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I am submitting herewith a dissertation written by George Chennakattu Jacob entitled “Automotive Crashworthiness of Adhesively Bonded Carbon Fiber Polymer Composite Structures.” I have examined the final electronic copy of this dissertation for form and content and recommend that it be accepted in partial fulfillment of the requirements for the degree of Doctor of Philosophy, with a major in Polymer Engineering.

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Automotive Crashworthiness of Adhesively
Bonded Carbon Fiber Polymer Composite
Structures

**A Dissertation Presented
for the Doctor of Philosophy Degree
The University of Tennessee, Knoxville**

George Chennakattu Jacob

May 2006

Dedicated To My Mother

&

All Those Who Hope and Pray For My Well Being

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ABSTRACT

In passenger vehicles, the ability to absorb impact energy and be survivable for the occupant is called the “crashworthiness” of the structure. The ACC (Automotive Composite Consortium) has been and continues to be very interested in investigating the use of fiber-reinforced composites as crash energy absorbers. It would have been ideal if the composite structure to be used as a crash energy absorber were manufactured as an integral, monolithic component, but limitations in the present day manufacturing technology necessitate the presence of joints in composite structures.

While many scientists have investigated the energy absorption characteristics in various fiber reinforced composite materials, there is no literature available on the energy absorption and crushing characteristics of these materials when they are used in a bonded structure. The influence of having a bonded joint within the crush zone of a composite structure has not been adequately characterized in the past. After reviewing the existing literature and based on our own work done in automotive crashworthiness studies it can be concluded that investigating the strain rate dependence of fiber reinforced polymer composites and bonded structures made from them are also very important since the amount of energy they absorb and their performance properties vary with loading rate. The above is the last stage in crashworthiness research, where in one would like to determine how best fiber composite structures can be bonded together in the pursuit of designing the most crashworthy adhesively bonded automotive composite structure.

Hence, a comprehensive experimental methodology to analyze and design adhesively bonded automotive composite structures made of carbon fiber polymer composites to sustain axial, off-axis and lateral crash/impact loads is developed and strain rate effects on the crashworthiness of these bonded carbon fiber composite structures are studied. The experimental results from this work are being used to provide the building blocks for model developments – first the coupon level, then progressing in complexity to component level. Correlation with experimental results will provide the basis for which the analytical developments including development of constitutive laws, materials models, damage algorithms and new finite elements, are made.

TABLE OF CONTENTS

Chapter 1	Introduction and Literature Review	1
1.1	Introduction	1
1.2	Literature Review on Strain Rate Dependence of Energy Absorption in Polymer Composite Materials for Automotive Crashworthiness	7
1.2.1	Abstract	7
1.2.2	Introduction	7
1.2.3	Test Methodologies	8
1.2.3.1	Quasi-static Testing	8
1.2.3.2	Impact Testing	9
1.2.4	Crushing Modes and Mechanisms	9
1.2.4.1	Catastrophic Failure Modes	9
1.2.4.2	Progressive Failure Modes	10
1.2.4.2.1	Transverse Shearing or Fragmentation Mode	11
1.2.4.2.2	Lamina Bending or Splaying Mode	11
1.2.4.2.3	Brittle Fracturing	14
1.2.4.2.4	Local Buckling or Progressive Folding	14

1.2.5	Calculation of Specific Energy Absorption	17
1.2.6	Literature Survey	19
1.2.6.1	Strain Rate Effects on the Energy Absorption Capability of a Polymer Composite Material	20
1.2.7	Conclusion	24
1.3	Literature Review of the Effect of Loading Rate on the Fracture Toughness of Fiber Reinforced Polymer Composites	29
1.3.1	Abstract	29
1.3.2	Introduction	29
1.3.3	Literature Survey	30
1.3.3.1	Strain Rate Effects on Mode I Fracture Toughness of Fiber Reinforced Polymer Composites	30
1.3.3.2	Strain Rate Effects on Mode II Fracture Toughness of Fiber Reinforced Polymer Composites	35
1.3.3.3	Strain Rate Effects on Mixed Mode (I+II) Fracture Toughness of Fiber Reinforced Polymer Composites	38
1.3.4	Conclusion	39

1.4	Literature Review of Strain Rate Effects on the Tensile, Compressive, Shear, and Flexural Properties of Polymer Composite Materials	45
1.4.1	Abstract	45
1.4.2	Introduction	45
1.4.3	Literature Survey	47
1.4.4	Conclusion	55
1.5	Final Conclusions Drawn from the Literature Review	61
Chapter 2	Experimental	62
2.1	Project Objective	62
2.2	Materials	63
2.3	Substrate	64
2.4	Bulk Adhesive and Bonded Joints	66
2.5	Specimen Fabrication	77
2.6	Experimental Apparatus	84
Chapter 3	Results and Discussion	90
3.1	Chopped Carbon Fiber Epoxy Composites	90
3.2	Braided Carbon Fiber Epoxy Composite Substrate Material	128
3.2.1	Tension, Compression, and Shear	128
3.3	Epoxy Bulk Adhesive	137

3.3.1	Thermal Characterization	137
3.3.2	Tension and Compression	137
3.3.3	Shear	139
3.3.4	Fracture Toughness	143
3.4	Adhesive Joint	167
3.4.1	Single Lap Shear and Double Notch Shear	167
3.4.2	Fracture Toughness	174
3.4.2.1	Double Cantilever Beam Testing	174
3.4.2.2	Driven Wedge Test Method	181
3.4.2.3	End Load Split Test	207
3.5	Bonded and Unbonded Tube Structures	211
Chapter 4	Conclusions and Future Work	223
4.1	Conclusions	223
4.2	Future Work	224
	List of References	227
	Vita	245

LIST OF TABLES

Table 1	Summary of Published Data on the Effects of Loading Rate on Energy Absorption	26
Table 2	Summary of Published Data on the Effects of Loading Rate on Fracture Toughness Properties	40
Table 3	Summary of Published Data on the Effects of Loading Rate on Tensile, Compressive, Shear, and Flexural Properties	56
Table 4	Test Plan for the Carbon Fiber Reinforced Composite Substrate	67
Table 5	Test Plan for the Bulk Adhesive and Adhesive Joints	78
Table 6	Experimental Data from Tests Conducted with a Profile Block of Radius 0.635 cm at 0.5 cm/min Loading Rate on P4	100
Table 7	Experimental Data from Tests Conducted with a Profile Block of Radius 0.635 cm at 0.5 cm/min Loading Rate on HexMC	102
Table 8	Experimental Data from Tests Conducted with a Profile Block of Radius 0.635 cm at 0.5 cm/min Loading Rate on CCS-100	104
Table 9	Experimental Data from Tests Conducted	

	with a Profile Block of Radius 0.635 cm at 15.2 cm/min Loading Rate on P4	111
Table 10	Experimental Data from Tests Conducted with a Profile Block of Radius 0.635 cm at 762 cm/min Loading Rate on P4	113
Table 11	Fracture Toughness Data from Tests Conducted at 0.15 cm/min Loading Rate on HexMC	116
Table 12	Fracture Toughness Data from Tests Conducted at 0.15 cm/min Loading Rate on CCS150	117
Table 13	Fracture Toughness Data from Tests Conducted at 0.15 cm/min Loading Rate on CCS100	118
Table 14	Fracture Toughness Data from Tests Conducted at 0.15 cm/min Loading Rate on P4	119
Table 15	P4 4-Point Flexural Properties	123
Table 16	HexMC 4-Point Flexural Properties	124
Table 17	Fracture Toughness Data from Tests Conducted at 15.2 cm/min Loading Rate on P4	125
Table 18	Fracture Toughness Data from Tests Conducted at 762 cm/min Loading Rate on P4	126
Table 19	Shear Test Results of the Substrate Material	132
Table 20	Shear Test Results of the Bulk Adhesive	147
Table 21	ELS Mode II Fracture Test Results	214

LIST OF FIGURES

Figure 1	Fragmentation Crushing Mode	12
Figure 2	Splaying Crushing Mode	13
Figure 3	Brittle Fracturing Crushing Mode	15
Figure 4	Progressive Folding Crushing Mode	16
Figure 5	Typical Load Displacement Curve for a Progressively Crushed Composite Tube	18
Figure 6	Substrate Compression Test Specimen	65
Figure 7	An Untested SLS and DNS Test Specimen	68
Figure 8	DCB Specimen to Determine Mode I Fracture Toughness	70
Figure 9	ELS Specimen to Determine Mode II Fracture Toughness	71
Figure 10	Typical Dog Boned Bulk Adhesive Specimen used for Tensile Testing	72
Figure 11	Different Test Specimen Geometries used for Determining the Shear Properties of the Bulk Adhesive	74
Figure 12	Illustration of Compact Tension Specimen showing Dimensions for 8 mm Wide Specimens	75
Figure 13	An Untested and a Tested Bulk Adhesive Compact Tension Specimen	76
Figure 14	Sketch of the Experimental Plan	80
Figure 15	Schematic of the Adhesive Plaque Preparation	82

Figure 16	Schematic of the Bonded Joint Panel Preparation	85
Figure 17	Typical Dynamic Structural Test Capabilities at General Motors and Ford	87
Figure 18	Test Machine for Automotive Composites (TMAC)	88
Figure 19	Schematic of Test Fixture Design	92
Figure 20	Test Fixture Assembly	93
Figure 21	Roller Ways and Contact Profile Constraint	94
Figure 22	Constraint Conditions	95
Figure 23	Load Displacement Traces for the Various Random Chopped Fiber Reinforced Composites	98
Figure 24	Matrix Cracking	99
Figure 25	Specific Energy Absorption (SEA) in CCS 100, HexMC, and P4 Composites	106
Figure 26	Load Displacement Traces for a Test Conducted on P4 in the Loose Constraint Condition at Loading Rates of 0.5, 15.2, and 762 cm/min	109
Figure 27	Specific Energy Absorption (SEA) in P4 Composites at Loading Rates of 0.5, 15.2, and 762 cm/min respectively	115
Figure 28	Comparison of Fracture Toughness for HexMC, CCS150, CCS100, and P4 Composites	120
Figure 29	Comparison of Fracture Toughness for P4	

	Composites at Loading Rates of 0.15, 15.2, and 762 cm/min respectively	127
Figure 30	Typical Tensile Stress Strain Curve for the Substrate Material	129
Figure 31	Typical Compression Stress Strain Curve for the Substrate Material	130
Figure 32	Slack Adapter	133
Figure 33	Illustration of the Need for a Slack Adapter	134
Figure 34	Variation of Ultimate Tensile Strength of Substrate with Loading Rate	136
Figure 35	Differential Scanning Calorimetry Results for First and Second Heats	138
Figure 36	Typical Tensile Stress Strain Curve for the Bulk Adhesive	140
Figure 37	Typical Compression Stress Strain Curve for the Bulk Adhesive	141
Figure 38	Variation of Ultimate Tensile Strength of Bulk Adhesive with Loading Rate	142
Figure 39	Measured Shear Stress-Strain Response of the Solid Rod and Hollow Cylindrical Bulk Adhesive	144
Figure 40	Shear Stress-Strain Response of the V-notched, Solid Rod, and Hollow Cylindrical Bulk Adhesive	145
Figure 41	Modulus Determination of the Bulk Adhesive	146

Figure 42	Load Displacement Curves from Compact Tension Test using Large Slack Adapter	149
Figure 43	Simple Slack Adaptor Fabricated from a #2 Morse Taper Extension	150
Figure 44	Smooth Load Profile obtained for a String pulled in Tension at 1.0 m/sec using the Small Slack Adapter	151
Figure 45	Simplified Load Train used for Compact Tension Tests at Higher Loading Rates	152
Figure 46	Typical Load Displacement Response for Compact Tension Tests at Various Loading Rates	154
Figure 47	Comparison of Load Displacement Traces at the Highest Loading Rate	155
Figure 48	Measured Fracture Toughness Values as a Function of Crosshead Rate	156
Figure 49	Illustration of Typical Stress Whitening Extent for Range of Crosshead Rates	158
Figure 50	Illustration of Intersection of Crack Plane with Outer Edge of Specimen showing Crack Plane Rotation at Higher Crosshead Rates	159
Figure 51	Summary of Compact Tension Tests Conducted at Subambient Temperature Levels at Various Crosshead Rates	160

Figure 52	Variation of Fracture Toughness with Crosshead Rate at Room Temperature and Subambient Temperature Testing	161
Figure 53	SEM Image of the Fracture Surface of a Compact Tension Specimen Tested at a Slow Rate of 10^{-5} m/s	162
Figure 54	SEM Image of the Fracture Surface of a Compact Tension Specimen Tested at an Intermediate Rate of 0.01 m/s	163
Figure 55	SEM Image of the Fracture Surface of a Compact Tension Specimen Tested at the Highest Rate of 1 m/s	164
Figure 56	SEM Image of the Fracture Surface of a Compact Tension Specimen Tested at the Highest Rate of 1 m/s Showing the Formation of a Lip	165
Figure 57	Load Displacement Traces at Different Loading Rates for an SLS Test Specimen	168
Figure 58	Variations in Shear Strength with Loading Rate for the SLS and DNS Test Specimens	170
Figure 59	Variation of Failure Load for SLS Test Specimens with Bond Thickness	171
Figure 60	Failure in the Joints	172
Figure 61	Different Load Trains used for SLS and DNS Tests at Higher Loading Rates	173
Figure 62	Comparison of the Load Displacement Responses Recorded from Different Test Setups for SLS and DNS Tests	175

Figure 63	Variation of Fracture Energy with Loading Rate for the 11 ply DCB Specimens	176
Figure 64	Variation of Fracture Energy with Loading Rate for the 36 ply DCB Specimens	177
Figure 65	Comparison of Load Displacement Traces for 11 ply DCB Specimens at Different Loading Rates	178
Figure 66	11 ply DCB Specimens Tested at Various Loading Rates	179
Figure 67	36 ply DCB Specimens Tested at Various Loading Rates	180
Figure 68	Variation of Fracture Energy with $\frac{d\sqrt{G}}{dt}$ for the 11 ply DCB Specimens	182
Figure 69	Variation of Fracture Energy with $\frac{d\sqrt{G}}{dt}$ for the 36 ply DCB Specimens	183
Figure 70	Illustration of the Driven Wedge Test Method	185
Figure 71	DCB Specimen vs. Driven Wedge Specimen	187
Figure 72	Illustration of the Large Number of Fracture Points obtained Per Specimen using the Driven Wedge Test Method at Various Loading Rates	188
Figure 73	Typical Initiation and Arrest Fracture Energies Obtained During 75 mm Crosshead Travel	189
Figure 74	Results Obtained from the Driven Wedge Test	

	Conducted on an 11 Ply Bonded Specimen	190
Figure 75	Results Obtained from the Driven Wedge Test	
	Conducted on a 20 Ply Bonded Specimen	191
Figure 76	Results Obtained from the Driven Wedge Test	
	Conducted on a 36 Ply Bonded Specimen	192
Figure 77	Variation of Fracture Energy with Loading Rate	
	for an 11 Ply Bonded Specimen	194
Figure 78	Variation of Fracture Energy with Loading Rate	
	for a 20 Ply Bonded Specimen	195
Figure 79	Variation of Fracture Energy with Loading Rate	
	for a 36 Ply Bonded Specimen	196
Figure 80	SEM Images of the Fracture Surface of a Driven Wedge	
	Specimen Tested at a Slow and Fast Rate	197
Figure 81	Comparison of Load Displacement Traces for an 11 ply	
	Driven Wedge Test Specimen at Different Loading Rates	198
Figure 82	11, 20, and 36 Ply Driven Wedge Specimens Tested	
	at Loading Rates of 0.01, 0.1, and 1.0 m/s	199
Figure 83	SEM images focusing on the Extend of the Stress	
	Whitened Zones on the Fracture Surfaces of a Driven	
	Wedge Specimen Loaded at a Slow and Fast Rate	201
Figure 84	Variation of Fracture Energy with $\frac{d\sqrt{G}}{dt}$ for	

	the 11 ply Driven Wedge Test Specimens	202
Figure 85	Variation of Fracture Energy with $\frac{d\sqrt{G}}{dt}$ for the 20 ply Driven Wedge Test Specimens	203
Figure 86	Variation of Fracture Energy with $\frac{d\sqrt{G}}{dt}$ for the 36 ply Driven Wedge Test Specimens	204
Figure 87	Comparison of the Magnitude of Crack Jump Recorded in the 11 Ply DCB and Driven Wedge Test Specimens	205
Figure 88	Comparison of the Magnitude of Crack Jump Recorded in the 36 Ply DCB and Driven Wedge Test Specimens	206
Figure 89	Mode II Fracture Toughness Test Setup	208
Figure 90	Compliance Plots obtained using the Clamp Calibration Technique	209
Figure 91	Determination of the Clamp Correction	210
Figure 92	Plot of Compliance versus Cube of Crack Length for the 20 Ply in the Cracked Specimen Compliance Calibration Technique	212
Figure 93	Plot of Compliance versus Cube of Crack Length for the 11 Ply in the Cracked Specimen Compliance Calibration Technique	213
Figure 94	Load Displacement Response for an Unbonded	

	Tube Crushed at 5 m/s	215
Figure 95	Progressive Crush Response of Bonded and Unbonded Tubes each Tested at 0.5 and 4 m/s	217
Figure 96	Unbonded Tube Before and After Crushing	218
Figure 97	Variation of SEA with Rate for the Chamfered Unbonded and Bonded Tubes with Various Configurations of Bond Widths (BW) and Bond Thicknesses (BT)	219
Figure 98	Variation of SEA of Tubes with Loading Rate	220
Figure 99	New 18 m/s Test Machine at ORNL	226

LIST OF ACRONYMS AND SYMBOLS

UT: University of Tennessee

ORNL: Oak Ridge National Laboratory

NTRC: National Transportation Research Center

DOE: Department of Energy

ACC: Automotive Composite Consortium

TMAC: Test Machine for Automotive Composites

SEA: Specific Energy Absorption

DCB: Double Cantilever Beam

WTDCB: Width-Tapered Double Cantilever Beam

HTDCB: Height-Tapered Double Cantilever Beam

PEEK: Polyetheretherkeetone.

