen geometry was modelled (Figure 5.2) as accurately as possible, especially around the pin jointed loading points, in order to minimise approximations. As the specimen is symmetric about the centre of the width, it was only necessary to model one half of the

specimen. The total number of elements used in the global model was 2064 with 3 elements across the thickness and 4 elements across the width. Ridged elements were used to model the pins and 27 noded hexahedral contact elements were used to simulate the steel inserts. The necessity for this contact modelling method is due to the large deformations of the model such that the loading points change orientation consistent with the deformation during the analysis (Figure 5.3). Due to the curvilinear geometry, it was necessary to convert the element co-ordinate frame from cartesian to polar coordinates to avoid erroneous results.

There were two sub-models for the 913UD model and one sub-model for the 914NCF and 916W models. The first sub-model was used to improve the accuracy of the results by using a very fine mesh (1800 elements) with 10 elements across the thickness and 5 elements across the width. The model was still assumed to be homogeneous as shown in Figure 5.2. In the second sub-model each layer was modelled in order to allow for heterogeneity of the material as shown in Figure 5.2. This also allowed the material properties of a unidirectional laminate, obtained from the manufacture's data sheet (Table 4.5), to be employed instead of the previously determined averaged properties. 39 elements in the through-thickness direction were used and were given orientation angles of 0° or 90° as appropriate, relative to the principal material coordinate system.

5.4.1 The Mechanics and Validation of the Sub-Modelling Technique

The sub-modelling technique is most useful when it is necessary to obtain an accurate solution in a local region, the detailed modelling of which has a negligible effect on the overall solution. The response at the boundary of the local region is defined by the solution for the global model which, together with any loads applied to the local region, define the boundary conditions for the sub-model solution which is run as a separate analysis. The only link

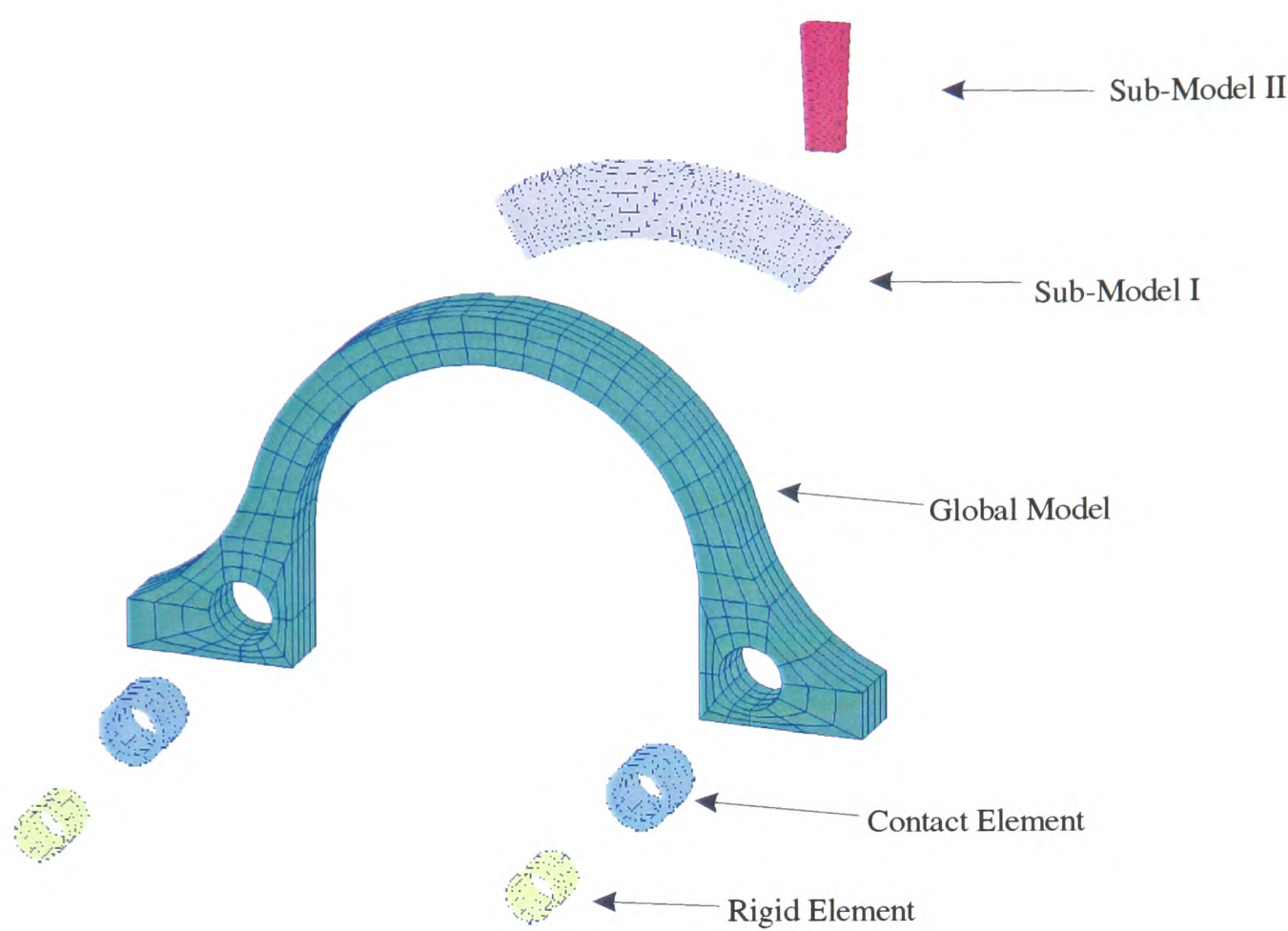


Figure 5.2 PCB, Various Element Sets Employed for a Complete Model.

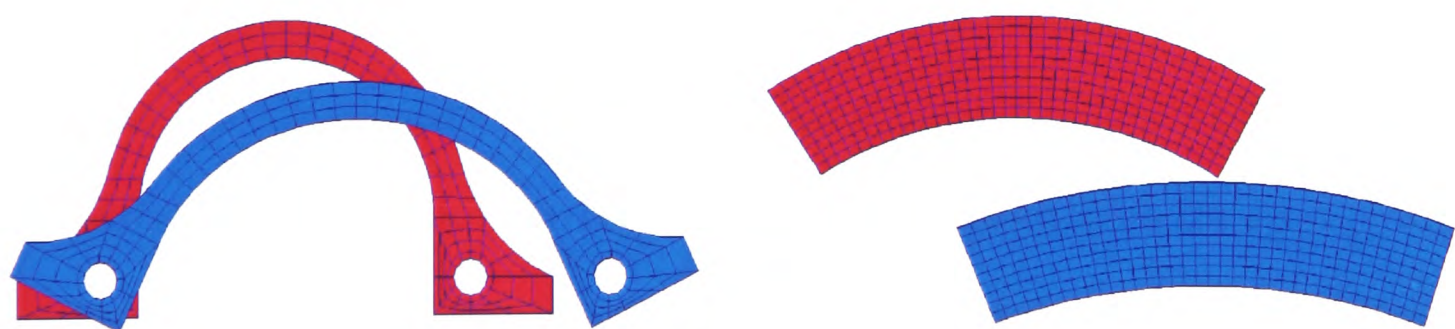


Figure 5.3 PCB, Displacement Plot of the Global and Sub-model.

between the sub-model and the global model is the transfer of the values of the time dependent variables to the relevant boundary nodes (the 'driven nodes') of the sub-model. The only information in the global model available to the sub-model analysis is the file output data written during the global model analysis. This contains the undeformed coordinates of all global model nodes and element information for all elements in the global model. In the solid-to-solid case (current analysis) the position of the sub-model boundary nodes are determined with respect to the global model and the appropriate element interpolation functions are used to obtain the values of the degrees of freedom at these driven nodes.

For the purpose of assessing the reliability of the 'sub-modelling' function in the ABAQUS FEA package, a series of simple analyses were conducted. Two global model geometries, with three different loading conditions, were tested, ie. simple tension, compression and cantilever bending. The first global model geometry consisted of three 20 noded hexahedral elements in series, as shown in Figure 5.4. The stress and strain distribution and the reaction forces at each node were recorded. For the simple tension and compression tests, one end was constrained and two surfaces were constrained such that the element could deform longitudinally and the cross section could deform due to Poisson's contraction. This process eased the visual comparison of the deformation analyses during the ABAQUS post processing. The encastre (built-in) boundary condition was used for the bending analyses with point loads being applied at the free end. Again, stress, strain and reaction forces at each node were recorded. The analyses were conducted using a homogeneous material (steel) and then repeated using an orthotropic material (913UD). The centre element was then sub-modelled using both 4 and 64 elements in order to assess the continuity along the boundary where the solution from the global model is interpolated.

The second global model geometry consisted of seven 20 noded hexahedral elements

in series which helped to identify the presence of any element scale effects. Again, the centre element was sub-modelled using both 100 and 400 elements. From these simple tests, it was found that the ‘sub-model’ technique yielded identical values to that of the basic analysis, i.e. a uniform stress field for the tension and compression analyses and a linear stress gradient for the bending analysis.

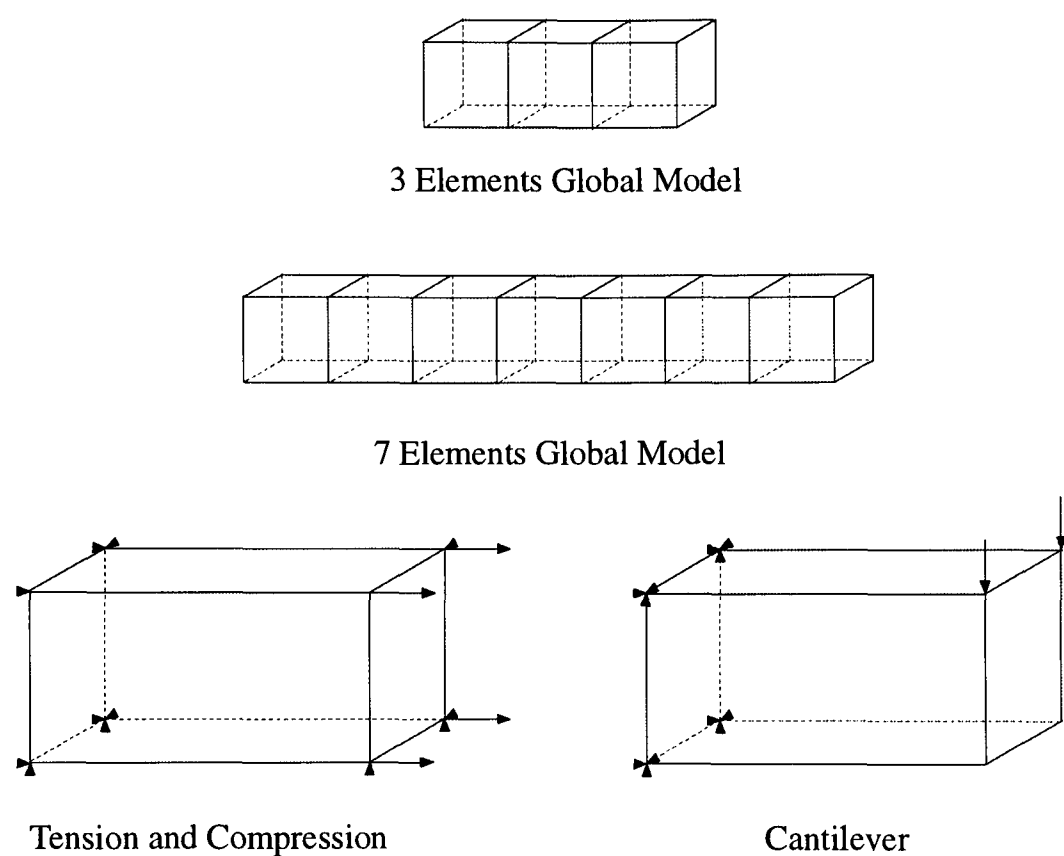


Figure 5.4 The Test Geometries and the Associated Boundary Conditions.

5.4.2 Results and Discussion

It can be seen that the total displacement was conserved from the global model to the first sub-model (Figure 5.3) then to the second sub-model (Figure 5.5). The average stress contour plots (Figures 5.6 - 5.10) in this section are presented with the abbreviated specimen and material type, i.e. PCB3, PCB4 and PCB6, denoting Plain Curved Beam specimens made from the 913UD, 914NCF and 916W materials respectively. The failure stresses for each

specimen are tabulated in Tables 5.2 - 5.4. A comparison of the recorded interlaminar tensile strength with that presented in the available literature is presented in Section 7.3.2 in conjunction with results obtained from the NRP and ORP tests.

Test No.	Interlaminar Tension	In-Plane Tension	In-Plane Compression
1	34.4	584.1	487.2
2	35.8	613.0	512.4
3	35.8	613.0	512.4
Average	35.3	603.3	503.7

Table 5.2 PCB3, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	In-Plane Tension	In-Plane Compression
1	22.6	429.3	378.5
2	22.6	429.3	378.5
3	22.3	423.1	373.5
Average	22.5	427.2	376.8

Table 5.3 PCB4, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	In-Plane Tension	In-Plane Compression
1	24.6	584.0	515.4
2	24.2	572.3	504.5
3	23.3	540.3	476.8
Average	24.0	565.5	498.9

Table 5.4 PCB6, The Failure Stresses (MPa) Estimated from the FEA.

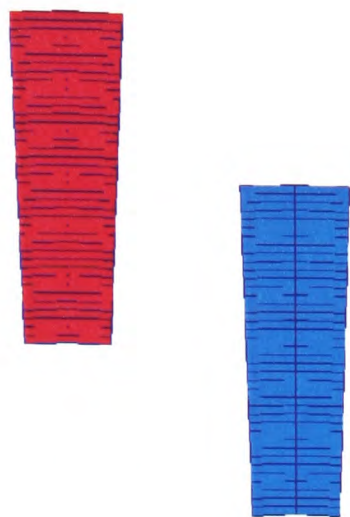


Figure 5.5 Displacement Plot of Sub-Model.

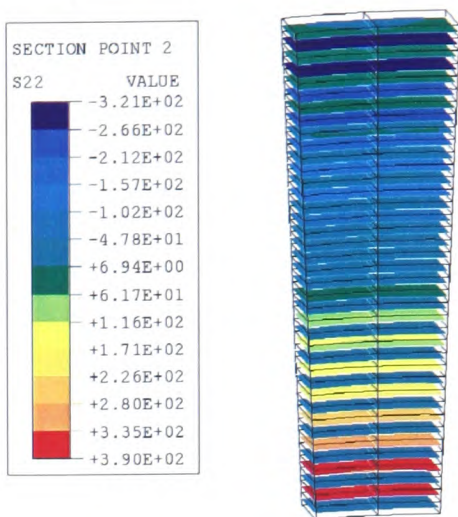


Figure 5.8 PCB3, In-Plane Stresses.

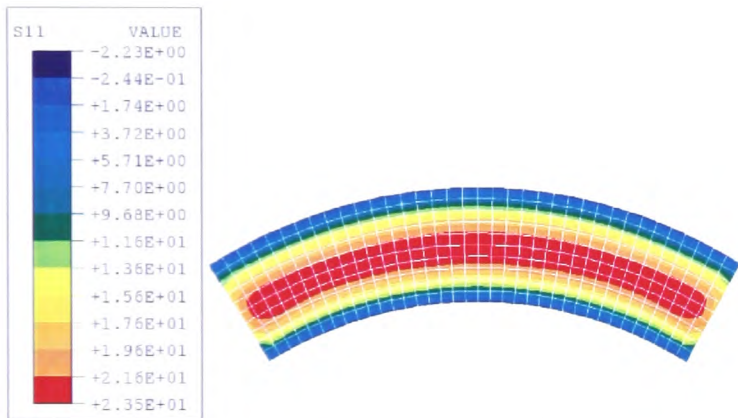


Figure 5.6 PCB4, Interlaminar Tension.

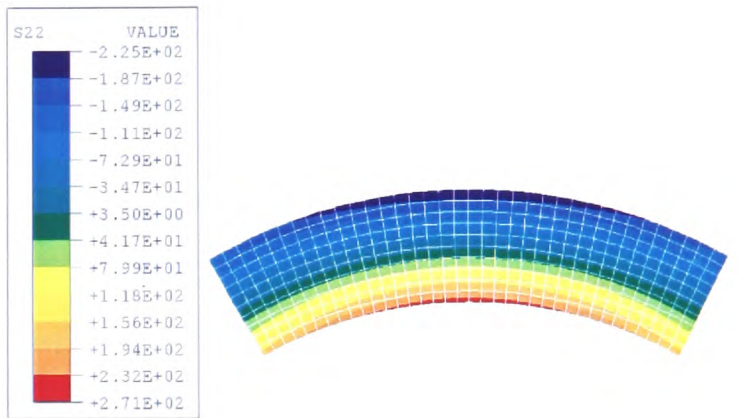


Figure 5.9 PCB4, In-Plane Stresses.

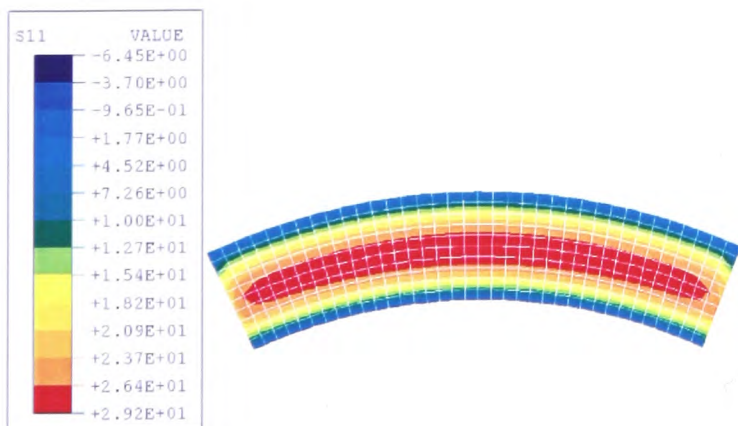


Figure 5.7 PCB6, Interlaminar Tension.

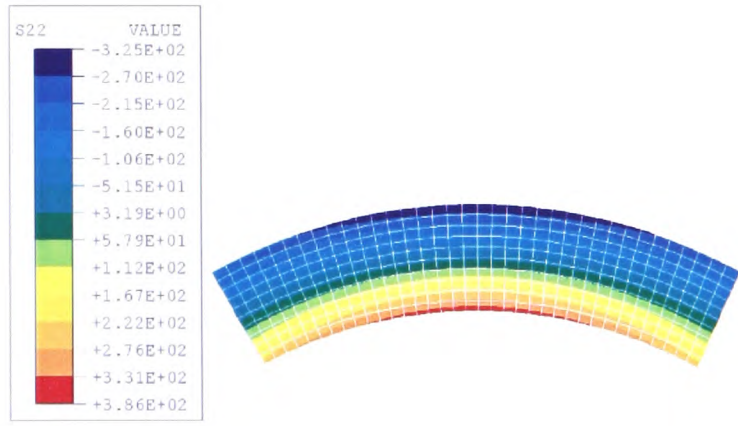


Figure 5.10 PCB6, In-Plane Stresses.

The second sub-model of PCB3 (Figure 5.8) estimated accurately the delamination location of the specimen as previously presented in Table 4.13. It may clearly be seen that failure was due to local matrix failure caused by in-plane compression. This local matrix failure initiated a crack at the interface between a 0° ply and a 90° ply which subsequently propagated along this 90 ply (36th or 38th ply).

Figures 5.6 and 5.9 show the interlaminar stress distribution for the PCB4. The location of the peak interlaminar tensile stress was estimated to be at approximately one third of the thickness from the inner radius. The delamination position, observed from experiment (Table 4.15), agrees well with the FEA. The simulated material behaviour of the quasi-isotropic lay-up configuration proved very similar to that of an isotropic material. This allowed the application of the ‘homogenised orthotropic material’ assumption to this quasi-isotropic lay-up configuration.

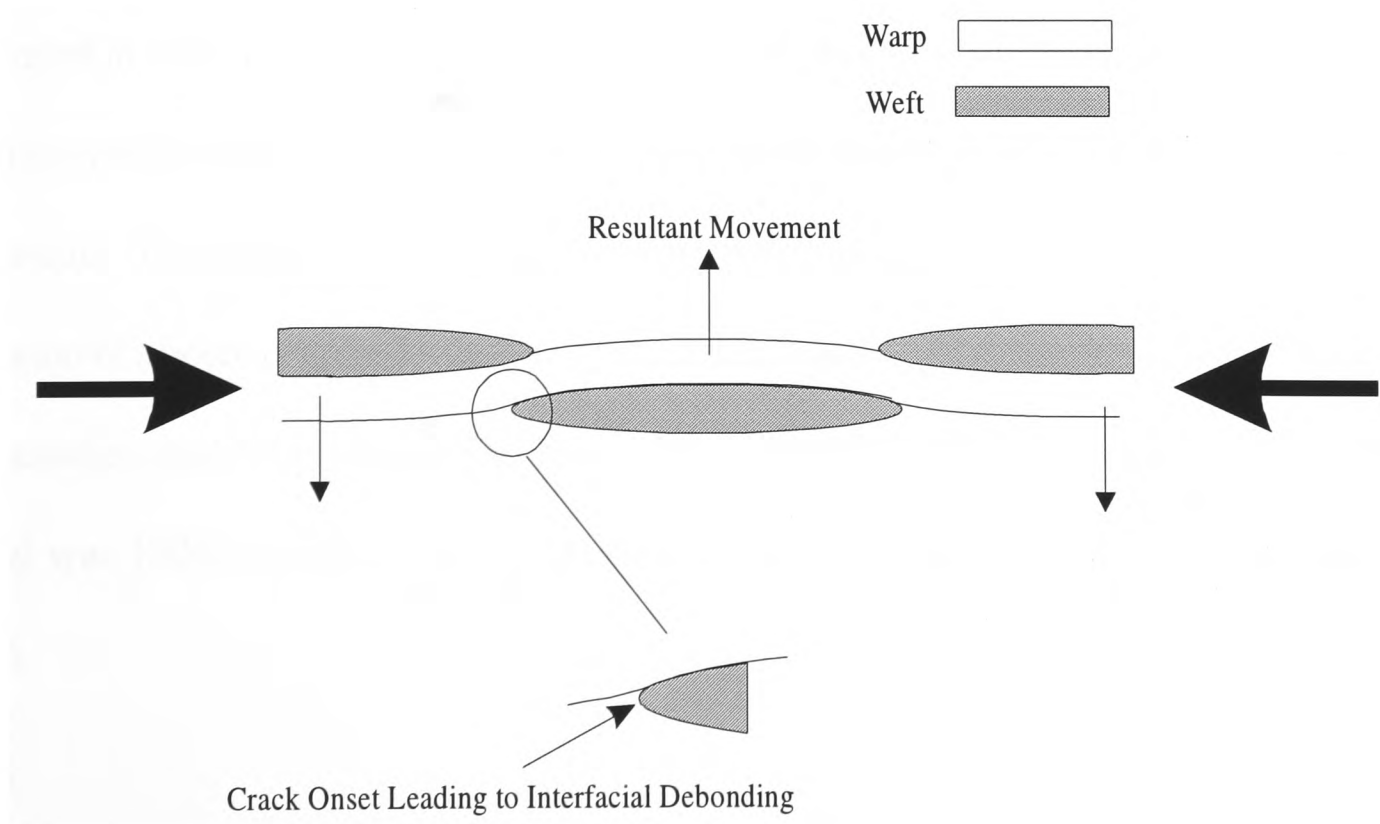


Figure 5.11 A Woven Lamina Under Compressive In-Plane Stresses.

As discussed in Section 4.5.3, PCB6 did not exhibit delamination failure but instead

failed due to hinging at the apex. The in-plane stress distribution is presented in Figure 5.10 and shows the large magnitude of the in-plane tensile and compressive stresses. Since the in-plane tensile strength is generally higher than the in-plane compressive strength, it is most likely that the failure initiated on the in-plane compressive side. This in-plane compressive stress would have increased the amplitude of the warp as it threads under over the weft (Figure 5.11). This behaviour is not to be confused with buckling which happens suddenly at some critical load. The increased amplitude of the warp would cause local matrix failures at the concave side of the fibre bundles and subsequently lead to the ‘hinge’ like failure observed at the notch apex.

5.5 The Notched Curved Beam Specimen (NCB)

Similarly to the Plain Curved Beam model, the ‘sub-modelling’ technique was employed in order to obtain a more accurate and detailed solution. Both the global model and the sub-model elastic material properties were representative of an orthotropic homogeneous continuum. The main difference between this model and the Plain Curved Beam model is the inclusion of a notch (Figure 5.12). The global model used 2232 elements with 3 element across the thickness and 4 elements across the width. The total number of elements used in the sub-model was 1900 elements with 10 elements across the thickness and 5 elements across the width.

5.5.1 Results and Discussion

Again, the number after the abbreviation ‘NCB’ denotes the type of the material used to construct the specimen. The deformations of the NCB specimens at failure (Figure 5.13) were smaller than that of the PCB specimens (Figure 5.2) due to the stress concentration at the

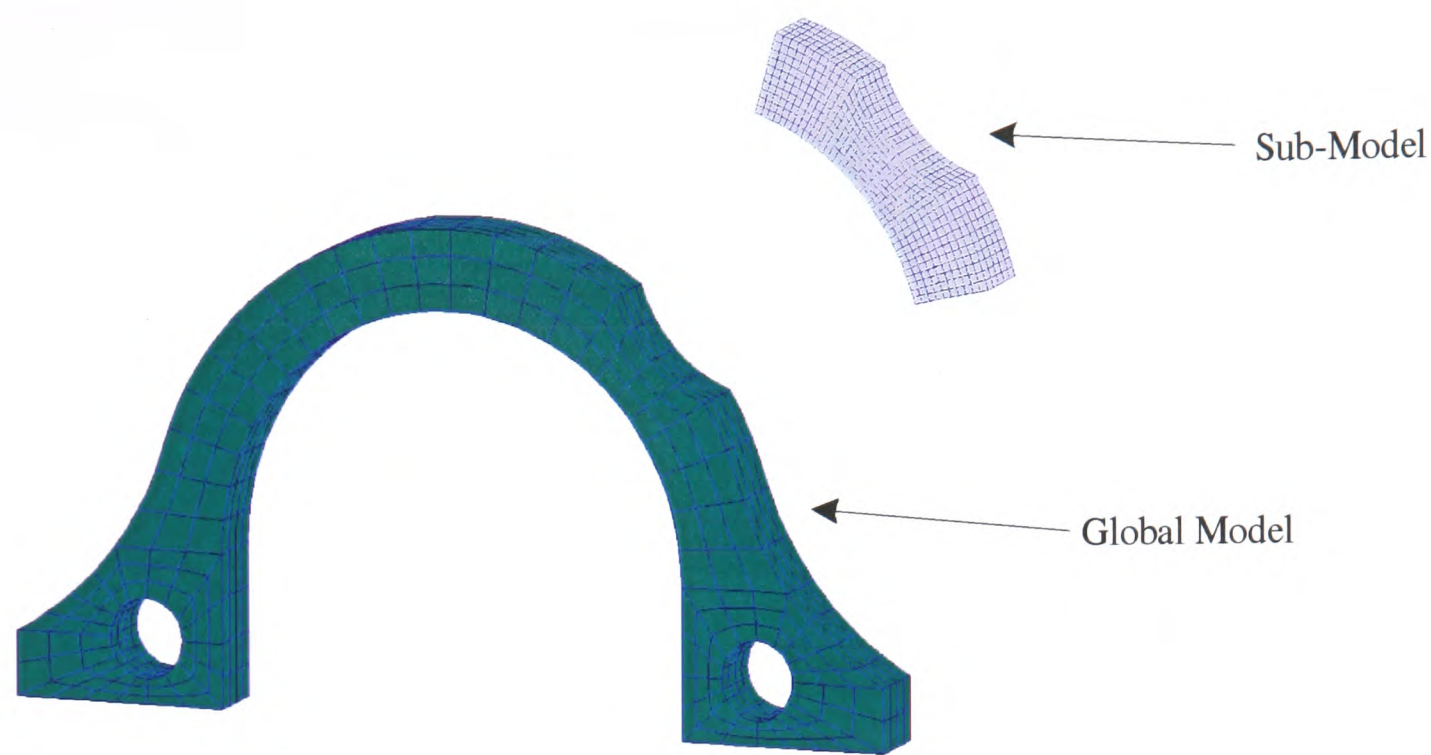


Figure 5.12 NCB, Global and Sub-Model Meshes.

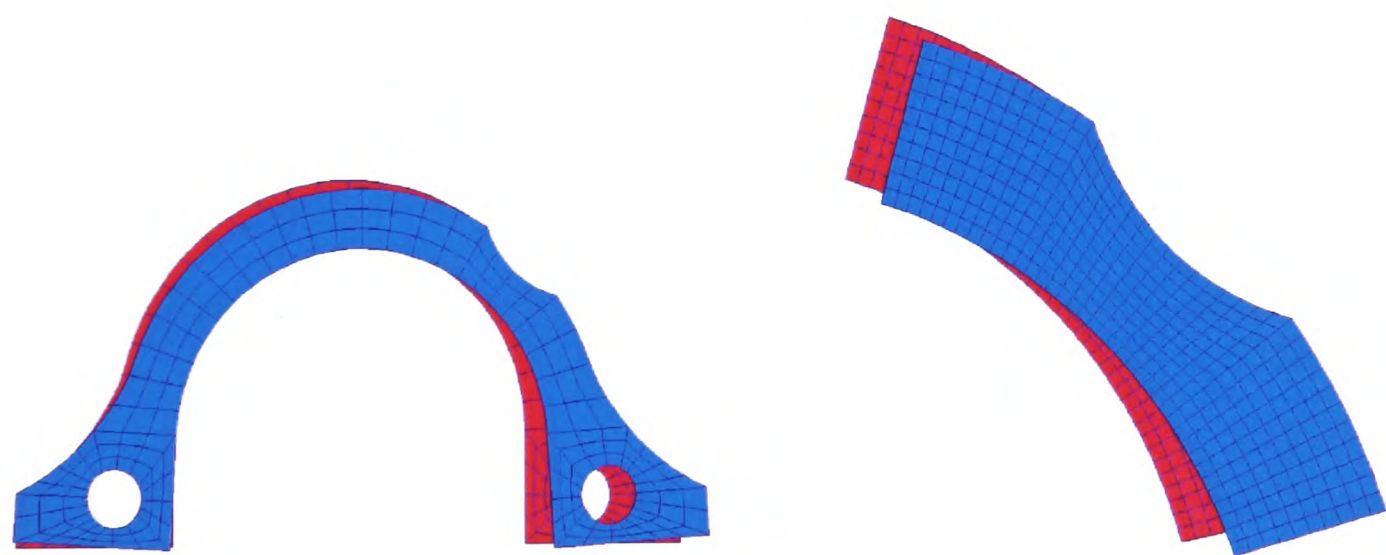


Figure 5.13 NCB, Displacement Plot of the Global and Sub-Model.

notch. The average stress distributions are presented in Figures 5.14 - 5.22 and the failure stresses for each material are tabulated in Tables 5.5 - 5.7.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Compression
1	14.4	35.5	130.7
2	15.8	38.3	143.7
3	14.4	35.5	130.7
Average	14.8	36.4	135.0

Table 5.5 NCB3, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Compression
1	20.9	46.9	159.3
2	20.9	46.9	159.3
3	20.3	45.6	155.1
Average	20.7	46.4	157.9

Table 5.6 NCB4, The Failure Stresses (MPa) Estimated from the FEA.

The induced stress distribution around the notch is shown to be invariant with respect to the elastic material properties. The in-plane compressive stress caused local fibre buckling and matrix failure at the root of the notch as explained in Section 4.5.1. Delamination initiated at the root of the notch and then propagated between this and the adjacent ply. In summary, the mechanism of failure began with fibre bulking followed by interlaminar shear (Mode II) and, finally, separation of the laminae due to interlaminar tension (Mode I) leading to catastrophic failure.

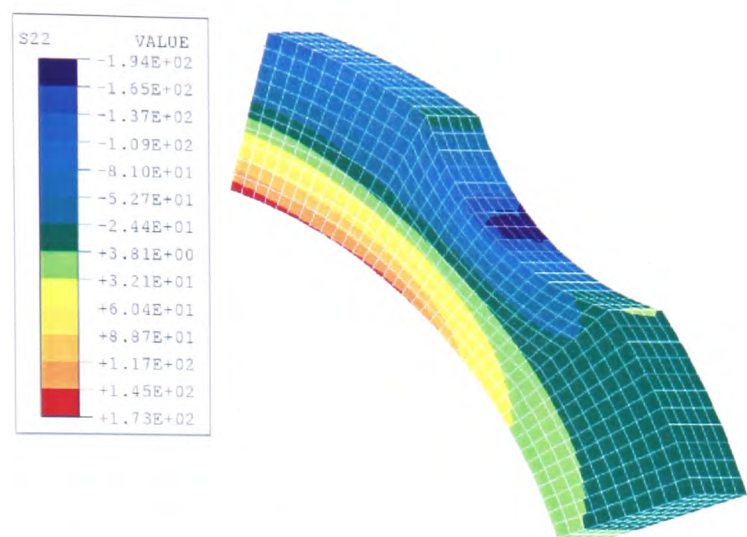


Figure 5.14 NCB3, In-Plane Compression.

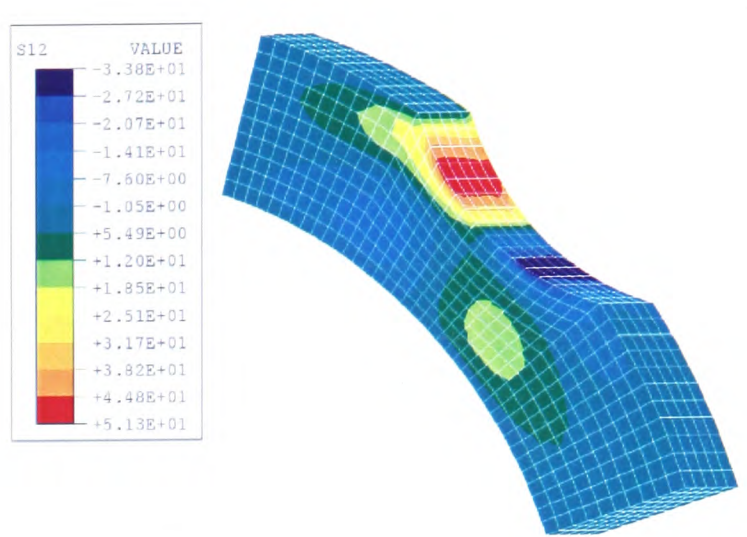


Figure 5.17 NCB3, Interlaminar Shear.

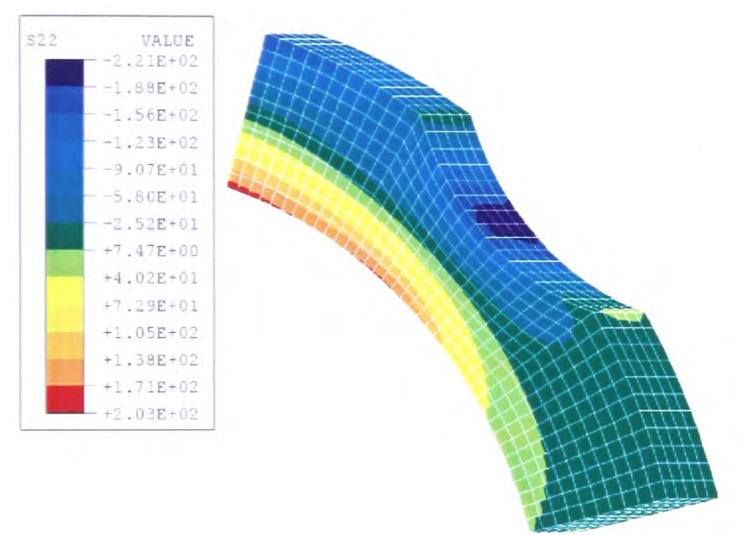


Figure 5.15 NCB4, In-Plane Compression.

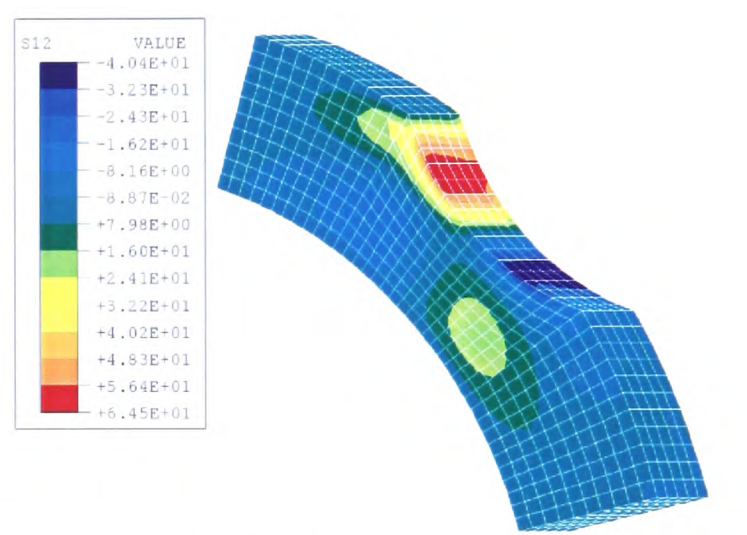


Figure 5.18 NCB4, Interlaminar Shear.

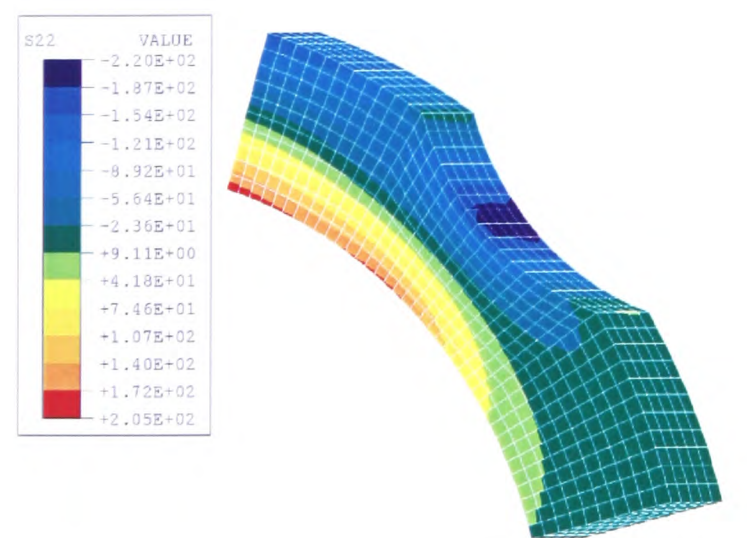


Figure 5.16 NCB6, In-Plane Compression.

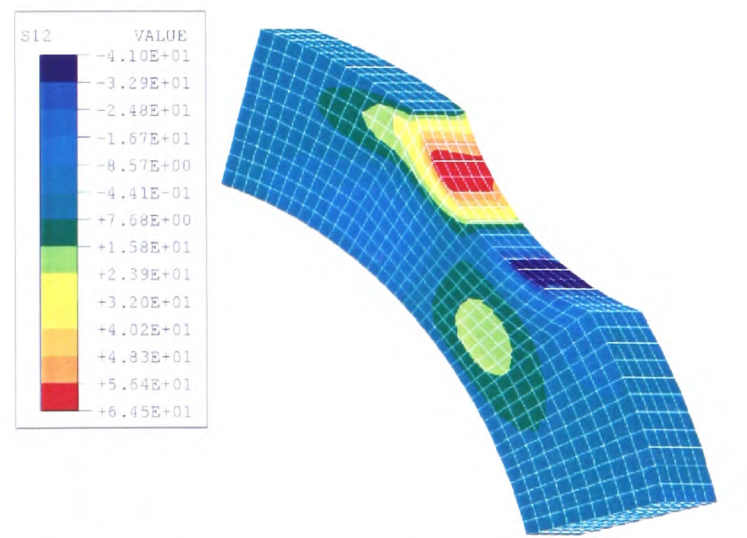


Figure 5.19 NCB6, Interlaminar Shear.

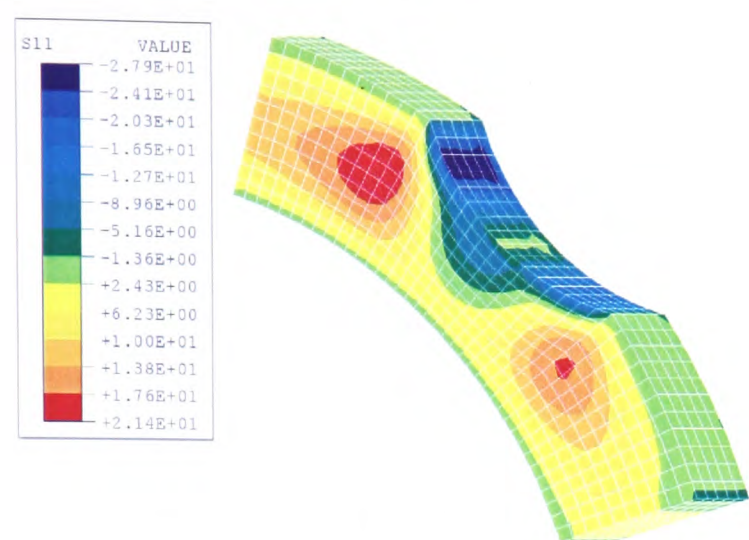


Figure 5.20 NCB3, Interlaminar Tension.

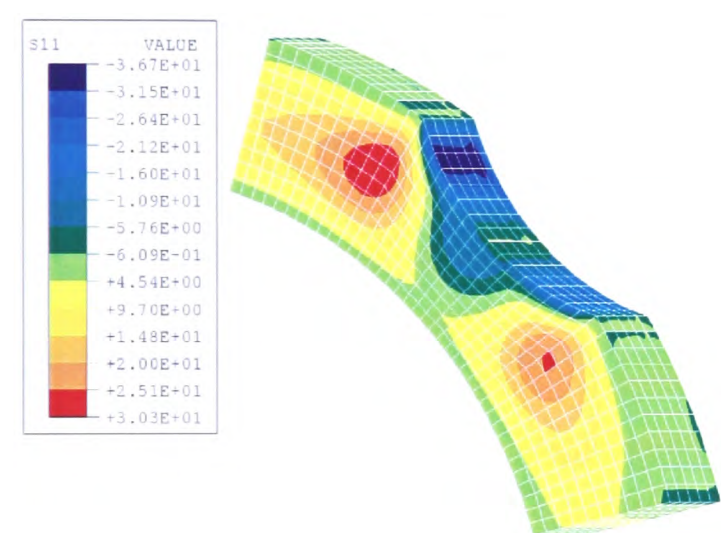


Figure 5.21 NCB4, Interlaminar Tension.

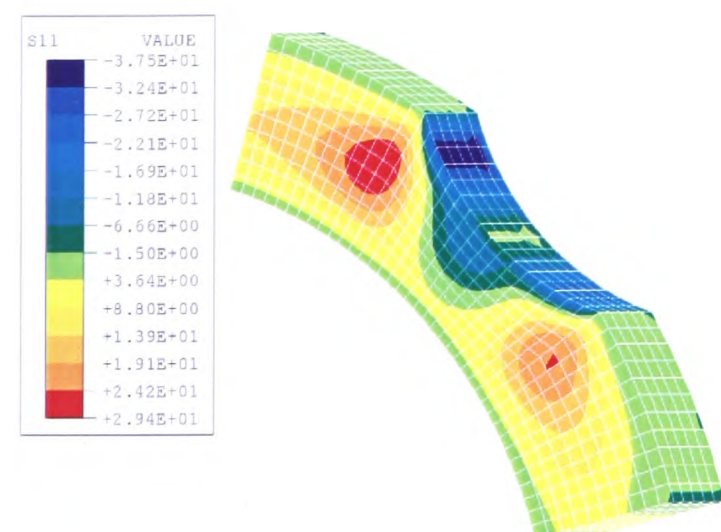


Figure 5.22 NCB6, Interlaminar Tension.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Compression
1	20.7	47.6	161 .8
2	20.7	47.6	161.8
3	19.7	45.2	153.4
Average	20.3	46.8	159.0

Table 5.7 NCB6, The Failure Stresses (MPa) Estimated from the FEA.

5.6 The Centrally Notched Beam Specimen (CNB)

Three mesh configurations were constructed for this specimen geometry and are shown in Figures 5.23 -5.25. The total number of elements for the test thicknesses of 2, 3 and 4 mm were 496, 512 and 368 respectively. The specimen is symmetrical about the mid planes of both the width and the length and thus it was only necessary to model one quarter of the specimen. Finally, it was necessary to assign a local coordinate frame to each element across the length in order to conserve the orthotropic material coordinates of these elements relative to the global coordinate frame irrespective of the induced deformations.

5.6.1 Results and Discussion

The deformations for each specimen of thickness, t , are plotted in Figures 5.26 - 5.28. The average stress distributions for each material and each test thickness are shown in Figures 5.29 - 5.44. The failure stresses for each specimen are also tabulated in Tables 5.8 - 5.10. It should be noted that the deformation plot for the 4 mm specimens is magnified by a factor of two in order to clarify the deformed shape. The deformation of the 4 mm thick specimen is close to that of the uniform beam specimen due to the small notch depth in relation to the thickness. Because of the additional deformation of the specimen outside the region of the

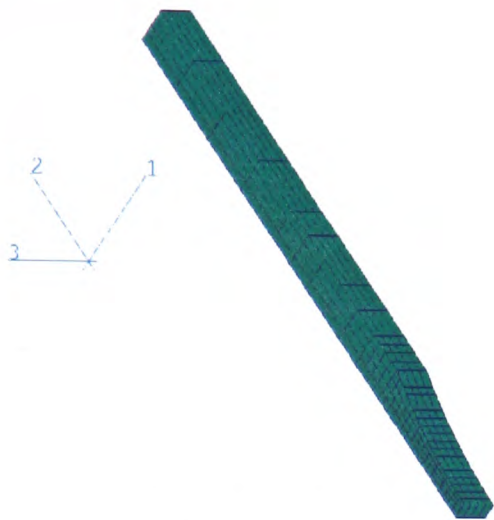


Figure 5.23 CNB, $t=2$, Mesh Configuration.

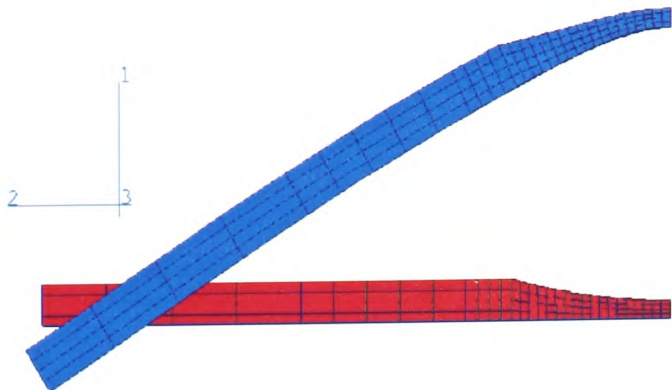


Figure 5.26 CNB, $t=2$ Displacement Plot.

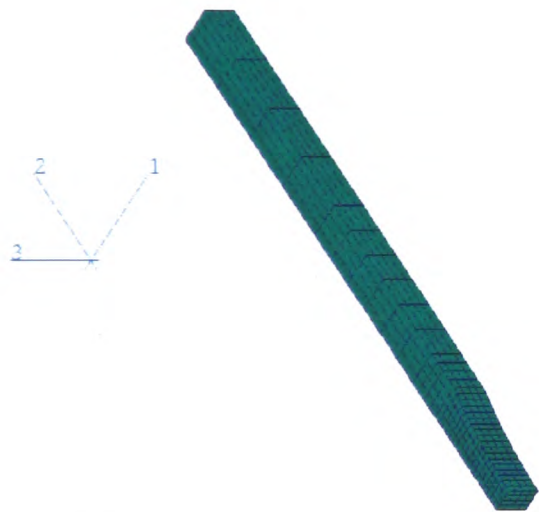


Figure 5.24 CNB, $t=3$, Mesh Configuration.

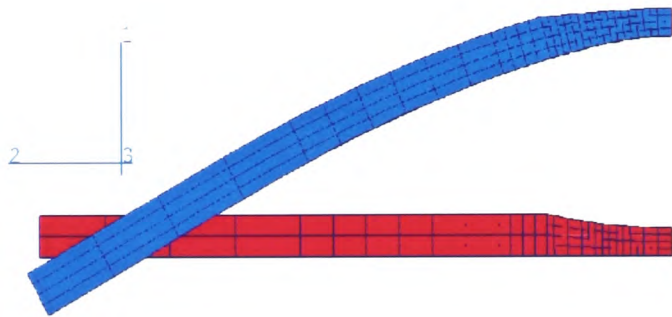


Figure 5.27 CNB, $t=3$, Displacement Plot.

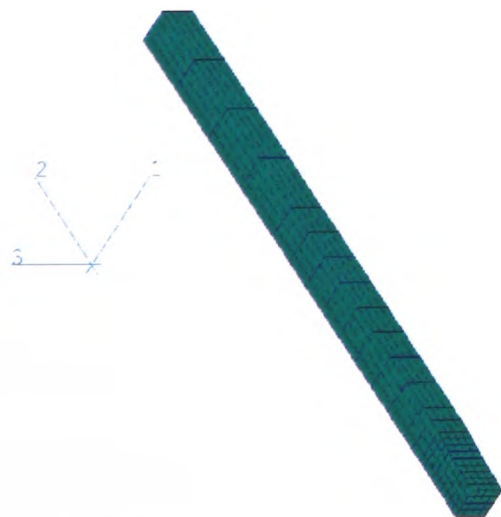


Figure 5.25 CNB, $t=4$, Mesh Configuration.

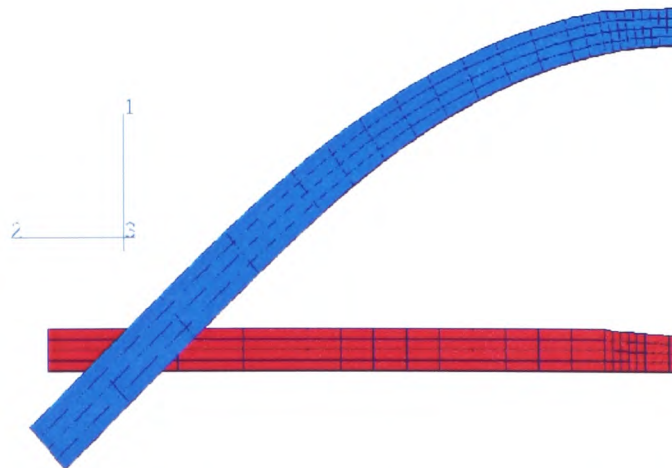


Figure 5.28 CNB, $t=4$, Displacement Plot.

notch, there is an increase in the interlaminar tensile stress (Figures 5.32 to 5.34). However, the magnitude of this interlaminar tensile stress was not large enough to cause delamination.

Test No.	t	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	2	5.5	32.7	642.5
2	2	5.8	34.6	611.8
3	2	4.9	28.9	587.6
Average		5.4	32.0	613.9
1	3	6.8	39.6	536.7
2	3	7.1	41.6	526.4
3	3	6.5	37.7	547.4
Average		6.8	39.6	536.8
1	4	4.8	34.8	491.3
2	4	4.6	33.2	481.8
3	4	4.7	34.2	501.1
Average		4.7	34.0	491.4

Table 5.8 CNB3, The Failure Stresses (MPa) Estimated from the FEA.

As discussed in Section 4.4.3, delamination was observed near the root of the notch. The interlaminar shear and interlaminar tensile stresses and the longitudinal in-plane stress were plotted in Figures 5.45 - 5.52. These stresses are normalised with respect to their maximum values tabulated in Section 7.4.1. Figures 5.45 - 5.52 show the large longitudinal in-plane stress at the centre of the specimen which drove the failure. In summary, the mechanism of failure began with matrix cracking of a 90° ply or fibre distortion of a 0° ply due to the longitudinal in-plane stress.

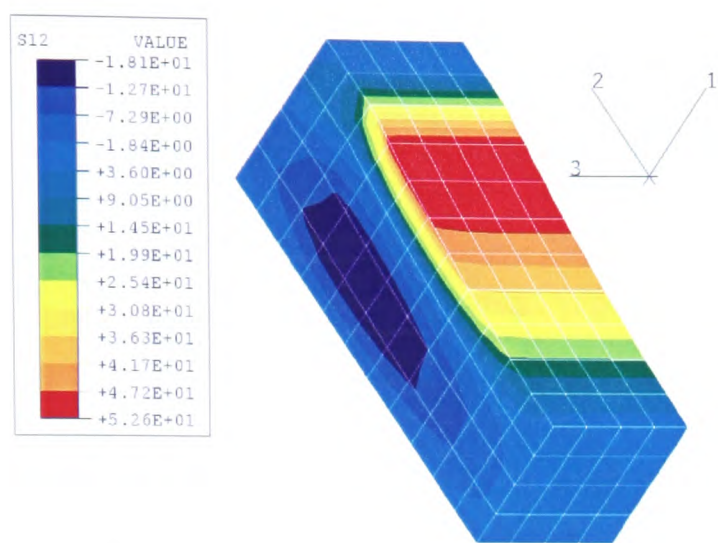


Figure 5.29 CNB3, t=4, Interlaminar Shear.

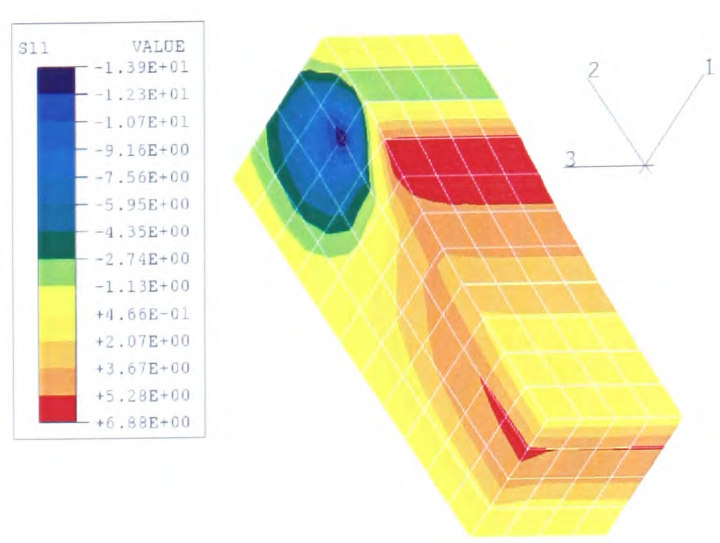


Figure 5.32 CNB3, t=4, Interlaminar Tension.

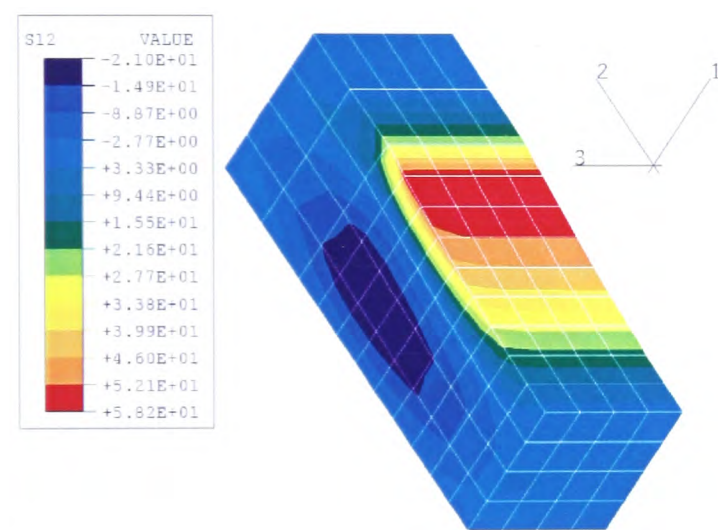


Figure 5.30 CNB4, t=4, Interlaminar Shear.

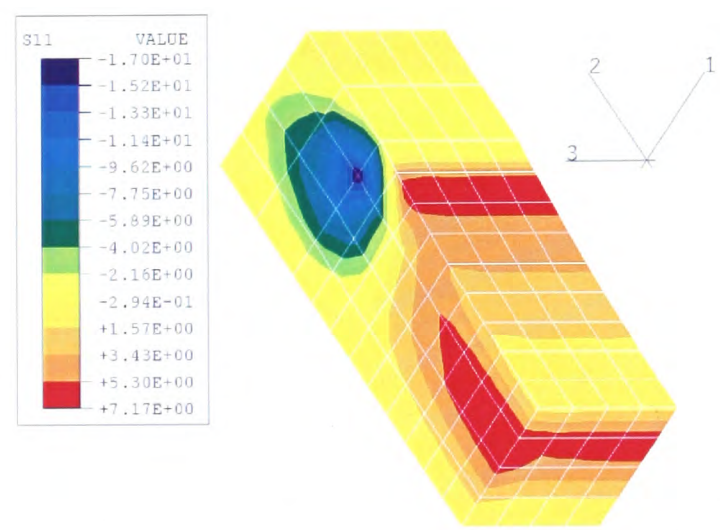


Figure 5.33 CNB4, t=4, Interlaminar Tension.

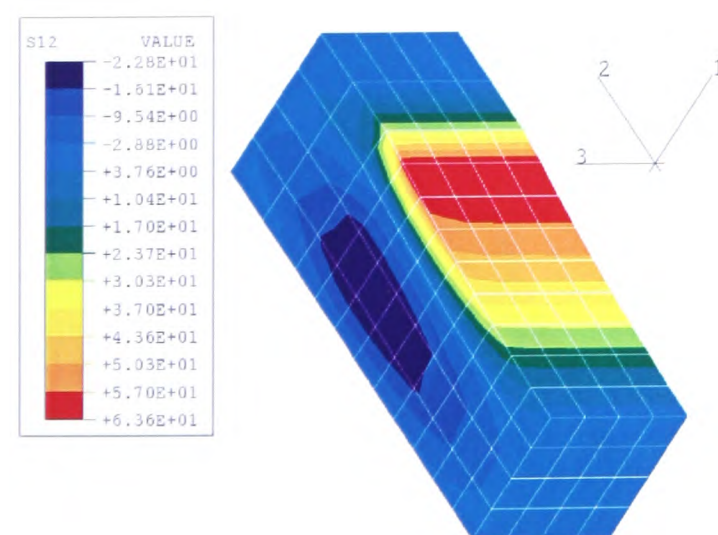


Figure 5.31 CNB6, t=4, Interlaminar Shear.

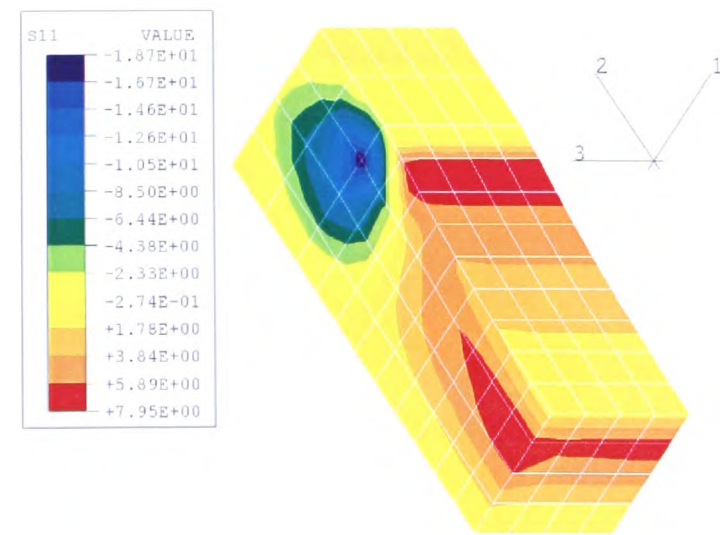


Figure 5.34 CNB6, t=4, Interlaminar Tension.

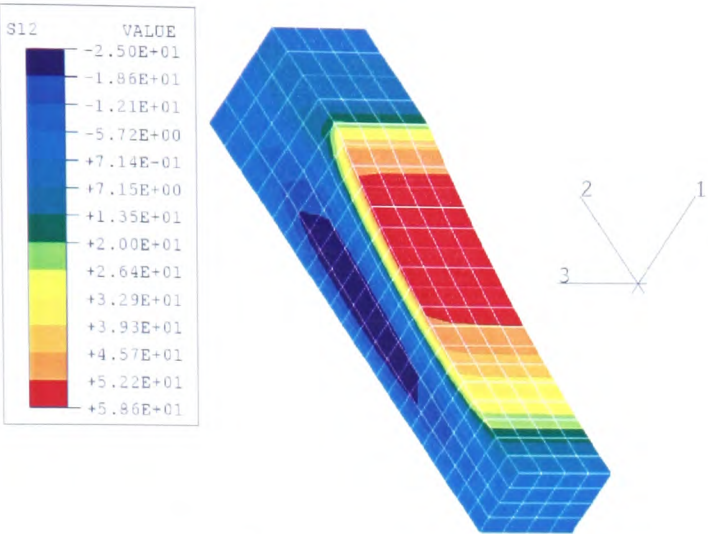


Figure 5.35 CNB3, t=3, Interlaminar Shear.

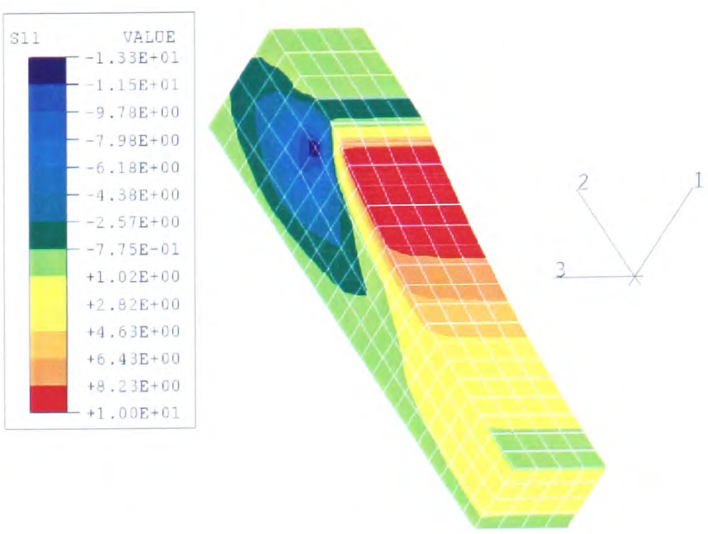


Figure 5.38 CNB3, t=3, Interlaminar Tension.

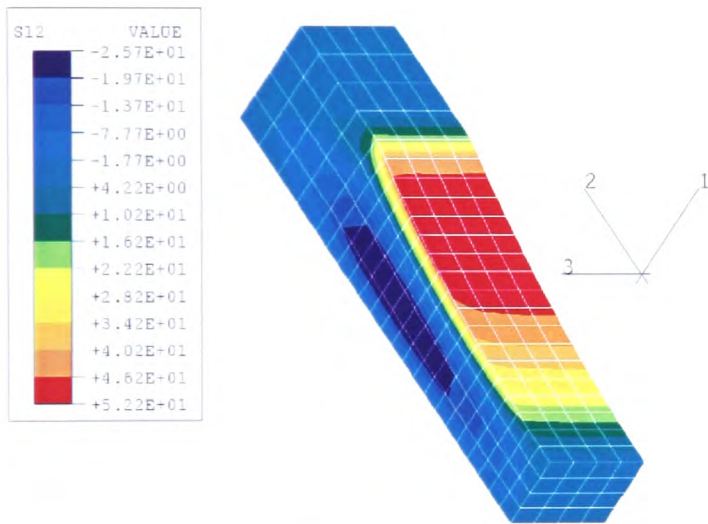


Figure 5.36 CNB4, t=3, Interlaminar Shear.

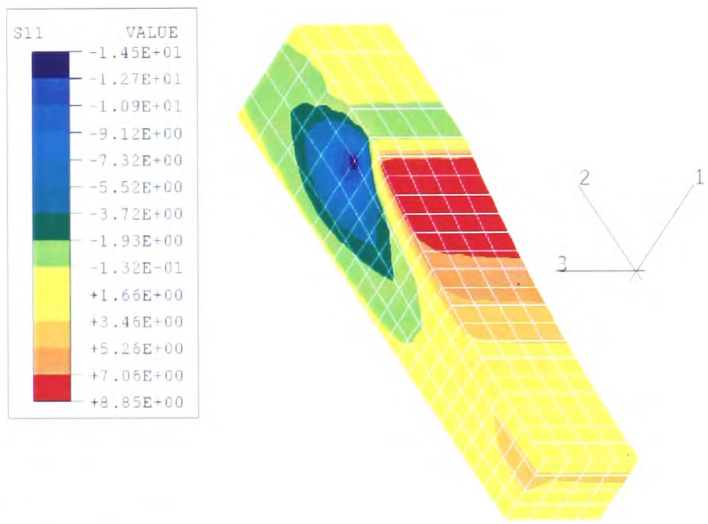


Figure 5.39 CNB4, t=3, Interlaminar Tension.

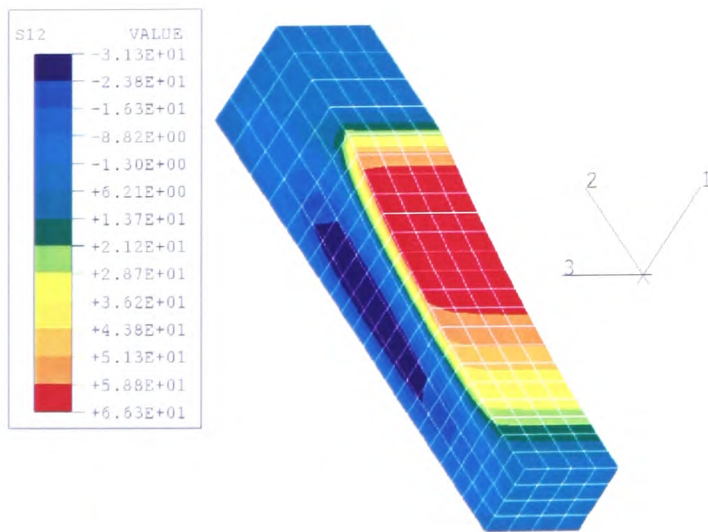


Figure 5.37 CNB6, t=3, Interlaminar Shear.

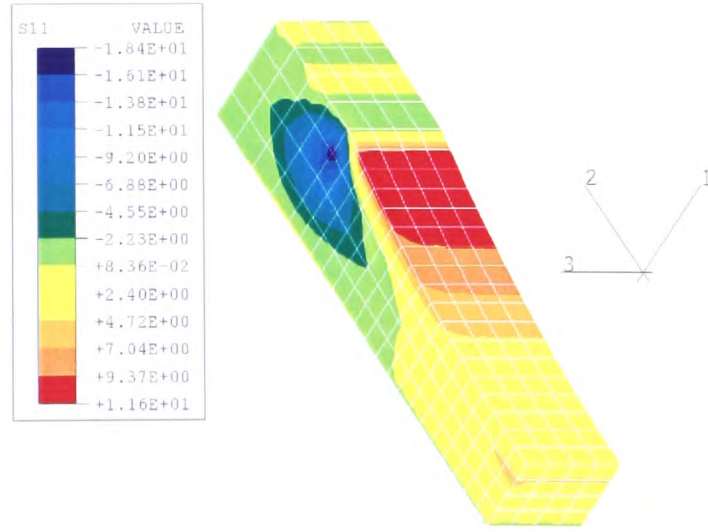


Figure 5.40 CNB6, t=3, Interlaminar Tension.