WIF: Wedge-Insert Fracture

CT: Compact Tension

FFT: Fast Fourier Transform

ENF: End Notch Flexure

SHPB: Split Hopkinson Pressure Bar

SEM: Scanning Electron Microscope

CNF: Center Notch Flexure

CDD: Curvature Driven Delamination

ELS: End Loaded Split

MMF: Mixed Mode Flexure

FRMM: Fixed Ratio Mixed Mode

HDPE: High Density Polyethylene

ASTM: American Society for Testing and Materials

SLS: Single Lap Shear

DNS: Double Notch Shear

HSS: High Speed Steel

PTFE: Polytetrafluoroethylene

GM: General Motors

USCAR: United States Council for Automotive Research

PIDF: Proportional, Integral, Derivative, Feed Forward

SENB: Single Edge Notch Bend

COV: Coefficients of Variability

DSC: Differential Scanning Calorimetry

K_{IC} : Plane Strain Fracture Toughness

TTSP: Time Temperature Superposition

SBT: Simple Beam Theory

CBT: Corrected Beam Theory

ECM: Experimental Compliance Method

SERR: Strain Energy Release Rate

G_C : Initiation Strain Energy Release Rate

G_{IC} : Critical Strain Energy Release Rate

BW: Bond Width

BT: Bond Thickness

σ : Mean Crush Stress

ρ : Density

kJ: Unit of Energy in kilo Joules

J: Unit of Energy in Joules

kg: Unit of Mass in Kilograms

lbs: Unit of Mass in Pounds

m: Unit of length in Meters

mm: Unit of Length in Millimeters

in: Unit of Length in Inches

N: Unit of Force in Newton

Pa: Unit of Stress in Pascal

sec: Unit of Time in Seconds

CHAPTER 1

INTRODUCTION AND LITERATURE REVIEW

1.1 Introduction

Passenger vehicle crashworthiness is concerned with the absorption of energy via controlled failure mechanisms and modes that enable the maintenance of a gradual decay in the load profile. Current legislation for automobiles requires vehicles to be designed such that, in the event of an impact at speeds up to 15.5 m/s (35 mph) with a solid, immovable object, the occupants of the passenger compartment should not experience a resulting force that produces a net deceleration greater than 20g. Hence crashworthy structures should be designed to absorb impact energy in a controlled manner, thereby bringing the passenger compartment to rest without the occupant being subjected to high decelerations, which can cause serious internal injury, particularly brain damage.

In the crashworthiness of automotive structures, the primary issues to the automotive industry are the overall economy and the weight of the material without sacrificing performance. To reduce the weight and improve the fuel economy, polymer composite materials are finding more application in vehicles. The tailorability of composites, in addition to their attributes of high strength-to-weight and stiffness-to-weight ratios, corrosion and fatigue resistance, makes them very attractive as crashworthy structures.

Crashworthiness of a material is often expressed in terms of a parameter called specific energy absorption, SEA, which is defined as the energy absorbed per unit mass of material. From the definition of crashworthiness, it is understood that an ideal crashworthy material used in a car, in the event of a crash, must do the following. One, absorb the kinetic energy of the car and two, dissipate this energy over a time frame that ensures the deceleration of the car to be less than a critical value, above which the passengers will experience irreversible brain damage. So while testing specimens in the lab to determine their crashworthiness before using them in cars, one needs to measure the magnitude of the energy that it is capable of absorbing and the length of time over which this energy will be absorbed. Both the magnitude and the rate of energy absorption is dependent on the particular material and its microstructural characteristics (For a given fiber/matrix material system we will have different SEAs for woven vs. braids vs. random etc.). If there were two different types of materials with similar energy absorption capabilities, the material that dissipated this energy over a longer period of time would be considered more crashworthy. Therefore, in the course of evaluating the crashworthiness of a material, measurement of time is also important. Detailed discussions of the importance of the above time factor can be found in one of our previous publications where in we attempted to raise certain issues regarding crashworthiness studies [1].

It is also important to determine other performance properties (like tensile, compressive, shear, and fracture toughness properties) of materials being considered for use in crashworthy structures in pursuit of a thorough understanding of the fundamental

characteristics of these candidate automotive material systems. In the ultimate design if these properties are not studied and understood well, one could not have done a good job since in a head on collision the crashworthy structure might serve well but might not during side impact.

It is often said that the main draw back of polymer composite systems is their inability to resist defect initiation and propagation when compared to metallic systems. The ability to resist defect propagation is characterized by the fracture toughness of the material. So one would like the fracture toughness properties of a composite material to be as high as possible. However, if we were considering material systems for use as crashworthy structures, optimal fracture toughness properties would be most helpful. Too high a fracture toughness will prevent failure initiation and controlled crack propagation, which are prerequisites for the crashworthy structure to dissipate energy while maintaining a gradual decay in the load profile. On the contrary very low fracture toughness properties will prevent loads that can cause the desirable absorption of all of the kinetic energy of the car. Hence, in the ultimate selection of the crashworthy material all of the above issues need to be addressed and made satisfactory.

Very briefly, the challenge is determining what specific design features are needed in the geometry, what material systems will enable greater safety without negatively affecting the overall economics of fabrication and production, and what appropriate fastening/joining methods are required for an efficient energy absorption structure.

It can be expected that carbon fiber reinforced polymer composites be bonded together to successfully design crashworthy adhesively bonded structures since they display higher specific energy absorption when compared to other fiber reinforced composite materials. This is a direct result of the lower density of carbon fiber reinforced materials, since specific energy absorption is defined as the ratio of mean crush stress and density of the composite. Nevertheless, before that one needs to find answers to the following important questions: When a bonded structure is being used, how will key joint parameters, e.g., bond-line thickness, bond length, and fillets, affect the crashworthiness behavior? What are the effects of strain rate on the performance properties? An experimental plan has been developed and results reported in this document that will provide definite answers to the above questions.

The ACC (Automotive Composite Consortium) has been and continues to be very interested in investigating the use of fiber-reinforced composites as crash energy absorbers. While many scientists including us [1-9] have investigated the energy absorption characteristics in various fiber reinforced composite materials, there is no literature available on the energy absorption and crushing characteristics of these materials when they are used in a bonded structure. After reviewing the existing literature and based on our own work done in automotive crashworthiness studies [3] it can be concluded that investigating the strain rate dependence of fiber reinforced polymer composites is important since the amount of energy they absorb and their performance properties vary with loading rate.

Joining of composites can be accomplished by either mechanical fastening or adhesive bonding, or a combination of both. Mechanical fastening using bolts or rivets is not suitable for fiber-reinforced composites due to the stress concentrations that occur in the vicinity of the bolts and rivets. In addition, fiber/matrix debonding or interply/intraply splitting may occur to alleviate the localized stresses. Also, machining and drilling of composites may lead to additional damage and increased susceptibility to interlaminar shear failure in the composite. All this will result in the composite material not being able to realize its full performance potential. The inherent advantage of adhesive bonding is that stresses can be distributed, rather than concentrated. It is in the areas of stress concentration that failure initiates. Adhesive bonding is also advantageous from the standpoint that a reduction in weight and smooth external surfaces can be obtained. In addition, assembly costs can be reduced since adhesive bonding is less costly than mechanical fastening on large area bonds. Disadvantages of adhesive bonding are that special adherend surface preparation might be required and nondestructive inspection is always difficult. It would have been ideal if the composite structure would be manufactured as an integral, monolithic component but limitations in the present day manufacturing technology necessitate the presence of joints in composite structures.

Hence, any effort to determine the usefulness of carbon fiber composites as crashworthy structures would not be complete without a plan to develop a comprehensive experimental methodology to analyze and design adhesively bonded automotive composite structures made of carbon fiber composites to sustain axial, off-axis and lateral

crash/impact loads. The focus will be on adhesive joint related issues and we would like to research how best carbon fiber composite structures can be bonded together in the pursuit of designing the most crashworthy adhesively bonded automotive composite structure. The adhesive will have to effectively transfer the load between the different composite structures bonded together. The influence of having a bonded joint within the crush zone of a composite structure has not been adequately characterized in the past.

Experimental tasks include materials testing under quasi-static and dynamic loads for carbon fiber polymer composites, adhesives (epoxy), and joints; strain rate sensitivity studies to help characterization (determination of the various properties, performance and responses); fracture toughness testing; and test method development as required. Included in these tasks are structural tests to be conducted on the new intermediate-rate test machine at the Oak Ridge National Laboratory (ORNL) called TMAC (Test Machine for Automotive Composites).

The above experimental results will be used to provide the building blocks for model developments – first the coupon level, then progressing in complexity to component level. Correlation with experimental results will provide the basis for which the analytical developments including development of constitutive laws, materials models, damage algorithms and new finite elements, are made.

1.2 Literature Review on Strain Rate Dependence of Energy Absorption in Polymer Composite Materials for Automotive Crashworthiness

1.2.1 Abstract

Energy absorption in fiber reinforced polymer composite materials is dependent on fiber type, matrix type, fiber architecture, specimen geometry, processing conditions, fiber volume fraction, and testing speed. Changes in these parameters can cause subsequent changes in the specific energy absorption of composite materials up to a factor of two. Below is a detailed review of the strain rate dependence of energy absorption in polymer composite materials for automotive crashworthiness. An attempt is made to draw together all the work done in the past to investigate the effect of strain rate in various crashworthiness studies in order to better understand the strain rate effects on the energy absorption capability of composite materials.

KEYWORDS: Crashworthiness, Energy Absorption, Composite Materials, Crushing, Strain Rate.

1.2.2 Introduction

Contrary to metals, especially in compression, most composites are generally characterized by a brittle rather than ductile response to the applied loads. While metal structures collapse under crush or impact by buckling and/or folding in accordion (concertina) type fashion involving extensive plastic deformation, composites fail through a sequence of fracture mechanisms involving fiber fracture, matrix crazing and cracking, fiber-matrix de-bonding, de-lamination and inter-ply separation. The actual

mechanisms and sequence of damage are highly dependent on the geometry of the structure, lamina orientation, type of trigger, and crush speed, all of which can be suitably designed to develop high-energy absorbing mechanisms.

The crashworthiness of a material is expressed in terms of its specific energy absorption, which is defined as the energy absorbed per unit mass of material. It is dependent on the particular material and its microstructural characteristics. Mathematically specific energy absorption, $SEA = \sigma/\rho$, where ‘ ρ ’ is the density of the composite material and ‘ σ ’ is the mean crush stress.

1.2.3 Test Methodologies

Crush tests can be carried out in two conditions namely quasi-static and impact conditions.

1.2.3.1 Quasi-static Testing

In quasi-static testing, the test specimen is crushed at a constant speed. Quasi-static tests may not be a true simulation of the actual crash condition because in an actual crash condition, the structure is subjected to a decrease in crushing speed, from an initial impact speed, finally to rest. Many materials used in designing crashworthy structures are rate sensitive. That means their energy absorption capability is dependent on the speeds at which they are crushed. Therefore, the determination of materials as good energy absorbers after quasi-statically testing them does not ensure their satisfactory performance as crashworthy structures in the event of an actual crash.

The following are some advantages of quasi-static testing.

1. Quasi-static tests are simple and easy to control.

2. Impact tests require very expensive equipment to follow the crushing process because the whole crushing takes place in a split second. Hence, quasi-static tests are used to study the failure mechanisms in composites, by selection of appropriate crush speeds.

The following is a major disadvantage of quasi-static testing.

- Quasi-static tests may not be a true simulation of the actual crash conditions since certain materials are strain rate sensitive.

1.2.3.2 Impact Testing

The crushing speed decreases from the initial impact speed to rest as the specimen absorbs the energy.

The following is a major advantage of impact testing

- It is a true simulation of the crash condition since it takes into account the stress rate sensitivity of materials.

The following is a major disadvantage of impact testing.

- In impact testing, the crushing process takes place in a fraction of a second. Therefore, it is difficult to study the crushing unless provided with expensive equipment like a high-speed camera and other high-speed sensors.

1.2.4 Crushing Modes and Mechanisms

1.2.4.1 Catastrophic Failure Modes

Catastrophic failure modes are not of interest to the design of crashworthy structures. It occurs

- When unstable intralaminar or interlaminar crack growth occurs.

- In long thin walled tubes because of column instability.
- In tubes composed of brittle fiber reinforcement, when the lamina bundles do not bend or fracture due to interlaminar cracks being less than a ply thickness.

The following are the disadvantages of catastrophic failure in the design of crashworthy structures.

- Catastrophic failure is characterized by a sudden increase in load to a peak value followed by a low post failure load. As a result of this, the actual magnitude of energy absorbed is much less and the peak load is too high to prevent injury to the passengers.
- Structures designed to react to loads produced by catastrophically failing energy absorbers are heavier than structures designed to react to loads produced by progressively failing energy absorbers.

1.2.4.2 Progressive Failure Modes

Progressive failure can be achieved by providing a trigger at one end of an axi-symmetrical circular, square, or rectangular tube. A trigger is a stress concentrator that causes failure to initiate at a specific location within the structure. From there on, the failure, in a controlled predictable manner, progresses through the body at the loading speed. A trigger reduces the initial load peak that accompanies failure initiation followed by controlled collapse. The most widely used method of triggering is to chamfer one end of the tube. A number of other trigger geometries such as bevels, grooves, and holes are also in use.

The following are the advantages of progressive failure in the design of crashworthy structures.

- The energy absorbed in progressive crushing is larger than the energy absorbed in catastrophic failure.
- A structure designed to react to loads produced by progressively failing energy absorbers are lighter than structures designed to react to loads produced by catastrophically failing energy absorbers.

Below are the four characteristic types of progressive crushing modes.

1.2.4.2.1 Transverse Shearing or Fragmentation Mode

- The fragmentation mode depicted in Figure 1 is characterized by a wedge shaped laminate cross section with one or multiple short interlaminar and longitudinal cracks that form partial lamina bundles.
- Brittle fiber reinforcement tubes exhibit this crushing mode.
- The main energy absorption mechanism is fracturing of lamina bundles
- When fragmentation occurs, the length of the longitudinal and interlaminar cracks is less than that of the lamina.
- Mechanisms like interlaminar crack growth and lamina bundles control the crushing process for fragmentation.

1.2.4.2.2 Lamina Bending or Splaying Mode

- Very long interlaminar, intralaminar, and parallel to fiber cracks characterizes the splaying mode shown in Figure 2. The lamina bundles do not fracture.