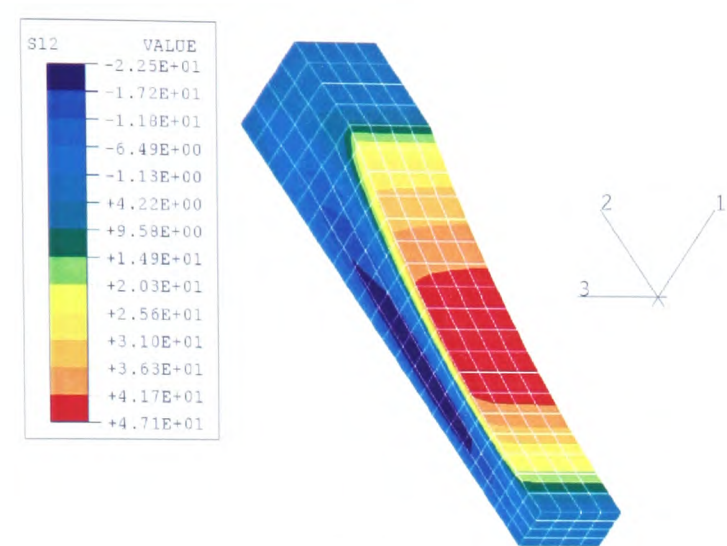


Figure 5.41 CNB3, t=2, Interlaminar Shear.

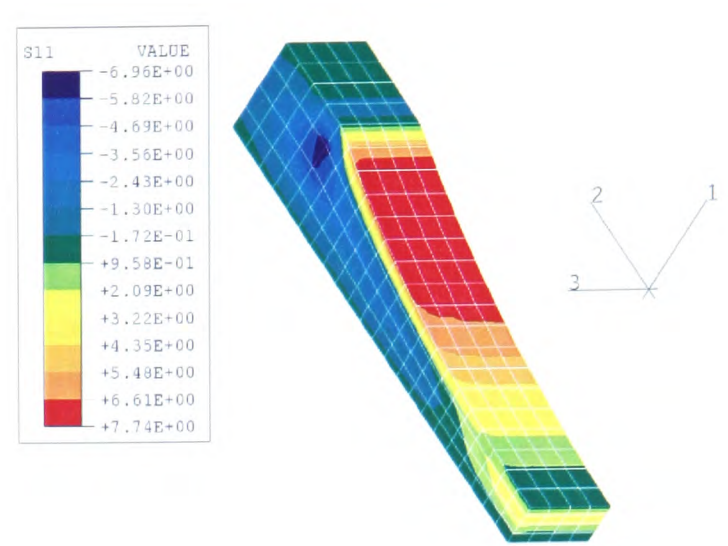


Figure 5.43 CNB3, t=2, Interlaminar Tension.

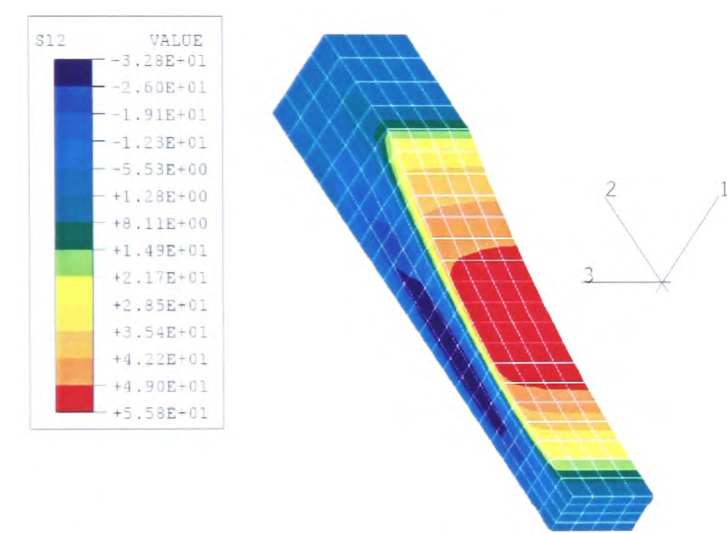


Figure 5.42 CNB4, t=2, Interlaminar Shear.

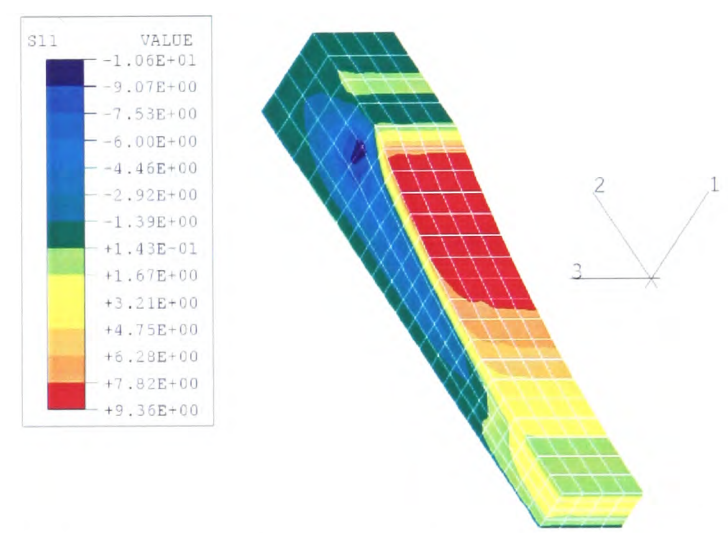


Figure 5.44 CNB4, t=2, Interlaminar Tension.

Test No.	t	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	2	-	-	-
2	2	6.5	38.0	544.8
3	2	6.3	37.1	531.6
Average		6.4	37.5	538.2
1	3	6.0	36.0	439.3
2	3	6.0	36.0	439.3
3	3	5.7	35.5	421.0
Average		5.9	35.8	433.2
1	4	4.9	37.6	545.1
2	4	5.0	38.1	552.0
3	4	4.9	37.4	541.8
Average		4.9	37.7	546.3

Table 5.9 CNB4, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	t	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	3	8.7	52.0	492.5
2	3	8.3	49.6	517.3
3	3	8.0	47.6	468.2
Average		8.3	49.7	492.6
1	4	5.1	38.1	460.0
2	4	5.0	37.3	439.5
3	4	5.2	38.8	453.2
Average		5.1	38.0	450.9

Table 5.10 CNB6, The Failure Stresses (MPa) Estimated from the FEA.

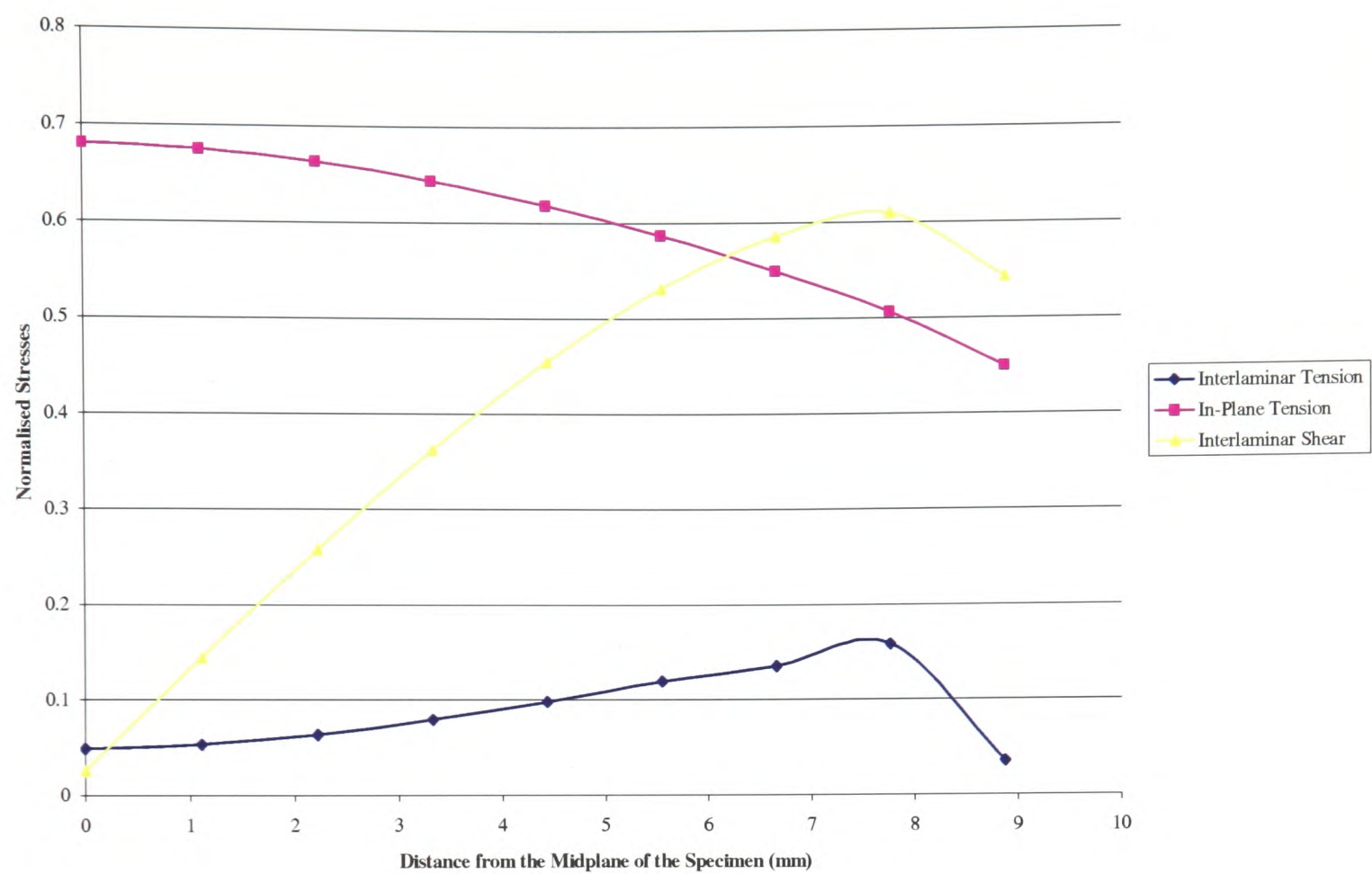


Figure 5.45 CNB3, t=4, Stress Interaction along the Notch.

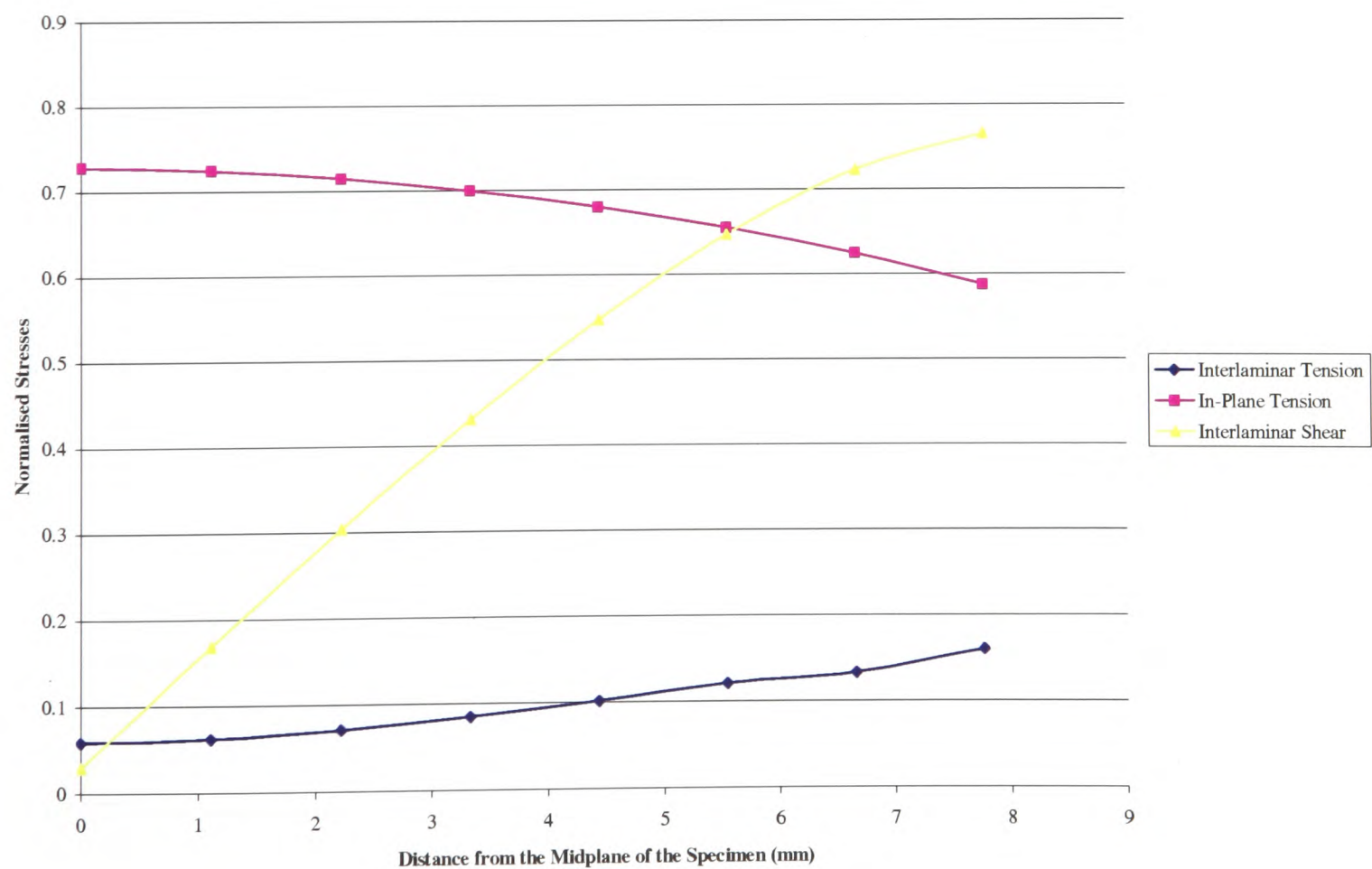


Figure 5.46 CNB4, t=4, Stress Interaction along the Notch.

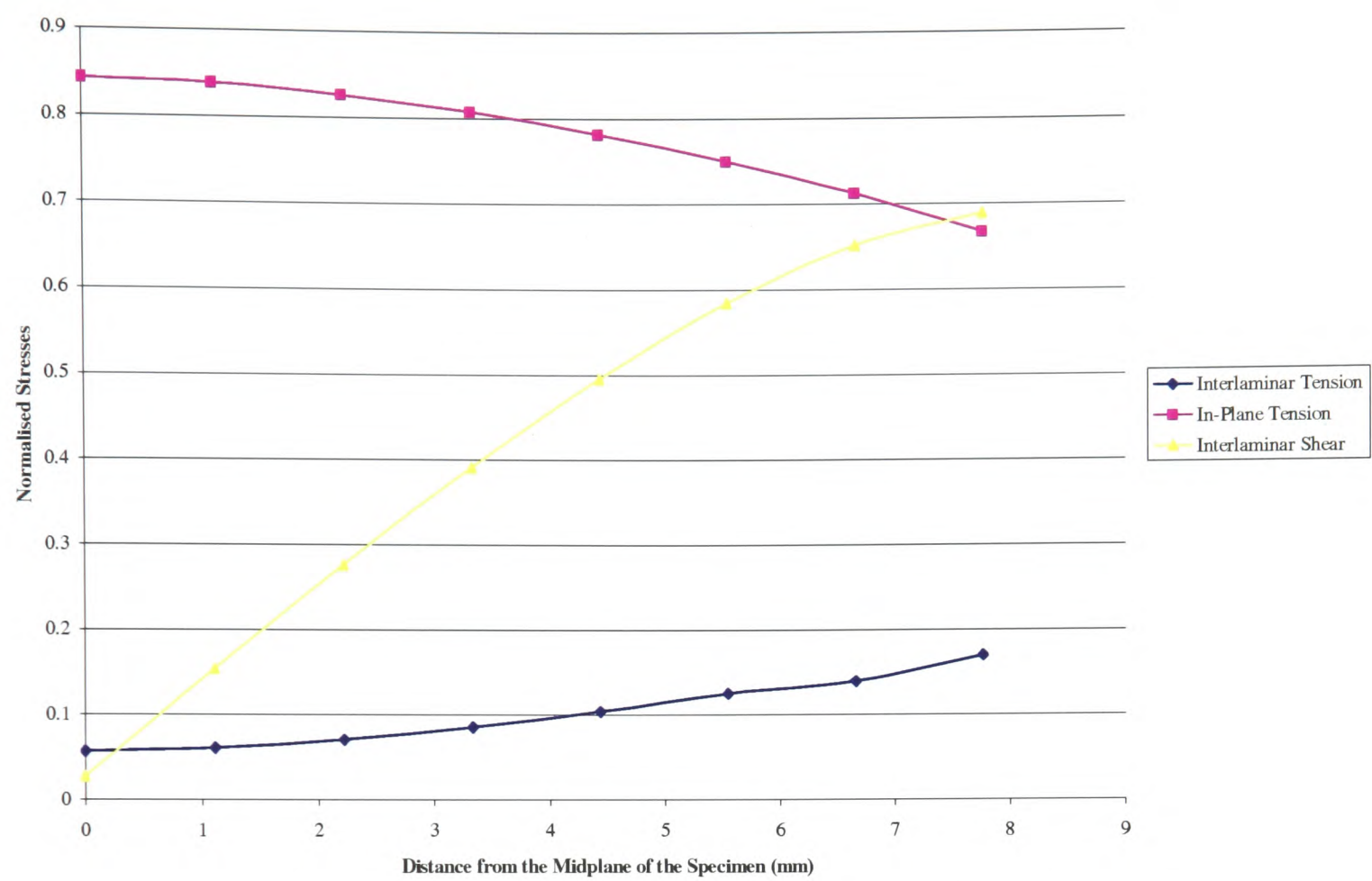


Figure 5.47 CNB6, t=4, Stress Interaction along the Notch.

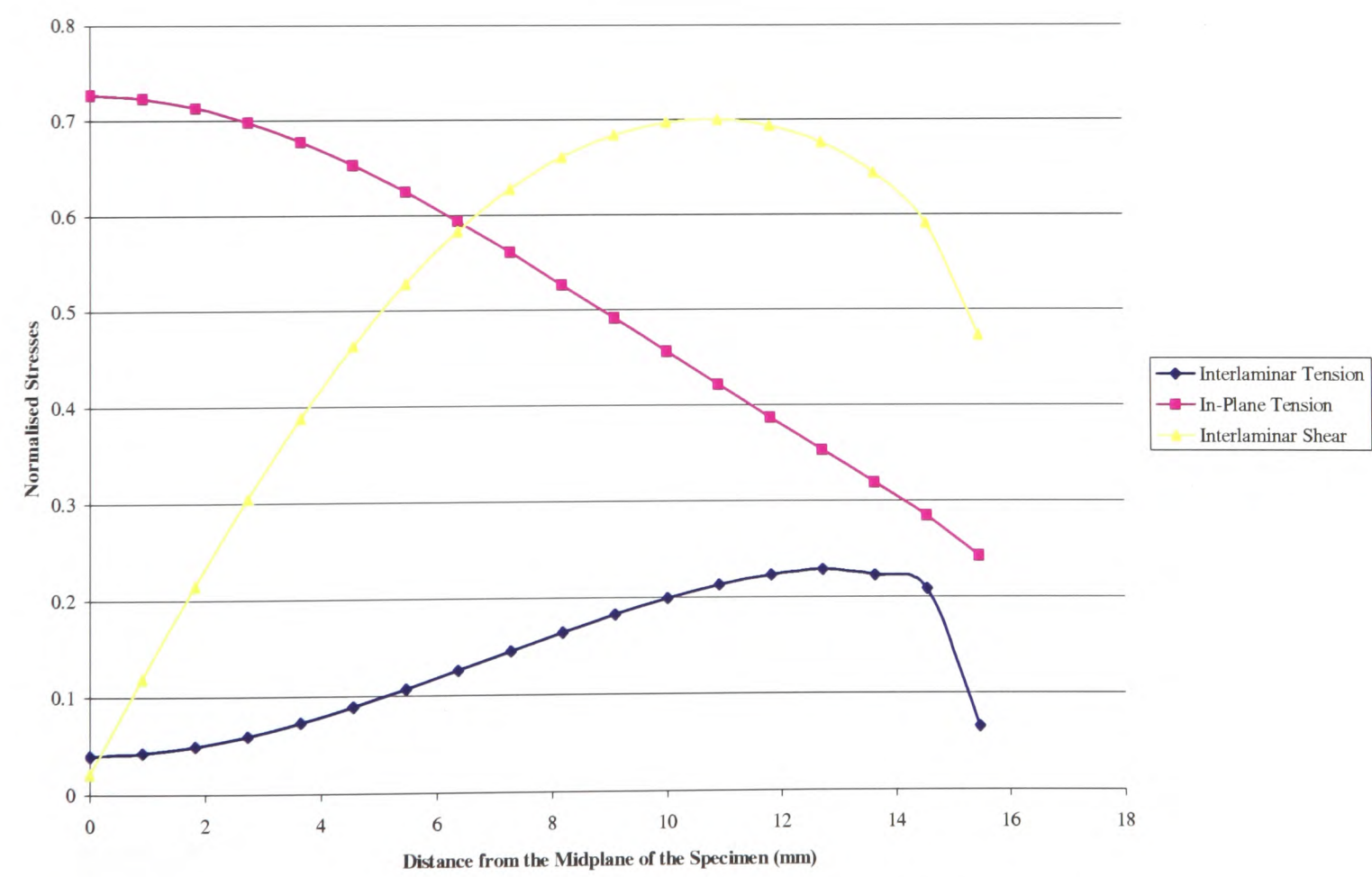


Figure 5.48 CNB3, t=3, Stress Interaction along the Notch.

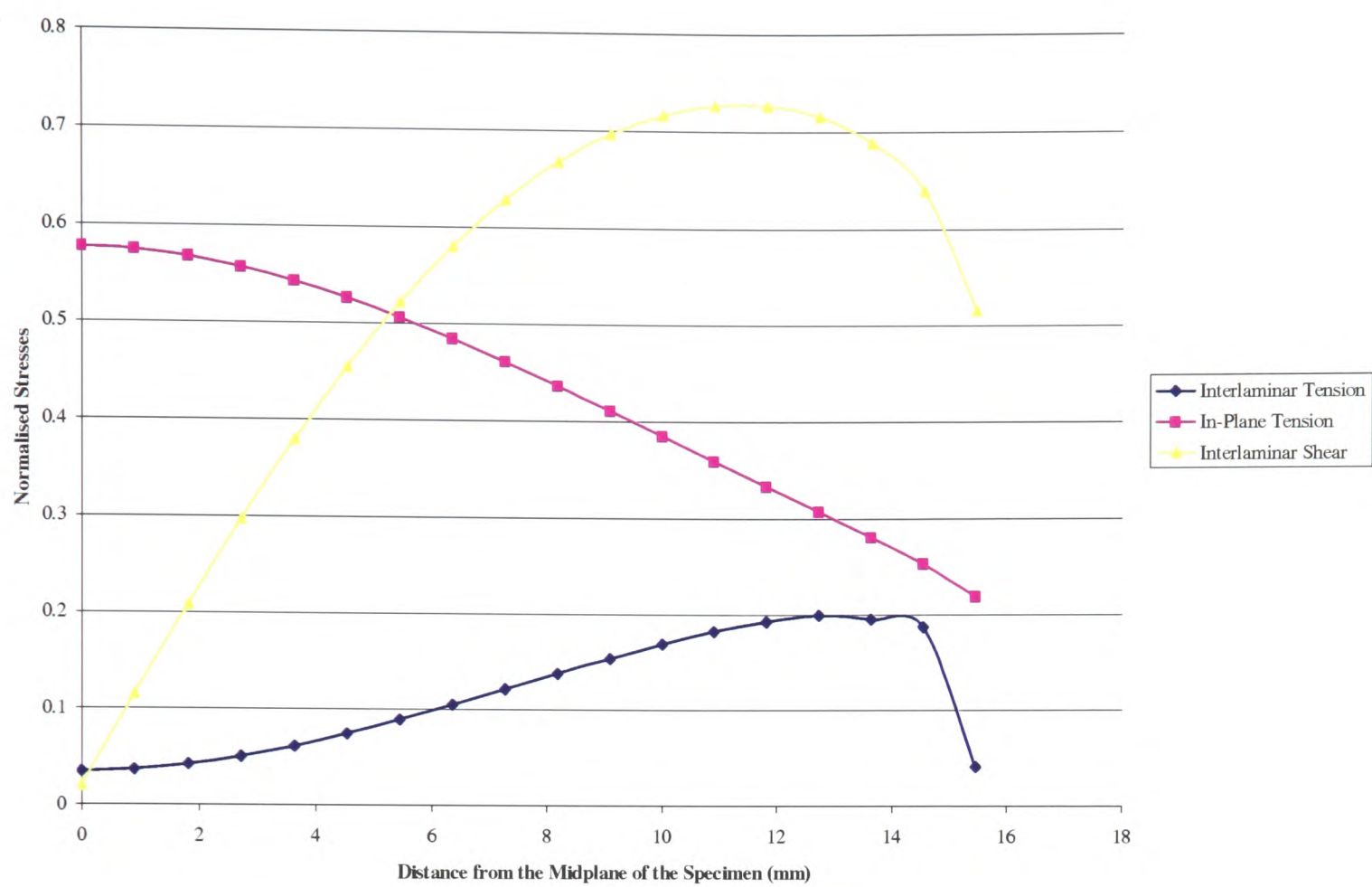


Figure 5.49 CNB4, $t=3$, Stress Interaction along the Notch.

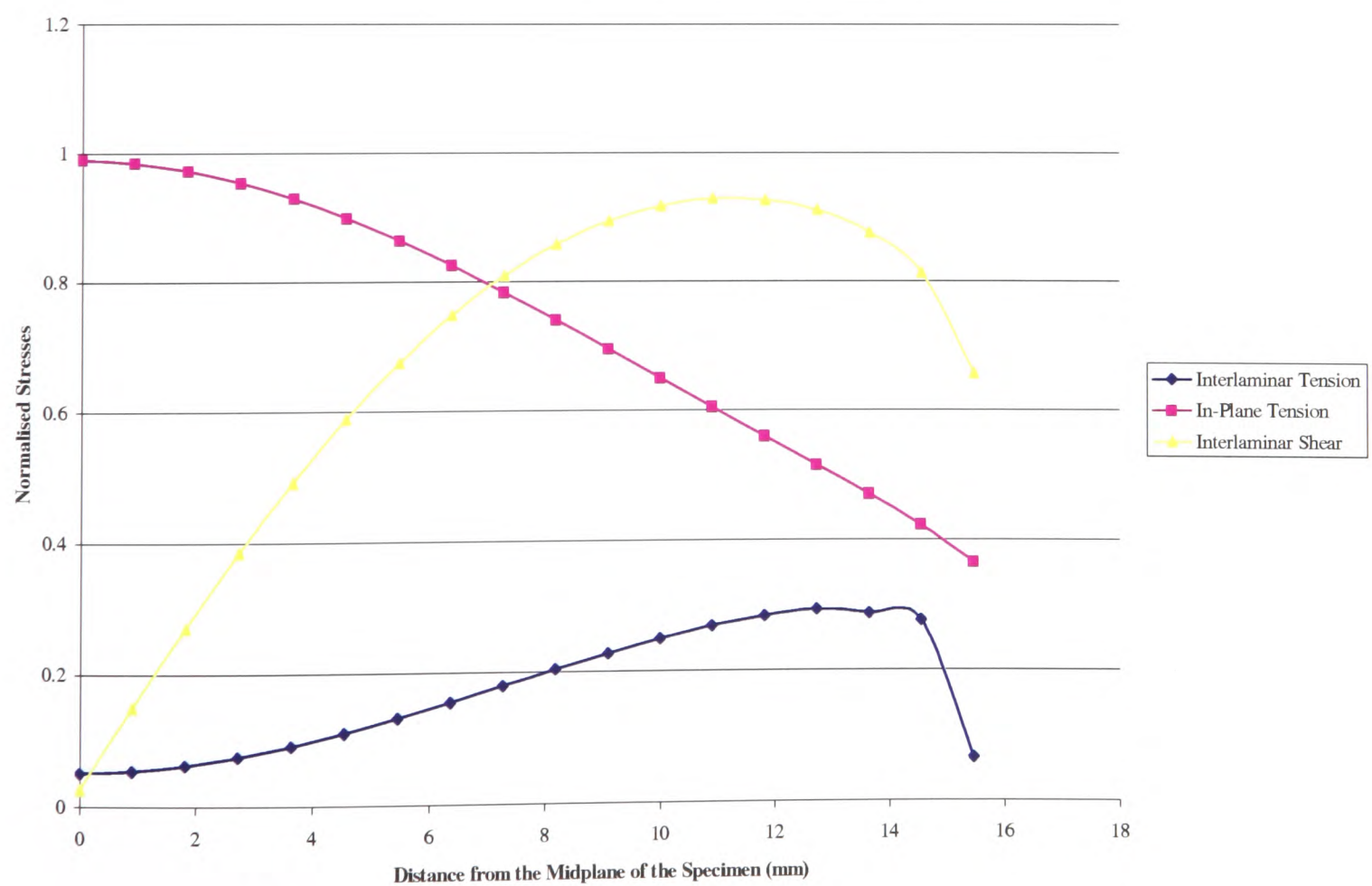


Figure 5.50 CNB6, $t=3$, Stress Interaction along the Notch.

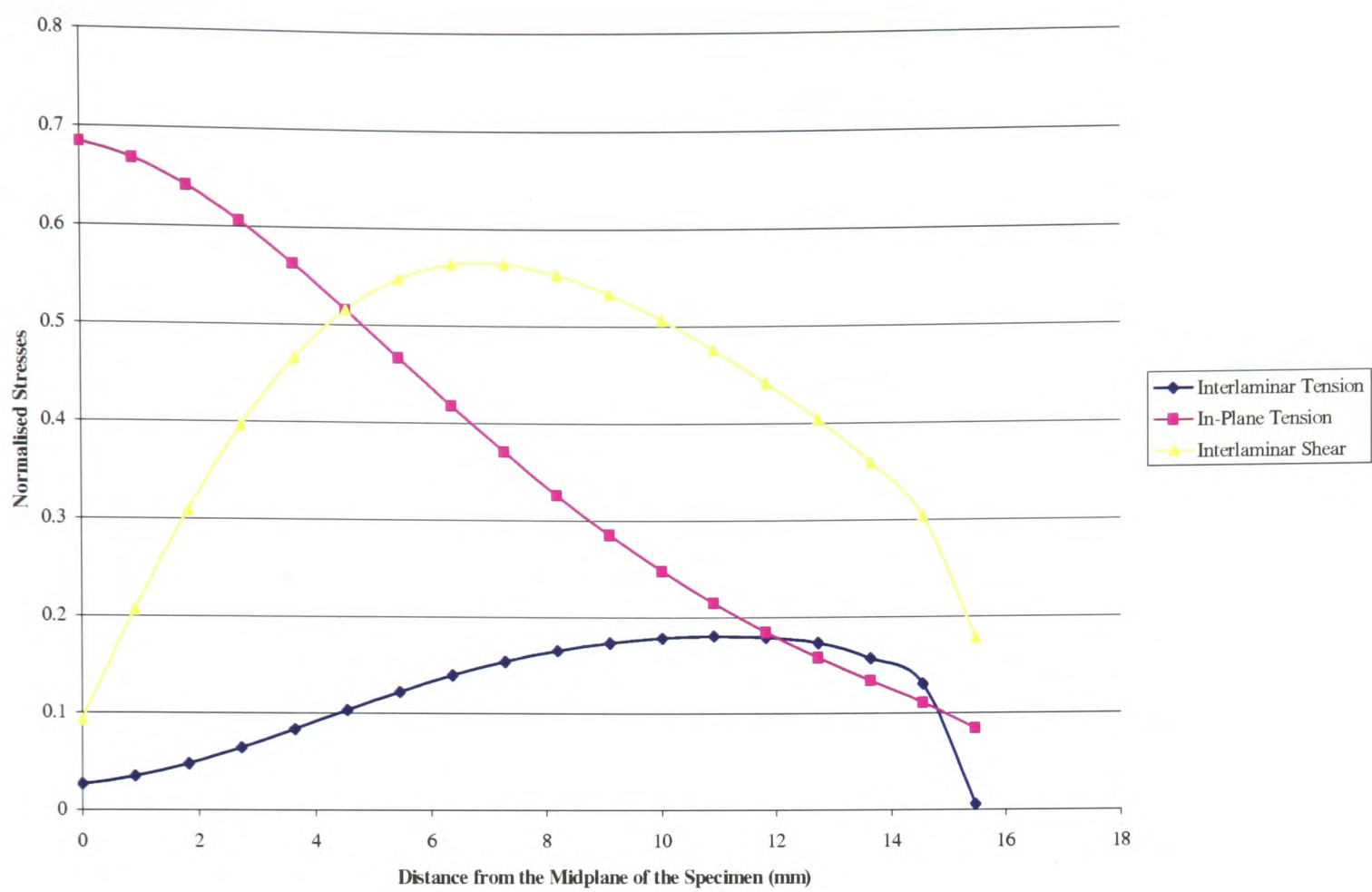


Figure 5.51 CNB3, t=2, Stress Interaction along the Notch.