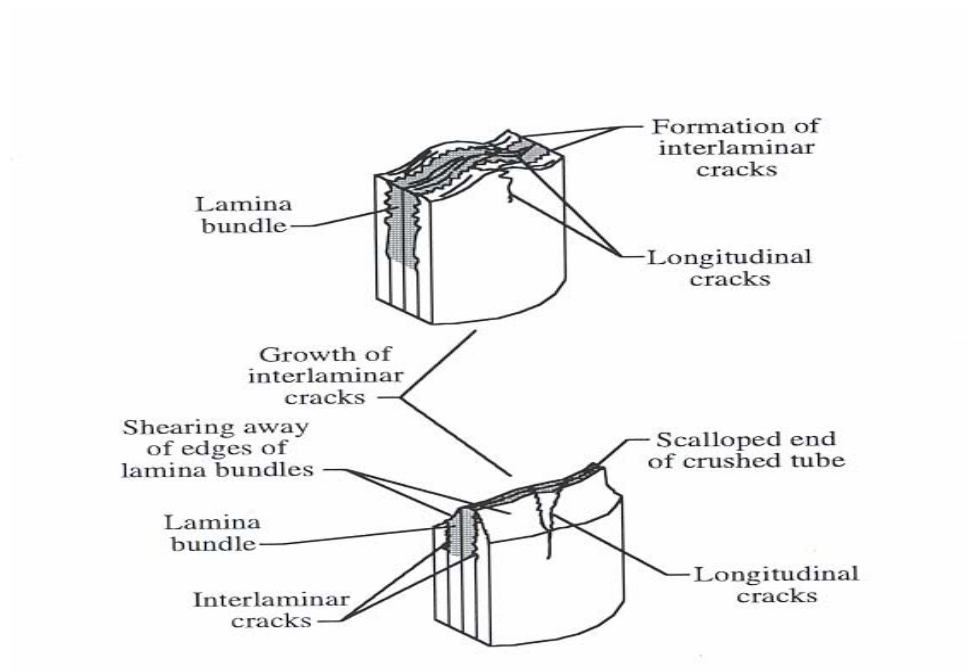


Figure 1. Fragmentation Crushing Mode [10]

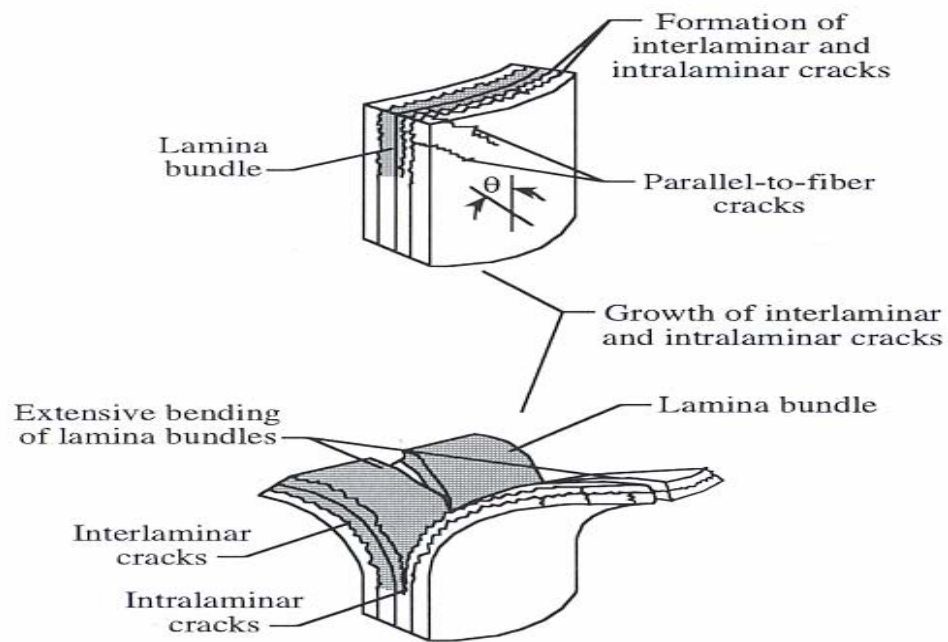


Figure 2. Splaying Crushing Mode [10]

- Brittle fiber reinforcement tubes exhibit this crushing mode.
- The main energy absorbing mechanism is matrix crack growth. Two secondary energy absorption mechanisms related to friction occur in tubes that exhibit splaying mode.
- Mechanisms like interlaminar, intralaminar, and parallel to fiber crack growth control the crushing process for splaying.

1.2.4.2.3 Brittle Fracturing

- The brittle fracturing crushing mode shown in Figure 3 is a combination of fragmentation and splaying crushing modes.
- This crushing mode is exhibited by brittle fiber reinforcement tubes
- The main energy absorption mechanism is fracturing of lamina bundles.
- When brittle fracturing occurs, the lengths of the interlaminar cracks are between 1 and 10 laminate thickness.

1.2.4.2.4 Local Buckling or Progressive Folding

- The progressive folding mode shown in Figure 4 is characterized by the formation of local buckles.
- This mode is exhibited by both brittle and ductile fiber reinforced composite material.
- Mechanisms like plastic yielding of the fiber and/or matrix control the crushing process for progressive folding.

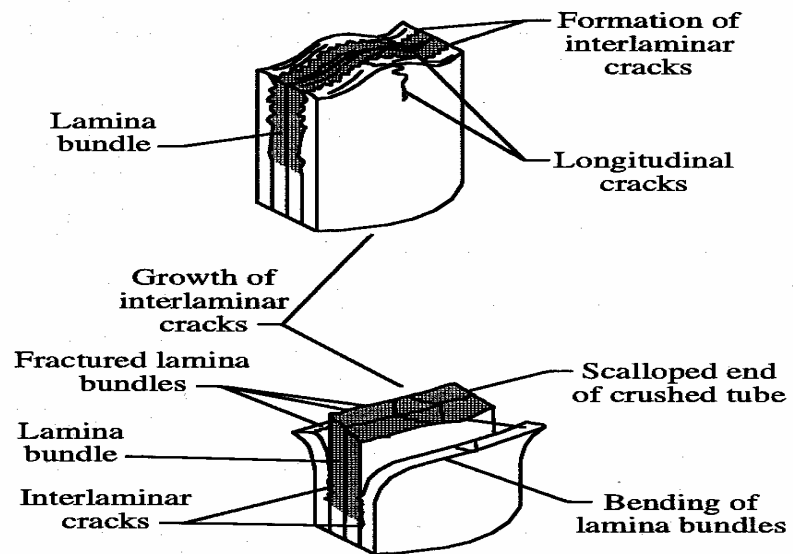


Figure 3. Brittle Fracturing Crushing Mode [10]

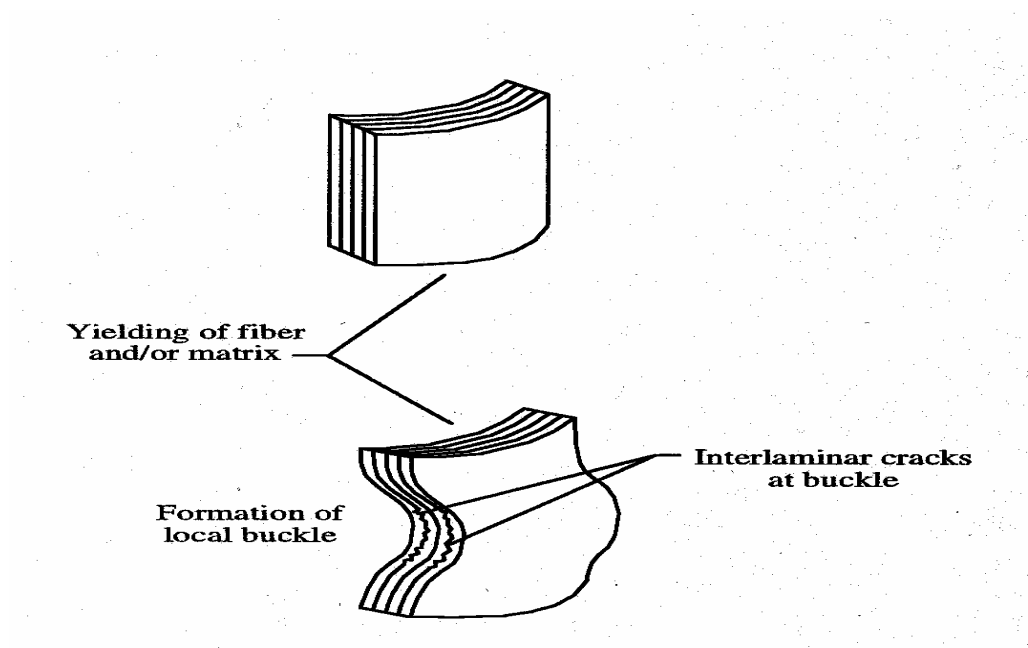


Figure 4. Progressive Folding Crushing Mode [10]

1.2.5 Calculation of Specific Energy Absorption

Specific energy absorption, SEA, is defined as the energy absorbed per unit mass of material. Figure 5 is a typical load displacement curve obtained from progressive crushing of a composite tube specimen. The area under the load-displacement curve is

$$W = \int_0^{S_b} P dS \quad (1)$$

where ‘W’ is the total energy absorbed in crushing of the composite tube specimen. A more characteristic property of progressive crushing mode is

$$W = \int_{S_i}^{S_b} P dS = \bar{P} (S_b - S_i) \quad (2)$$

where ‘S_b’ and ‘S_i’ are the crush distances as indicated in Figure 5 and ‘ $\bar{P}$ ’ is the mean crush load. The specific energy absorption capability, SEA, of a composite material defined as the energy absorbed per unit mass of material is given by

$$SEA = \frac{W}{m} \quad (3)$$

where ‘m’ is the mass of the composite material.

Combining the above two equations we get

$$SEA = \frac{W}{m} = \frac{\bar{P}(S_b - S_i)}{V\rho} \quad (4)$$

where ‘V’ is the volume of the crushed portion of the composite tube specimen and ‘ ρ ’ is the density of the composite material. We can also write

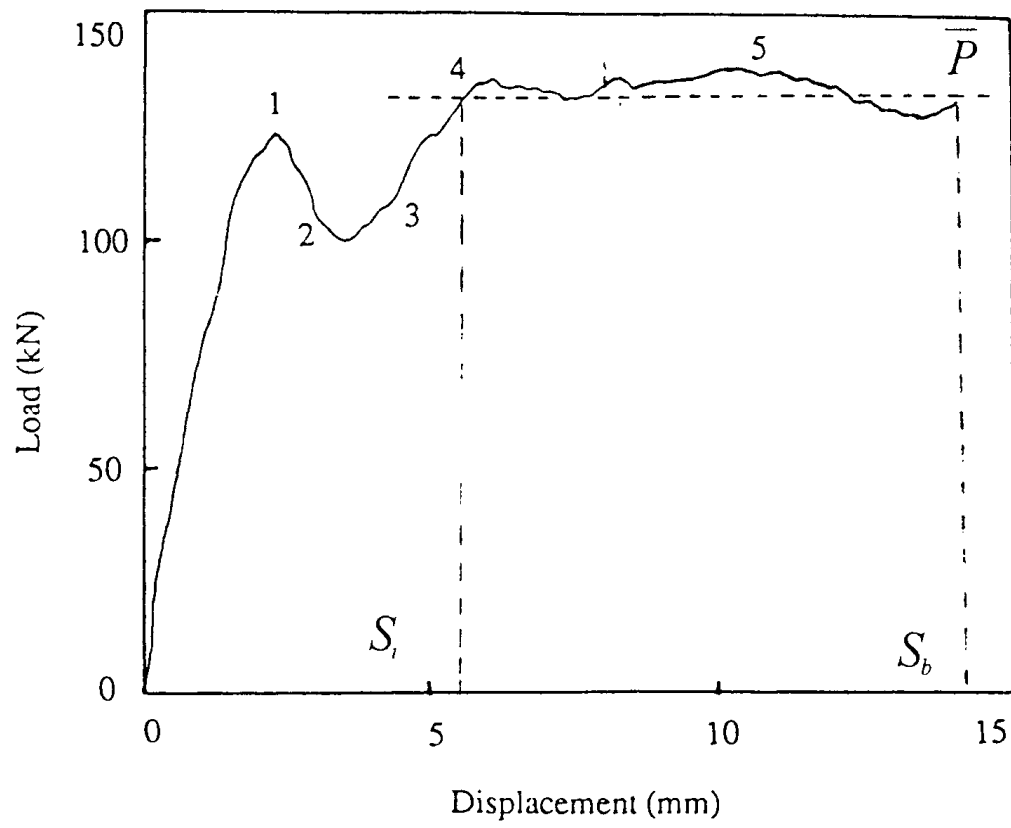


Figure 5. Typical Load Displacement Curve for a Progressively Crushed Composite Tube [11]

$$SEA = \frac{W}{m} = \frac{\bar{P}(S_b - S_i)}{V\rho} = \frac{\bar{P}(S_b - S_i)}{AL\rho} \quad (5)$$

where ‘A’ and ‘L’ are the cross sectional area and length of the crushed portion of the composite tube specimen, respectively.

In the case of polymer composites some times it is rather difficult to determine a specific value for the mean crush load, $\bar{P}$, from the load displacement curve because of the erratic changes in the magnitude of the load with displacement. One does not get the typical load displacement curve obtained from progressive crushing of a composite tube as shown in Figure 5. In that case, an alternative procedure followed for calculating the energy absorbed, W , is to just determine the area under the whole load displacement curve.

1.2.6 Literature Survey

Many researchers have conducted studies on the energy absorption capability of composite materials. Axi-symmetrical tubes, because they are easy to fabricate and close to the geometry of the actual crashworthy structures, have been used to carry out much of the experimental work on the energy absorption of composite materials. Moreover, composite tubes can be easily designed for stable crushing. They can be designed to absorb impact energy in a controlled manner by providing a trigger to initiate progressive crushing. The following survey focuses on the experimental work conducted on axi-symmetric tubes.

1.2.6.1 Strain Rate Effects on the Energy Absorption Capability of a Polymer Composite Material

Bannerman and **Kindervater** [12] while investigating carbon/epoxy and Kevlar/epoxy tubular and beam specimens reported an increase in energy absorption with crushing speed. **Thornton** [13] reported very little change in the specific energy absorption of 0/90 graphite/epoxy, Kevlar/epoxy, and glass/epoxy composite tubes over a wide range of compression rates (10^{-1} to 2×10^4 inches/min). **Thornton** [14] also investigated the energy absorption behavior of Pultruded glass/polyester and glass/vinyl ester tubes in the crushing speed range from 2.1×10^{-4} m/s to 15 m/s. He reported a 10% decrease with increasing test speed in the case of glass/vinyl ester tubes and a 20% increase in energy absorption in the case of glass/polyester tubes. The above observations can be attributed to the change in crushing mode with change in crushing rates. **Price** and **Hull** [15] also observed changes in the crushing mode of continuous glass strand mat reinforced polyester resin tubes with change in crush rate, these changes being accompanied by decreases in the energy absorption. **Thornton** et al [16] later investigated glass fiber/vinyl ester rods with testing speed in the range 0.13 to 2.54 mm/min. The specific energy was seen to increase essentially linearly with log of testing rate. The rate dependence of the energy absorption of the composite rods comes from the deformation rate dependence of the matrix in the rods.

Farley [17] investigated Kevlar/epoxy, carbon/epoxy, and glass/epoxy composite tubes with fiber architecture $[0 \pm \theta]_4$ at speeds of quasi-static and 7.6 m/sec impact and found specific energy to be independent of crushing speed. When **Farley**

[18] investigated carbon/epoxy and Kevlar/epoxy tubes with $[\pm\theta]_3$ fiber architecture he found a 35% increase in specific energy with the change in the crushing condition from quasi-static to impact. To understand why tubes of the same material exhibit different energy absorption characteristics, **Farley** [19] thought one must examine the mechanisms that control the crushing process. For the $[0\pm\theta]_4$ tubes, the mechanical properties of the fibers control the crushing process. Since the mechanical properties of the fiber are not strain rate sensitive, the energy absorption capability of $[0\pm\theta]_4$ tubes were not a function of crushing speed. For the $[\pm\theta]_3$ tubes, the mechanical properties of the matrix control the crushing process. Since the mechanical properties of the matrix are a function of strain rate, the energy absorption capabilities of these tubes were a function of crushing speed. The magnitude of effects of crushing speed on specific energy was hence concluded to be a function of the mechanism that controls the crushing process.

Mamalis et al [20, 21] observed that friction mechanisms, which are developed between the composite material and the crushing surface and the various new surfaces, which have formed after interlaminar crack growth, are also affected by the strain rate. Static and dynamic crushing tests in a speed range of 18 – 24 m/sec were conducted by **Mamalis** et al [22] on three different composite materials. Two of the composite materials consisted of fiberglass and vinyl ester resins. The third was made up of fiber glass and polyester resin. The specimens under investigation had different geometries: square, circular, and circular cone. The specific energy of thin walled circular conical specimen made of polyester resin and random chopped strand mat of

glass fiber were reduced by 35% under a crushing speed of about 21 m/sec. It was hence concluded that crush speed interacts with cone angle and wall thickness of the specimen. Reduction in specific energy caused by the increase of crush speed becomes more significant when wall thickness or cone angle is larger. However, the crushing speed was not observed to have a significant effect on the specific energy absorption of thin walled circular or square tubes made of the three kinds of composite materials.

To the contrary, **Mamalis** et al [23, 24] in another investigation reported increasing specific energy with increasing loading rate for fiberglass/vinyl ester composite shells of various geometries, probably due to higher values of the dynamic friction coefficients between the wedge/fronds and fronds/platen interface. Similar observations were reported by **Berry** and **Hull** [25] with the specific energy increasing with increasing loading rate, up to 8.5 m/s for graphite/epoxy and glass/epoxy composites. Carbon/epoxy tubes investigated by **Kindervater** [26] showed as much as 20% degradation in energy absorption capability under impact loading up to 9 m/s, while high performance polyethylene fiber Dyneema SK60 in an epoxy matrix and carbon/thermoplastic polyamid tubes showed increases close to 50%. In another investigation, **Kindervater** [27] observed little difference between the quasi-static and dynamic energy absorption of Kevlar/epoxy tubes. Hence, he suggested that the effect of crushing speed on energy absorption would vary depending on the particular material system used.