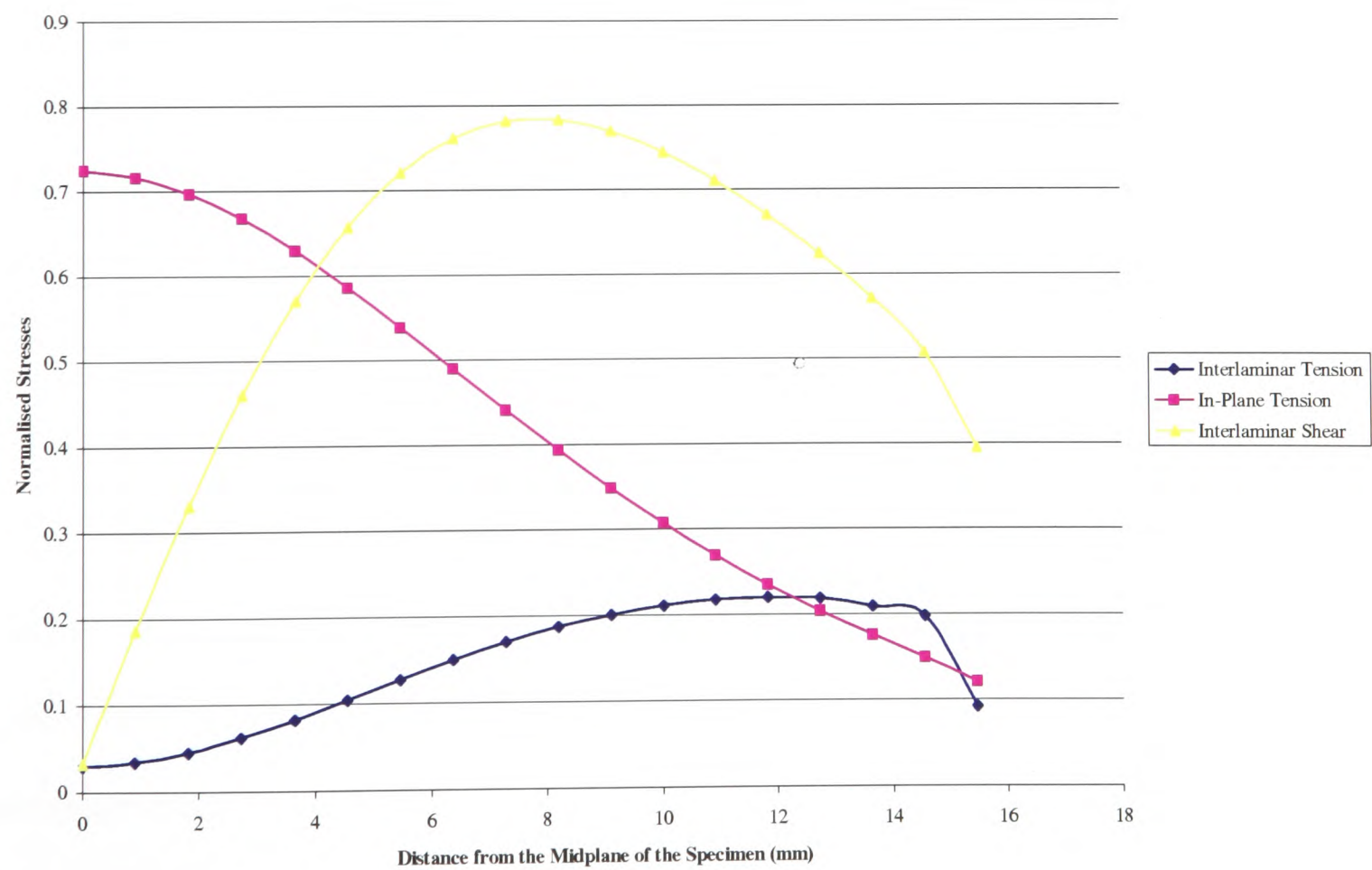


Figure 5.52 CNB4, t=2, Stress Interaction along the Notch.

Subsequently, the interlaminar shear stress caused tearing of the matrix layer (Mode II failure) and, finally, the interlaminar tensile stress opened the delamination zone leading to catastrophic failure (Mode I failure).

5.7 The Notched (NRP) and Oblique (ORP) Rectangular Prism Specimens

The model shown in Figure 5.53 represents one eighth of the NRP specimen and assumes symmetry about the mid planes of the length, width and breadth. During the iterative process of identifying the ideal notch profile, the elastic properties for steel were adopted, i.e. homogeneous and isotropic. Figure 5.54 shows a full scale model of the flat rectangular geometry employed for the ORP specimen. The total number of elements used for the NRP and ORP models were 360 and 216 respectively. For both the NRP and the ORP models, aluminum tab was modelled using 20 noded hexahedral elements and the adhesive layer was modelled using 27 noded hexahedral contact elements in order to accurately simulate the boundary conditions at the interface between the specimen surface and the adhesive layer.

The stress distribution is presented in Figure 5.55 and shows the stress concentration associated with the notch profile for the NRP specimen. The value of the stress concentration factor is 1.09. The stress concentration factor for the ORP specimens varied with the material oblique angle and the degree of anisotropy. This effect is discussed in Section 5.7.1.

The material oblique angle to the loading axis was specified using the ORIENTATION command in ABAQUS which specifies the orientation of an element or element group locally using either cartesian, cylindrical or spherical coordinates relative to the global cartesian coordinate frame [5.1]. The validation of the ORIENTATION command was conducted using an 8 noded hexahedral element with the elastic properties of 913UD. The boundary conditions are shown in Figure 5.56. The stresses resulting from the application of a uniaxial load in each

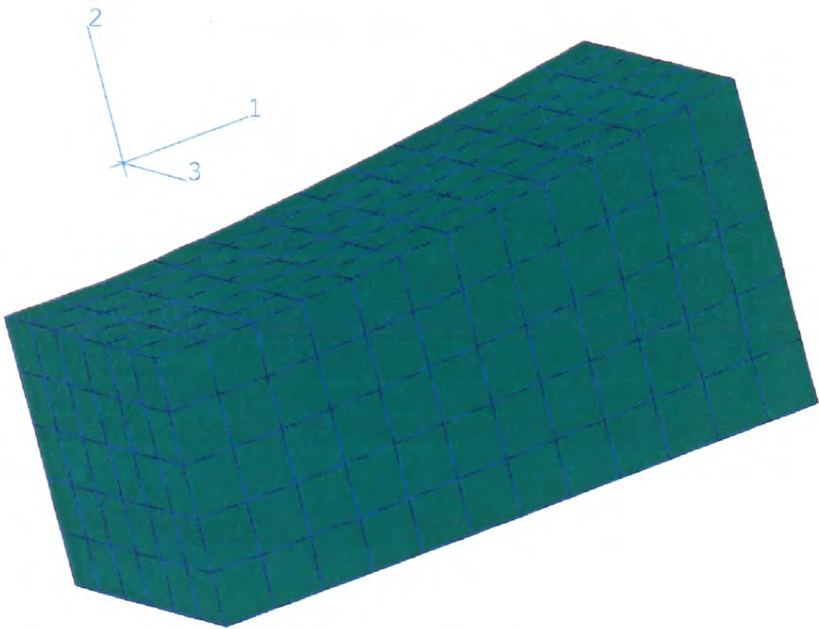


Figure 5.53 Mesh Used for Interlaminar Tension Test.

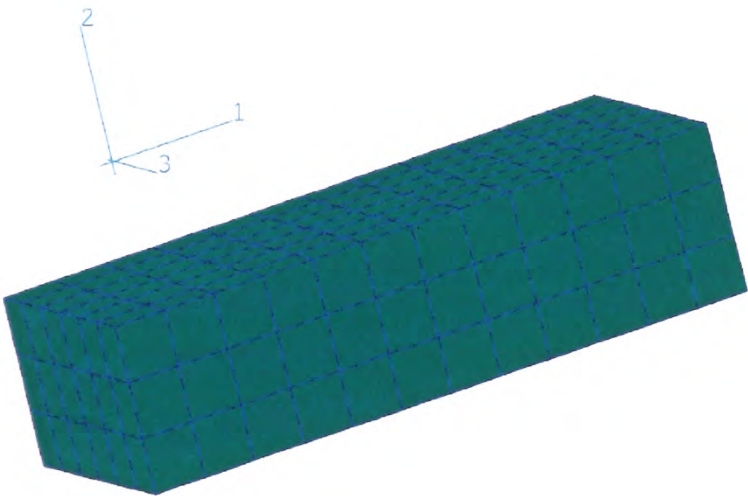


Figure 5.54 Mesh Used for the Oblique Specimens.

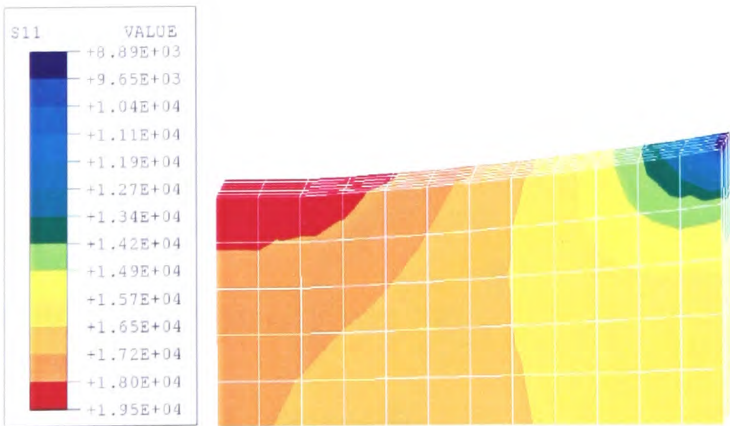


Figure 5.55 Steel, Direct Stress Distribution.

principal material direction were recorded. Subsequently, the same loading conditions were achieved by using the ORIENTATION command to reorient the element material properties relative to a fixed load vector. Effectively, for each principal material direction, a uniaxial load was again applied allowing a comparison of the resultant stresses with those obtained without using the ORIENTATION command. As expected, the stress and strain contour plots for corresponding directions were identical. This provides ample confidence for the use of this technique in this investigation.

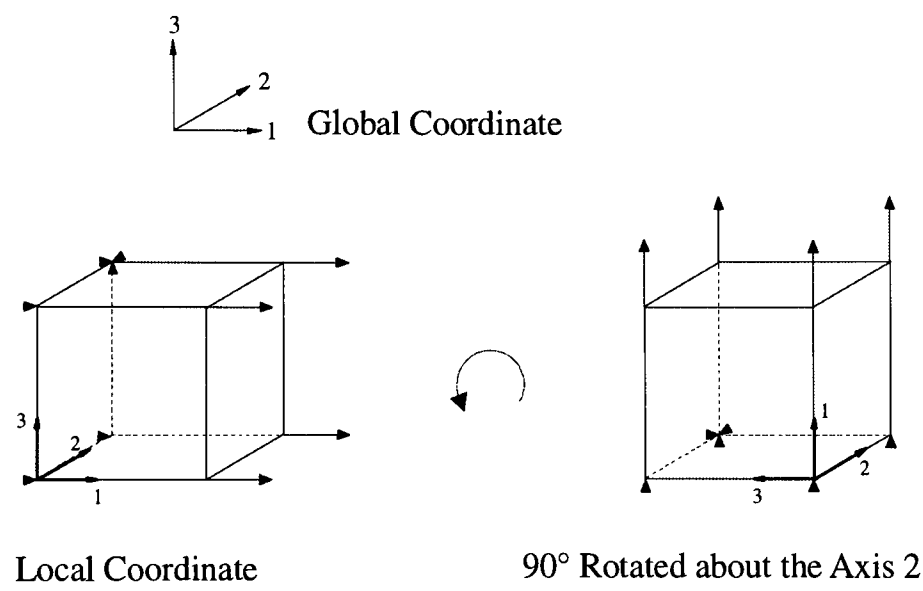


Figure 5.56 The Boundary Conditions Used for the Validating the Orientation Command.

The direct stress in the loading direction obtained from the FEA was converted into local stress components using the following equations:

The in-plane stress in the fibre direction is given by:

$$\sigma_L = \sigma_x \cos^2 \theta \tag{5.2}$$

where σ_x is the applied stress and θ is as previously defined in Figure 3.13.

The interlaminar tensile stress is given by:

$$\sigma_T = \sigma_x \sin^2 \theta \tag{5.3}$$

The interlaminar shear stress is given by:

$$\tau_{LT}=\sigma_x\sin\theta\cos\theta$$

(5.4)

5.7.1 Results and Discussion

Table 5.11 presents the stress concentration factors due to the material oblique angle and the degree of anisotropy. These stress concentration factors were calculated as a ratio of the highest stresses to the average stresses. The stress distributions for each material and each angle are presented in Figures 5.57 - 5.68. The interlaminar tensile and shear stresses and the in-plane stresses for each test along with the respective average values are presented in Tables 5.12 - 5.23. Also, the stress distributions along the top surface for each material and each angle are presented in Figures 5.69 - 5.72. Finally, Figures 5.69 - 5.72 show the locations of each failure as observed in the experiments. These experimental results agree well with those predicted by the FEA.

Angle	913UD	914NCF	916W
90°	1.18	1.17	1.18
67.5°	1.34	1.22	1.22
45°	1.09	1.07	1.04
22.5°	1.13	1.10	1.16

Table 5.11 The Stress Concentration Factor for Each Specimen Configuration.

It can be seen from Figures 5.57 - 5.62 that, for the oblique angles of 22.5° and 45°, the stress distribution is close to symmetrical. The 67.5° model (Figure 5.70) yields an asymmetric stress distribution despite identical boundary conditions to the 22.5° and 45°

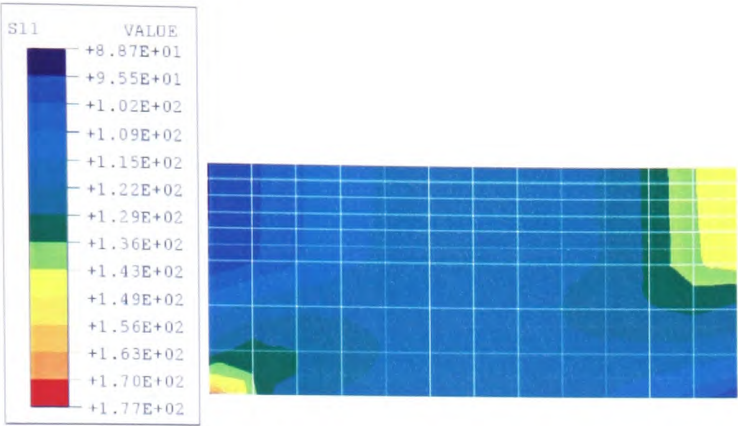


Figure 5.57 ORP3, $\theta=22.5^\circ$, Direct Stress.

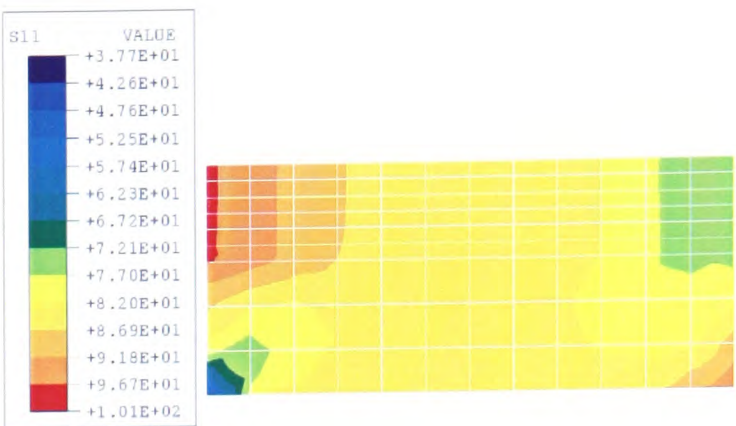


Figure 5.60 ORP3, $\theta=45^\circ$, Direct Stress.

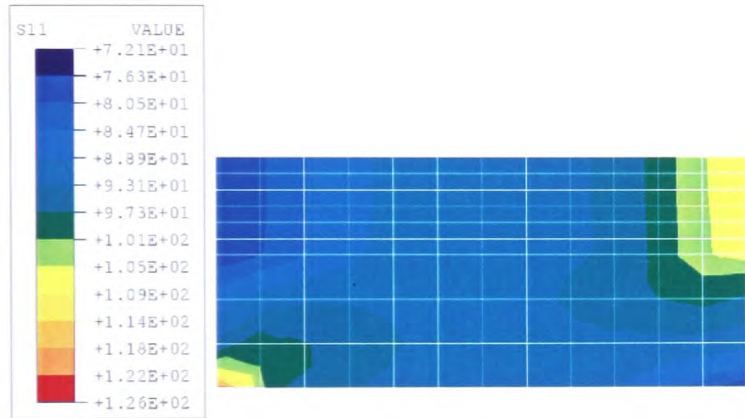


Figure 5.58 ORP4, $\theta=22.5^\circ$, Direct Stress.

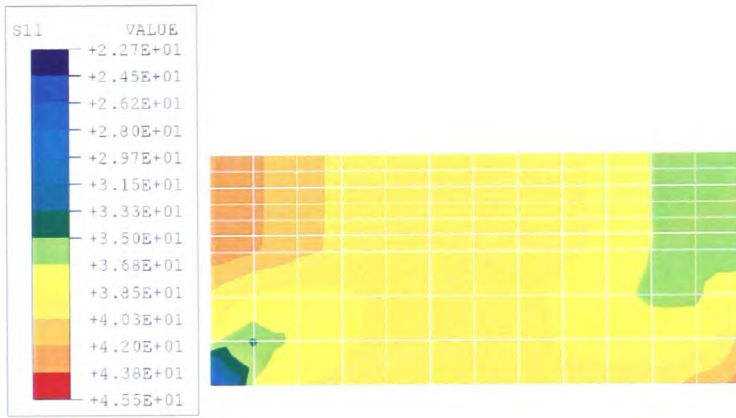


Figure 5.61 ORP4, $\theta=45^\circ$, Direct Stress.

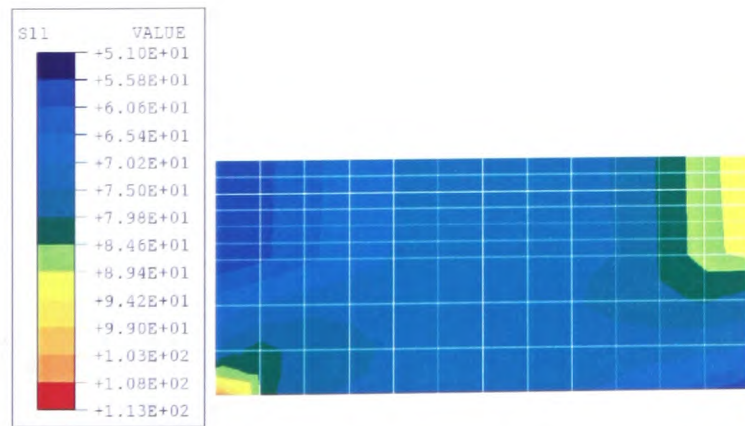


Figure 5.59 ORP6, $\theta=22.5^\circ$, Direct Stress.

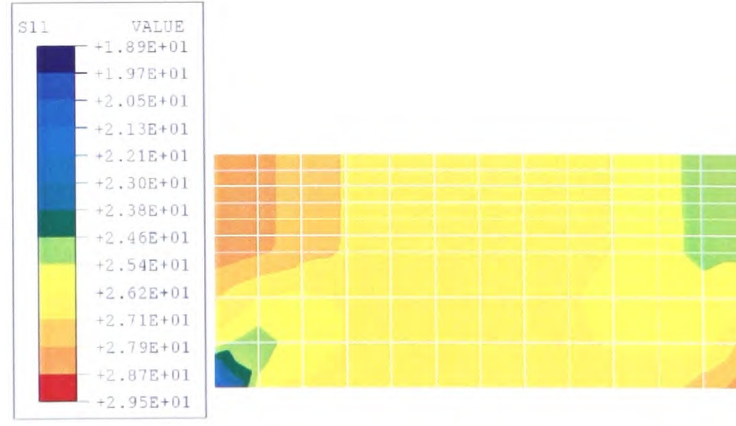


Figure 5.62 ORP6, $\theta=45^\circ$, Direct Stress.

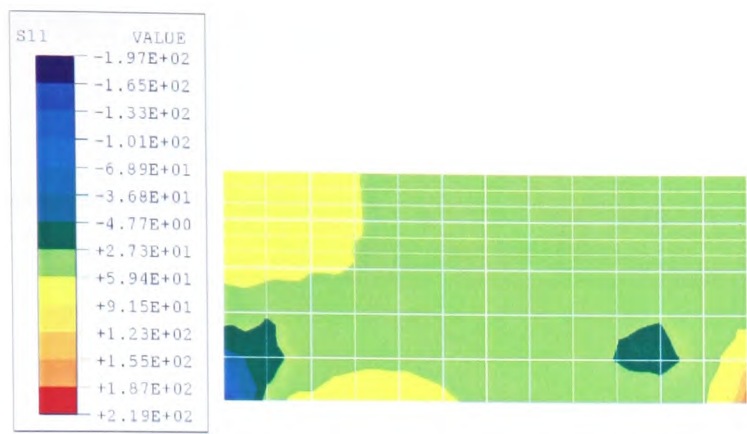


Figure 5.63 ORP3, $\theta=67.5^\circ$, Direct Stress.

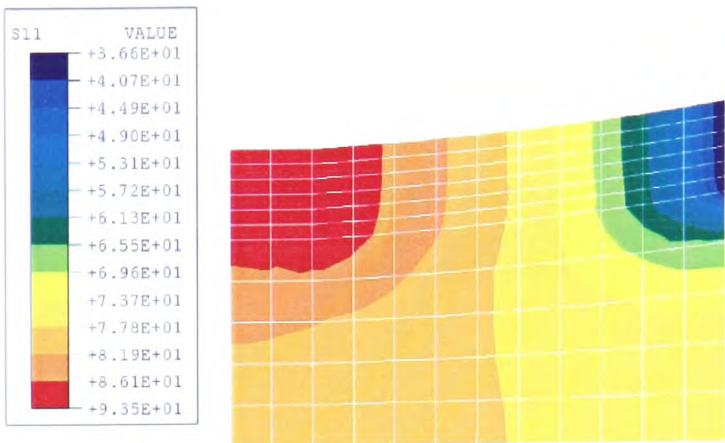


Figure 5.66 NRP3, $\theta=90^\circ$, Direct Stress.

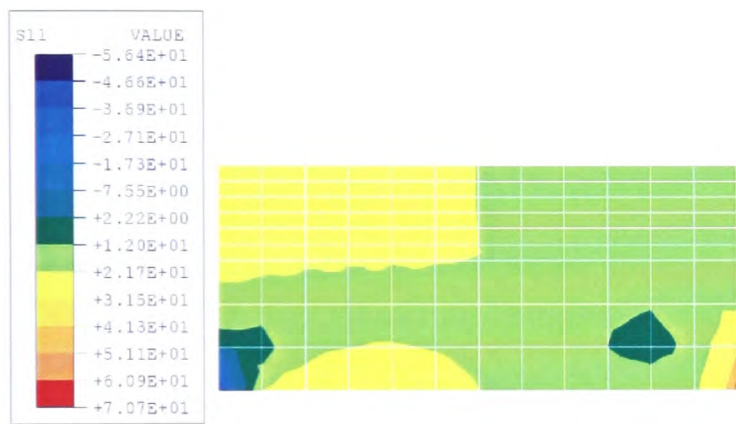


Figure 5.64 ORP4, $\theta=67.5^\circ$, Direct Stress.

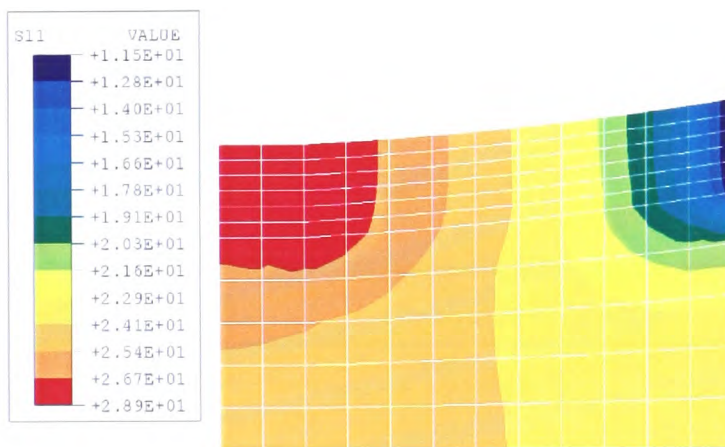


Figure 5.67 NRP4, $\theta=90^\circ$, Direct Stress.

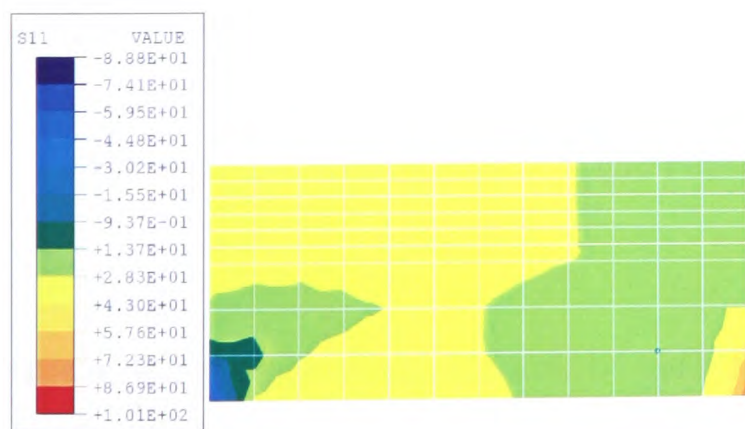


Figure 5.65 ORP6, $\theta=67.5^\circ$, Direct Stress.

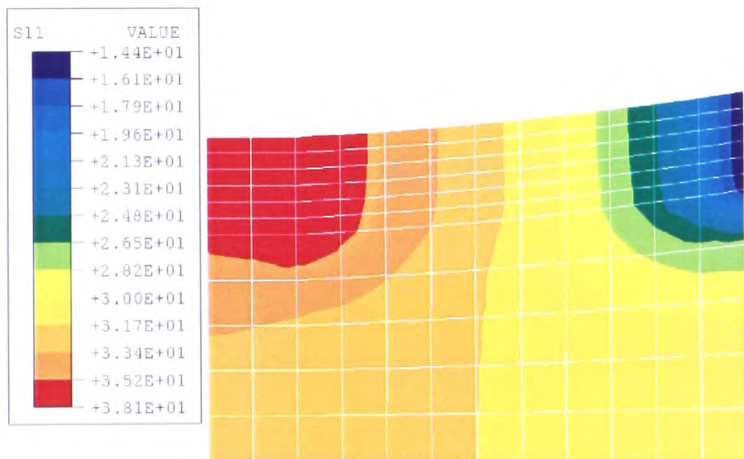


Figure 5.68 NRP6, $\theta=90^\circ$, Direct Stress.

models.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	77.3	0	0
2	83.0	0	0
3	74.0	0	0
4	82.3	0	0
5	70.3	0	0
Average	77.4	0	0

Table 5.12 NRP3, $\theta = 90^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	58.8	27.1	10.3
2	60.1	28.5	10.4
3	61.7	26.4	10.3
4	66.2	27.5	10.3
5	58.8	25.0	10.4
Average	61.1	26.9	10.3

Table 5.13 ORP3, $\theta = 67.5^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

This is due to edge effects induced by the loading fixture. Using the off-axis coupons (oblique angled specimen) Chamis *et al.* [5.4] demonstrated that large oblique angles are sensitive to edge effects induced by the loading fixture. However, Figure 5.70 shows that the failure did not occur at the edge of the loading fixture. The precise locations are presented in Tables 4.20, 4.24 and 4.28. The 45° specimens produced the longest bandwidth across the failure region due

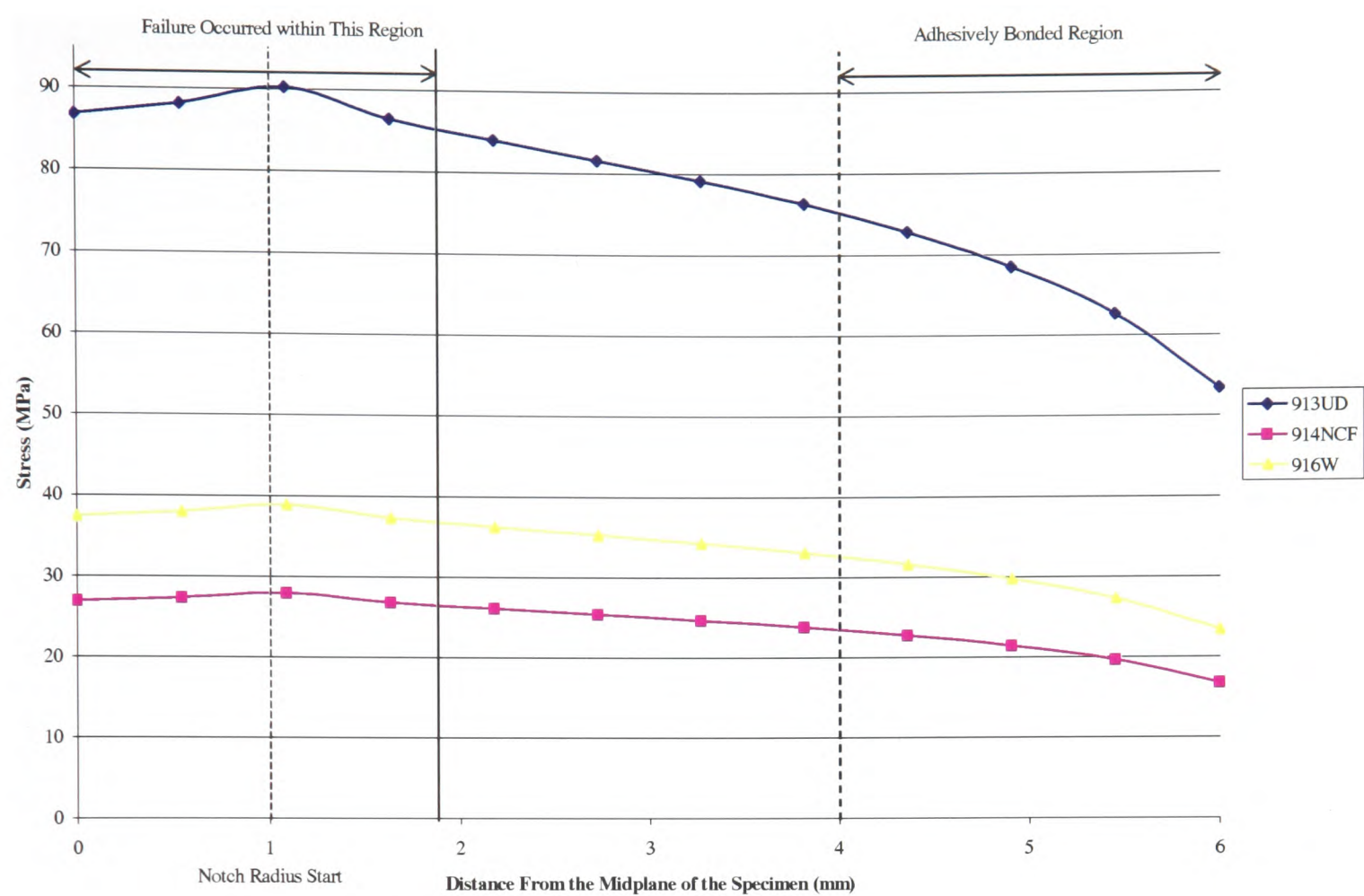


Figure 5.69 $\theta=90^\circ$, The Stress Distribution along the Notch.

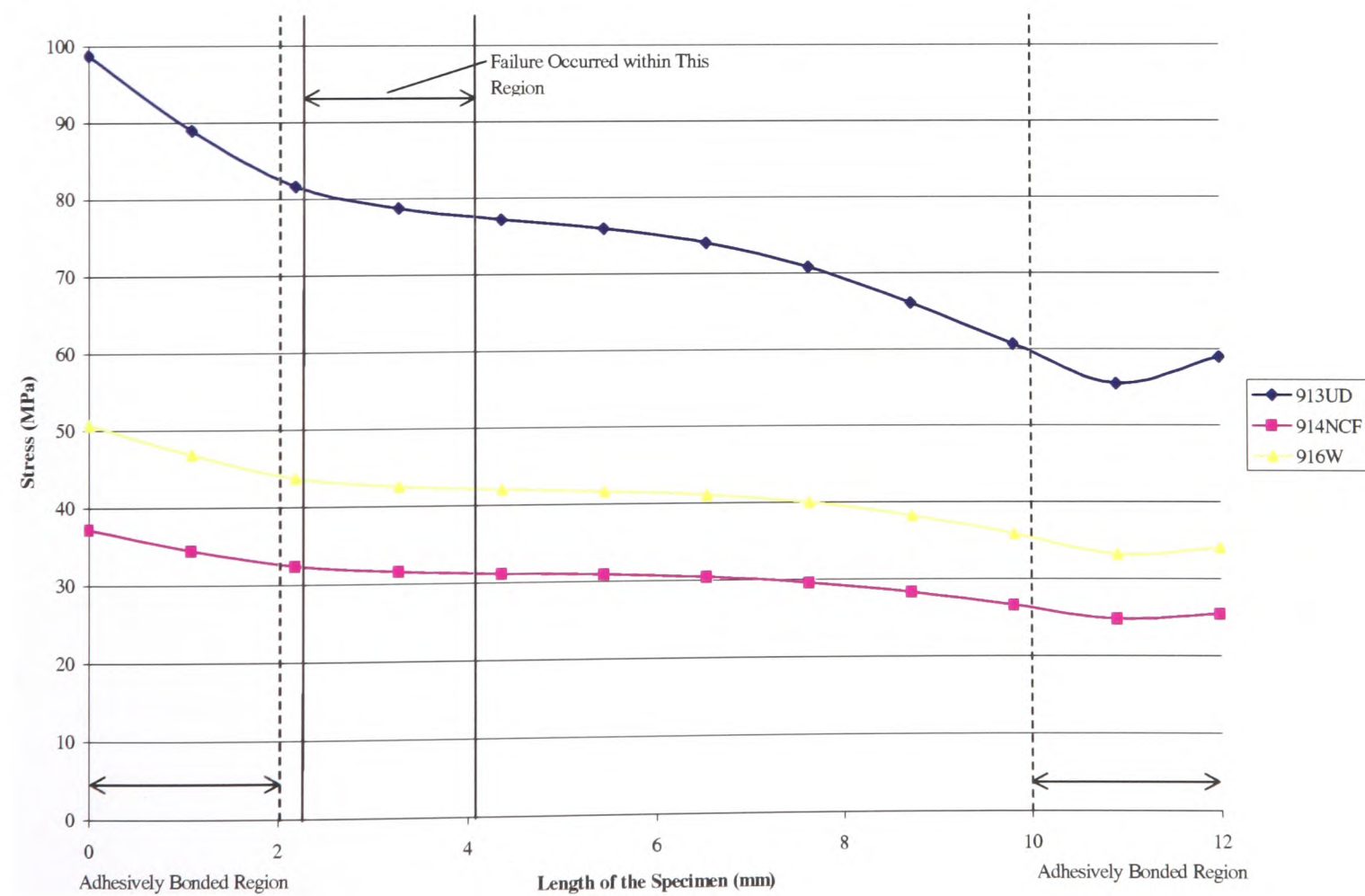


Figure 5.70 $\theta=67.5^\circ$, The Stress Distribution along the Top Surface of Specimen.