Keal [28] reported that the specific energy of glass/polyester tubes dropped more than 20% as crush speed increased from 2.33×10^{-3} m/s while that of glass/epoxy

tubes increased with increasing loading speed. **Schmueser** and **Wickliffe** [29] reported a decrease of up to 30% in energy absorption of impacted carbon/epoxy, glass/epoxy, and Kevlar/epoxy tubes with fiber architecture $[0_2/\pm 45]_s$, as compared to static test results. **Ramakrishna** and **Hull** [30] observed a decrease in the specific energy absorption capability of knitted carbon fabric reinforced epoxy tubes with increasing crushing speed. This was thought to be closely related to the change in the morphology of the crush zones for the quasi-static and impact tested composite tubes. The progressive crushing mechanism in impact tested tubes was mainly by the fragmentation process, where as in quasi-static tested tubes it was dominated by the splaying mode.

Ramakrishna [31] later again studied the effect of testing speed on the specific energy absorption capability of knitted glass fiber/epoxy and knitted carbon fiber fabric/epoxy composite tubes. The specific energy of both types of composite tubes decreased by 20% with change in testing condition from quasi-static to impact. This is attributed to the decrease in fracture toughness (G_{IC}) of composite materials with increasing test speeds. Decreased fracture toughness means less resistance to the longitudinal cracking of the tube wall and therefore lower energy absorption. Impact tests were carried out by **Hamada** and **Ramakrishna** [32] on carbon/PEEK tubes at 8.5 m/s using a drop tower testing machine and the results were compared with that of static tests conducted at 1 mm/min. The impact tested tubes absorbed 50% lower energy than the static tested tubes. The impact tested tubes were crushed by brittle fracture of tube wall unlike the splaying mode crushing of static tested tubes. Brittle fracture was attributed to the reduced fracture toughness of carbon/PEEK composite material at high

strain rates. The lower fracture toughness resulted in reduced energy absorption under impact conditions.

Tests carried out on tubes (made from a variety of composite materials: Glass/polyester, glass/epoxy, carbon/epoxy, and Kevlar/epoxy) by **Hull** [33-35] at speeds up to 4 m/s using a servohydraulic compression machine and at 12-15 m/s using a catapult machine found the energy absorption in the composite materials to be almost rate independent. The influence on energy absorption of loading rate was evaluated for three composite material systems (graphite/PEEK, graphite/epoxy, and a hybrid graphite-Kevlar/epoxy) by **Lavoie** et al [36, 37]. Generally, all materials tested lost some energy absorption capacity at high crush rates (5-7 m/s). In particular, the energy absorption capacity of graphite/PEEK composite dropped significantly at the dynamic crush rate due to a transition to a less efficient crushing mode, like caused by reduced toughness of PEEK at high strain rates.

1.2.7 Conclusion

In addition to a material being crashworthy, many criteria like low manufacturing costs and the material being readily available have to be met before one can begin the use of a particular composite as a crash energy absorber in automobiles. Once a composite material is identified to meet the above necessary requirements, one ought to know the effect all the controllable parameters (like fiber arrangement, specimen geometry, testing speed etc.) will have on its energy absorption capabilities, in an attempt to design the most crashworthy structure. In this section, an attempt is made to review all the work done in the past to investigate the strain rate effects on the energy absorption

capability of composite materials for automotive crashworthiness. The work is summarized in Table 1. Upon reviewing the literature, there seems to be a lack of consensus about the influence of test speed on the energy absorption in composite materials. Hence more work needs to be done in pursuit of the above goal.

However a couple of general statements can be made which suit most test results: That for most brittle composites an increase in loading speed causes an increase in the energy absorption in the polymer composite due to the increase in the fracture toughness of the composite with loading rate. The increase in the fracture toughness of the composite is due to the increased fracture toughness of the matrix resin in the composite with increasing loading rate. Increased fracture toughness of the composite with increasing loading rate means more resistance to crack formation. Therefore, there is more energy absorption in the composite at higher loading rates. However, in the case of thermoplastic polymer composites an increase in the loading speed causes a decrease in the energy absorption in the composite due to the decrease in the fracture toughness of composite with increased loading rates. The fracture toughness of the composite decreases with increasing loading rate because of the decrease in the fracture toughness of the thermoplastic matrix in the composite with increasing speed.

There is also no literature available on the effect of strain rate on the energy absorption and crushing characteristics of random chopped fiber reinforced composite materials, which can find extensive use as crash energy absorbers primarily due to the low costs involved in their manufacture thus making them cost effective for automotive applications. Hence, one suggests the need for more work to be done to better understand

**Table 1. Summary of Published Data on the Effects of Loading
Rate on Energy Absorption**

Authors	Materials Studied	Range of Rates Investigated	Observations
Bannerman and Kindervater [12]	Carbon/Epoxy and Kevlar/Epoxy		Energy absorption increased with crushing speed.
Thornton [13, 14, 16]	Graphite/Epoxy, Kevlar/Epoxy, Glass/Epoxy, Glass/Polyester, and Glass/Vinylester	0.13 mm/min → 15 m/sec	Very little change in specific energy absorption with crushing speed for graphite, Kevlar, and glass epoxy composites. 10% decrease and 20% increase in energy absorption with increasing testing speed for glass/vinylester and glass/polyester composites, respectively.
Price and Hull [15]	Continuous Strand Mat Reinforced Polyester Resin Tubes	0.1 mm/sec → 16 m/sec	Energy absorption decreased with increasing crushing speed.
Farley [17, 18]	Kevlar/Epoxy, Glass/Epoxy and Carbon/epoxy	0.01 m/sec → 12 m/sec	Energy absorption was independent of crushing speed for all 3 composite tubes with fiber architecture $[0\pm\theta]_4$. Increase in energy absorption with crushing speed for carbon and Kevlar epoxy composite tubes with $[\pm\theta]_3$.

Table 1. Continued

Authors	Materials Studied	Range of Rates Investigated	Observations
Mamalis [22]	Glass/Vinylester and Glass/Polyester	18 m/sec → 24 m/sec	Energy absorption decreased with increase in crushing speed for the circular conical specimen but remained constant for the thin walled circular and square tube composites with increasing crushing speeds.
Mamalis [23, 24]	Glass/Vinylester	10 mm/min → 10 m/sec	Energy absorption increased with increasing crushing speed.
Berry and Hull [25]	Graphite/Epoxy and Glass/Epoxy	1.67×10^{-7} m/sec → 10 m/sec	Energy absorption increased with increasing crushing speed.
Kindervater [26, 27]	Carbon/Epoxy Polyethylene/Epoxy Carbon/Polyamid and Kevlar/Epoxy	2 mm/min → 9 m/sec	Decrease in energy absorption with increasing crushing speed for the carbon/epoxy tube. Energy absorption increased with increasing crushing speed for the polyethylene/epoxy and carbon/polyamid tubes. Very little change in energy absorption with crushing speed for Kevlar/epoxy composites.

Table 1. Continued

Authors	Materials Studied	Range of Rates Investigated	Observations
Keal [28]	Glass/Polyester and Glass/Epoxy	2.33×10^{-3} m/sec $\rightarrow$ 13.8 m/sec	Energy absorption decreased with increasing crushing speed for the glass/polyester tubes. Energy absorption increased with increasing crushing speed for the glass/epoxy tubes.
Schmueser and Wickliffe [29]	Carbon/Epoxy, Kevlar/Epoxy, and Glass/Epoxy	0.1 cm/min $\rightarrow$ 5.5 m/sec	Energy absorption decreased with increasing crushing speed.
Ramakrishna and Hull [30, 31]	Carbon/Epoxy and Glass/Epoxy	0.001 m/sec $\rightarrow$ 13 m/sec	Energy absorption decreased with increasing crushing speed.
Hamada and Ramakrishna [32]	Carbon/PEEK	1.67×10^{-5} m/sec $\rightarrow$ 8.5 m/sec	Energy absorption decreased with increasing crushing speed.
Hull [33-35]	Glass/Polyester, Glass/Epoxy, Carbon/Epoxy, and Kevlar/Epoxy	4 mm/sec $\rightarrow$ 15 m/sec	Energy absorption was rate independent.
Lavoie et al [36, 37]	Graphite/PEEK, Graphite/Epoxy, and Hybrid Graphite-Kevlar/Epoxy	1.25 mm/min $\rightarrow$ 7 m/sec	Energy absorption decreased with increasing crushing speed.

the strain rate effects on the energy absorption capability of random chopped fiber composites.

1.3 Literature Review of the Effect of Loading Rate on the Fracture Toughness of Fiber Reinforced Polymer Composites

1.3.1 Abstract

Below is a detailed review of the strain rate dependence of fracture toughness properties in polymer composite materials. An attempt is made to draw together all the strain rate studies done in the past and to elucidate the reasons given by the authors of the reviewed papers for the trends resulting from their studies in order to better understand the strain rate effects on the fracture toughness of fiber reinforced polymer composite materials.

KEYWORDS: Polymer Composite Materials, Strain Rate, Fracture Toughness.

1.3.2 Introduction

Composites in the past have been mainly used for weight savings in secondary structures. With several advances made in understanding the behavior of composite materials, many fiber reinforced polymer composite materials are finding increasing use as primary load bearing structures and in a wide range of high technology engineering applications. The ability to tailor composites, in addition to their attributes of high stiffness-to-weight and strength-to-weight ratios, fatigue resistance, corrosion resistance, and lower manufacturing costs makes them very attractive when compared with conventional metals.

The main draw back of composite systems is their inability to resist defect initiation and propagation when compared to metallic systems. The ability to resist defect propagation is characterized by the fracture toughness of the material. It has always been a cause for concern that the fracture toughness properties of a composite material may be poor at high rates of strain. This calls for investigating the strain rate dependence of fracture toughness properties of composite materials. Indeed, high velocity impact tests on various composites have suggested that beyond a certain threshold velocity a change in failure mode occurs and the composite material experiences a sudden drop in mechanical performance.

In this section an attempt is made to review much of the work published in the literature that investigates the strain rate effects on the Mode I, Mode II, and Mixed Mode (I+II) fracture toughness properties of fiber reinforced polymer composite materials.

1.3.3 Literature Survey

1.3.3.1 Strain Rate Effects on Mode I Fracture Toughness of Fiber Reinforced Polymer Composites

Aliyu and **Daniel** [38] used Double Cantilever Beam (DCB) specimens to study the effect of loading rate on fracture toughness of AS-4/3501-6 carbon/epoxy composites. At the lower loading rates crack extension was monitored visually while at higher rates crack extension was monitored by strain gages mounted on the surface of the specimen or on a conductive paint circuit attached to the edge of the specimen. A 28% increase in the critical strain energy release rate, G_{IC} , was observed over three orders of

magnitude of loading rate. DCB and Width-Tapered Double Cantilever Beam (WTDCB) interlaminar fracture tests by **Daniel** et al [39, 40] on a carbon/elastomer modified epoxy composite at various loading rates resulted in a 20% decrease in G_{IC} over 3 decades of crack velocity which was attributed to the lower strain to failure of the rubber modified matrix at high strain rates. Using Height-Tapered Double Cantilever Beam (HTDCB) specimen geometry to achieve higher crack velocities **Yaniv and Daniel** [41] found that the maximum value of G_{IC} for the AS-4/3501-6 carbon/epoxy composites was around 46% higher than the quasi-static value. In addition to the HTDCB specimen geometry allowing the attainment of much higher crack propagation velocities possible with uniform DCB or WTDCB specimens, they also helped produce stable and smooth crack propagation at high rates of loading. The results obtained by Daniel et al [38, 41] while investigating the AS-4/3501-6 graphite/epoxy composites was attributed to the rate sensitivity (insensitivity or positive or negative rate sensitivity – in this case positive rate sensitivity) exhibited by the polymer matrix (epoxy) in the composite since Mode I fracture toughness in a composite is a matrix dominated property.

Double Cantilever Beam (DCB) tests by **Barbezat** [42] on carbon/epoxy composites showed that the Mode I interlaminar fracture toughness does not vary with strain rate. Similar tests by **Gillespie Jr** et al [43] on carbon/epoxy composites and on a thermoplastic matrix composite, carbon/PEEK, have shown that over a wide range of strain rates the Mode I fracture toughness remains invariant of strain rate. However, beyond a certain threshold, the fracture toughness of the carbon/PEEK composite drops

dramatically to approximately 20% of its original value. This decrease was attributed to a ductile to brittle transition of the polymer in the process zone.

While investigating strain rate effects on fracture toughness of carbon/epoxy and carbon/PEEK composites, **Blackman** et al [44] found that fracture toughness of carbon/epoxy composites remained invariant of strain rate (the value being about 0.3 kJ/m²) and that of the carbon/PEEK composite reduced by 20% at the highest rate. In his work, he showed that great care must be taken in the experimental aspects when undertaking high rate tests. To reinforce the comments made on the dynamic effects associated with high rate testing he noted that the reduction in the fracture toughness value obtained from crack initiation from his work on the Carbon/PEEK composite at high strain rate would be far greater if he had employed the unreliable and inaccurate values of the measured load at crack initiation in order to determine the value for G_{IC} . **Kusaka** et al [45] investigated the effect of loading rate on the Mode I fracture toughness of DCB and Wedge-Insert Fracture (WIF) carbon/epoxy composite specimens and found that the value of fracture toughness was constant over a relatively large range of loading rates. The trends resulting from his study was explained using a simple kinetic model.

The DCB test geometry was utilized by **Smiley and Pipes** [46] to investigate the rate effects of Mode I interlaminar fracture toughness in graphite/PEEK and graphite/epoxy composites over a range of crosshead speeds from 4.2×10^{-6} m/s to 6.7×10^{-1} m/s. The Mode I interlaminar fracture toughness of the graphite/PEEK composite decreased from 1.5 to 0.35 kJ/m² over five decades of loading rate while that of the graphite/epoxy composite decreased from 0.18 to 0.04 kJ/m² over four decades of loading

rate. The observed rate dependency of the composite fracture toughness was attributed to the rate dependent toughness of the viscoelastic matrix [47]. The rate dependency of the composite toughness is similar to that of the matrix toughness. **Vu-Khanh** and **Fisa** [48] found the dynamic fracture toughness of glass-flake reinforced polypropylene composite to be rate dependent. The dynamic fracture toughness first decreases with the increase in impact velocity, reaches a minimum value, and then increases with impact speed. The increase in fracture toughness with loading rate is attributed to the blunting effect of the crack tip, which is induced by a local temperature increase (adiabatic heating).

You and **Yum** [49] reported a 73% increase in the Mode I interlaminar fracture toughness of brittle carbon/epoxy composite with increasing loading rate from 2 to 120 mm/s. A new technique was proposed from which many crack propagation lengths could be measured in one specimen during high rate testing. However, they did not explain the results they obtained during their investigation. Using Compact Tension (CT) specimens, **Karger-Kocsis** and **Friedrich** [50] reported a decrease in the fracture toughness of 30 wt% short glass fiber reinforced PEEK composite with increasing deformation rate down to a level of 1-2 MPa/m². The reduction of fracture toughness was explained by a reduced molecular mobility and thus a lower ductility of the polymer matrix (increase in the loading rate caused a total embrittlement of the PEEK matrix between the fibers) at higher loading velocities. Investigating the effect of loading rate on the Mode I interlaminar fracture toughness of a woven carbon/PEEK laminate by **Mall** et al [51, 52] highlighted rapid reduction in the fracture toughness with increasing loading rate. The fracture toughness of the DCB specimens decreased up to 65% over

five decades of loading rate. The extent of plastic deformation decreasing with increasing loading rate was explained to be the reason for the decrease in fracture toughness with increasing loading rate.

The effect of loading rate on the Mode I fracture toughness of epoxy resin composites filled with silica particulates were investigated by **Koh** et al [53]. Fracture toughness under static loading was found to be slightly lower than that of impact loading. The loading rate dependence was related to the dynamic effects of the impact tests and the particle-matrix debonding near the initial crack tip. Slow loading rates promoted interfacial debonding of otherwise well bonded particles, which caused a reduction in the resistance of material to gross failure. The debonding deteriorated the full capability of the matrix material for shear deformation due to premature failure. The dynamic effects included the relatively high contact stiffness of the impact striker-specimen interface compared to that of the specimen, and the loss and regaining of contact between the striker and the specimen accelerating and decreasing relative to the striker during impact loading. All the effects resulted in an increasing number of oscillations observed in the force –displacement curve of the impact test as the impact velocity was increased. DCB tests by **Beguelin** et al [54] on unidirectional IM6 graphite/PEEK composites showed a small decrease in the Mode I interlaminar fracture toughness at very high strain rates. At higher rates, the analysis was performed by means of Fast Fourier Transform (FFT) filtering.

1.3.3.2 Strain Rate Effects on Mode II Fracture Toughness of Fiber Reinforced Polymer Composites

End Notch Flexure (ENF) specimen geometries were used by **Smiley** and **Pipes** [55] to investigate the loading rate effects on the Mode II interlaminar fracture toughness of carbon/epoxy (AS4/3501-6) and carbon/PEEK (APC-2) composites. The fracture toughness of both carbon/epoxy and carbon/PEEK composites decreased by about 85% at high loading rates. The reduction in the fracture toughness of the thermoplastic carbon/PEEK composite was attributed to a decrease in the development of plastic deformation during loading. **Kageyama** and **Kimpara** [56] investigated the effect of loading rate on the Mode II interlaminar fracture toughness of a unidirectional carbon/epoxy laminate. The fracture toughness was found to increase with increasing impact velocity and the value at the impact velocity of 8 m/sec was 1.8 times higher than the static value. No explanation was give for the observed results.