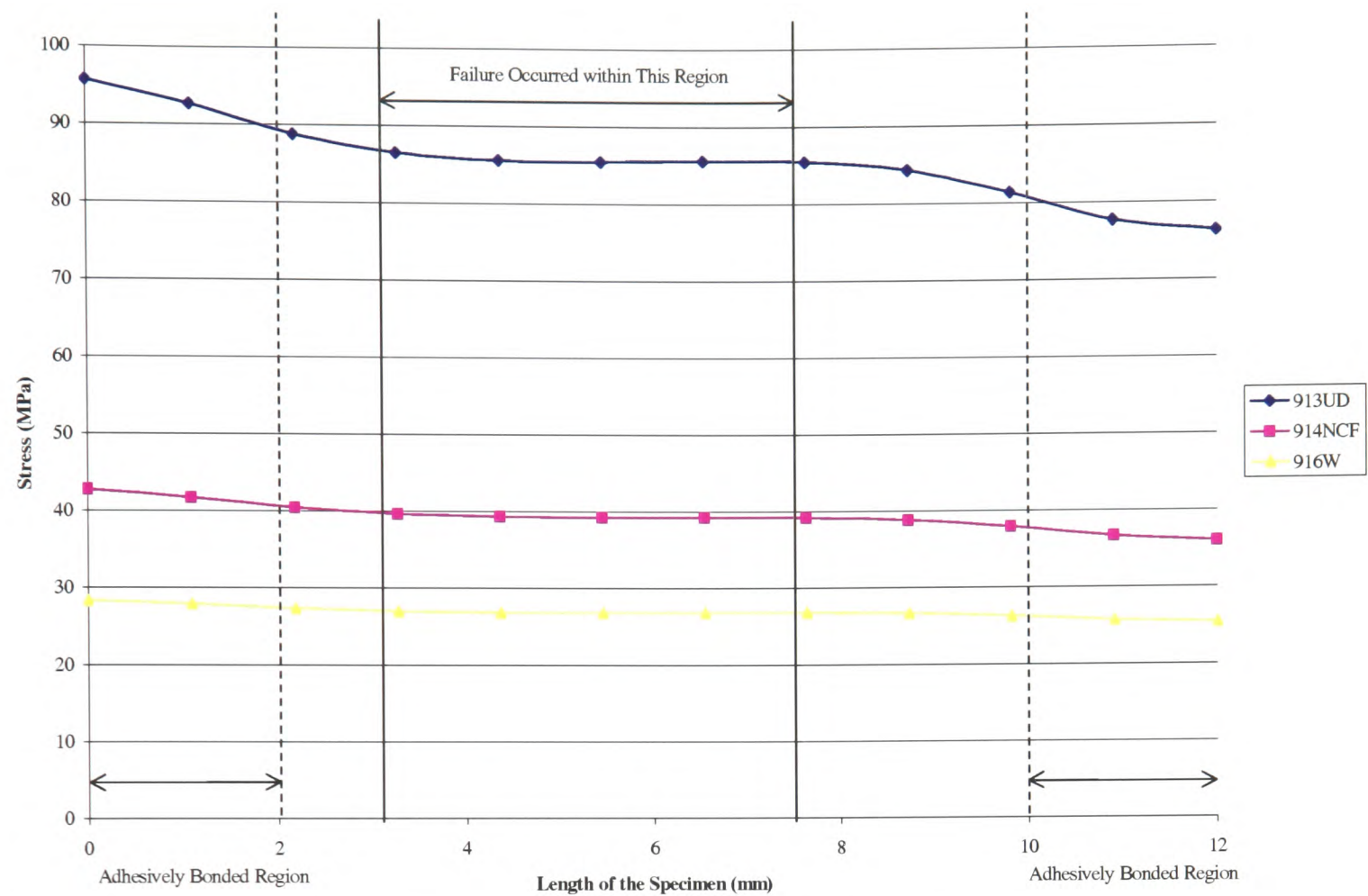


Figure 5.71 $\theta=45^\circ$, The Stress Distribution along the Top Surface of Specimen.

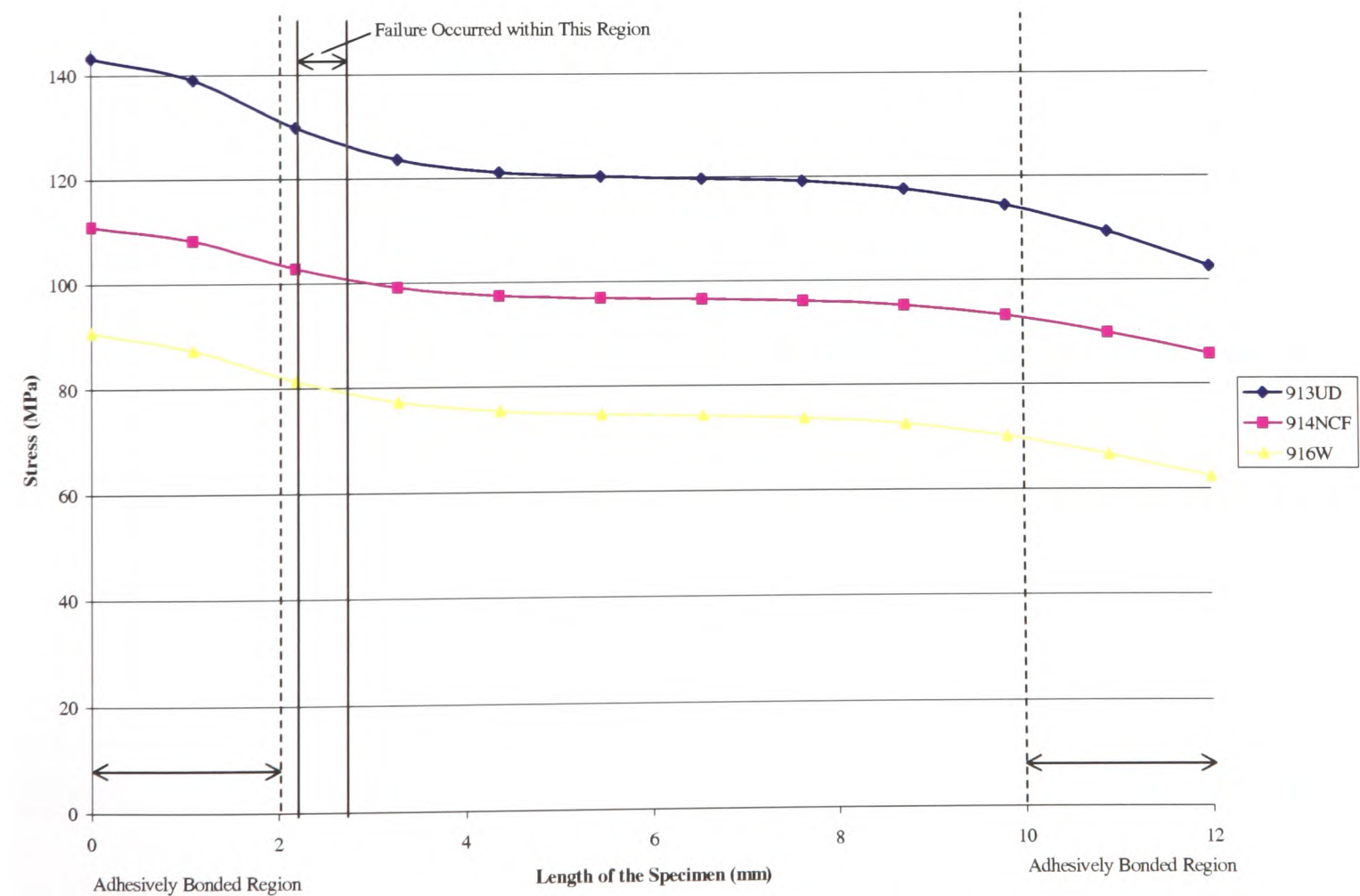


Figure 5.72 $\theta=22.5^\circ$, The Stress Distribution along the Top Surface of Specimen.

to the low stress gradient along the surface (Figure 5.71). Although the 22.5° specimens show a more uniform stress distribution than that of the 67.5° specimens, the region where failure occurred is concentrated near the edge of the loading fixture (Figure 5.72). This effect should not be confused with stress concentrations along the edge of the loading fixture. Though there is a slight stress gradient, this effect was mainly due to the large failure surface length across the thickness of specimen, as previously shown in Figure 4.41.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	48.8	48.8	48.8
2	41.7	41.7	41.7
3	45.8	45.8	45.8
4	45.8	45.8	45.8
5	45.8	45.8	45.8
Average	45.6	45.6	45.6

Table 5.14 ORP3, $\theta = 45^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	18.3	32.7	155.1
2	24.6	34.6	143.6
3	17.1	28.9	147.9
4	23.4	39.5	136.5
5	22.4	41.6	130.8
Average	21.1	35.4	142.8

Table 5.15 ORP3, $\theta = 22.5^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	25.0	0	0
2	21.3	0	0
3	24.3	0	0
4	22.6	0	0
5	23.6	0	0
Average	23.4	0	0

Table 5.16 NRP4, $\theta = 90^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	24.1	9.5	5.0
2	23.0	9.6	5.2
3	23.4	9.7	5.9
4	23.3	10.8	5.7
5	23.2	9.0	5.4
Average	23.4	9.7	5.4

Table 5.17 ORP4, $\theta = 67.5^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	18.3	18.3	18.3
2	17.9	17.9	17.9
3	19.5	19.5	19.5
4	19.5	19.5	19.5
5	17.9	17.9	17.9
Average	18.6	18.6	18.6

Table 5.18 ORP4, $\theta = 45^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	13.0	31.6	119.4
2	12.7	31.0	118.1
3	15.0	30.5	102.4
4	15.1	30.8	103.1
5	16.5	34.0	110.9
Average	14.5	31.5	110.8

Table 5.19 ORP4, $\theta = 22.5^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	24.0	0	0
2	35.3	0	0
3	31.3	0	0
4	29.3	0	0
5	35.0	0	0
Average	31.0	0	0

Table 5.20 NRP6, $\theta = 90^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	33.4	13.8	5.7
2	33.4	13.8	5.7
3	32.0	13.2	5.4
4	30.5	12.6	5.2
5	29.1	12.0	5.0
Average	31.7	13.1	5.4

Table 5.21 ORP6, $\theta = 67.5^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	17.9	17.9	17.9
2	19.4	19.4	19.4
3	21.5	21.5	21.5
4	23.6	23.6	23.6
5	28.4	28.4	28.4
Average	20.2	20.2	20.2

Table 5.22 ORP6, $\theta = 45^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

Test No.	Interlaminar Tension	Interlaminar Shear	In-Plane Tension
1	10.8	57.3	60.4
2	8.6	60.0	50.5
3	8.9	56.7	51.9
4	9.5	58.7	55.4
5	9.8	58.7	57.6
Average	9.4	58.1	55.1

Table 5.23 ORP6, $\theta = 22.5^\circ$, The Failure Stresses (MPa) Estimated from the FEA.

5.8 The Accuracy of the Finite Element Models

The accuracy of the FEA estimations were established by comparison with the experimentally measured displacements at the failure loads (Table 5.24) expressed as percentage differences. As discussed in Section 5.2, all of the models for a given specimen type were identical with the exception of geometric and material characteristics. Despite variations in the estimated displacements, all of the FE analyses were conservative. The 913UD and

916W models showed good agreement with experiment whilst the 914NCF models were more conservative.

	913UD	914NCF	916W
PCB	3	11	4
NCB	0.3	12	5
CNB, $t = 2$ mm	4	8	-
CNB, $t = 3$ mm	2	7	4
CNB, $t = 4$ mm	3	7	4
ORP, $\theta = 22.5^\circ$	2	9	6
ORP, $\theta = 45^\circ$	3	7	5
ORP, $\theta = 67.5^\circ$	3	7	6
NRP, $\theta = 90^\circ$	2	6	4
Average	2.5	8.2	4.8

Table 5.24 A Comparison of the Load Point Displacement at Failure.

Two main factors were thought to be responsible for the 914NCF results:

- a)
- As mentioned in Section 4.2.4, the elastic properties employed for all of the FE models were based on the elastic properties obtained from the 13 mm thick plate which showed the greatest difference in the fibre volume fraction from the 914NCF curved plate, this being 4 % higher than that of the 13 mm plate (Table 4.2). It was previously thought that this 4% difference would not significantly affect the results of the FE analysis. However, the FEA results for 914NCF indicate that this is not the case and that, as a consequence of the corresponding inaccuracies in the elastic properties, the model had insufficient stiffness.

- b) The 916W material configuration requires one lamina and the 913UD material configuration requires at least two laminae to represent its lay-up configuration, i.e. the $0^\circ/90^\circ$ cross-ply, whereas the 914NCF material configuration requires at least four laminae, i.e. the $0^\circ/-45^\circ/+45^\circ/90^\circ$ quasi-isotropic ply, to represent its lay-up configuration. However, due to the limited thickness of the parent plates which could be manufactured, the strain gauge length used was limited to 1 mm in the through-thickness direction. Within a 1 mm gauge length there were approximately 8, 7, and 4 laminae for the 913UD, 914NCF, and 916W material configurations respectively. Thus, the average bulk properties of 914NCF were only measured over 1.75 sets of basic lay-up configurations with a consequential increase in the probability of error.

References

- 5.1 Abaqus Standard User's Manual Volume II, Hibbitt, Karlsson & Sorensen Inc. 1995.
- 5.2 Hognestad G., Analysis of Composite Flange Connections and Determination of Interlaminar Strength, D.Phil Thesis, University of Oxford, 1993.
- 5.3 Zhou G., Green R. and Morrison C., In-Plane and Interlaminar Shear Properties of Carbon/Epoxy Laminates, Composites Science and Technology, Vol.55, 1995, p187-193.
- 5.4 Chamis C.C. and Sinclair J.H., Ten Degree Off-Axis Test for Shear Properties in Fibre Composites, Experimental Mechanics, Vol.17, No.9, 1977, p339-376.

Chapter 6

Moiré Interferometry

6.1 Introduction

In numerous publications regarding the FEA of composite materials, the results of the models used were validated by comparing the experimental displacement at failure with the displacement yielded by the model when subjected to the failure load. This approach is common practice for many conventional isotropic materials, i.e. steel, aluminium, iron etc., and is backed up by many years of research. However, in the analysis of anisotropic materials, the availability of analytical solutions is very limited. Moreover, stress distributions in simple geometries can not be established intuitively due to the heterogeneity which alters the stress distribution significantly over that of a homogeneous material with identical geometry. In the present investigation, a nonlinear notched geometry, e.g. notched curved beam, had to be employed due to the limitations associated with the length of the specimens. This chapter presents a validation of the FE models using the experimental technique, Moiré Interferometry.

Moiré interferometry is a relatively new method, developed largely in the 1980's. It matured quickly and is now employed in laboratories all over the world. It is known by various names, including laser Moiré interferometry, interferometric Moiré and high sensitivity Moiré. It provides whole-field maps of in-plane displacement fields. It does so with subwavelength sensitivity, high spatial resolution, high signal-to-noise ratio and an extremely large displacement range. The principals of Moiré interferometry are discussed in The SEM Handbook [6.1].

6.2 Description of the Apparatus

Figure 6.1 shows a schematic illustration of the laser apparatus configuration. A cross-line diffraction grating of frequency 1200 /mm is replicated on the specimen and deforms together with the specimen. A 10 mW Helium-Neon laser provides the light beam which is expanded through a pin hole and collimated using a collimating lens. This illuminates the specimen grating and the interferometer which consists of a light frame and three adjustable mirrors, *M1*, *M2*, and *M3*. In order to obtain the *u* displacement field, half the incident beam, *B1*, (Figure 6.2) impinges directly on the specimen surface while the other half, *B2*, impinges on the mirror, *M3*, and then reflects to the specimen in a symmetrical direction to form a virtual grating (reference grating). This virtual grating has a frequency twice that of the specimen grating and interacts with the vertical set of lines of the specimen grating to form a Moiré fringe pattern which is recorded using a CCD camera. Similarly, two incident beams, *A1* and *A2*, impinge on the mirrors, *M1* and *M2*, and then illuminate the set of grating lines perpendicular to the *y* axis to produce the *v* displacement field. In this four-beam optical arrangement, the *u* and *v* displacement fields are obtained by using the beam pairs *A1* & *A2* and *B1* & *B2* simultaneously.

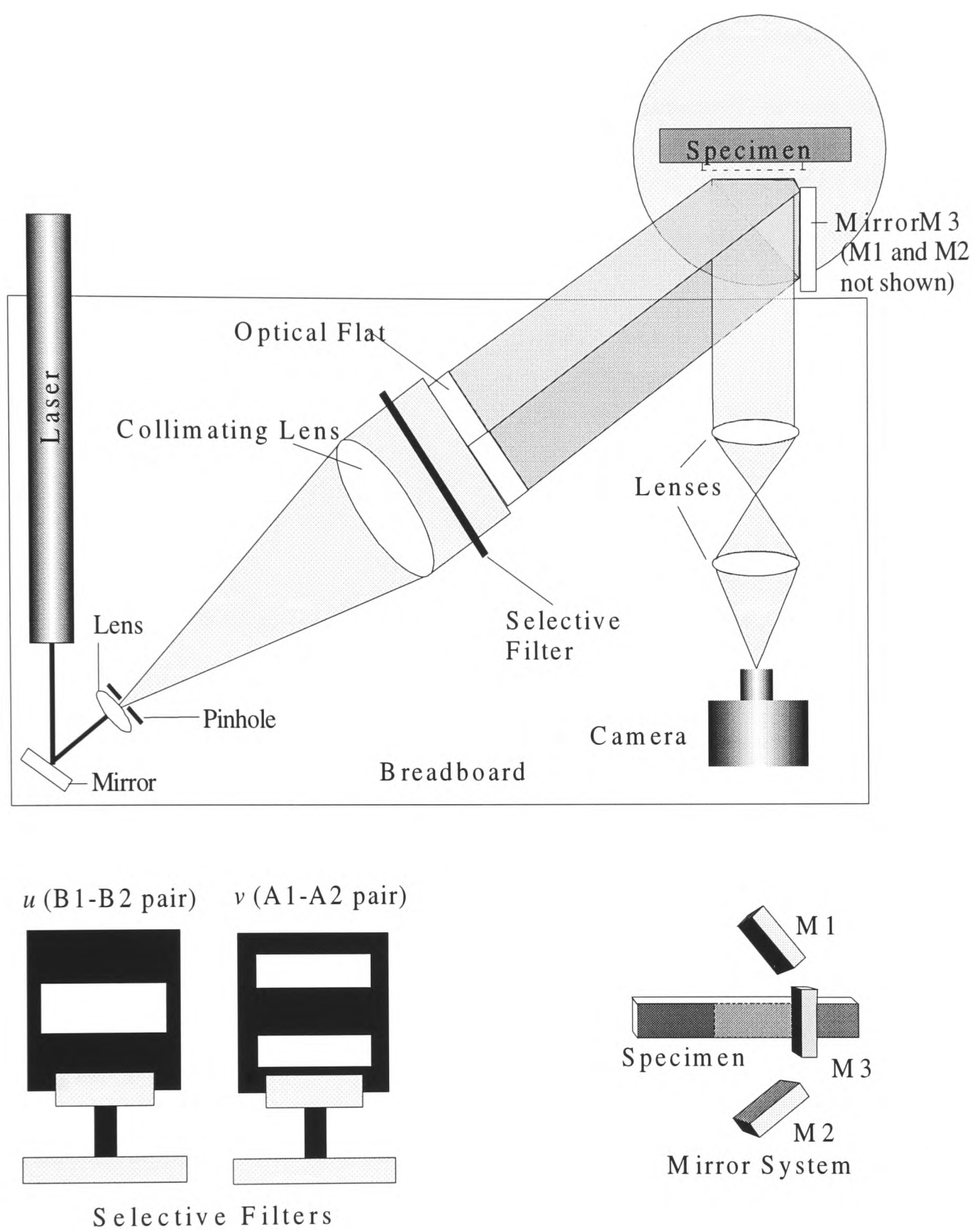


Figure 6.1 The Optical System Used in Moiré Interferometry.

This was achieved by selectively filtering the beam with an opaque object with either one or two slots allowing only the required beam pairs to illuminate the interferometer, as shown in Figure 6.1. The resulting interferograms were then recorded with a CCD camera and digitised into an array of 256 by 256 pixels of 8 bit resolution (i.e. 256 grey levels) using a personal

computer. The displacement sensitivity of the Moiré fringes is determined by the frequency of the reference grating. For the system described here, the frequency of the virtual grating is 2400 lines/mm and therefore the corresponding displacement sensitivity is $0.417 \mu\text{m}$ per fringe.

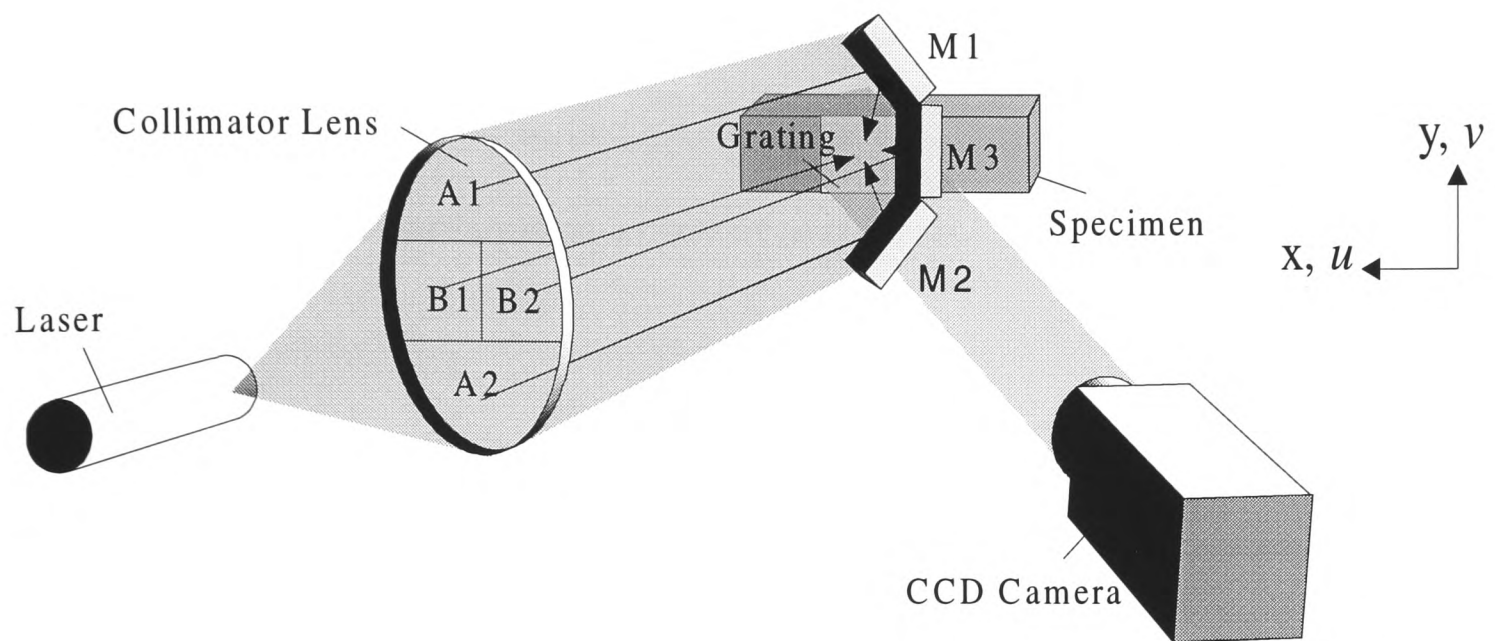


Figure 6.2 The Optical Arrangement for Obtaining the u and v Displacements.

6.3 Analysis Procedure

The conventional method of analysing Moiré fringe patterns involves manually locating the fringe centres and determining the order and the gradient of the fringes. The use of phase stepping methods eliminates these tedious and labourious procedures by allowing computers to be used to analyse the fringe patterns and produce phase maps automatically [6.2]. Furthermore, these methods reveal the grey scale information between fringe extremities (that is, the fractional fringe orders) and thus the displacement resolution of the system is greatly enhanced. The automation of the fringe pattern analysis in this research was done using the temporal phase stepping (TPS) method [6.3]. The method is based on the acquisition of at least three interferograms with constant phase shift and then determining the phase (corresponding

to the displacement value) at each pixel. The intensity of an interferogram at any point (x,y) is given by:

$$I_n(x,y) = g(x,y) + s(x,y)\cos[\lambda(x,y) + \delta_n] \quad (6.1)$$

where n , $g(x,y)$, $s(x,y)$, $\lambda(x,y)$ and δ denote each phase shifted interferogram, the background intensity, the local contrast, the phase to be determined and the phase shift respectively.

By using the three phase-shifted interferograms, the three unknowns in eqⁿ. 6.1, namely g , s and λ , can also be determined on a pixel by pixel basis.

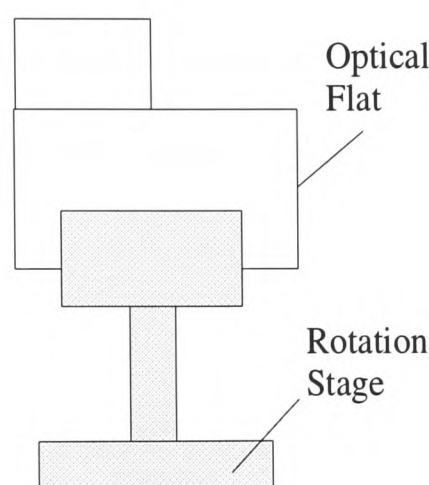


Figure 6.3 L-Shaped Optical Flat.

The phase can also be obtained by acquiring more than three interferograms which leads to an over-deterministic system that affords errors in the phase shift setting to a certain extent and therefore results in more accurate phase maps. In general, the phase shift is introduced by changing the path length of one of the interfering beams. This is accomplished in the proposed system by placing an optical flat into the path of the collimated beam. The optical flat is an 'L' shape (Figure 6.3) which changes the path length of one half of the beam for both the u and v displacement measurements. It may be seen from eqⁿ. 6.1 that $\lambda(x,y)$ is periodic and varies from $-\pi$ to π between two adjacent fringes. This function contains sharp discontinuities which may

be removed via an unwrapping process [6.3] yielding a continuous function [6.4] which represents the true nature of $\lambda(x,y)$.

6.4 Specimen Preparation

The standard method for preparing a specimen using the replication process is illustrated in Figure 6.4. The crossed-line grating is contained within a glass master mould upon which an ultrathin reflective aluminium coating is deposited. This master mould was attached to the edge of the specimen using adhesive subjected to constant pressure applied with a weight. Conventional cellotape was then used to fix the assembly to the base in order to prevent the adhesive from creeping due to the pressure.

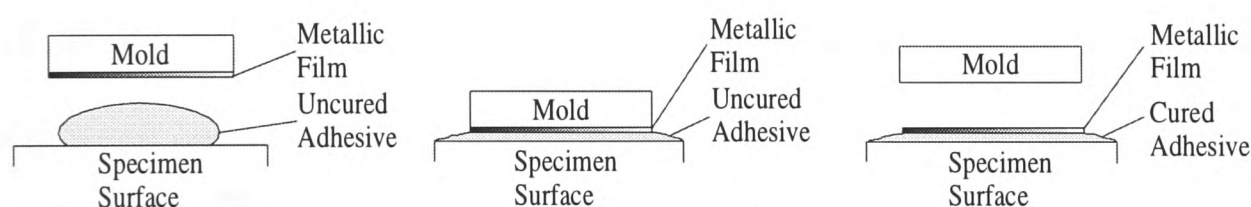


Figure 6.4 The Steps Used to Prepare a Specimen Using the Replication Process.

The adhesive used was a two part epoxy (PC-10, Measurement Group Inc.) which, during the mixing process, formed small bubbles. These bubbles cause circular defects in the grating because they prevent the ultrathin reflective aluminium coating from attaching to the specimen surface. They were eliminated by placing the adhesive container in a vacuum tank for 30 minutes. After curing, the excess adhesive was removed, firstly by the crude use of a scraper and then more finely under a low powered microscope using a scalpel. Finally, the mould was carefully removed. The first grating prepared had damage along its edge which was caused by the removal of the master mould from the specimen and the subsequent removal of excess

adhesive from the grating edge. In order to resolve this problem, cellotape was used along the front and back surfaces of the specimen to prevent excess adhesive from bonding to the adjacent faces. This simple method was found to be very successful and was adopted for the production of subsequent specimens.

6.5 Results and Discussion

In the following sections, the strain distributions obtained experimentally using Moiré interferometry are compared with those obtained from the FE analyses. It should be noted that the applied load and resultant displacement for each specimen type were recorded during each Moiré analysis and these values were then used to obtain the corresponding FE strain distributions. This should not be confused with the FE analyses presented in Chapter 5 which used the displacement at failure load. It should also be noted that there is an offset in the Moiré plots due to small, unavoidable, misalignments in the setup.

6.5.1 The Plain Curved Beam Specimen

In an attempt to validate the FE analysis, the strain distribution in two principal directions, i.e. the through-thickness direction and circumferential direction, were compared with the results of the Moiré interferometry analysis.

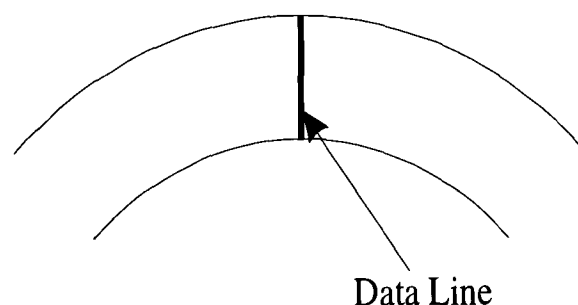


Figure 6.5 PCB, The Location of the Strain Distribution Data Used for Comparison.

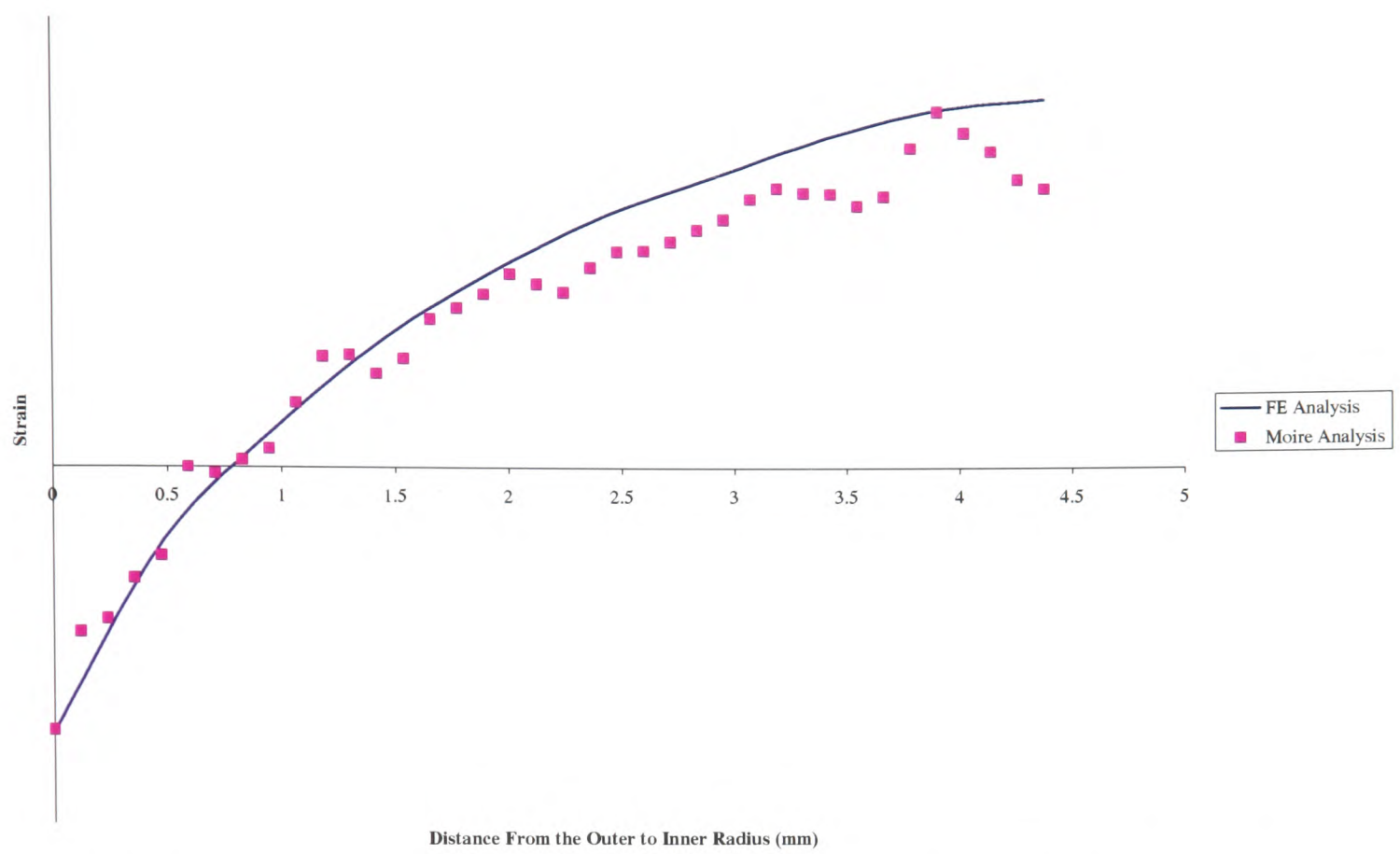


Figure 6.6 PCB6, The Interlaminar Through-Thickness Strain Distribution.

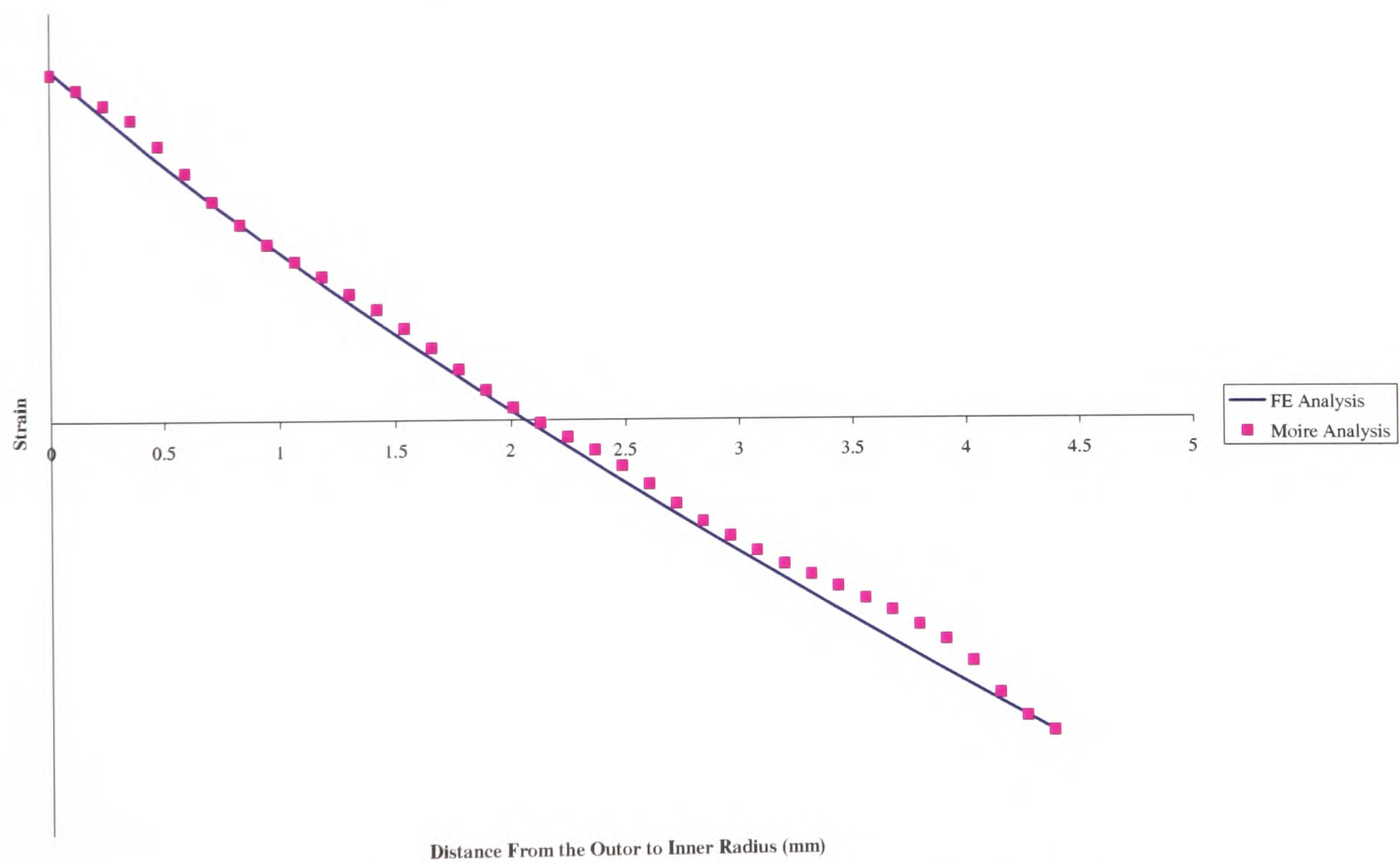


Figure 6.7 PCB6, The In-Plane Strain Distribution.

Figure 6.6 shows the location where the strain data was collected. It was thought that the PCB6 FEA results would be the least consistent with the experimental results as no delamination was observed at failure. Thus, the 916W specimen was chosen for the Moiré analysis. The in-plane strain distribution (Figure 6.7) agrees well with that of the FEA results. The interlaminar through-thickness strain distribution shows slightly larger differences between the Moiré results and the FEA results. However the nature of the curves are essentially the same. The differences may be attributed to the large difference in magnitude between the in-plane strain and the interlaminar through-thickness strain. The in-plane strain due to bending is much greater than the interlaminar through-thickness strain and for this reason, the Moiré fringe pattern for the in-plane strain is more clear. However, due to the restriction placed on the deformation of the aluminium grating, a higher load could not be used in order to obtain a better fringe pattern for the interlaminar through-thickness strain.

6.5.2 The Notched Curved Beam Specimen

All of the specimens exhibited almost identical stress distributions in the FE analyses and thus it was considered adequate to analyse only one specimen via Moiré interferometry for comparison of the resulting strain distribution with the FEA results.

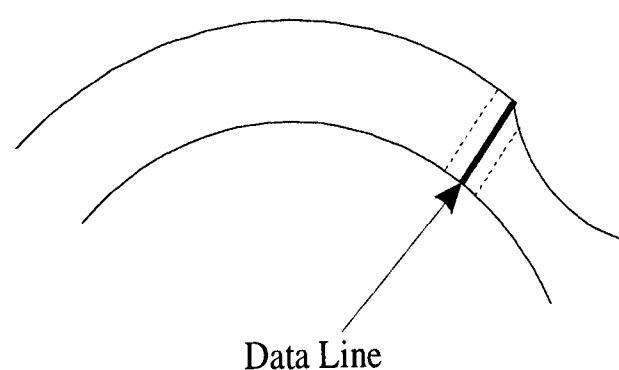


Figure 6.8 NCB, The Location of the Strain Distribution Data Used for Comparison.

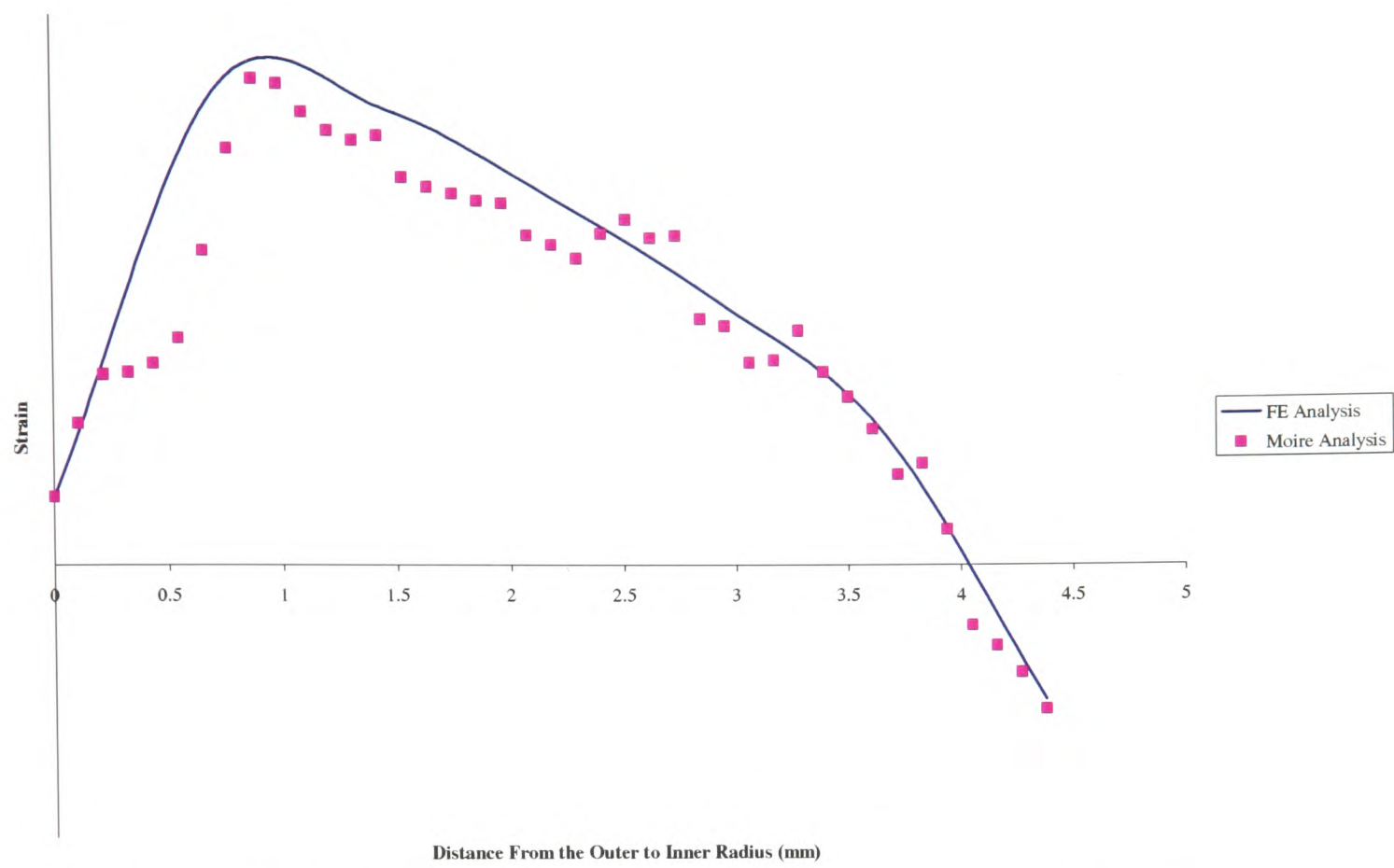


Figure 6.9 NCB6, The Interlaminar Through-Thickness Strain Distribution.

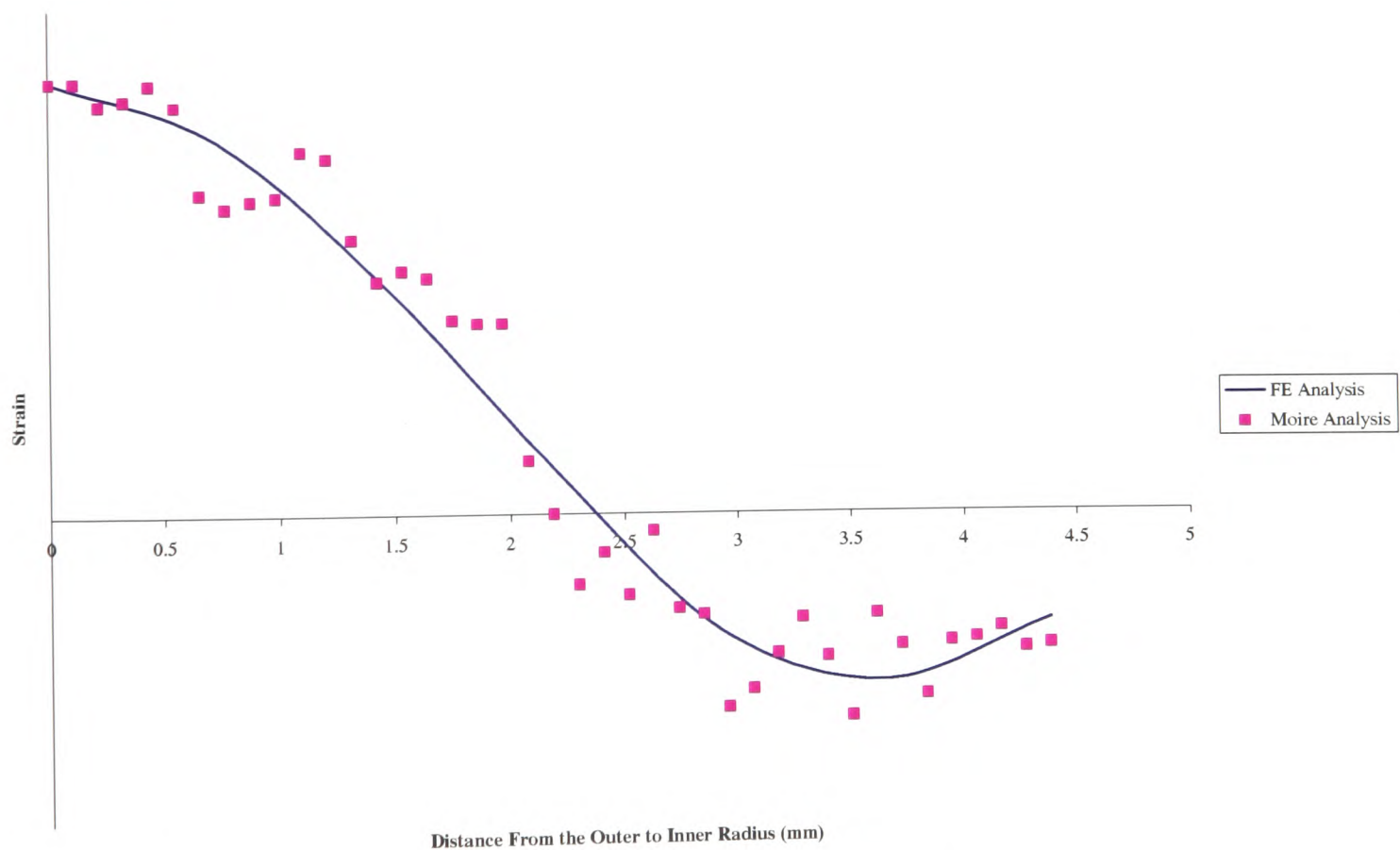


Figure 6.10 NCB6, The Interlaminar Shear Strain Distribution.

Thus, only the 916W specimen was analysed for this specimen type. Figure 6.8 shows the position of the data points collected from the specimen. The strain distribution profiles agrees well with the FEA results (Figures 6.9 - 6.11) in spite of being subjected to the same load limitation as seen with the PCB specimen due to the restriction placed on the deformation of the aluminium grating. Additionally, collecting the data from the Moiré analysis was difficult due to both the curvilinear geometry and the data output format (described in Section 6.2) which consisted of an array of 256 by 256 data points. Because of the size of this array, it was necessary to filter the data in order to reduce the number of points. This process reduced the resolution of the data and, in turn, led to greater errors between the FEA and Moiré results.

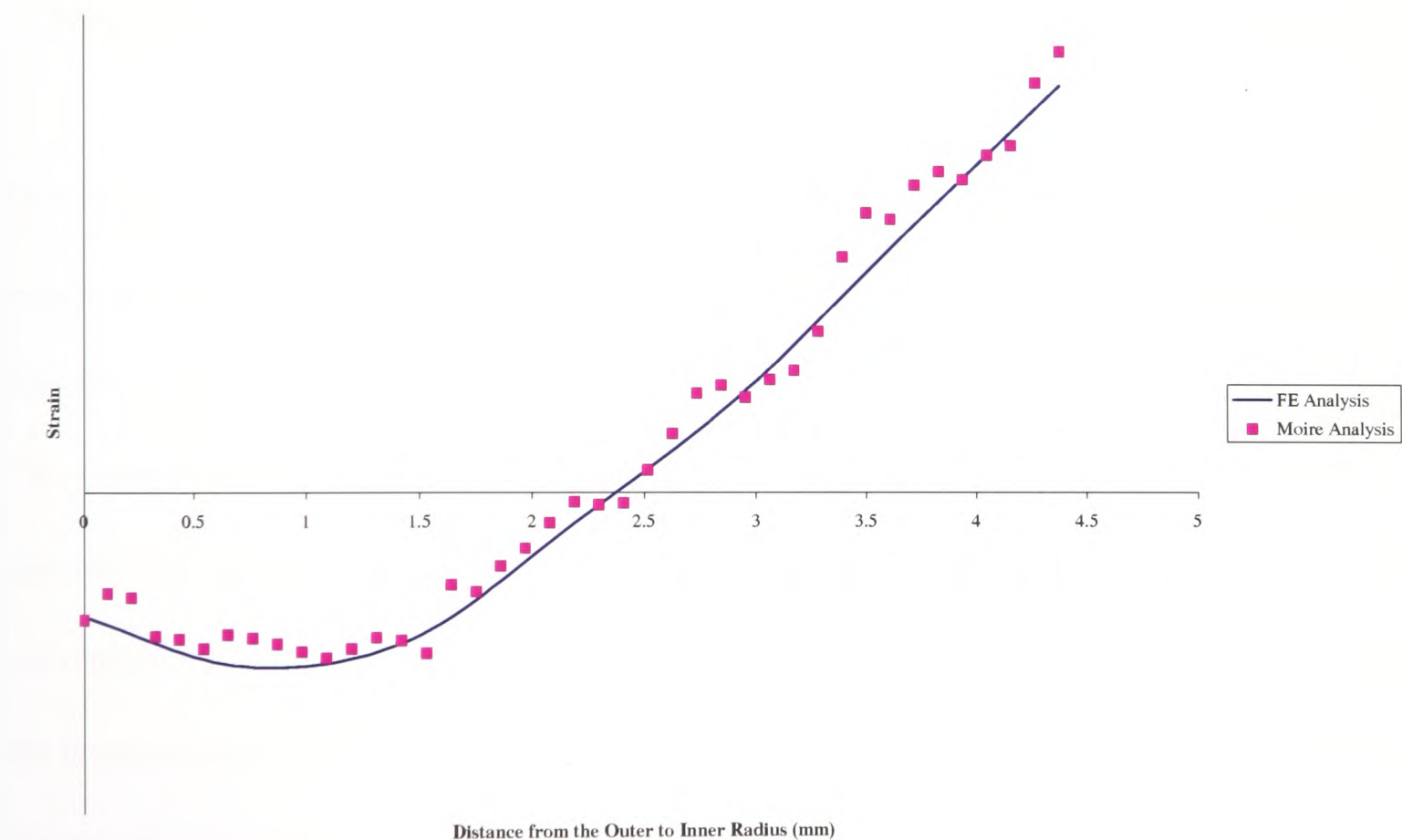


Figure 6.11 NCB6, The In-Plane Strain Distribution.

6.5.3 The Centrally Notched Curved Beam Specimen

As mentioned in Section 5.2, all of the FE models for the same material and specimen

type, in spite of varying thicknesses, use the same modelling functions. The FE models for each material of the same test thickness show almost identical stress distributions. Therefore, it was assumed that, if the stress distribution of one specimen configuration obtained via Moiré interferometry agrees well with the FEA results, this FE model would be equally valid for all specimen thicknesses. In this Moiré analysis, 4 mm thick CNB6 specimens were used and Figure 6.12 shows the location of the data points collected from the specimen.

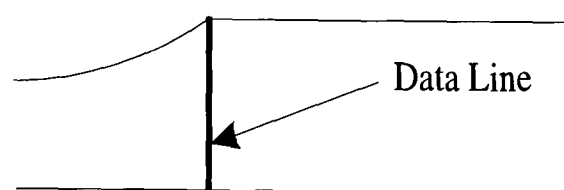


Figure 6.12 CNB, The Location of the Strain Distribution Data Used for Comparison.

One major difficulties experienced during the Moiré analysis was the high elastic deformations which resulted in the test section moving beyond the focus zone of the image objective lens. Therefore, the specimen was loaded to the limiting point at which focus could be achieved. This caused the difference in the magnitude of the in-plane and the interlaminar stresses, as discussed in Section 6.5, to become more pronounced. It may be seen from in Figure 6.13 the interlaminar through-thickness strain distribution was not clear due to insufficient load. Since the in-plane and interlaminar shear strain distributions (Figures 6.14 and 6.15) showed good agreement with the FEA results, it is reasonable to assume that, if sufficient load could be achieved, the interlaminar through-thickness strain would also yield an improved correlation. However, redesigning the optical system in order to increase the focus zone was not attempted due to constraints upon time and resources.

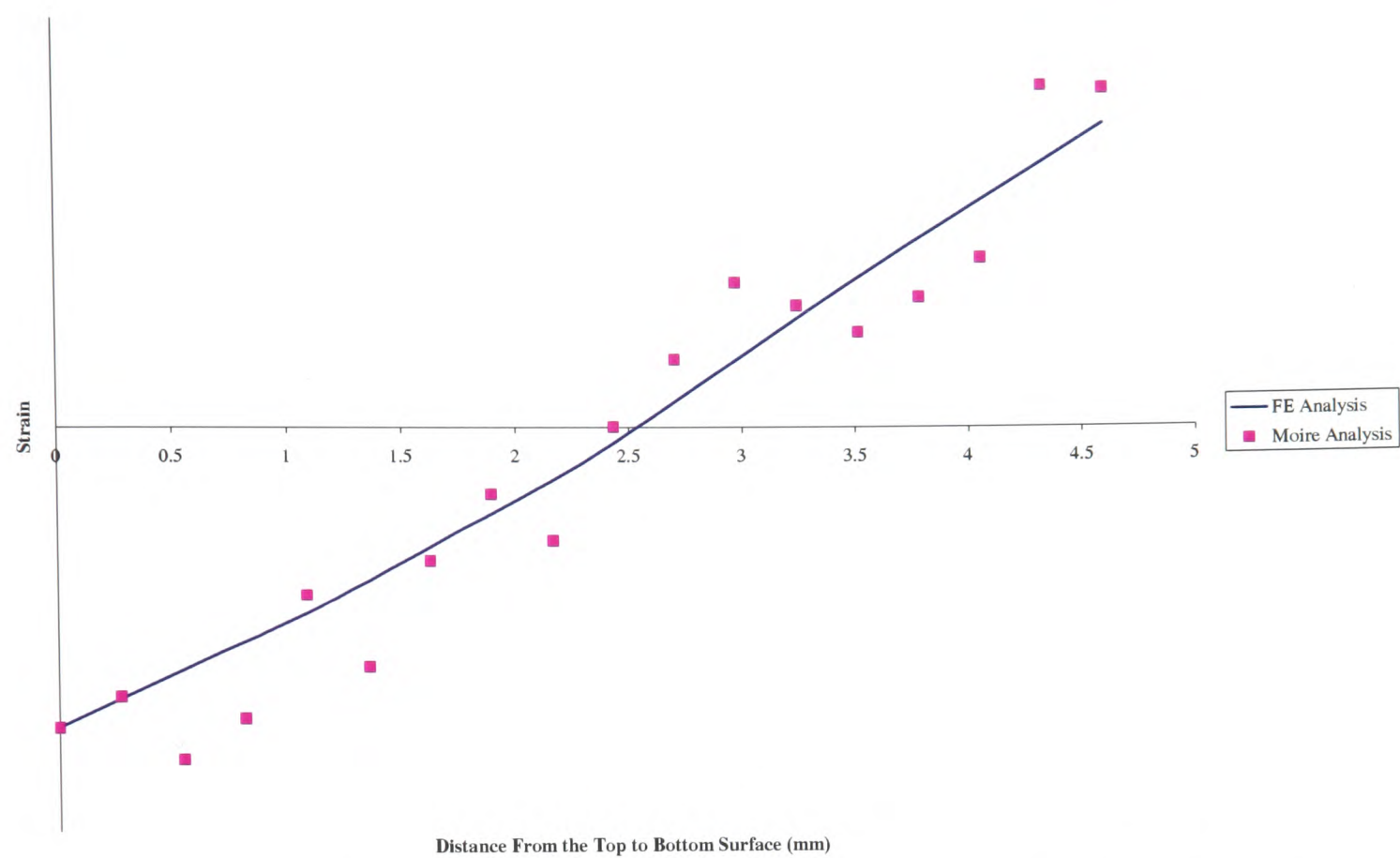


Figure 6.13 CNB6, The Interlaminar Through-Thickness Strain Distribution.

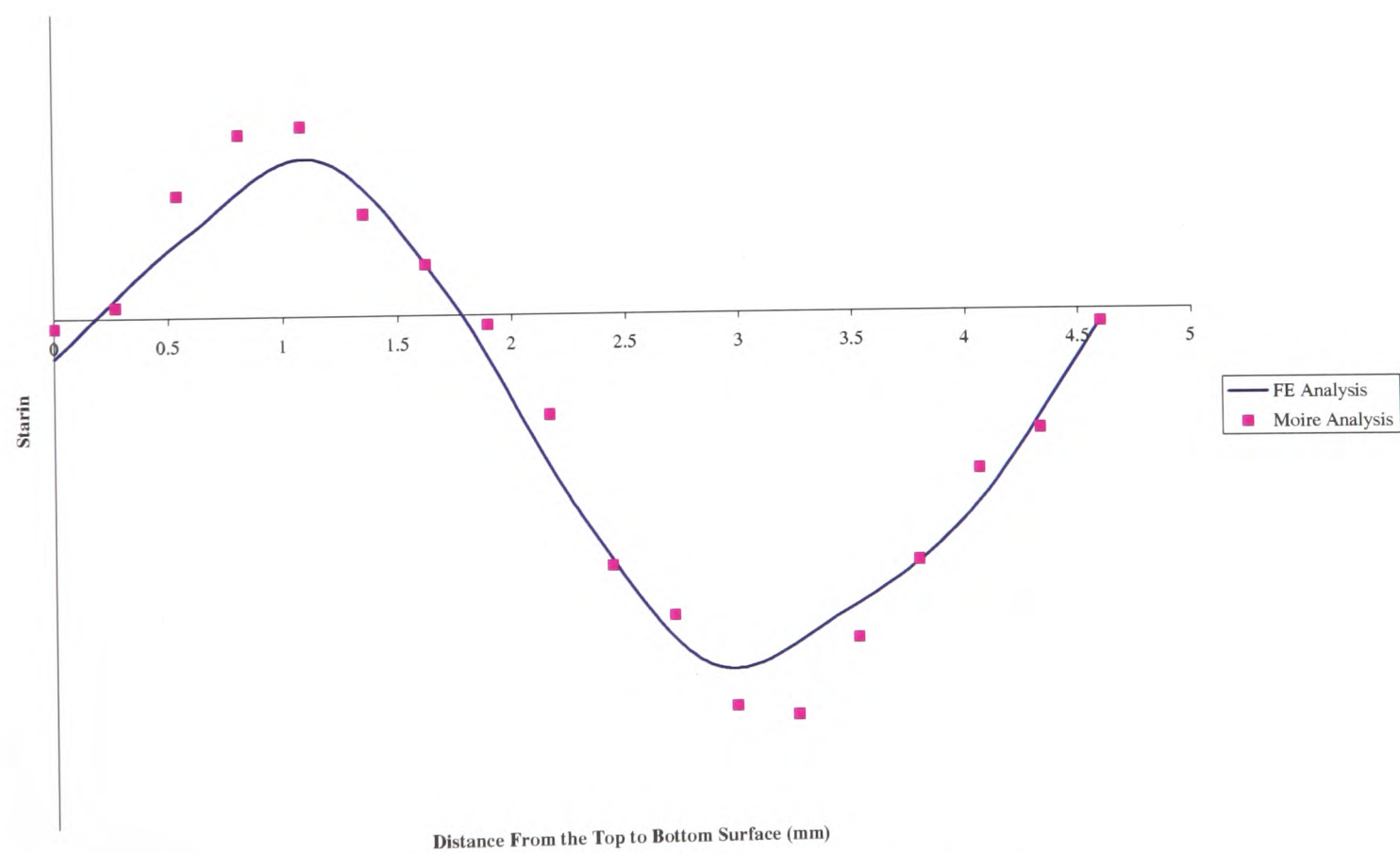


Figure 6.14 CNB6, The Interlaminar Shear Strain Distribution.

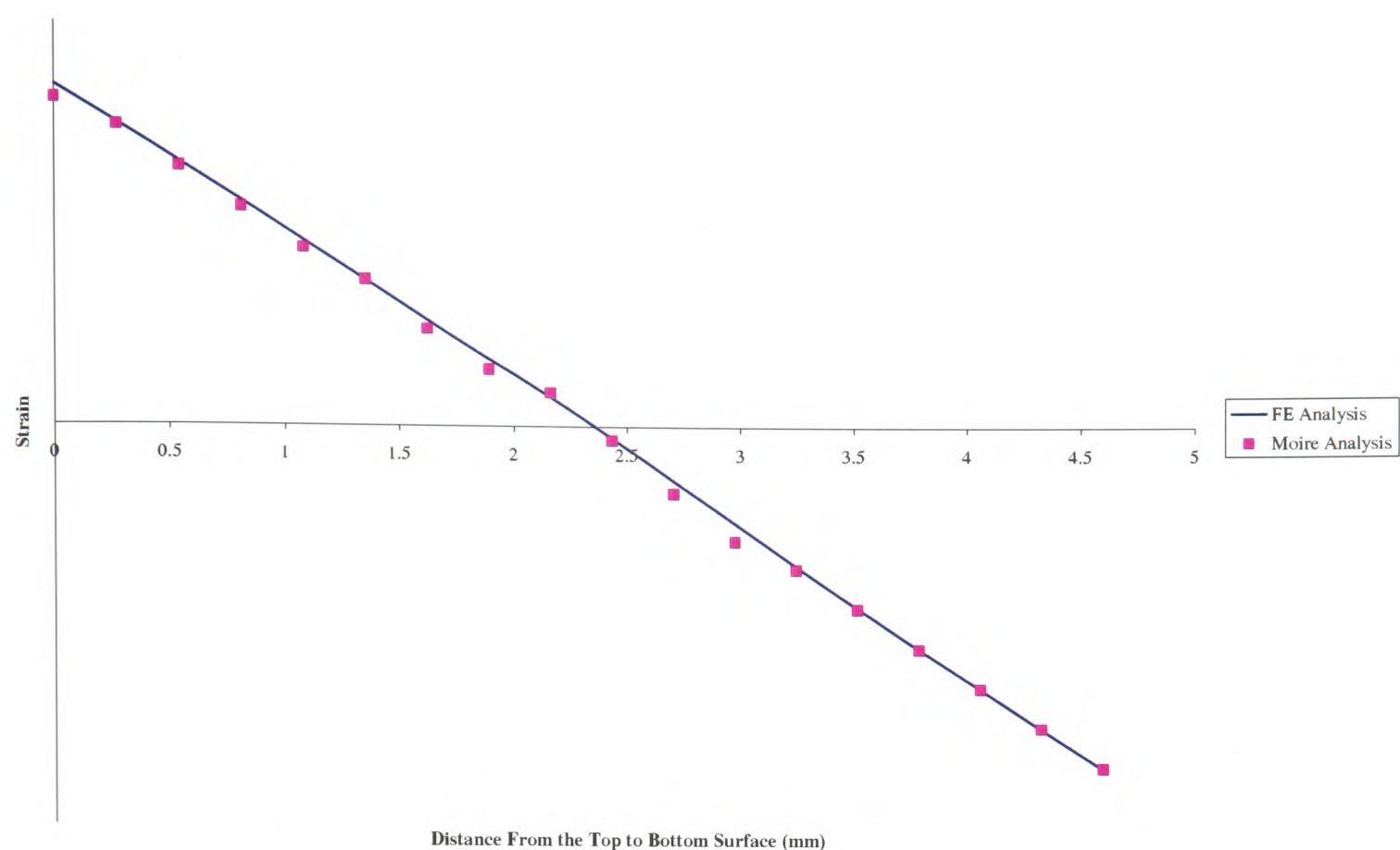


Figure 6.15 CNB6, The In-Plane Strain Distribution.

References

6.1 Kobayashi A. S., Hand Book on Experimental Mechanics, Society for Experimental Mechanics Inc., Prentice-Hall Inc., 1987.

6.2 Greivenkamp J.E. and Bruning J.H., Phase Shifting Interferometry, Optical Shop Testing, 2nd Edition, Ed. Malacara D., John Wiley & Sons Inc., 1992.

6.3 Poon C.Y., Kujawinska M. and Ruiz C., Automated Fringe Pattern Analysis for Moiré Interferometry, Experimental Mechanics, Vol.33, No.3, 1993, p234-241.

6.4 Takeda M., Ina H. and Kobayashi S., Fourier Transform Method of Fringe Pattern Analysis for Computer based Topography and Interferometry, Journal of Optics Society of America, Vol.72, 1982, p156-160.

Chapter 7

Correlation and Interpretation of Results

7.1 Introduction

The scanning electron microscopic (SEM) fractographs previously taken from the Iosipescu, Plain Curved Beam, Notched Curved Beam and Centrally Notched Beam specimens are discussed in this chapter in an attempt to correlate the macroscopic failure mechanism to the microscopic failure mechanism. The interlaminar tensile and interlaminar shear strength values obtained experimentally are also correlated with those in the available literature.

Most of the existing strength information is based upon the uniaxial stress state, whereas practical applications invariably involve more complex stress fields. In view of this, two-dimensional and three-dimensional failure maps were developed. The two-dimensional failure map shows clearly the effect of interactions between the interlaminar tensile and the interlaminar shear stresses. This two-dimensional failure map was subsequently expanded to three-dimensional space in order to include stress interactions at failure between interlaminar

stresses and in-plane stresses.

7.2 Analysis of the Failure Surface

The Iosipescu specimen was analysed in order to confirm that the failure was due to pure shear. This would confirm the validity of the Iosipescu test when using dovetail clamps. Two different methods were employed in this investigation, direct and indirect, as described in Section 3.5. The Notched Rectangular Prism and Oblique Rectangular Prism specimen configurations are the direct methods which create a relatively uniform stress field at the failure section. The Plain Curved Beam, Notched Curved Beam, and Centrally Notched Beam specimens are the indirect methods which usually utilise bending to create dramatically non-uniform stress distributions at the failure surface. Unlike the Notched Rectangular Prism and Oblique Rectangular Prism tests, the stresses in the Notched Curved Beam and Centrally Notched Beam tests were not defined analytically. In view of this, and in order to validate the results obtained from the FE analyses, SEM analyses were conducted on these specimens.

The morphology of interlaminar fractures in UD graphite/epoxy composites has been characterised by Arcan *et al.* [7.1] in order to correlate characteristic fractographic patterns for different failure modes. The tests were conducted with a specially designed loading system which allowed the induction of Mode I and II failure either in isolation or in combination. It was found from the SEM fractographs that the fibres were covered with a film of matrix material and that the matrix fracture surface consisted of hackles resulting from interfacial tensile or interfacial shear failure. The density of the hackles increased from Mode I to II. Similar work using UD graphite/epoxy laminates [7.2, 7.3] also showed that, for Mode I, the fracture surface was relatively smooth and featureless whilst, for Mode II, the surface showed numerous inclined hackles of epoxy oriented in the shear direction. Mixed mode failures

showed good continuity between the two extremes, i.e. the number of hackles increased with the shear loading. Although all of the work described above is based upon the unidirectional lay-up configuration, it was found that the failure surface for the cross-ply lay-up configuration still resembles that of a unidirectional laminate [7.4].