Kusaka et al [57, 58] explored the strain rate effects of fracture toughness of unidirectional carbon/epoxy composites using a split Hopkinson pressure bar (SHPB) and found fracture toughness decreased by 20% over 8 decades of loading rate. The scanning electron microscopy (SEM) observations indicated that the results were caused by fractographic differences: The specimen fracture surfaces were smooth at high strain rates as a result of debonding at the fiber matrix interface and the matrix surface is only deformed a little but the specimen fracture surfaces at low rates highlighted the presence of hackle markings due to ductile fracture in the matrix resin. The dynamic strength of

bonding between reinforcing fibers and matrix resin might have been lower than the static strength.

Berger and **Cantwell** found that the Mode II interlaminar fracture toughness of a carbon fiber reinforced phenolic resin decreased with increasing load rate [59] while that of carbon fiber reinforced PEEK increased with increasing loading rate [60]. SEM observations of a number of samples indicated the interlaminar fracture toughness of the carbon/phenolic resin composite was determined by the development of the damage zone in the crack tip region. It was suggested that the Mode II interlaminar fracture energy was directly dependent on the amount of plastic deformation in front of the crack tip [59]. The Mode II interlaminar fracture toughness of the carbon/PEEK composite was believed to be strongly influenced by the yield stress of the thermoplastic matrix. Conditions that reduce the yield stress of the polymer (such as decreasing the loading rate) precipitate similar reductions in the value of Mode II fracture toughness [60]. **Cantwell** [61, 62] while investigating the effect of loading rate in the fracture toughness of a carbon/PEEK composite material found that the Mode II interlaminar fracture toughness of the composite increased with increasing loading rate. The viscoelastic response exhibited by the matrix of the fiber-reinforced plastic and the interphase was suggested to influence the fracture toughness properties.

Maikuma et al [63] investigated the effect of loading rate on the fracture toughness of Center Notch Flexure (CNF) specimen geometries of carbon/PEEK and carbon/epoxy composites. The initiation value of fracture toughness was determined using a beam theory analysis and it was observed that the impact initiation toughness of

carbon/PEEK and carbon/epoxy composites were approximately 20% and 28% lower than their corresponding static values. This decrease was attributed to less ductile tearing and plastic deformation at higher loading rates. **Todo** et al [64] reported a 53% increase in the Mode II interlaminar fracture toughness of a carbon fiber reinforced polyamide as the loading rate was increased from 1 mm/minute to 1.1 m/s and attributed this effect to the positive rate sensitivity of the thermoplastic matrix. **Jar** et al [65, 66] used the CNF geometry to study the loading rate effects on the interlaminar fracture toughness of glass/epoxy, glass/vinylester, and glass/polyester composites and found dynamic values of interlaminar fracture toughness were about 60% of the static values. No explanations were given for the results.

Compston et al [67] investigated the effect of loading rate on the Mode II interlaminar fracture toughness of unidirectional glass fiber composites with brittle and rubber toughened vinyl ester matrices by conducting Mode II tests on ENF specimens at test rates ranging from 1 mm/minute to 3 m/s. There was no significant effect of loading rate on fracture toughness for the glass/vinyl ester composite. Fracture surface micrographs for the composite at different rates showed no significant difference in matrix deformation between rates and the clean fibers surfaces indicated significant interfacial failure at various rates. These observations supported the conclusion of no rate effect. **Chapman** et al [68] while investigating the effect of loading rate on Mode II interlaminar fracture toughness of ENF specimen geometries of carbon/PEEK and carbon/epoxy composites found a reduction in the fracture toughness of both composites

at high rates. The drop in toughness was attributed to a decrease in plastic deformation and change from ductile to brittle behavior as rate was increased.

Matsumoto et al [69] used the Curvature Driven Delamination (CDD) test to study the effect of loading rate on the Mode II interlaminar fracture toughness of glass/polycarbonate and glass/epoxy composites. The fracture toughness of the glass/polycarbonate composite increased by approximately 22% over 3 decades of loading rate and so did that of the glass/epoxy composite. However no explanations for the above trend were given. While investigating strain rate effects on Mode II fracture toughness of carbon/epoxy and carbon/PEEK composites using the End Loaded Split (ELS) test geometry, **Blackman** et al [70] found that fracture toughness of both the composites remained invariant of strain rate. In this investigation, similar to his previous work [44], he showed that great care must be taken in the experimental aspects when undertaking high rate tests.

1.3.3.3 Strain Rate Effects on Mixed Mode (I+II) Fracture Toughness of Fiber Reinforced Polymer Composites

Kusaka et al [71] used the Mixed Mode Flexure (MMF) specimen and Split Hopkinson Pressure Bar (SHPB) system to measure the mixed mode (I+II) fracture toughness of an interlayer toughened carbon fiber/epoxy composite system over a wide range of loading rates. The experimental results showed that the mixed mode fracture toughness was loading rate sensitive; the impact fracture toughness was about 30-38% lower than the static value. The microscopic fracture morphology was rather sensitive to loading rate: The impact fracture surface was smoother than the static fracture surface.

Cantwell et al [72] used MMF specimens to investigate the effect of loading rate on the Mixed Mode (I+II) fracture toughness of carbon/PEEK composites. Tests were conducted over 6 decades of loading rate and it was found that the mixed mode fracture toughness tended to increase slightly with loading rate. The increase in fracture toughness with loading rate was attributed to the increased localized damage that occurred at high rates of loading.

Blyton [73] investigated loading rate effects on the Mixed Mode (I+II) fracture toughness of carbon/epoxy, glass/polypropylene, and woven carbon/toughened epoxy composites and found all of the composites to be rate insensitive. **Blackman** et al [70] used the Fixed Ratio Mixed Mode (FRMM) test to investigate the effect of strain rate on the Mixed Mode (I+II) fracture toughness of carbon/epoxy and carbon/PEEK composites. The Mixed Mode (I+II) fracture toughness of both the composites was found to be strain rate invariant.

1.3.4 Conclusion

In this section an attempt is made to review all the work done in the past to investigate the strain rate effects on the Mode I, Mode II, and Mixed Mode (I+II) fracture toughness properties of fiber reinforced polymer composite materials. The work is summarized in Table 2. Over several decades of loading rate, the fracture toughness is some times found to change as much as 70%. An effort is made to elucidate the reasons given by the authors of the reviewed papers for the trends observed in their work. Upon reviewing the literature, there is a lack of consensus about the influence of loading rate on the fracture toughness properties. Hence more work needs to be done in the pursuit of

Table 2. Summary of Published Data on the Effects of Loading Rate on Fracture Toughness Properties

Authors	Materials Studied	Range of Rates Investigated	Observations
<i>Effect of Loading Rate on Mode I Fracture Toughness Properties</i>			
Daniel et al [38-41]	Carbon/Epoxy and Carbon/Elastomer Modified Epoxy	0.0075 mm/sec → 460 mm/sec	Fracture toughness increased for carbon/epoxy composites while decreased for carbon/elastomer modified epoxy composites with increasing loading rate.
Barbezat [42]	Carbon/Epoxy	20 mm/min → 3 m/sec	Fracture toughness was rate insensitive.
Gillespie Jr [43]	Carbon/Epoxy and Carbon/PEEK	0.25 mm/min → 250 mm/min	Fracture toughness of carbon/PEEK decreased with increasing loading rate while that of carbon/epoxy was rate insensitive.
Blackman et al [44]	Carbon/Epoxy and Carbon/PEEK	2 mm/min → 15 m/sec	Fracture toughness of carbon/PEEK decreased with increasing loading rate while that of carbon/epoxy remained invariant of strain rate.
Kusaka et al [45]	Carbon/Epoxy	0.01 mm/min → 20 m/sec	Fracture toughness was rate independent.

Table 2. Continued

Authors	Materials Studied	Range of Rates Investigated	Observations
Smiley and Pipes [46]	Carbon/Epoxy and Carbon/PEEK	4.2×10^{-6} m/sec $\rightarrow$ 6.7×10^{-1} m/sec	Fracture toughness decreased with increasing loading rate.
Vu-Khanh and Fisa [48]	Glass Flake/Polypropylene	0.01 m/sec $\rightarrow$ 5 m/sec	Fracture toughness decreased with increasing loading rate and after reaching a minimum value then increased with impact speed.
You and Yum [49]	Carbon/Epoxy	0.02 mm/sec $\rightarrow$ 120 mm/sec	Fracture toughness increased with increasing loading rate.
Karger-Kocs and Friedrich [50]	Short Glass/PEEK	0.1 mm/min $\rightarrow$ 1000 mm/min	Decrease in fracture toughness with increasing loading rate.
Mall et al [51, 52]	Carbon/PEEK	0.05 cm/min $\rightarrow$ 100 cm/min	Fracture toughness decreased with increasing loading rate.
Koh et al [53]	Silica Particulates/Epoxy	5 mm/min $\rightarrow$ 2.93 m/sec	Increase in fracture toughness with increasing loading rate.
Beguelin et al [54]	Graphite/PEEK	1×10^{-6} sec ⁻¹ $\rightarrow$ 8×10^{-1} sec ⁻¹	Fracture toughness decreased with increasing loading rate.

Table 2. Continued

Authors	Materials Studied	Range of Rates Investigated	Observations
<i>Effect of Loading Rate on Mode II Fracture Toughness Properties</i>			
Smiley and Pipes [55]	Carbon/Epoxy and Carbon/PEEK	4.2×10^{-6} m/sec $\rightarrow 9.2 \times 10^{-2}$ m/sec	Fracture toughness decreased with increasing loading rate.
Kageyama and Kimpara [56]	Carbon/Epoxy	Static $\rightarrow 8$ m/sec	Increase in fracture toughness with increasing loading rate.
Kusaka [57, 58]	Carbon/Epoxy	$10^{-5} \text{ sec}^{-1} \rightarrow 10^2 \text{ sec}^{-1}$	Fracture toughness decreased with increasing loading rate.
Berger and Cantwell [59, 60]	Carbon/Phenolic Resin and Carbon/PEEK	0.1 mm/min $\rightarrow$ 500 mm/min	Fracture toughness increased for carbon/PEEK composites while decreased for carbon/phenolic resin composites with increasing loading rate.
Cantwell [61, 62]	Carbon/PEEK	0.01 mm/min $\rightarrow 3$ m/sec	Increase in fracture toughness with increasing loading rate.
Maikuma et al [63]	Carbon/Epoxy and Carbon/PEEK	1.25 m/sec $\rightarrow 3$ m/sec	Decrease in fracture toughness with increasing loading rate.
Todo et al [64]	Carbon/Polyamide	1 mm/min $\rightarrow 1.1$ m/sec	Fracture toughness increased with increasing loading rate.

Table 2. Continued

Authors	Materials Studied	Range of Rates Investigated	Observations
Jar et al [65, 66]	Glass/Epoxy, Glass/Vinylester, and Glass/Polyester	1 mm/min → 3 m/sec	Decrease in fracture toughness with increasing loading rate.
Compston et al [67]	Glass/Vinylester	1 mm/min → 3 m/sec	Fracture toughness was rate independent.
Chapman et al [68]	Carbon/Epoxy and Carbon/PEEK	4.2×10^{-6} m/sec → 9.2×10^{-2} m/sec	Fracture toughness decreased with increasing loading rate.
Matsumoto et al [69]	Glass/Polycarbonate and Glass/Epoxy		Fracture toughness increased with increasing loading rate.
Blackman et al [70]	Carbon/Epoxy and Carbon/PEEK	1 mm/min → 5 m/sec	Fracture toughness was rate independent.
<i>Effect of Loading Rate on Mixed Mode (I+II) Fracture Toughness Properties</i>			
Blackman et al [70]	Carbon/Epoxy and Carbon/PEEK	1 mm/min → 5 m/sec	Fracture toughness was found to be rate invariant.
Kusaka et al [71]	Carbon/Epoxy	10^{-6} m/sec → 10 m/sec	Fracture toughness decreased with increasing loading rate.
Cantwell et al [72]	Carbon/PEEK	0.05 mm/min → 3 m/sec	Increase in fracture toughness with increasing loading rate.
Blyton [73]	Carbon/Epoxy and Glass/Polypropylene		Fracture toughness was rate independent.

eliminating all the disagreements that currently exist regarding the effect of loading rate on fracture toughness properties. In some studies, no attempt was made to explain the trends resulting from the investigation. Some of the researchers whose work has been reviewed in this section have shown that great care must be taken in the experimental aspects when conducting high rate tests. Lack of sensitivity towards dynamic effects by many researchers might be the cause of the lack of consensus on the effects of high rates on fracture toughness properties.

A couple of general statements can be made which suit most test results: Changes in loading rate can affect the properties of the polymer matrix, which can in turn decide the effects of loading rate on the fracture toughness of the composite. So basically, the rate sensitivity of the polymer matrix properties determines the rate sensitivities of the polymer composite. In addition, changes in loading rate can affect the failure mode in the composite, which can in turn decide the loading rate effects on the fracture toughness properties. Transition from a ductile to a brittle failure mode with increasing loading rates is accompanied by a reduction in the fracture toughness of the composite with increasing loading rates.

It must also be noted that there is no literature available on the effect of strain rate on fracture toughness properties of random chopped fiber reinforced composite materials, which can find extensive use in a wide range of load-bearing engineering and industrial process applications, primarily due to the low costs involved in their manufacture, in addition to the ease of manufacture. Hence, one suggests the need for

investigating and characterizing the strain rate effects on fracture toughness of random chopped fiber composites.

1.4 Literature Review of Strain Rate Effects on the Tensile, Compressive, Shear, and Flexural Properties of Polymer Composite Materials

1.4.1 Abstract

Below is a detailed review of the strain rate dependence of some mechanical properties of polymer composite materials. An attempt is made to present and summarize much of the published work relating to the effect of strain rate studies done in the past on the tensile, shear, compressive, and flexural properties of composite materials in order to better understand the strain rate effects on these mechanical properties of fiber reinforced polymer composite materials.

KEYWORDS: Polymer Composite Materials, Strain Rate, Mechanical Properties.

1.4.2 Introduction

High strain rate loading is probable in many of the applications where fiber reinforced polymer composites find use as candidate materials. It has always been a cause for concern that the mechanical properties of composite materials may be poor at high rates of strain. Hence, one ought to study how the mechanical properties of these composites would change with strain rate in order to design structures that would not fail prematurely and unexpectedly at high loading rates. Determination of dynamic mechanical properties of these composites would also ensure the design of composite

structures that are weight efficient and structurally sound when they are subjected to higher dynamic loads. The above argument reinforces the need for dynamic characterization of fiber reinforced polymer composite materials to understand the strain rate effects on their mechanical properties. In this section, an attempt is made to review much of the work published in the literature that investigates the strain rate effects on the tensile, shear, compressive and flexural properties of fiber reinforced polymer composite materials.

Inertial effects prevalent at elevated rates of strain are an experimental difficulty encountered by scientists investigating the effects of strain rate on performance properties of a composite material. For example, test fixtures can be subject to inertial disturbances at the medium to high rates of strain. These disturbances are due to the phenomena of mechanical resonance that the test equipment acquire at higher speeds. Inertial responses of test systems increase with test speed and obscure test data causing the analysis of the test data to be inaccurate. Therefore, it is important for investigators to overcome the inertial problems while studying strain rate effects on composites.

Various test methods have different advantages and limitations and have to be chosen appropriately to produce good and comparable results. The drop weight impact test allows easy variation of strain rate and is inexpensive. However, it is difficult to increase the maximum limit of strain rate since the speed is directly related to drop height. The use of hydraulic machines is convenient and accurate but they are expensive and the strain rate is limited. Hopkinson bars are used for dynamic characterization above 1000 sec^{-1} . However, the system is very sensitive to the contact surface

conditions. The use of thin ring specimens under internal or external pressure can also be used for high rate dynamic testing but it is expensive and complex.

1.4.3 Literature Survey

Davies and **Magee** [74, 75] studied the effect of strain rate on the ultimate tensile strength of glass/polyester composites. They reported the glass/polyester composites to be rate sensitive with the magnitude of the ultimate tensile strength increasing by 55% over the strain rate change. **Rotem** and **Lifshitz** [76] investigated the effect of strain rate on the tensile properties of unidirectional glass fiber /epoxy composites and found that the dynamic strength is three times the static value and the dynamic modulus is 50% higher than the static value. However while investigating angle ply glass/epoxy laminates **Lifshitz** [77] found that the elastic modulus was independent of strain rate and the dynamic failure stress was only moderately higher than the static value (20-30% higher).

The dependence of the transverse tensile properties on strain rate of a high performance carbon/epoxy composite loaded in transverse tension was investigated by **Melin** and **Asp** [78]. Dog-bone shaped specimens were tested in quasi-static and dynamic loading conditions (10^{-3} - 10^3 s^{-1}). The average transverse modulus was observed to be independent of strain rate while the initial transverse modulus was found to decrease slightly with increased strain rate. The strain to and stress at failure was found to increase slightly with increased strain rate. Thus when loaded in the transverse direction it was concluded that the carbon/epoxy composite exhibited a weak dependence on strain rate.

Tensile tests were performed on a glass epoxy laminate at different rates (1.7×10^{-2} -2000 mm/s) by **Okoli** and **Smith** [79, 80] to determine the effects of strain rate on the Poisson's ratio (Ratio of transverse strain to the corresponding axial strain below the proportional limit) of the material. The Poisson's ratio was found to be rate insensitive. It was suggested that the rate insensitivity in the Poisson's ratio of the laminates tested is due to the presence of fibers in the composites. The effect of strain rate on the tensile properties of a glass/epoxy composite was investigated by **Okoli** and **Smith** [81]. The tensile strength of the composite was found to increase with strain rate. This increase in tensile strength with strain rate was attributed to the increased strength of the glass fibers with strain rate.

In other studies the effects of strain rate on the tensile, shear, and flexural properties of glass/epoxy laminate was investigated by **Okoli** and **Smith** [79, 82]. Tensile modulus increased by 1.82%, tensile strength increased by 9.3%, shear strength increased by 7.06% and shear modulus increased by 11.06%, per decade increase in log of strain rate [79]. The above observation was in agreement with the results of the investigation conducted by **Armenakas** and **Sciamarella** [83] that suggested a linear variation of the tensile modulus of elasticity of unidirectional glass/epoxy composites with the log of strain rate. However, the ultimate tensile strain and stress of the composite decreased with the increase in strain rate. An increase in tensile, shear, and flexural energy of 17%, 5.9%, and 8.5 % respectively, per decade of increase in the log of strain rate was observed [82]. The study indicated that it is a change in failure modes as strain rate is increased, which brought about the increase in energy observed.

Work done by **Vashchenko** et al [84] on glass/polyamide composites also suggested a linear relationship between the tensile strength characteristics of the composite and the log of strain rate. A systematic study of the strain rate effects on the mechanical behavior of glass/epoxy angle ply laminates was done by **Staab** and **Gilat** [85, 86] using a direction tension split Hopkinson bar apparatus for the high strain rate tests and a servo hydraulic testing machine for the quasi-static tests. The tensile tests at higher strain rates (in the order of 1000 sec^{-1}) showed a marked increase in the maximum normal stress and strain when compared to the values obtained in the quasi-static tests. Although both fibers and matrix are strain rate sensitive, the fibers were thought to influence laminate rate sensitivity more than the matrix.

Harding and **Welsh** validated a dynamic tensile technique by performing tests (over the range 10^{-4} to 1000 s^{-1}) on graphite/epoxy, glass/epoxy, glass/polyester, graphite/polyester, and Kevlar/polyester composites [87, 88]. The modulus, failure stress, and failure mode of the graphite/epoxy composite was found to be strain rate insensitive. The dynamic modulus and strength for the glass/epoxy composite was about twice the static value. This increase in failure strength was explained based on the observed change in failure mode. Similarly, the elastic tensile modulus of the glass/polyester, graphite/polyester, and Kevlar/polyester composites increased with strain rate and the strain rate dependence of the elastic modulus was suggested to be derived from the elastic interaction between the reinforcement and the matrix and was determined by the strain rate dependence of the matrix strength.

Tensile tests were performed at up to five displacement rates, from about 1 mm/s to 30000 mm/s, by **Roberts** and **Harding** [89] to determine the effect of strain rate on the tensile properties of a glass/phenolic resin composite. A significant increase in the tensile strength, stiffness, and displacement at failure was observed at higher displacement rates. This was attributed to the rate dependence of the resistance of the resin matrix to fiber straightening and of the fracture strength of the glass fibers. The tensile mechanical behavior of a short carbon fiber filled liquid crystalline polymer (LCP) composite, Vectra A320, was examined under static loading (10^{-2} s^{-1}) and dynamic loading (400 s^{-1}) by **Shim** et al [90]. A pendulum type tensile split Hopkinson bar device was used to apply dynamic tension. The fracture strain and the Young's modulus of the composite were found to be noticeably influenced by changes in the strain rate. Experimental studies on the effects of strain rate on the tensile properties of glass bead/HDPE composites were conducted by **Bai** et al [91]. Both the Young's modulus and the tensile strength of the glass bead/HDPE composite were found to increase with strain rate.

Daniel et al [92] investigated the dynamic response of carbon/epoxy composites at high strain rates using three different test methods. In the first test method used for dynamic testing of thin laminates in tension, a carbon/epoxy laminate was characterized under longitudinal, transverse, and in-plane shear loading at strain rates up to 500 s^{-1} . In the longitudinal direction, the modulus increased moderately with strain rate (up to 20% over the static value) but the strength and ultimate strain did not vary significantly. The modulus and strength increased sharply over static values in the

transverse (to the fiber) direction but the ultimate strain only increased slightly. There was a 30% increase in the in-plane shear modulus and strength. In the second test method used for dynamic testing of thin laminates in compression, longitudinal properties were obtained up to a strain rate of 90 s^{-1} . The longitudinal modulus increased with strain rate (up to 30% over the static value) but the strength and ultimate strain were equal to or a little lower than static values. The dynamic modulus and strength at 210 s^{-1} increased sharply over static values in the transverse (to the fiber) direction while the ultimate strain was lower than the static one. There was a 30% increase in the in-plane shear modulus and strength. . In the third test method used for dynamic testing of thick laminates in compression, transverse properties were obtained up to a strain rate of 80 s^{-1} . The transverse modulus moderately increased with strain rate (up to 18% over the static value) but the strength and ultimate strain increased by 50% and 30% over corresponding static values.

Hayes and **Adams** constructed a specialized pendulum impactor to investigate the strain rate effects on the tensile properties of unidirectional glass/epoxy and graphite/epoxy composites [93]. The modulus and strength of the glass/epoxy composites were concluded to be rate insensitive at impact speeds in the range of 2.7 to 4.9 m/s. However, the modulus and strength of the graphite/epoxy composites decreased with increasing impact speeds. **Daniel** and **Liber** [94, 95] attempted to characterize the effect of strain rate on the mechanical properties of unidirectional boron/epoxy, glass/epoxy, graphite/epoxy, and Kevlar/epoxy composites. While the Kevlar/epoxy composite showed a 20% increase in tensile modulus and failure strength in the fiber

direction with increasing strain rate from 10^{-4} to 27 s^{-1} , the tensile modulus and failure strength of the boron/epoxy, glass/epoxy, and graphite/epoxy composites were found to be rate insensitive. The increase in modulus and failure strength of the Kevlar/epoxy composite was 40% and 60% respectively during transverse and shear (off-axis) loading.

Work done by **Chamis** and **Smith** [96] and further investigations by **Daniel** et al [97] on unidirectional graphite/epoxy laminates yielded similar results where in the tensile strength in the fiber direction did not change with strain rate. However, there was an increase in the transverse tensile properties and shear properties with increasing loading rate.

The effect of strain rate (10^{-3} to 2000 s^{-1}) on the tensile properties of glass/polyester, glass/epoxy, graphite/epoxy, and graphite short fiber reinforced nylon 6,6 composites were investigated by **Kawata** et al [98, 99]. The strength of the graphite/epoxy and graphite/nylon 6,6 composites increased with strain rate while that of the glass/epoxy and glass/polyester composites decreased. The influence of strain rate on the tensile properties of glass/phenolic resin and glass/polyester resin composites was studied by **Barre** et al [100]. The elastic modulus and strength were found to increase with strain rate. **Peterson** et al [101] studied the tensile response of chopped glass fiber reinforced styrene/maleic anhydride materials in the range 10^{-3} to 10 s^{-1} and observed a 50% to 70% increase in the elastic modulus and strength with increase in strain rate.

Groves et al attempted to characterize the high strain rate response (in tension and compression) of continuous carbon/epoxy composites [102]. Strain rates from 0 to 100 s^{-1} were generated using conventional and high speed hydraulic test

machines, 10 to 1000 s^{-1} were generated using a high energy drop tower, from 1000 to 3000 s^{-1} were generated using a split Hopkinson bar. The experimental results indicated an increase in both the compression and tensile properties (strength and modulus) with increasing strain rate. **Powers** et al [103] used a split Hopkinson pressure bar to obtain compressive mechanical properties of a unidirectional graphite epoxy composite at different strain rates varying from 49 sec^{-1} to 1430 sec^{-1} . For each of the three principal directions, the yield stress increased with strain rate and so did the elastic strain energy. However, the ultimate strength, modulus of elasticity, and strain energy density to failure were found to be strain rate insensitive. In another study, a split Hopkinson pressure bar was used by **Powers** et al [104] to obtain compressive mechanical properties of graphite/epoxy composites and graphite/polyimide composites. For both the composites, in all the three directions, the modulus of elasticity, strain to failure and mean ultimate strength did not change with strain rate.

Li et al [105] investigated the effect of strain rate on the compression stress strain characteristics of a short glass fiber reinforced thermotropic liquid crystalline polymer (an aromatic copolyester consisting of p-hydroxybenzoic acid (HBA) and 2,6—hydroxy-naphthoic acid (HNA)) over a wide range of strain rates (10^{-4} to 350 s^{-1}). The low strain rate compression tests were conducted using an Instron universal tester while the high strain rate tests were carried out using a split Hopkinson pressure bar (SHPB) technique. The compression modulus was found to be insensitive to strain rate in the low strain rate regime (10^{-4} to 10^{-2} s^{-1}) but it increased more rapidly with strain rate at higher strain rates. The compression strength changed linearly with log of strain rate over the

entire strain rate range. Macroscopic inspection of the compression-failed specimens indicated that the strain rate had a strong influence on the failure mode.

Takeda and **Wan** [106] studied the effects of strain rate on the compression strength of unidirectional glass fiber reinforced polyester resin composites using the compression type improved split Hopkinson pressure bar apparatus, where the impact loading can be stopped at any moment in the impact process so that the specimen can be recovered at various levels of loading. The compressive strength was found to increase with increasing strain rates. **Tzeng** and **Abrahamian** [107-109] attempted to characterize the dynamic responses of composite materials for ballistic engineering applications. An experimental set up had been developed to investigate the dynamic effects on graphite/epoxy composite materials at strain rates typically found during launching of a projectile. An air gun system and a test fixture with a designed crashing mechanism was used to simulate a loading condition resulting from gun firing. Strain rate effects on the compressive strength of graphite/epoxy composites with lay up construction of $[(0/45/-45/0)_4]$ were determined at strain rates of 10-100 in/s. A 10% increase in the compressive strength was observed with increasing strain rate. A 1.5% strain was measured under impact failure, which is greater than the ultimate strain of 1.1% at a static loading condition.

Amijima and **Fuji** [110] investigated the strain rate effects on the compressive strength of unidirectional glass/polyester and woven glass/polyester composites and found that the compressive strength of both the composites increased with strain rate, the increase being higher in the case of the woven composite. Study of

the effect of strain rate (over the range 10^{-3} to 600 s^{-1}) on the compressive strength of unidirectional graphite/epoxy composite specimens by **Cazeneuve** and **Maile** [111] highlighted a 50% increase in the longitudinal strength and a 30% increase in the transverse strength

Sims et al [112] investigated the effect of strain rate on the flexural strength of glass mat/polyester laminates and reported increasing flexural strengths over a wide range of displacement rates from 10^{-6} to 10^{-1} m/s .

1.4.4 Conclusion

It can be seen from this detailed review that the effect of varying loading rate on the tensile, compressive, shear, and flexural properties of fiber reinforced composite materials has been investigated by a number of workers and a variety of contradictory observations and conclusions have resulted which is summarized in Table 3. Hence more work needs to be done in the pursuit of eliminating all the disagreements that currently exist regarding the effect of loading rate on the tensile, compressive, shear, and flexural properties of a fiber reinforced polymer composite material. There is also not much literature available on the effect of strain rate on the tensile, compressive, and shear properties of random chopped fiber reinforced composite materials, which can find extensive use in a wide range of load-bearing engineering, and industrial process applications primarily due to the low costs involved in their manufacture in addition to the ease of manufacture. Hence, one suggests the need for investigating and characterizing in detail the strain rate effects on the tensile, compressive, shear, and other mechanical properties of random chopped fiber composites.

Table 3. Summary of Published Data on the Effects of Loading Rate on Tensile, Compressive, Shear, and Flexural Properties

Authors	Materials Studied	Range of Rates Investigated	Observations
Davies and Magee [74, 75]	Glass/Polyester	$10^{-3} \text{ sec}^{-1} \rightarrow 10^3 \text{ sec}^{-1}$	Increase in ultimate tensile strength with increasing loading rate.
Rotem and Lifshitz [76]	Glass/Epoxy	$10^{-6} \text{ sec}^{-1} \rightarrow 30 \text{ sec}^{-1}$	Tensile strength and modulus increased with increasing loading rate for unidirectional glass/epoxy composites.
Lifshitz [77]	Glass/Epoxy	Static $\rightarrow 4.2 \text{ m/sec}$	Tensile modulus and failure stress were strain rate independent for the angle ply glass/epoxy laminate.
Melin and Asp [78]	Carbon/Epoxy	$10^{-3} \text{ sec}^{-1} \rightarrow 10^3 \text{ sec}^{-1}$	Transverse tensile properties only exhibited a weak dependence on strain rate.
Okoli and Smith [79, 81, 82]	Glass/Epoxy	$0.008 \text{ mm/sec} \rightarrow 4 \text{ m/sec}$	Tensile strength, tensile modulus, shear strength, and shear modulus increased with increasing loading rate. Increase in tensile, shear, and flexural energy with increase in loading rate.
Armenakas and Sciamarella [83]	Glass/Epoxy	$0.0265 \text{ min}^{-1} \rightarrow 30000 \text{ min}^{-1}$	Tensile modulus increased with increasing loading rate while ultimate tensile strain and stress decreased with increasing loading rate.

Table 3. Continued

Authors	Materials Studied	Range of Rates Investigated	Observations
Vashchenko et al [84]	Glass/Polyamide	3.3×10^{-5} m/sec $\rightarrow$ 12 m/sec	Tensile strength increased with increasing loading rate.
Staab and Gilat [85, 86]	Glass/Epoxy	$10^{-5} \text{ sec}^{-1} \rightarrow 10^3 \text{ sec}^{-1}$	Maximum tensile stress and strain increased with increasing loading rate.
Harding and Welsh [87, 88]	Graphite/Epoxy, Glass/Epoxy, Glass/Polyester, Graphite/Polyester, Kevlar/Polyester	$10^{-4} \text{ sec}^{-1} \rightarrow 10^3 \text{ sec}^{-1}$	Tensile modulus and failure stress for graphite/epoxy were strain rate insensitive. Tensile modulus for glass/epoxy, glass/polyester, graphite/polyester, and Kevlar/polyester increased with increasing loading rate.
Roberts and Harding [89]	Glass/Phenolic Resin	1 mm/sec $\rightarrow$ 20000 mm/sec	Increase in tensile strength, stiffness, and displacement with increasing loading rate.
Bai et al [91]	Glass Bead/HDPE	$3 \times 10^{-5} \text{ sec}^{-1} \rightarrow 8 \times 10^{-3} \text{ sec}^{-1}$	Tensile modulus and strength increased with increasing loading rate.

Table 3. Continued

Authors	Materials Studied	Range of Rates Investigated	Observations
Daniel et al [92]	Carbon/Epoxy	$1 \times 10^{-4} \text{ sec}^{-1}$ $\rightarrow 500 \text{ sec}^{-1}$	Longitudinal tensile and compression modulus increased with increasing loading rate. Longitudinal tensile and compression strength and strain were loading rate insensitive. Transverse tensile and compression modulus and strength increased with increasing loading rate while the tensile strain was loading rate insensitive.
Hayes and Adams [93]	Glass/Epoxy and Graphite/Epoxy	1.7 mm/sec $\rightarrow 4.9 \text{ m/sec}$	Tensile modulus and strength of glass/epoxy was strain rate insensitive while that of graphite/epoxy decreased with increasing loading rate.
Daniel and Liber [94, 95]	Boron/Epoxy, Glass/Epoxy, Kevlar/Epoxy, Graphite/Epoxy	$1.4 \times 10^{-4} \text{ sec}^{-1}$ $\rightarrow 27 \text{ sec}^{-1}$	Increase in tensile modulus and failure strength of Kevlar/epoxy with increasing loading rate while that of boron/epoxy, glass/epoxy, and graphite/epoxy remained strain rate insensitive.