7.2.1 The Iosipescu Specimen

Figure 4.12 shows the failure surface for a cross-ply lay-up configuration. Numerous hackles can be observed in the matrix between the fibres. The fibres themselves display smooth surfaces which indicates poor bond strength between the fibre and matrix materials. However, the failure surface for the quasi-isotropic lay-up configuration, as shown in Figure 4.13, exhibits a thin matrix film attached to the fibres with numerous hackles clearly visible. The failure surface for a woven laminate is shown in Figure 4.14 and is similar to that shown in Figure 4.13 with the addition of broken fibres which are misaligned with the rest of the lamina. This implies that there is an interaction between the fibres due to the woven fabric architecture. The fractographic pictures match those previously discussed for interlaminar shear failure (Mode II). We may, therefore, conclude that the dovetail clamps do produce pure shear at the V- notched test section.

7.2.2 The Plain Curved Beam Specimen

The failure surface for the cross-ply lay-up configuration is shown in Figure 4.34 and has hackles which point in a direction normal to the viewing plane. This indicates that the matrix failed due to interlaminar tension (Mode I) induced by the in-plane compressive stress. As discussed in Section 5.4.2 the large in-plane compressive stress caused the outer 0° ply (37^{th} or 39^{th}) to buckle and separate from the adjacent 90° ply. Figure 4.36 shows the failure surface

of the quasi-isotropic lay-up configuration. Unlike Figure 4.34, this depicts a rough surface in the troughs previously occupied by fibres. The right corner of the picture shows a portion of the matrix material which bonded these two adjacent laminae. This is indicative of, a) high interfacial bond strength forcing tearing of the matrix material and b) classical matrix failure through the matrix rich layers between fibre bundles. The lower fibre volume fraction of PCB4 in comparison with that of PCB3 is further evidence of the presence of matrix rich layers (Table 4.2). Although Figures 4.34 and 4.36 show different failure surface characteristics, the location of the maximum interlaminar tensile stress in the FEA correlates well with the experimentally observed failure positions. This clearly indicates Mode I failure in spite of the poor correlation of the SEM results with those in the established literature.

7.2.3 The Notched Curved Beam Specimen

Figure 4.35 shows a mixed mode failure surface for a cross-ply lay-up configuration. As discussed in Section 7.2 the density of hackles increases from Mode I to Mode II and, in view of this, this surface indicates that shear stresses were dominant over tensile stresses in this failure. The FEA results confirm that the magnitude of the interlaminar shear stress is greater than that of the interlaminar tensile stress at this location. Figures 4.37 and 4.38 show the failure surfaces for the quasi-isotropic and woven lay-up configurations respectively. These figures are inconclusive, a result which is considered attributable to the differences in the characteristics of the matrix materials. As previously mentioned in Section 7.2.2, Figure 4.36 depicts a purely interlaminar tensile failure (Mode I), however, Figure 4.37 shows more hackles than can be seen in Figure 4.36 which is indicative of the presence of interlaminar shear (Mode II). We may, therefore, conclude that Figure 4.37 depicts mixed mode failure.

7.2.4 The Centrally Notched Beam Specimen

The failure surface for the cross-ply lay-up configuration is shown in Figure 4.24. The fibres are covered with a film of matrix material and the failure surface consists of hackles resulting from mixed mode failure. The presence of both interlaminar shear and interlaminar tensile stresses correlate well with the FEA results which predict that the magnitude of the interlaminar shear stress is larger than that of the interlaminar tensile stress. Figure 4.25 depicts the failure surface for the quasi-isotropic lay-up configuration which shows smooth fibre surfaces indicating poor interfacial bond strength. In contrast, Figure 4.26, which depicts the failure surface for the woven lay-up configuration, shows a rough surface covered with inclined hackles indicating the presence of interlaminar shear stress. Also visible are broken fibres, some of which are well aligned with the parent lamina fibres and indicate the presence of in-plane tensile stress in the failure zone, others are misaligned with the parent lamina fibres and indicate the presence of fibre interactions due to the woven fabric architecture.

7.3 Correlation of the FEA Results with those in the Available Literature

Unlike carbon/epoxy composites, there is relatively little information in the available literature on the mechanical properties of glass/epoxy composites. This is especially so with respect to the availability of mechanical properties data in the through-thickness direction. Therefore, an attempt was made to validate the strength data obtained from this investigation by a) direct comparison with data available for the exact laminate configuration used or b) correlating the available data with similarities, i.e. the same matrix or fibre architecture. The FEA results for the average interlaminar shear stress and the average interlaminar tensile stress are tabulated in Table 7.1.

	Specimen Type	913UD	914NCF	916W
Interlaminar Shear (MPa)	IO	69.2	59.6	67.5
Interlaminar Tension (MPa)	PCB	35.3	22.5	-
	NRP	77.4	23.4	31.0

Table 7.1 The Average Interlaminar Stresses Estimated Using FEA.

7.3.1 The Interlaminar Shear Strength

In a previous investigation at the Oxford UTC using Iosipescu specimens, the average shear strength of the carbon/epoxy (T300/914) with a cross-ply lay-up configuration was found to be 60 Mpa [7.5]. Other studies using Iosipescu specimens reported that the interlaminar shear strength of carbon/epoxy (T300/934) with a unidirectional lay-up configuration was approximately 56 MPa [7.6]. Though this is not a direct comparison between identical laminate types, the carbon/epoxy (T300/914) results agree well with that of 914NCF. Since both of these laminates were made using the same matrix material and with the interlaminar shear strength being a test of matrix dominated properties, this indirect comparison was the closest available validation of the results of this research.

7.3.2 The Interlaminar Tensile Strength

Using the C-shape specimen configuration, Hognestad [7.5] tested T300/914 carbon/epoxy laminates and found that the average interlaminar tensile strength was 30 MPa. Hallet [7.7], who tested the same material using the cylindrical specimen configuration, found the strength to be 73.43 MPa. The interlaminar strength of flat rectangular specimens made from XAS/914 carbon/epoxy was found to be 83 Mpa [7.8]. Using the elliptical curved beam specimen configuration, Hiel *et al.* [7.9] obtained an average interlaminar strength of 107.1

MPa for T300/934 laminates. Cui [7.10] investigated YO227/SER300 woven glass/epoxy laminates using the C-shape specimen configuration and found the average interlaminar tensile strength to be 31.8 MPa.

The interlaminar strength obtained from the NRP3 tests was higher than that obtained from the PCB3 test. At the interface between plies of different orientation there is a mismatch in Poisson's ratios which gives rise to an increase in interlaminar tensile stress at, and near, the cross sectional surface of the laminate when subjected to in-plane tension [7.11]. It was thought that the PCB3 would be subjected to these free edge effects. However, the dominant failure zone was subjected to in-plane compression and indicates that the failure was most likely due to fibre buckling leading to delamination (Section 5.4.2).

The interlaminar strength obtained from the PCB4 tests agrees well with that obtained from the NRP4 tests. Hognestad [7.5] reported that the typical delamination location was between one third and one half of the thickness from the inner radius of the C-shape specimens. The delamination location of the PCB4 specimens obtained in this research was consistent with these findings. Also, the average interlaminar strength obtained herein agrees well with Hognestad's results.

Since the PCB6 specimen did not exhibit delamination failure, a comparison with Cui's results could not be drawn. Instead the interlaminar tensile strength obtained from the NRP6 specimen was compared with that of Cui's C-shape specimen. The NRP6 specimen yielded a similar interlaminar strength than that obtained by Cui.

7.4 An Evaluation of the Test Techniques Employed

The Iosipescu specimen configuration, utilising 'dovetail' clamps, has been shown to produce reliable and representative interlaminar shear strength data. Although the interlaminar

tensile strength data for carbon/epoxy laminates using the Plain Curved Beam specimen configuration have been found to be reliable, its applicability to glass/epoxy laminates is less convincing. In this investigation, the in-plane stresses are close to their maximum values due to the lower Young's modulus of glass fibre in comparison to that of carbon fibre. Consequently, the in-plane stresses dominate local matrix failure and magnify free edge effects. The Notched Rectangular Prism specimen configuration produced reliable interlaminar tensile strength data. For combined stress conditions, the Centrally Notched Beam specimen and Notched Curved Beam specimen configurations are found to be unreliable as the induced stress fields in the region of the notches are too complex to yield meaningful strength data. The Oblique Rectangular Prism specimen configuration produces reliable data for interlaminar strength under a combined state of stress and facilitates precise control over the interlaminar tensile stress to interlaminar shear stress ratio.

7.5 Establishing Failure Maps

Failure of thick laminated composites is very different from that of unidirectional laminates. When a thick laminated composite is subjected to in-plane loading, it is generally true that microscopic failures occur in one or more layers of the laminate prior to the final failure of the laminate into two or more pieces. These microscopic failures are arrested from catastrophic crack growth by the constraints of adjacent layers. However, if a thick laminate is subjected to a combination of in-plane and out-of-plane loading, the macroscopic theories discussed in Chapter 2 are not applicable. This is because these theories assume that each layer is in a state of plane stress, an assumption which is not compatible with the occurrence of interlaminar stresses in the through-thickness direction [7.12]. It must be emphasised that, whilst these stresses are called interlaminar, indicating that they act between layers, they are

not limited to the interface between layers. Stress continuity requires that they vary in a continuous manner through the thickness of all of the layers of the laminate [7.13].

7.5.1 The Two Dimensional Failure Envelope

Owing to the lack of simple yet representative lamination theories which realistically account for interlaminar stresses in the through-thickness direction. The empirical establishment of a failure envelope, which accounts for stress interactions at failure, was attempted using the data obtained in this investigation. The interlaminar and in-plane strength values are tabulated in Table 7.2. It was assumed that, with the cross-ply fibre configuration, the transverse fibres had no contribution to the longitudinal strength. Thus, we may consider an analogy between the cross-ply configuration and the unidirectional configuration. This analogy allows us to estimate the strength of the cross-ply lay-up by calculating a new fibre volume fraction ignoring the transverse fibres and using a linear estimation based upon the strength and fibre volume fraction for a unidirectional laminate. This simple analogy allows us to obtain a value for the axial strength using the manufacturer’s data and results in a strength estimated for the cross-ply lay-up configuration of approximately one half of the strength for a unidirectional lay-up.

	913UD	914NCF	916W	Cui [7.10]
Interlaminar Shear (MPa)	69.2	59.6	67.5	34.0
Interlaminar Tension (MPa)	83.0	25.0	31.0	31.8
In-Plane Tension (MPa)	622.0	552.0	498.0	473.0
In-Plane Compression (MPa)	501.0	450.0	479.0	388

Table 7.2 The Strength for Each Material.

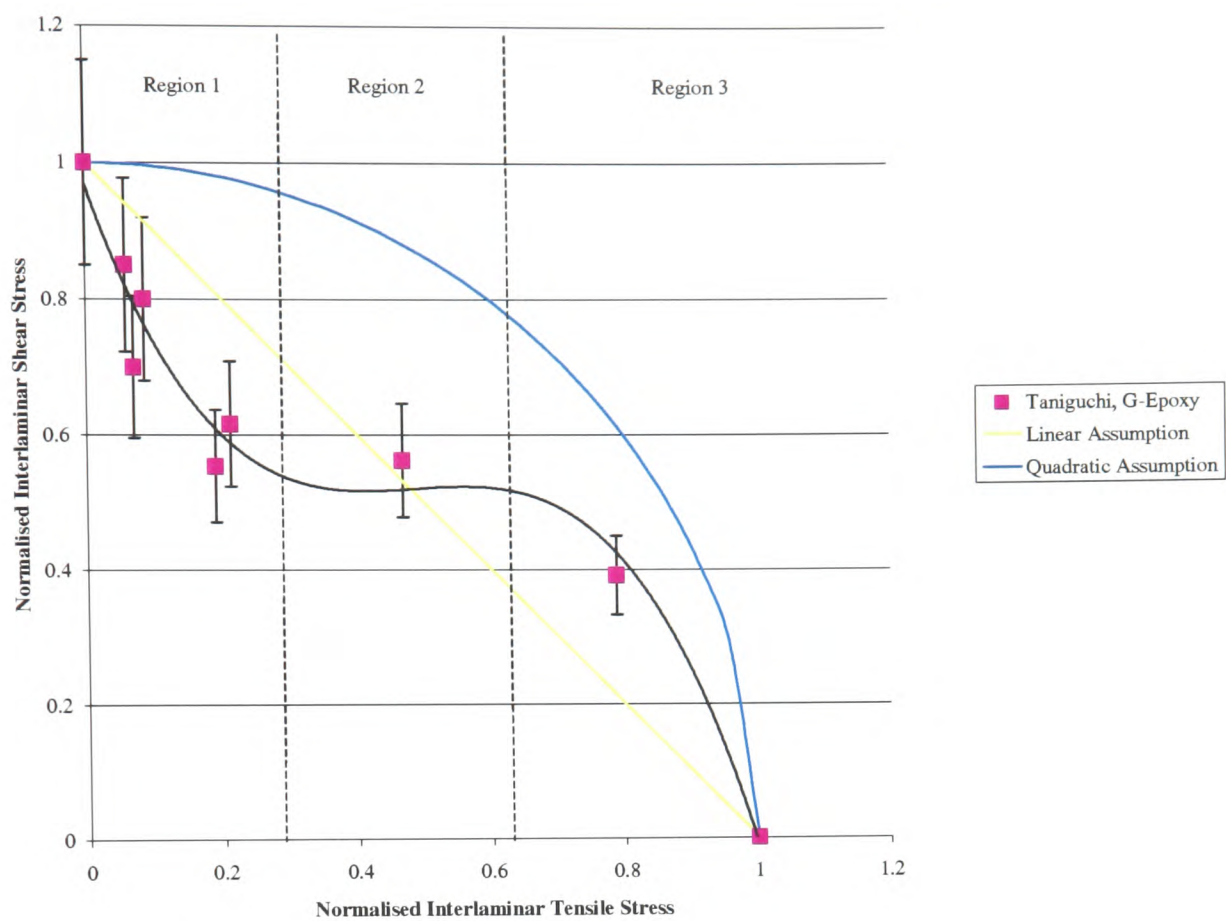


Figure 7.1 913UD, A 2-D Projection of the 3-D Failure Envelope.

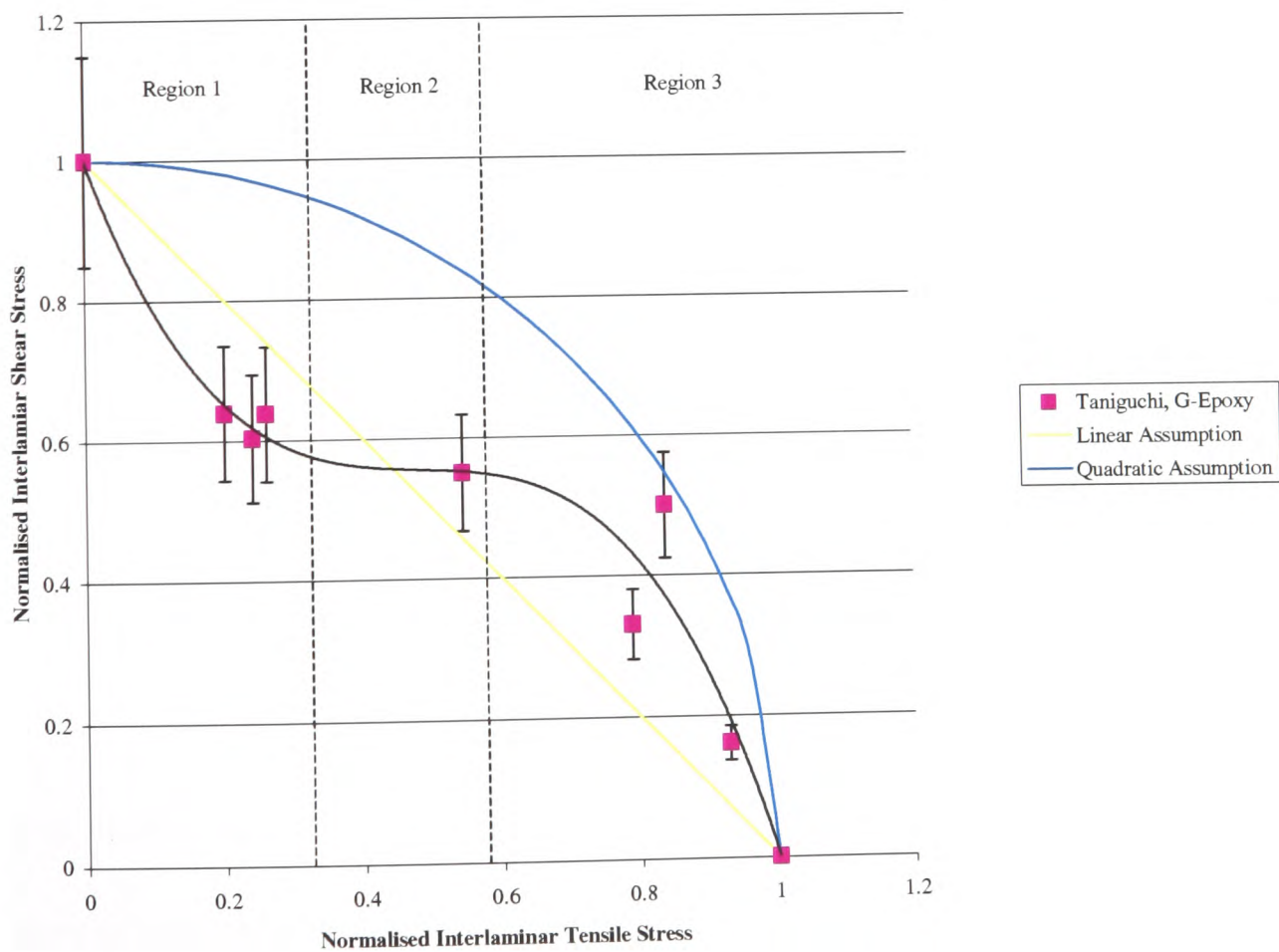


Figure 7.2 914NCF, A 2-D Projection of the 3-D Failure Envelope.

Figures 7.1 - 7.3 present failure envelopes for the 913UD, 914NCF and 916W material configuration respectively with the stresses normalised with respect to their maximum values. It is noteworthy that Figures 7.1 - 7.3 presents the projection of the three dimensional failure envelope data points onto the normalised interlaminar shear stress - normalised interlaminar tensile stress plane. Despite the different material configurations, Figures 7.1 - 7.3 yield similar polynomial curves. This indicates that the strength under combined interlaminar shear and interlaminar tensile stresses is not strongly related to the material lay-up configuration.

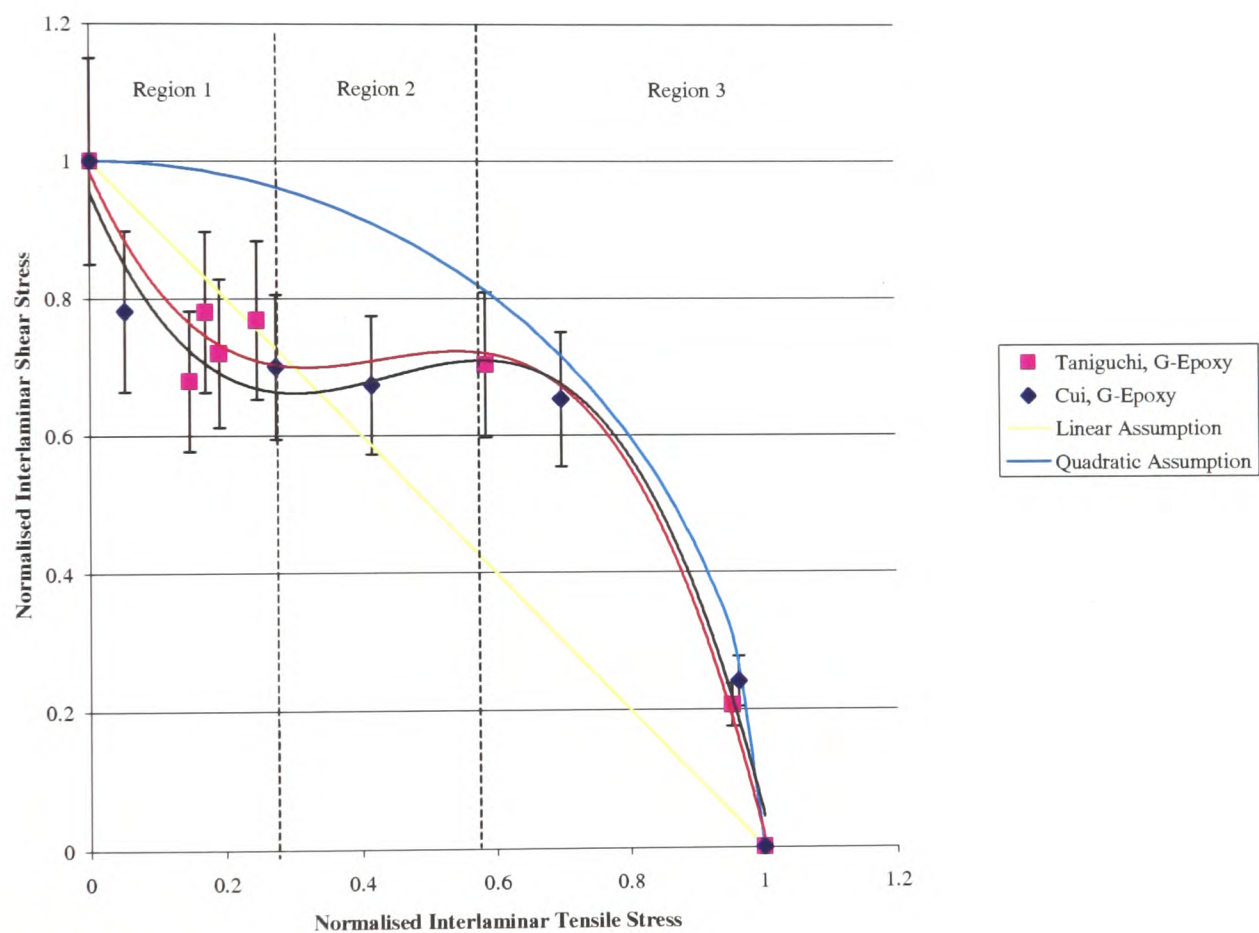


Figure 7.3 916W, A 2-D Projection of the 3-D Failure Envelope.

Cui used a different type of matrix material to that used in this research, though the plain weave fibre architecture is identical to that of the 916W material configuration. In spite of this, Cui’s results, tabulated in Appendix D, have been added to Figure 7.3 to allow comparison with the results of this research. Cui also used a specimen configuration similar

to the ORP specimen and tested oblique angles of 20° , 30° , 45° , 60° , 75° and 90° . These small increments of the oblique angle provided better resolution for the interlaminar failure strength envelope. It should be noted that Cui's failure envelope is almost identical to that for 916W. Therefore, we may conclude that this, highly non-linear result, is a real effect and is not due to experimental error.

In order to draw a comparison between existing theoretical approximations, i.e. linear and quadratic, two curves have been added to Figures 7.1 - 7.3 which have also been divided into 3 regions as indicated in the plots. The linear and quadratic assumptions are given by the equations:

$$\frac{\sigma}{\sigma^*} + \frac{\tau}{\tau^*} = 1 \quad (7.1)$$

$$\left(\frac{\sigma}{\sigma^*} \right)^2 + \left(\frac{\tau}{\tau^*} \right)^2 = 1 \quad (7.2)$$

where σ , σ^* , τ and τ^* are direct stress, ultimate strength, shear stress and ultimate shear strength respectively. Two distinct features can be observed from the linear assumption; a) in the first region, where the interlaminar shear stress is dominant, it is optimistic and b) in the third region, where the interlaminar tensile stress is dominant, it is conservative. The quadratic assumption represents a significantly more gross approximation with the exception of Figure 7.3. We can see from Figure 7.3 that the quadratic assumption only seems reasonably accurate beyond a normalised interlaminar tensile stress of approximately 0.7. However, even this limited correlation can not be confirmed without significantly more experimental data in this region. The large differences seen in Figures 7.1 - 7.3 are considered to be attributable to the complexity of the failure mechanism for thick composite materials in the through-thickness direction when subjected to combined interlaminar tensile and interlaminar shear stresses.

Let us define an ‘interlaminar interaction coefficient’, ξ , this being defined as the ratio of the interlaminar tensile stress to the sum of the interlaminar tensile and interlaminar shear stresses:

$$\xi = \frac{\sigma_t^i}{\sigma_t^i + \tau_t^i} \quad (7.3)$$

where σ_t^i and τ_t^i are the interlaminar tensile and interlaminar shear stresses respectively. Thus, for pure interlaminar shear $\xi = 0$ and for pure interlaminar tension $\xi = 1$. In Figure 7.3 the first region shows a decrease in the interlaminar shear strength from $\xi = 0$ to $\xi = 0.29$. The second region shows a plateau ranging from $\xi = 0.29$ to $\xi = 0.45$. Finally, the third region, in which the interlaminar tension becomes dominant, shows a steep decrease in the interlaminar shear strength ranging from $\xi = 0.45$ to $\xi = 1$. The ranges of the interlaminar interaction coefficient for each region and each material are tabulated in Table 7.3. When comparing 916W with 913UD and 914NCF, 916W shows superior interlaminar shear strength under combined stress conditions. It may be seen from Figure 7.3 that the interlaminar shear strength decreases dramatically beyond an interlaminar interaction coefficient of $\xi \approx 0.45$. Before this point, the interlaminar shear strength remains relatively insensitive to changes in the interlaminar tensile stress. This superior strength of 916W when subjected to combined stress conditions is due to the complex fibre architecture (plain weave) interacting with adjacent layers and, in turn, enhancing the interlaminar shear strength. Also, with reference to Table 7.2, we can see that, for a given fibre volume fraction, the cross-ply lay-up configuration (913UD) shows superior interlaminar shear strength when subjected to pure shear loading.

With reference to Table 7.3, the 913UD lay-up configuration shows superior interlaminar tensile strength under combined stress conditions when interlaminar tension is dominant (Region 3). This effect was thought to be due to the high fibre volume fraction and

the high interfacial bond strength between fibre and matrix. From Table 4.2 we may see that each 913UD plate had a fibre volume fraction over 0.6, in contrast with each 916W plate which had a fibre volume fraction of approximately 0.5. This fibre volume fraction leads to thinner matrix layers (Figure 5.1) and, in turn, yields a more uniform strain distribution across their thicknesses. Furthermore, this makes the interfacial bonding between fibre and matrix the limiting factor for interlaminar tensile strength. This is a mode of failure which has been observed in many polymer matrix composites [7.14] and which has also been seen in the SEM fractographs presented in Chapter 4. As a result, 913UD showed superior interlaminar tensile strength to that of 916W and 914NCF due to the higher fibre volume fraction. This effect also explains the difference in the interlaminar interaction coefficients between 914NCF and 913UD in region 3 of Figures 7.1 and 7.2.

	Region 1		Region 2		Region 3	
	Starts	Ends	Starts	Ends	Starts	Ends
913UD	0	0.35	0.35	0.54	0.54	1
914NCF	0	0.36	0.36	0.51	0.51	1
916W	0	0.29	0.29	0.45	0.45	1

Table 7.3 The Interlaminar Interaction Coefficient, ξ , for Each Material and Each Region.

7.5.2 The Three Dimensional Failure Envelope

In practice, a failure envelope which accounts for stress interactions at failure, requires at least a six dimensional stress space in order to account for the three in-plane stresses and the three interlaminar stresses. As discussed in Section 2.6, the failure of thick laminates is strongly influenced by interlaminar stresses. The interlaminar tensile and interlaminar shear

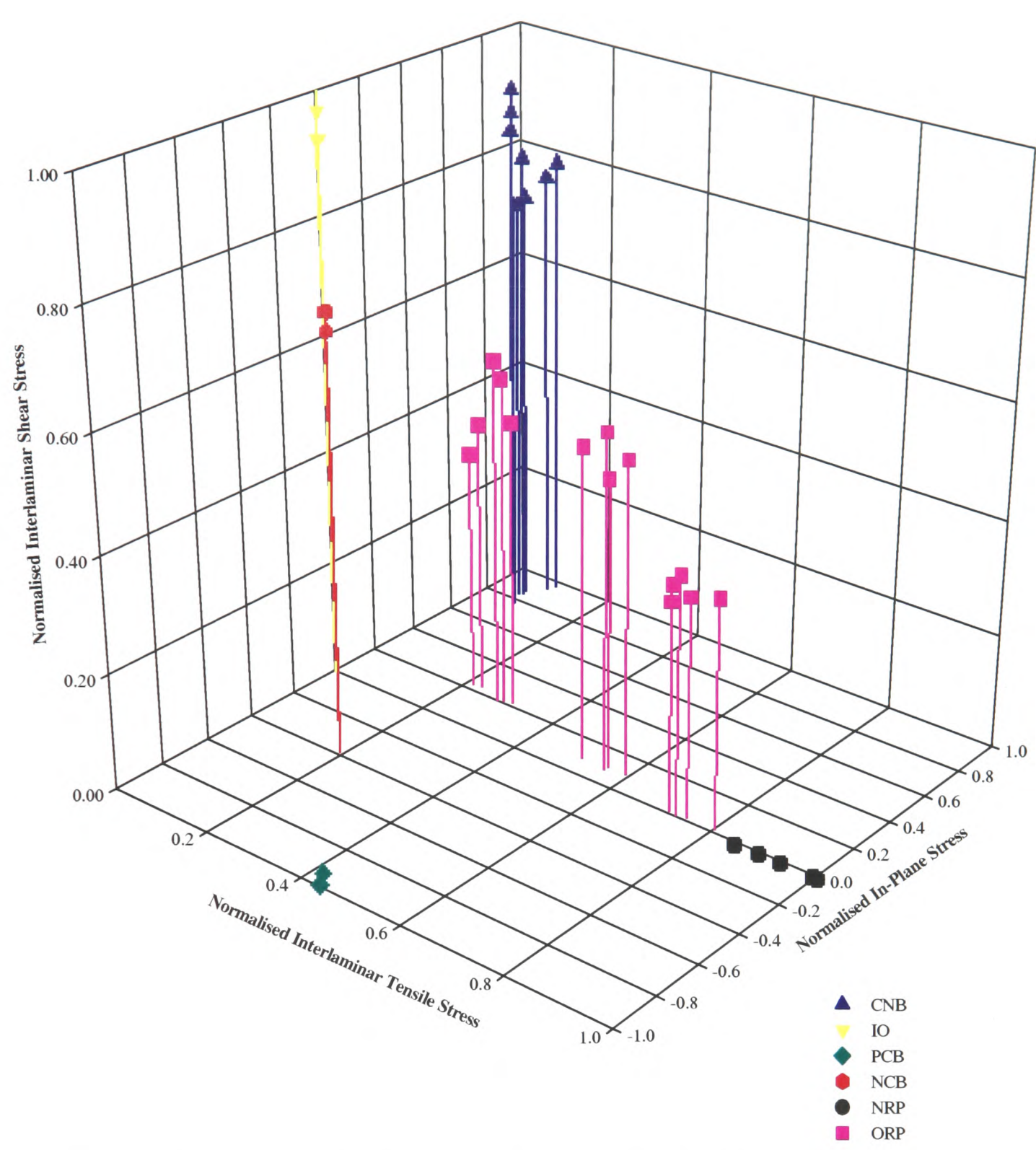


Figure 7.4 913UD, The Interaction Between Interlaminar and In-Plane Stresses.

stresses cause peeling and tearing respectively. These two interlaminar stresses lead to catastrophic delamination both in isolation and in combination. To characterise these effects, a two dimensional failure envelope has already been presented. However, Hognestad [7.5] discovered the effect of in-plane tensile stress in offsetting the in-plane normal stress and thus increasing the through-thickness strength. It was, therefore, decided to use the interlaminar tensile, interlaminar shear and in-plane tensile stresses to produce a three dimensional failure

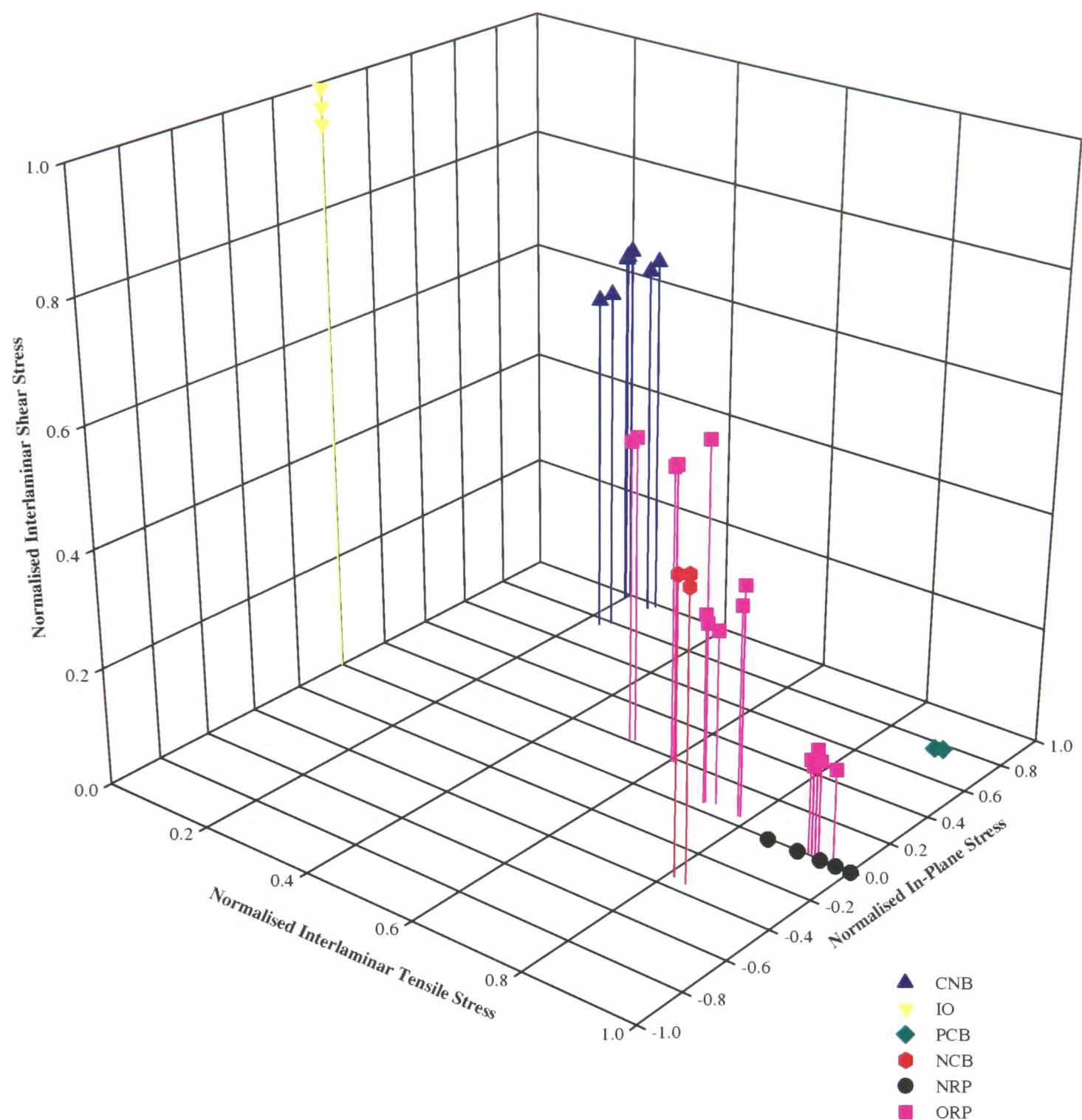


Figure 7.5 914NCF, The Interaction Between Interlaminar and In-Plane Stresses.

envelope. Figures 7.5 and 7.5 present part of the failure envelopes for the 913UD 914NCF and 916W material configurations respectively. The interlaminar tensile, interlaminar shear and in-plane tensile stresses are normalised with respect to their maximum values which have previously been presented in Table 7.2. Cui’s results are also included in Figure 7.6.