Table 3. Continued

Authors	Materials Studied	Range of Rates Investigated	Observations
Chamis and Smith [96]	Graphite/Epoxy	Static $\rightarrow$ 381 sec ⁻¹	Longitudinal tensile strength for graphite/epoxy was loading rate insensitive. Transverse tensile and shear properties increased with increasing loading rate.
Daniel et al [97]	Graphite/Epoxy	100 sec ⁻¹ $\rightarrow$ 500 sec ⁻¹	Longitudinal tensile strength for graphite/epoxy was loading rate insensitive. Transverse tensile and shear properties increased with increasing loading rate.
Kawata et al [98, 99]	Glass/Polyester, Glass/Epoxy, Graphite/Epoxy, Short Graphite Fiber/Nylon 6,6	0.001 sec ⁻¹ $\rightarrow$ 2000 sec ⁻¹	Tensile strength for graphite/epoxy and graphite/nylon 6,6 increased while that of glass/epoxy and glass/polyester decreased with increasing loading rate.
Barre et al [100]	Glass/Polyester and Glass/Phenolic Resin	0.1 sec ⁻¹ $\rightarrow$ 10 sec ⁻¹	Tensile modulus and strength increased with increasing loading rate.
Paterson et al [101]	Chopped Glass Fiber in Styrene/Maleic Anhydride Resin	1.67×10^{-3} sec ⁻¹ $\rightarrow$ 6 sec ⁻¹	Tensile modulus and strength increased with increasing loading rate.
Groves et al [102]	Carbon/Epoxy	0 sec ⁻¹ $\rightarrow$ 3000 sec ⁻¹	Compressive and tensile properties (strength and modulus) increased with increasing loading rate.

Table 3. Continued

Authors	Materials Studied	Range of Rates Investigated	Observations
Powers et al [103, 104]	Graphite/Epoxy and Graphite/Polyimide	$49 \text{ sec}^{-1} \rightarrow 1430 \text{ sec}^{-1}$	Compression yield stress and elastic strain energy increased with increasing loading rate for the graphite/epoxy composite while the ultimate strength and modulus of elasticity were strain rate insensitive for both the composites.
Li et al [105]	Short Glass Fiber/Liquid Crystalline Polymer	$10^{-4} \text{ sec}^{-1} \rightarrow 350 \text{ sec}^{-1}$	Compression modulus and strength increased with increase in loading rate.
Takeda and Wan [106]	Glass/Polyester	$10^{-3} \text{ sec}^{-1} \rightarrow 750 \text{ sec}^{-1}$	Compression strength increased with increasing loading rate.
Tzeng and Abrahamian [107-109]	Graphite/Epoxy	$10 \text{ in/sec} \rightarrow 100 \text{ in/sec}$	Compression strength and strain increased with increasing loading rate.
Amijima and Fuji [110]	Glass/Polyester	$10^{-3} \text{ sec}^{-1} \rightarrow 10^3 \text{ sec}^{-1}$	Compression strength increased with increasing loading rate.
Cazeneuve and Maile [111]	Graphite/Epoxy	$10^{-3} \text{ sec}^{-1} \rightarrow 600 \text{ sec}^{-1}$	Longitudinal and transverse compression strength increased with increasing loading rate.
Sims et al [112]	Glass mat/Polyester	$10^{-6} \text{ m/sec} \rightarrow 10^{-1} \text{ m/sec}$	Increase in flexural strength with increasing loading rate.

1.5 Final Conclusions Drawn from the Literature Review

Upon reviewing the literature, it can be seen that the influence of strain rate on the energy absorption, fracture toughness, tensile, compressive, shear, and flexural properties of fiber reinforced polymer composite materials has been investigated by a number of workers and it has resulted in a variety of contradictory observations and conclusions. Hence, more work needs to be done in the pursuit of developing a general consensus about the influence of strain rate on performance properties. There is also no literature available on the effect of strain rate on the energy absorption and crushing characteristics, fracture toughness, and joint properties of adhesively bonded composite structures. Therefore, one suggests the need for investigating and characterizing in detail the strain rate effects on these various performance properties of bonded composite structures.

CHAPTER 2

EXPERIMENTAL

2.1 Project Objective

Carbon fiber composite materials display superior specific energy absorption and if properly designed can be used to manufacture efficient crashworthy structures. Studying the effects of strain rate on adhesively bonded carbon fiber composite structures is our goal since composite and adhesive performance properties are found to be rate dependent [1, 3, 6, 12-112].

Adhesive joining is recognized as a potential enabling technology for a variety of material systems (including fiber reinforced composites and polymers) being considered in automotive structures where traditional fastening modes would be inappropriate. Additionally in certain circumstances, adhesive bonding of steel structures can provide benefits such as improved stiffness and/or reduction of stress concentrations common with welded, riveted, and bolted joints. Nevertheless, there is hesitation on the part of industry to replace traditional fasteners in primary structure applications for the most part due to the limited understanding of joint performance over the life of the structure (that is, overall joint toughness, creep and fatigue). Hence, there is a need to not only measure joint performance at the coupon level, but to be extended to include structural components intended as energy absorption structures. Provided with recent developments concerning composite crashworthiness the next logical step would be determining the correlation between measurable adhesive joint parameters, and their

influence on the structure to dissipate energy and ultimately predict crashworthiness for a particular design. Once the parameters that have significant influence on energy dissipation have been identified, they can be incorporated in modeling schemes to carry out the predictions on a variety of structural designs. Validity of these models can then be accessed through structural crash tests. The key to the development of a comprehensive experimental methodology to analyze and design adhesively bonded automotive composite structures to sustain axial, off-axis and lateral crash/impact loads is the understanding of how critical joint design parameters, example bond length and bond thickness, affect the energy absorption. The substrate, adhesive and representative subcomponent joint geometry selected for evaluation are assessed based on both static and dynamic testing at the coupon level followed by the building and testing of components under static and dynamic impact loads. The experimental results will be correlated with analytical results by developing finite element based analytical tools with appropriate material models and progressive damage algorithms.

2.2 Materials

Materials selection and screening was executed primarily by the ACC in consultation with Visteon and ORNL. Epoxy was chosen as the matrix resin for the carbon fiber composites. To focus on adhesive joint related issues, it is desirable to eliminate any variability or issues associated with the substrate material. Initial efforts to obtain consistent mechanical properties on chopped carbon fiber materials were unsuccessful. Therefore, a continuous braided carbon fiber composite was selected to

replace the chopped carbon fiber material for this study. The woven fabric prepreg comprised of T300B carbon fibers with a tow size of 3K and 42% (by weight) epoxy resin. The adhesive material system used was an epoxy system designated as Sovereign PL731. The Sovereign PL731 is a commercially available, two-part epoxy system produced by Sovereign Specialty Adhesives Inc. (Chicago, IL). The surface preparation and cure cycle was also selected in consultation with the ACC and the manufacturer.

2.3 Substrate

The carbon fiber reinforced polymer composite substrate material testing consisted of tension, compression, and shear tests. The strain rate dependence of the substrate was investigated by testing them as per ASTM standard D3039 [113] for tension. ASTM D3410 [114] and ASTM D5379 [115] were used for compression and in-plane shear strength tests, respectively. While dog bone specimens of the substrate having dimensions 215×25×3 mm were used for tensile testing, the rectangular shaped compression test specimens shown in Figure 6 had dimensions of 140×13×3 mm. The degree of anisotropy in the substrate material was qualified by testing specimens that are machined from two different orthogonal directions in the panels. Complete (unbonded) tubes of braided carbon/epoxy composite materials were tested using axial impact loads as part of our effort to establish their strain rate dependence. The tube geometry was square with nominal dimensions of 100×100×2 mm, and the length was 329 mm. Tests were conducted at three different dynamic loading rates. The intermediate strain rate test machine, TMAC, at ORNL was used for the dynamic testing. A minimum of five



Figure 6. Substrate Compression Test Specimen

identical specimens were tested under similar test conditions in order to achieve a set of reliable data. Table 4 details the test plan for the Carbon Fiber Reinforced Composite Substrate. Exceptions to the above statement were due to either the sample being very precious or the sample fabrication being extremely difficult. Hence, tube specimens were tested in replicates of three under similar test conditions.

2.4 Bulk Adhesive and Bonded Joints

In an effort to develop a comprehensive experimental methodology to analyze and design adhesively bonded automotive composite structures to sustain axial, off-axis and lateral crash/impact loads an understanding of the effects of critical joint design parameters such as bond thickness and bond length were accomplished using bonded tubes and the coupon level double notch and single lap joint geometries. Figure 7 displays an untested single-lap shear (SLS) and double-notch shear (DNS) test specimen. The coupon-level joint tests for SLS and DNS test specimen geometries with varying bond thicknesses were conducted based on ASTM D3165 [116] and ASTM D3846 [117], respectively. The tests were conducted at different rates and the overlap length of the joint in all of the tests was 12.7 mm. All of the coupon-level joint tests were conducted in tension.

Mode I [118] and Mode II [119] fracture toughness tests were conducted to determine the fracture characteristics of the joint. Fracture tests were performed on 25.4 mm wide adhesively bonded standard Double Cantilever Beam (DCB) type specimens at various loading rates in pursuit of determining the fracture energies associated with

Table 4. Test Plan for the Carbon Fiber Reinforced Composite Substrate

Test	Measured Parameters	Standard	Replicates	Geometry	Rates
Coupon	0 Tensile Strength and Failure Strain	D3039	5	215×25×3 mm	Quasi-static + 3 dynamic
	90 Tensile Strength and Failure Strain	D3039	5	215×25×3 mm	Quasi-static + 3 dynamic
	0 Compressive Strength and Failure Strain	D3410	5	140×13×3 mm	Quasi-static
	90 Compressive Strength and Failure Strain	D3410	5	140×13×3 mm	Quasi-static
	0 In-Plane Shear Strength and Failure Strain	D5379	3	75×20×3 mm	Quasi-static
	90 In-Plane Shear Strength and Failure Strain	D5379	3	75×20×3 mm	Quasi-static
Tube	Axial Impact Force, Displacement	N/A	3	100×100×L329 mm Square Tube	3 Dynamic

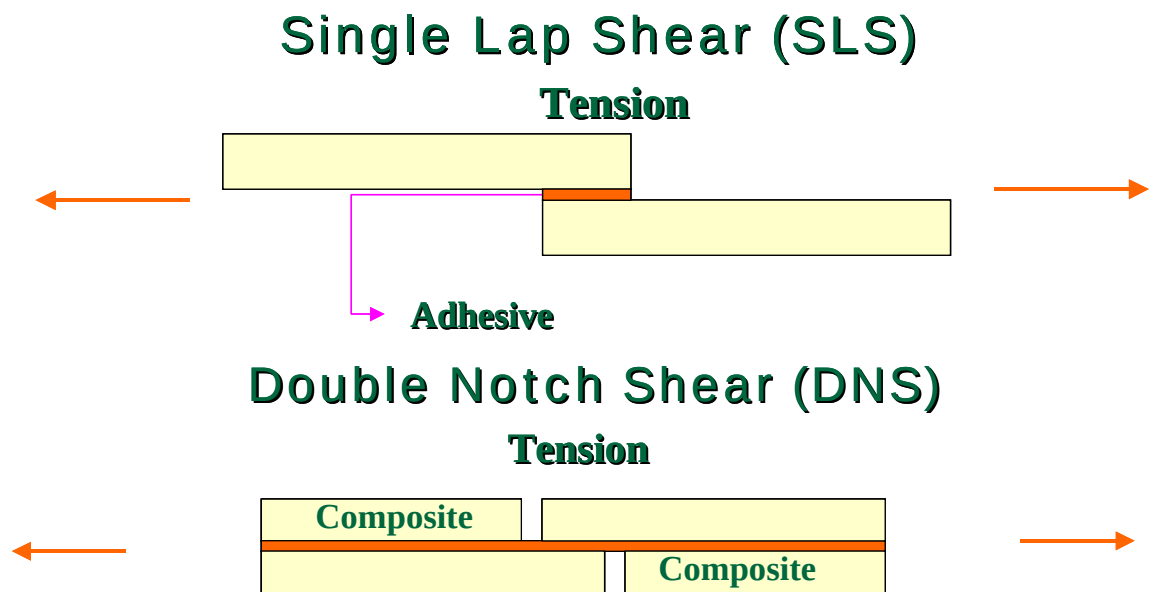


Figure 7. An Untested SLS and DNS Test Specimen

debonding in the joints. Adherends consisting of carbon fiber braided composites with varying thicknesses (11, 20, and 36 ply) and the PL731 adhesive were used to make the bonded panels. Figure 8 illustrates a standard DCB specimen used for Mode I fracture toughness testing. New test method development like the driven wedge technique was undertaken during the fracture toughness testing to get more fracture energy data points per specimen and used over a wide range of loading rates. Mode II fracture tests were conducted to calculate the fracture energies in adhesively bonded standard End Load Split (ESL) specimens illustrated in Figure 9. All of the above fracture toughness tests were done at various rates to investigate the rate effects on the joint performances.

The tensile, compressive, shear, and fracture toughness properties of the bulk adhesive were also determined for comparison purposes. Conducting tests as per ASTM D638 [120] and ASTM D695 [121] characterized the tensile and compression properties of the bulk adhesive, respectively. While dog bone specimens of the bulk adhesive shown in Figure 10 having dimensions 215×25×3 mm were used for tensile testing the rectangular shaped compression test specimens had dimensions of 140×13×3 mm. Refusal of the bulk adhesive to fail in shear resulted in the need for innovative test method development to ultimately determine the shear properties. A preliminary shear test for the bulk adhesive was conducted by applying a torsional load to a solid cylindrical rod specimen. The rod was 12 mm in diameter and 100 mm long. Additional shear tests for the bulk adhesive, where in the solid rod specimen geometry was modified to a hollow cylinder, were also conducted. Finally ASTM D5379 [115] traditionally used to determine the in-plane shear strength of polymer composites was employed for the

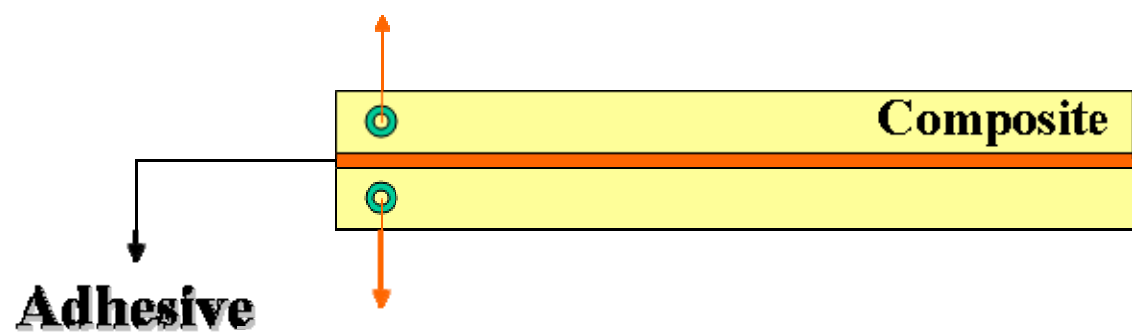


Figure 8. DCB Specimen to Determine Mode I Fracture Toughness

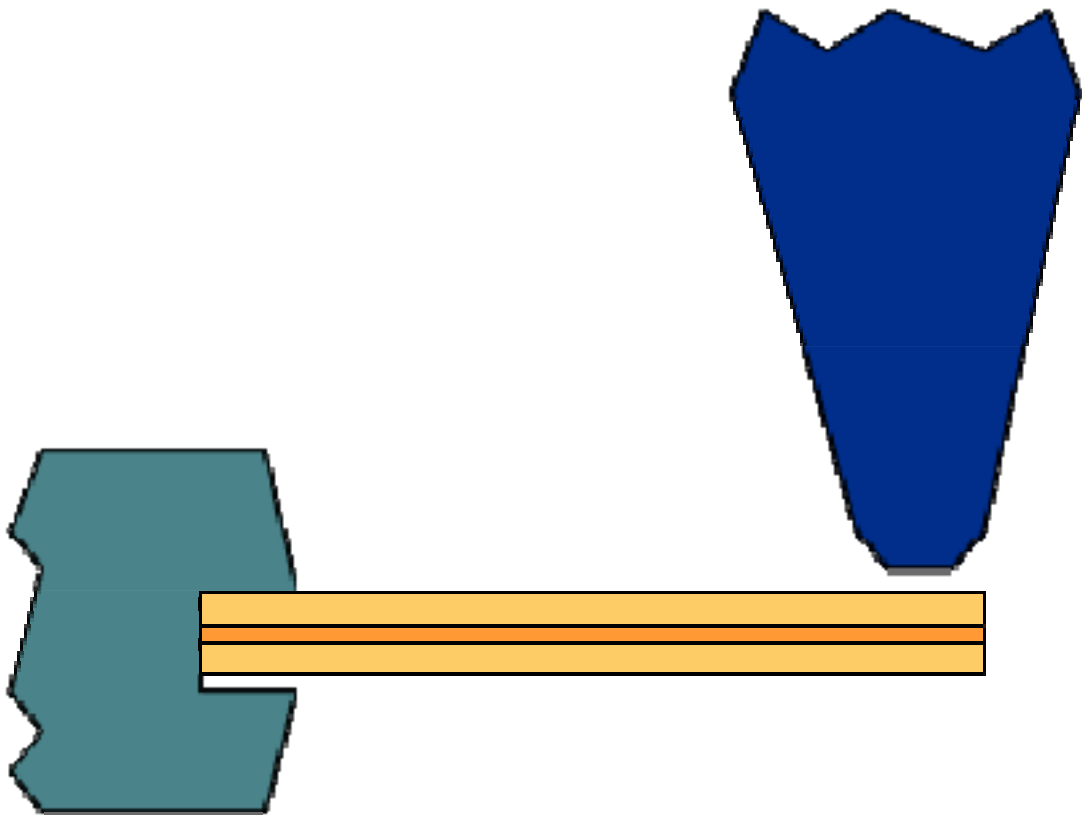


Figure 9. ELS Specimen to Determine Mode II Fracture Toughness



Figure 10. Typical Dog Boned Bulk Adhesive Specimen used for Tensile Testing

bulk adhesive. Figure 11 illustrates the different test specimen geometries used for determining the shear properties of the bulk adhesive. Quasi-static and dynamic fracture toughness tests were completed on the bulk adhesive using the compact tension (CT) specimen geometry. The specimens were machined from an 8-mm-thick bulk adhesive plaque per the geometry specified in ASTM D5045 [122]. Figure 12 illustrates a compact tension specimen showing dimensions for 8 mm wide specimens. An untested and tested compact tension specimen is shown in Figure 13.