The CNB and PCB specimen configurations induced in-plane stresses due to their geometries. All CNB specimens exhibited failure dominated by interlaminar shear (Mode II)

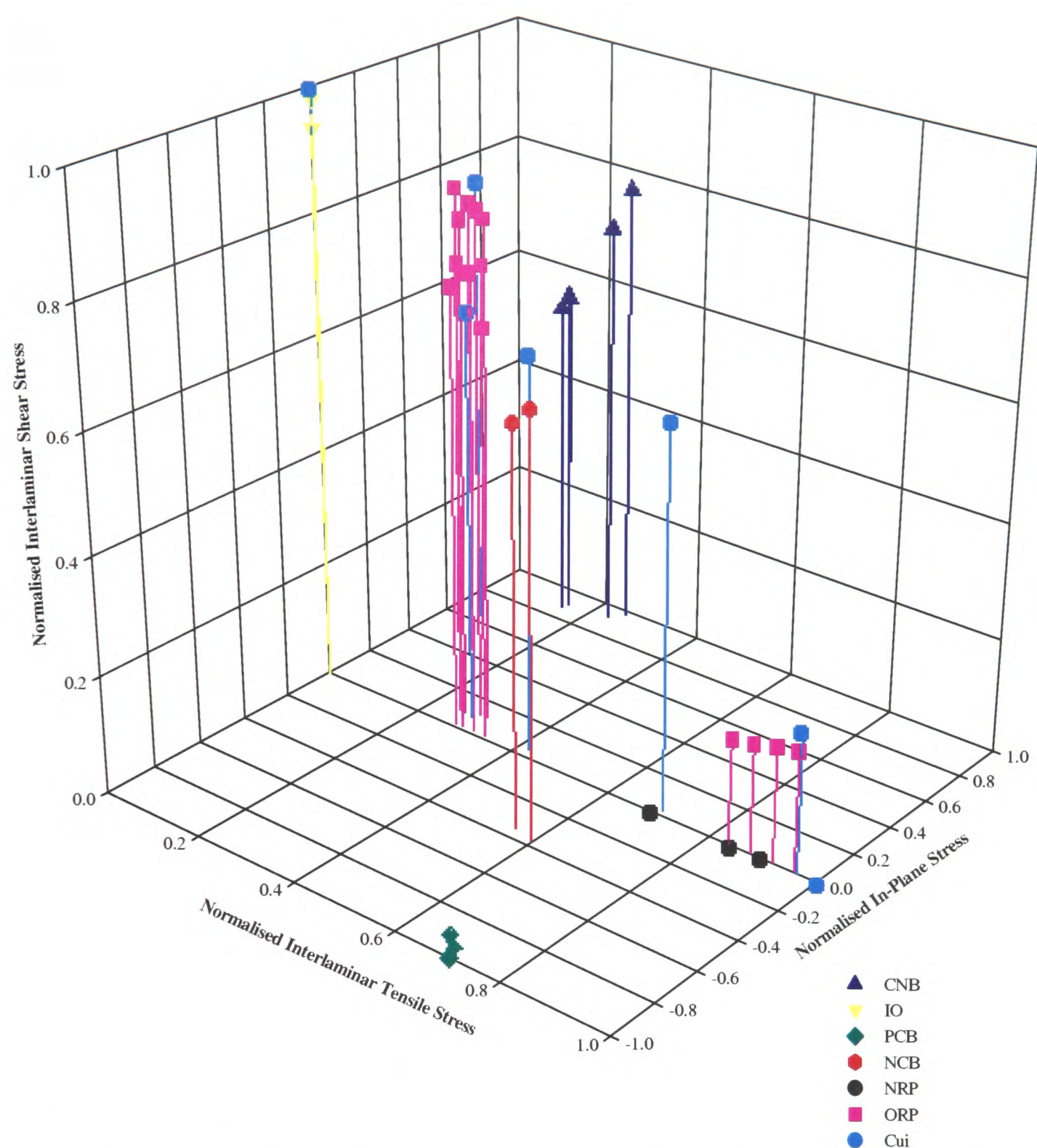


Figure 7.6 916W, The Interaction Between Interlaminar and In-Plane Stresses.

in spite of the presence of a large in-plane tensile stress. This was due to the presence of the notch and the large deformations of the specimens before failure which induced large interlaminar shear stresses. Figures 7.4 and 7.6 show that the failure of the PCB3 and PCB6 specimens were dominated by compressive in-plane stresses. The failure of the PCB4 specimen, Figure 7.5, was dominated by interlaminar tensile stress leading to delamination.

Since the magnitude of the in-plane stress is much smaller than that of the interlaminar

stresses in the NCB, and ORP specimen configurations, these two specimen configurations are relatively insensitive to interactions between these stress components. The low magnitude of the in-plane stress also dictates that any free edge effect will be small.

References

- 7.1 Arcan L., Arcan M. and Daniel I.M., SEM Fractography of Pure and Mixed Mode Interlaminar Fractures in Graphite/Epoxy Composites, Fractography of Modern Engineering Materials, Composite and Metals, ASTM STP 948, American Society for Testing and Materials, 1987, p41-55.
- 7.2 Hibbs M.F. and Bradley W.L., Correlations Between Micromechanical Failure Processes and the Delamination Toughness of Graphite/Epoxy Systems, Fractography of Modern Engineering Materials, Composite and Metals, ASTM STP 948, American Society for Testing and Materials, 1987, p68-75.
- 7.3 ESDU, Data Item 83025, Failure Modes of Fibre Reinforced Laminates, ESDU International plc., London, 1986.
- 7.4 Smith B.W. and Grove R.A., Determination of Crack Propagation Directions in Graphite/Epoxy Structures, Fracture of Modern Engineering Materials, Composite and Metals, ASTM STP 948, American Society for Testing and Materials, 1987, p154-169.
- 7.5 Hognestad G., Analysis of Composite Flange Connections and Determination of Interlaminar Strength, D.Phil Thesis, University of Oxford, 1993.
- 7.6 Adams D.F. and Walrath D.E., Further Development of the Iosipescu Shear Test Method, Experimental Mechanics, Vol.27, No.2, 1987, p133-119.
- 7.7 Hallet S., Small Specimen Impact Testing and Modelling of Carbon Fibre T300/914, D.Phil Thesis, University of Oxford, 1997.

- 7.8 Martin R.H. and Sage G.N., Prediction of the Fatigue Strength of Bonded Joints Between Muti-Directional Laminates of CFRP, Composite Structures, No.6, 1986, p141-163.
- 7.9 Hiel C.C., Sumich M. and Chappell D.P., A Curved Beam Test Specimens for Determining the Interlaminar Strength of a Laminated Composite, Journal of Composite Materials, Vol.25, 1991, p854-868.
- 7.10 Cui G.Y., Experimental Study of the Through-Thickness Strength of Laminated Composites, D.Phil Thesis, University of Oxford, 1994.
- 7.11 Kassapoglou C. and Lagace P.A., An Efficient Method for the Calculation of Interlaminar Stresses in Composite Materials, Journal of Allied Mechanics, Vol.53, 1986, p15-23.
- 7.12 Dong S.B., Pister K.S. and Taylor R.L., On the Theory of Laminated Anisotropic Shell and Plates, Journal of Aerospace Science, Vol.29, 1962, p969-975.
- 7.13 Reissner E. and Stavsky Y., Bending and Stretching of Certain Types of Heterogeneous Aelotropic Elastic Plates, Journal of Applied Mechanics, Vol.28, 1961, p402-408.
- 7.14 Nath R.B., Fenner D.N. and Galiotis C., Progression of Interfacial Failure in Kevlar 49 & Carbon Fibre Composites: Elasto-Plastic Finite Element Analysis, Proceedings, ECCM-7, Seventh European Conference on Composite Materials, Vol.1, 1996, p27-32.

Chapter 8

Conclusions and Further Work

8.1 Epilogue

In total, six tests, i.e. Iosipescu, Notched Rectangular Prism, Oblique Rectangular Prism, Centrally Notched Beam, Plain Curved Beam and Notched Curved Beam, have been conducted in an attempt to obtain parts of the through-thickness failure map. Three different material lay-up configurations, i.e. cross ply, quasi-isotropic ply and woven ply, were tested using these techniques in order to establish their strengths when subjected to interlaminar stresses. The use of the aforementioned test techniques helped to identify the interlaminar shear and interlaminar tensile strength of those laminate configurations both in isolation and in combination.

There is no existing standardised test protocol for either the interlaminar tension test or the combined interlaminar shear/interlaminar tension test. The design of the specimens was driven by the relevant test methods, i.e. direct and indirect. The direct methods create a

relatively uniform stress field at the failure section whereas the indirect methods usually utilise bending to create dramatically non-uniform stress distributions at the failure surfaces. In order to avoid any defects associated with the manufacturing processes for the specimens, special care was employed, i.e. periodic reconditioning of the grinding wheel, continuity of machine and operator for all specimens and continuous inspection of specimen integrity using a microscope and an ultrasonic C-scanner. This meticulous attention to detail was intended to minimise the levels of scatter in the results attributable to manufacturing damage and the experimental results showed correspondingly good consistency.

For the Iosipescu specimen, a modification to the experimental apparatus was developed which used a pair of dovetail clamps (Figure 4.6) to mechanically restrain the specimen. This new technique was verified experimentally via the use of photoelasticity which revealed the absence of any stress concentrations along the interface between the Iosipescu specimen and the dovetail clamps. This new technique facilitated the testing of small specimens cut from the same parent plate as the other specimen types and thus eliminated any errors associated with the manufacture of the requisite thicker plate, i.e. mismatch in the fibre volume fraction and the magnitude and distribution of residual stresses.

With the exception of the Iosipescu test, for which an analytical solution is available, all of the other tests had been solved numerically using the ABAQUS FEA package. The use of the ABAQUS FEA package, in conjunction with experimentally determined boundary and initial conditions as input, yielded the complete state of stress, strain and displacement. The FE models which were developed, were verified experimentally using the Moiré interferometry technique. More specifically, the strain distributions obtained via Moiré interferometry were compared with those of the FE analyses and found to correlate well. The accuracy of the FE models were estimated by comparing the experimentally obtained displacements at failure with

the FEA displacements at the failure load. All of the FEA results were conservative and the error bandwidth was found to average 5.2 %.

For the indirect test methods and the Iosipescu test, in order to correlate the macroscopic failure mechanism with microscopic failure mechanism, fractographic analyses were conducted using a SEM. In these fractographic analyses the stress components estimated using FEA were used as a basis for identifying the dominant failure stress acting at the failure surface. The features of each failure surface were then compared with those fractographs available in the literature and found to correlate well.

8.2 Conclusions

Cui [8.1] examined the behaviour of thick composite plates when subjected to interlaminar shear and interlaminar tensile stresses either in isolation or in combination and discovered the resulting characteristics to be highly non-linear. Cui felt that these results were the product of experimental errors. However, identical characteristics have been identified in this research (Figures 7.1 - 7.3) which leads to the conclusion that these characteristics must be real. Three independent material configurations exhibited almost identical failure envelopes in the interlaminar tensile vs. interlaminar shear stress plots. These failure envelopes consist of three regions; a) an initial steep decrease in the interlaminar shear strength as the interlaminar tensile stress increases from zero, b) a plateau region where the interlaminar shear strength is insensitive to increases in interlaminar tensile stress and c) a steep decrease in the interlaminar shear strength when the interlaminar tensile stress becomes dominant.

Of the three material configurations, the woven lay-up configuration exhibited the strongest resistance to interlaminar shear when subjected simultaneously to interlaminar tension. The cross-ply lay-up configuration exhibited the strongest resistance to interlaminar

tension when subjected simultaneously to interlaminar shear. The behaviour of the quasi-isotropic lay-up configuration was similar to that of the cross-ply lay-up configuration.

The most reliable and representative interlaminar shear strength data was obtained using the Iosipescu specimen configuration with dovetail clamps and is recommended by the author for use in subsequent research. This new technique enables testing of small specimen pieces cut directly from real structural composite components. It also eliminates both the production time for a thick test plate and the errors associated with imperfections and inconsistencies.

The Plain Curved Beam specimen configuration is not recommended as the in-plane stress dominates the failure and consequently free edge effects are magnified. Since the reliability of the experimental data is greatly dependent upon the quality of the manufacturing processes, the Centrally Notched Beam and Notched Curved Beam specimen configurations are not the most reliable test methods for obtaining the interlaminar strength of thick composites under combined stress conditions. Also, the induced stresses due to the presence of the notch are too complex to yield meaningful strength data.

Owing to their simple manufacturing process and good consistency of results, the Notched and Oblique Rectangular Prism specimen configurations are recommended for obtaining reliable interlaminar strength data. To obtain the interlaminar strength under combined stress conditions, the Oblique Rectangular Prism specimen offers a wide variety in interlaminar shear to interlaminar tension ratios.

The use of the sub-modelling technique, specific to the ABAQUS FEA package, was proven to be effective and efficient for heterogeneous models (sub-model). This technique enables the modelling of the actual geometry (global model) as a homogeneous body using bulk averaged anisotropic elastic properties with a local area of interest modelled exactly using lamina elastic properties, i.e. the orientation of each lamina and the number of laminae in the

through-thickness direction. Thus, this modelling method efficiently facilitates an accurate analysis of an anisotropic heterogeneous body.

8.2 Further Work

As stated in Section 2.6, the greatest deficiencies in current composite strength analysis are the lack of a simple yet representative lamination theory which realistically accounts for interlaminar stress and the quality and quantity of experimental data. The current investigation focussed mainly upon gathering high quality experimental data. It has also been demonstrated experimentally that the strength behaviour of thick composites when subjected to interlaminar stresses is highly non-linear. Further study of the interfacial bond strength between fibre and matrix under a combination of interlaminar shear and interlaminar tensile stresses may explain this phenomenon, in part or in whole, in terms of the microscopic failure mechanism. More empirical data is also essential and requires the continuation of the development of definitive test procedures for accurately quantifying the relevant material properties.

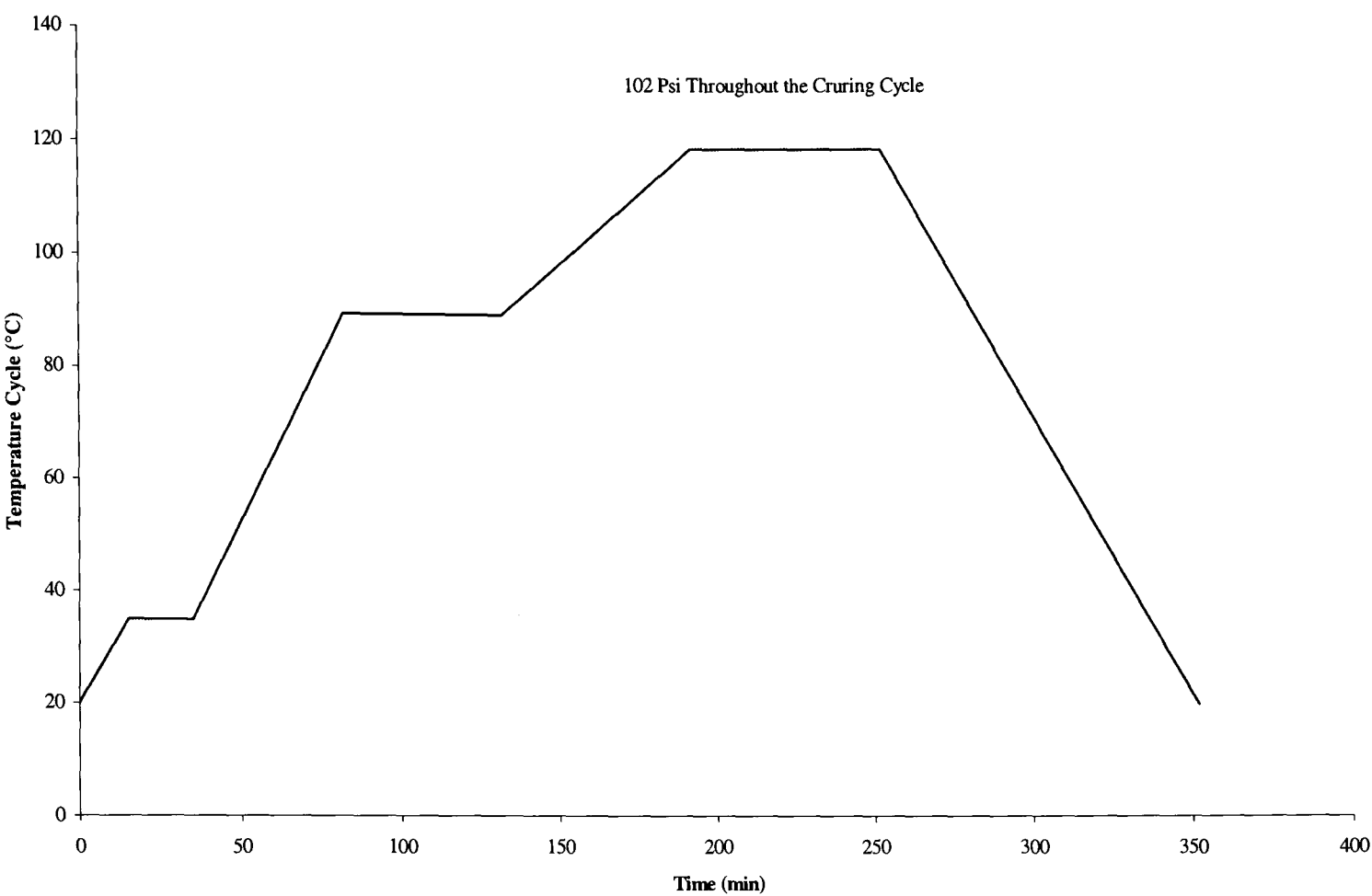
The quality and quantity of experimental data defining the interaction of in-plane and interlaminar stresses remains inadequate. The strength behaviour of thick composites when subjected to a combination of in-plane and interlaminar shear stresses has not yet been identified. This requires further investigation and the associated continued development of definitive test procedures for accurately quantifying the relevant material properties.

When a complete three dimensional failure envelope has been established, a representative lamination theory, accounting for both in-plane and interlaminar stresses, may be developed. If it is not found possible to define a general theory which explains, at a macroscopic level, the failure characteristics of thick composite materials when subjected to a combination of in-plane and interlaminar stresses, then significantly more work will be

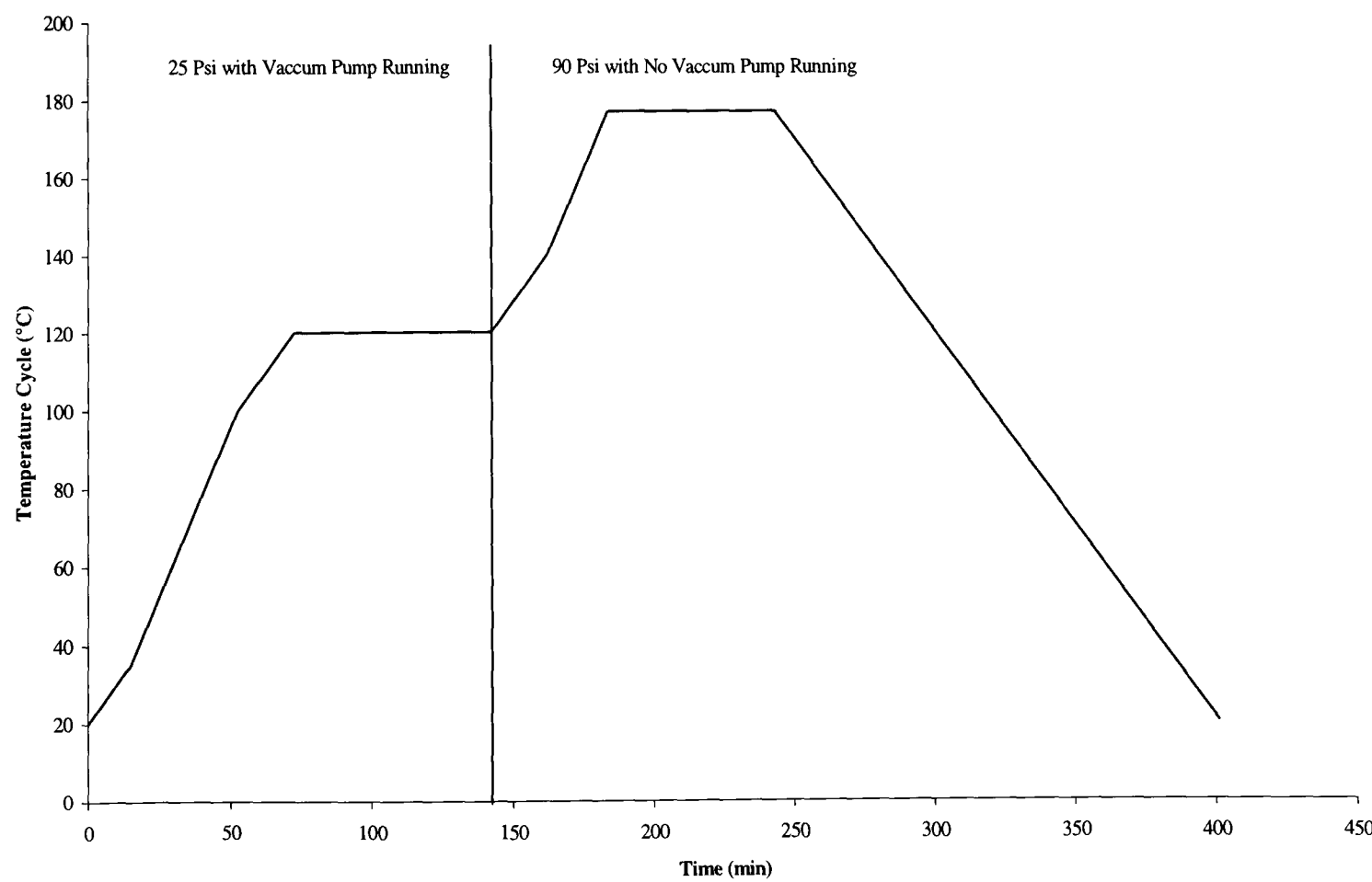
required in order to define the failure characteristics at a microscopic level.

References

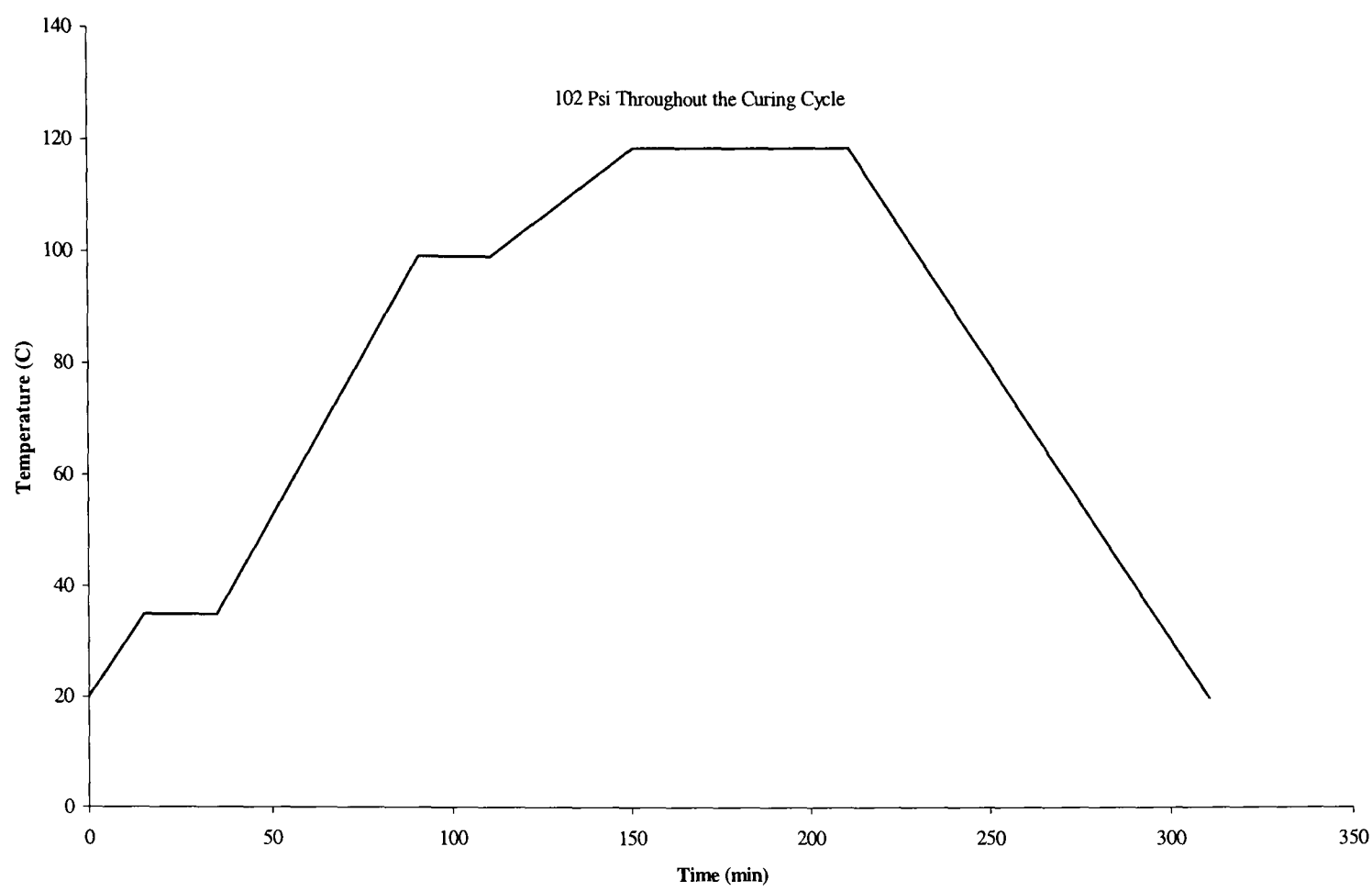
- 8.1 Cui G.Y., Experimental Study of the Through-Thickness Strength of Laminated Composites, D.Phil Thesis, University of Oxford, 1994.



The Recommended Autoclave Cycle for the 913UD Laminates.

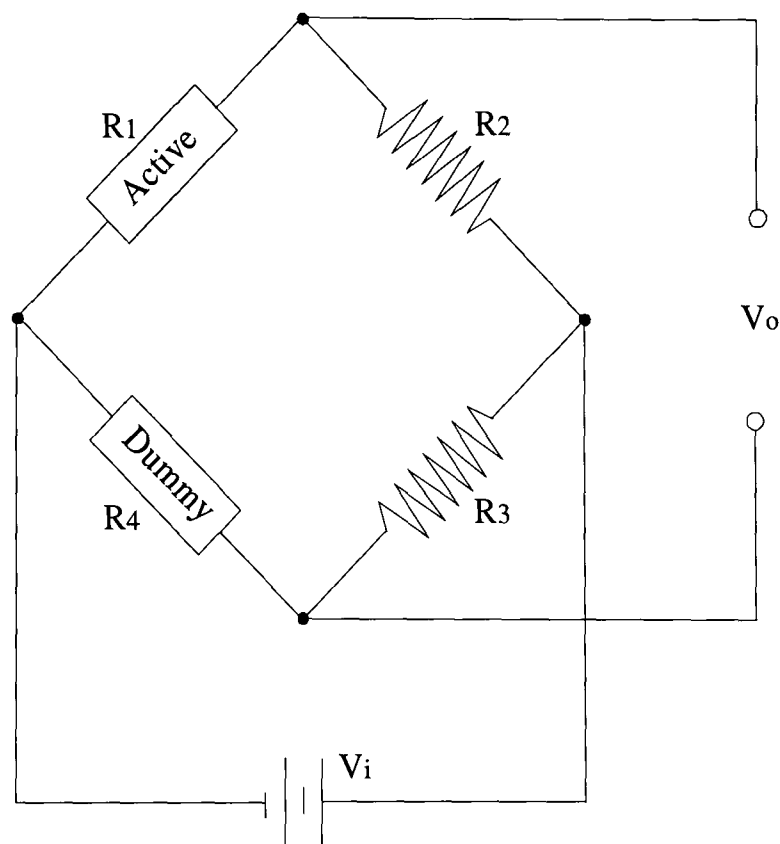


The Recommended Autoclave Cycle for the 914NCF Laminates.



The Recommended Autoclave Cycle for the 916W Laminates.

This half bridge arrangement uses two active arms R_1 and R_4 , with R_2 and R_3 as fixed resistors shown below. It provides temperature compensation for the single active gauge located in position 1 by using a dummy gauge in position 4 that is subjected to the same temperature as the active gauge. This requires that identical gauges be mounted on the same material and be subjected to the same temperature changes.



The Strain-gauge Arrangement in a Wheatstone Bridge.

The output equation of the above circuit is

$$\frac{V_o}{V_i} = \frac{G\varepsilon}{4 + 2G\varepsilon} \quad (b.1)$$

where V_o , V_i , G , and ε are the output voltage, bridge supply voltage, gauge factor and measured strain respectively. The bridge supply voltage was 5V and the gauge factors were 2.13 and 2.12 for the bidirectional and three-gauge rosettes respectively.

Figures c.1 and c.2 show the photoelasticity test configurations for a standard Iosipescu specimen and a dovetail clamped variation respectively. The specimens were placed between a set of polariscope lenses and were illuminated using white and sodium (Na) lights. Induced isocromatic fringe patterns were then recorded using a CCD camera.

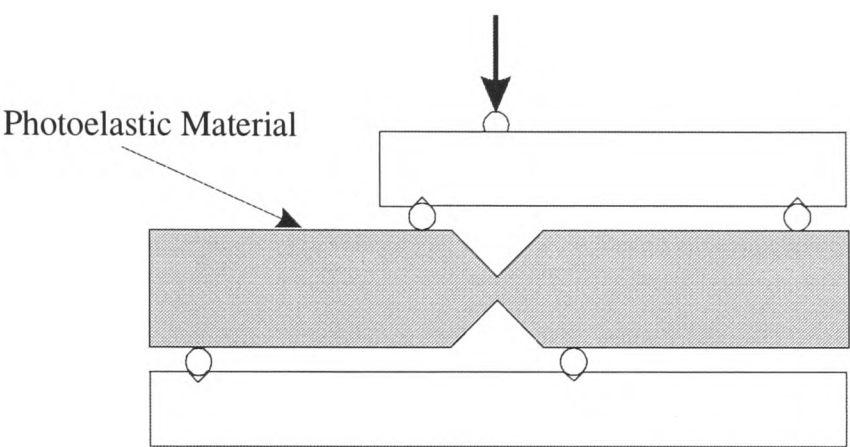


Figure c.1 The Photoelasticity Test Configuration for a Standard Iosipescu Specimen.

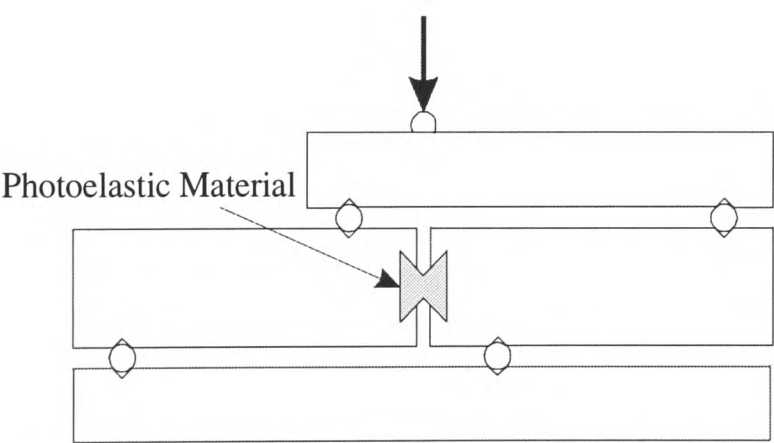


Figure c.2 The Photoelasticity Test Configuration for the Dovetail Clamped Variant.

Interlaminar Tensile Stress	Interlaminar Shear Stress	In-Plane Tensile Stress
13.2	22.9	39.6
22.2	22.2	22.2
30.6	8.2	2.2
31.8	0	0

Table d.1 Cui’s Average Results [d.1] for the NRO Specimen Type Configurations (MPa).

References

d.1 Cui G.Y., Experimental Study of the Through-Thickness Strength of Laminated Composites, D.Phil Thesis, University of Oxford, 1994