Adhesively bonded tubes of carbon/epoxy composites were dynamically tested under axial impact loads to establish their strain rate dependence and also to accomplish an understanding of the effects of critical joint design parameters such as bond thickness and bond length. The bonded tube geometry was square with nominal dimensions of 100×100×2 mm, and the length was 318 mm. The bonded tubes were fabricated by using two c-channels having ply drop-offs to form a stepped scarf joint. Tests were conducted at three different dynamic loading rates. The overlap lengths of the joint investigated in the tube tests were 12.5 and 25 mm while the bond thicknesses were 0.5 and 1.0 mm. TMAC, the intermediate strain rate test machine at ORNL, was used for the dynamic testing of the bonded tubes. Testing of the complete tubes at various rates was used to establish a base line SEA. All the data generated from the various tests mentioned above can be used to guide the joint design such that the bonded sections will be built to either fail or not fail in the joint. A minimum of five identical specimens were tested under similar test conditions in order to achieve a set of reliable data. Exceptions to the above statement were due to the sample fabrication being extremely difficult.

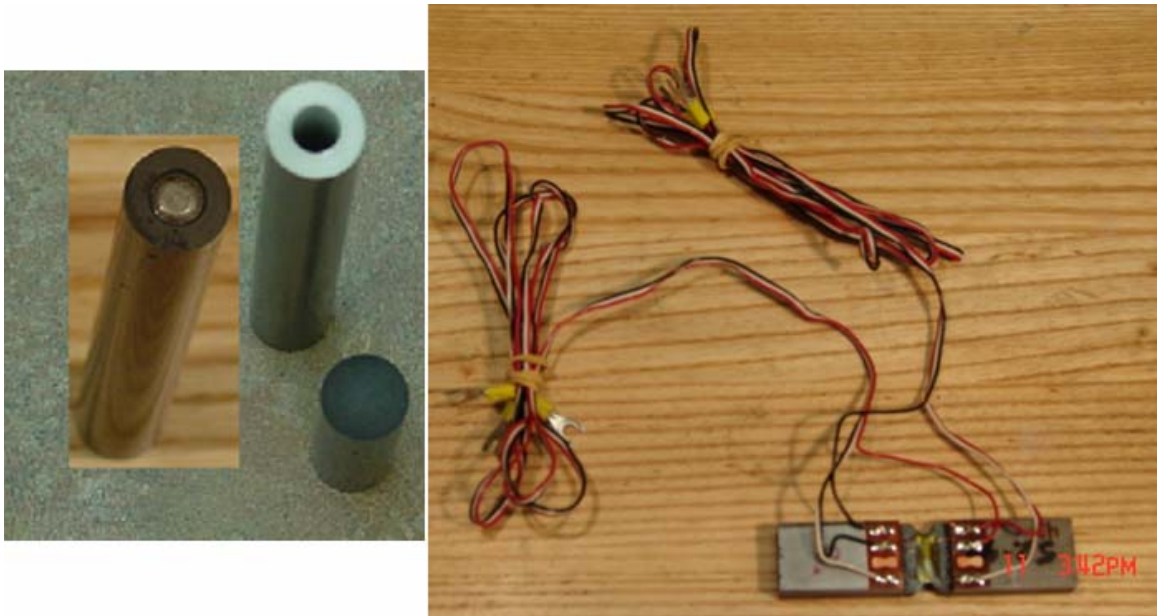


Figure 11. Different Test Specimen Geometries used for Determining the Shear Properties of the Bulk Adhesive

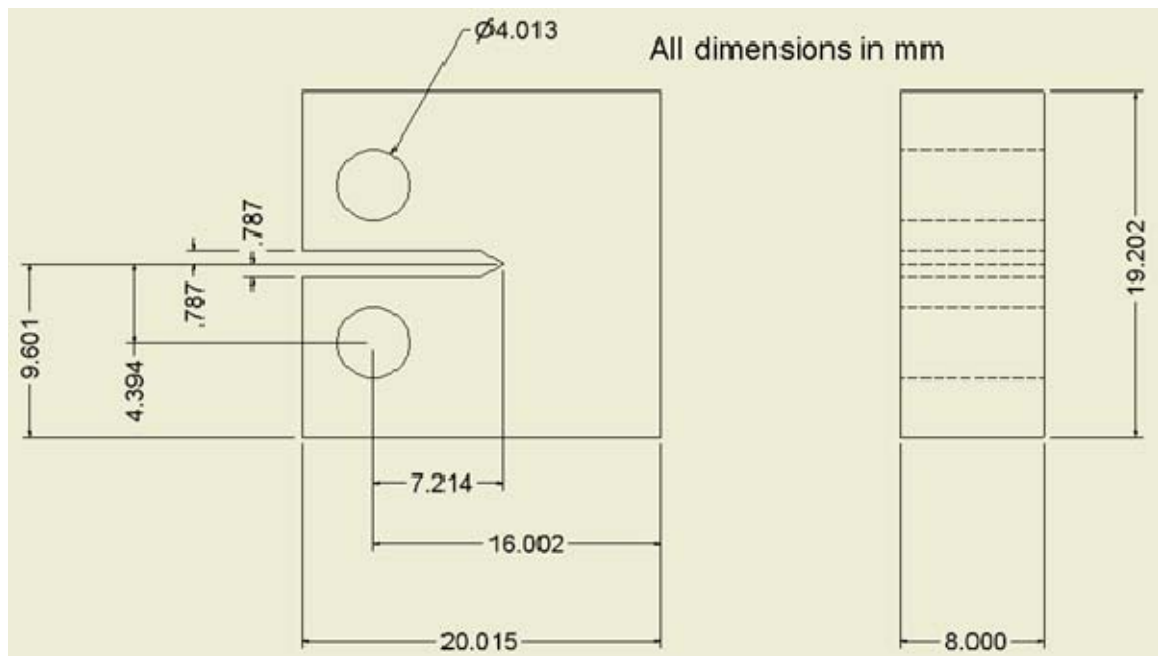


Figure 12. Illustration of Compact Tension Specimen showing Dimensions for 8 mm Wide Specimens

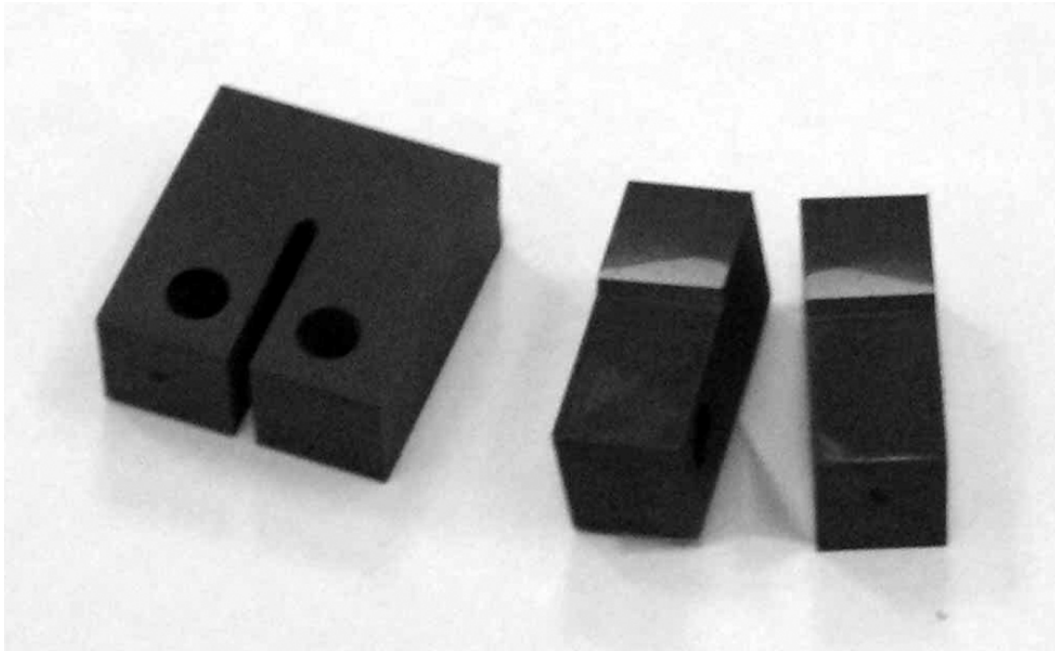


Figure 13. An Untested and a Tested Bulk Adhesive Compact Tension Specimen

Hence, the adhesively bonded tube specimens were tested in replicates of three under similar test conditions. Table 5 details the test plan for the bulk adhesive and the adhesive joints. Figure 14 shows a sketch of the entire experimental plan to help the reader have a clearer picture of the issues at hand.

2.5 Specimen Fabrication

The procedure detailed below was followed while making the neat adhesive plaques from which the compact tension specimens were cut out. The resin and hardener were mixed at a 4:1 ratio using a MixPac MC 10-24 static mixer on a Profill pneumatic dispenser. The adhesive was degassed by centrifuging for 15 minutes at room temperature in a container that could serve as a syringe for subsequent dispensing. The adhesive was then carefully squeezed onto a stainless steel plate that had been coated with Lilly RAM 225 mold release to allow for subsequent separation. A dam of silicone rubber tubing and rigid spacers of the desired thickness were used to support a second plate to produce a plaque with consistent thickness. The plates were clamped together and the assembly was held at 125^oC for 60 minutes in a convection oven. The curing procedure was used based on discussions with the manufacturer. Using this technique, neat adhesive plaques were prepared with a thickness of 8 mm. After curing, specimens were cut from the plaques using a milling machine, using water as the cutting fluid. Compact tension specimens were fabricated according to ASTM D 5045 [122], and appropriately sized to the thicknesses. Notches were cut into the samples with a high speed steel (HSS) slitting saw to a depth of 0.45 a/W (a-crack length, W-specimen

Table 5. Test Plan for the Bulk Adhesive and Adhesive Joints

Test	Measured Parameters	Standard	Replicates	Geometry	Rates
Adhesive (Bulk)	Tensile Strength and Failure Strain	D638	5	215×25×3 mm	Quasi-static + 3 dynamic
	Compressive Strength and Failure Strain	D695	5	140×13×3 mm	Quasi-static
	Shear Strength and Failure Strain	N/A	3	Solid and Hollow Cylindrical Rod, V Notch	Quasi-static
	Fracture Toughness	D5045	3	Compact Tension	Quasi-static + 6 dynamic
Adhesive (Joint)	Deformation Field, Force, Displacement	D3846	5	DNS (2 Bond Thicknesses)	Quasi-static (Tension)
	Deformation Field, Force, Displacement	D3846	5	DNS (2 Bond Thicknesses)	3 Dynamic (Tension)
	Deformation Field, Force, Displacement	D3165	5	SLS (2 Bond Thicknesses)	Quasi-static (Tension)
	Deformation Field, Force, Displacement	D3165	5	SLS (2 Bond Thicknesses)	3 Dynamic (Tension)
	Mode I, G_{IC}	D5528	3	DCB (2 Adherend Thicknesses)	Quasi-static + 3 dynamic
	Mode I, G_{IC}	N/A	3	New Test Method (3 Adherend Thicknesses)	Quasi-static + 3 dynamic
	Mode II, G_{IIC}	D6671	3	ELS (3 Adherend Thicknesses)	Quasi-static + 3 dynamic

Table 5. Continued

Test	Measured Parameters	Standard	Replicates	Geometry	Rates
Bonded Tube	Axial Impact Force, Displacement	N/A	3	100×100×L318 mm Square Tube (2 Bond Thicknesses and 2 Bond Lengths)	3 Dynamic

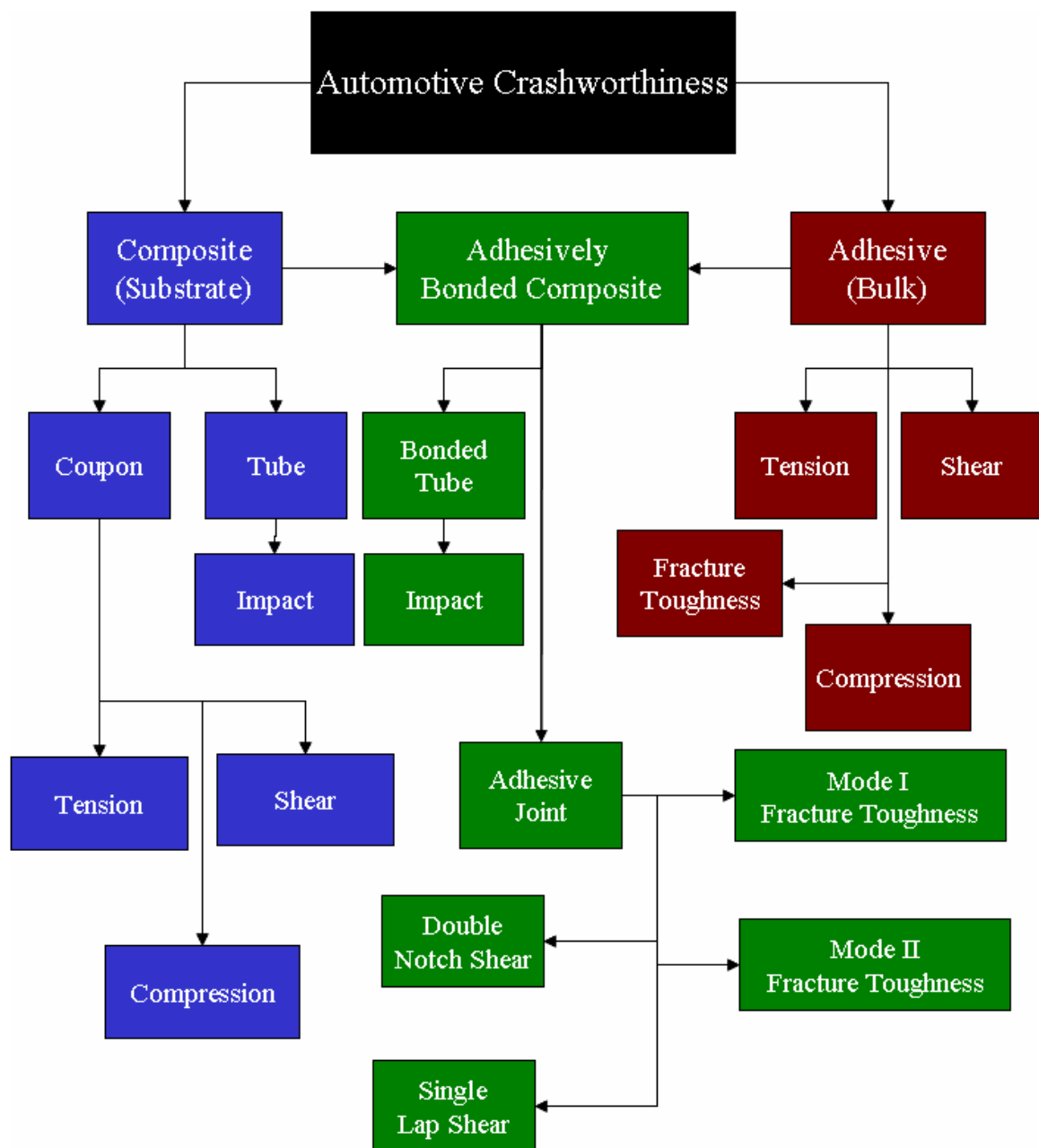


Figure 14. Sketch of the Experimental Plan

width), the standard's minimum acceptable crack length. Precracks were introduced in some specimens to a distance to exceed twice the cutter radius by tapping with a razor, a method that has been shown to produce smaller fracture toughness values. Completed specimens were typically aged for a minimum of 7 days at ambient conditions prior to testing. Tensile dogbone specimens were prepared in a similar fashion with a thickness of 3 mm. Blanks were cut from the plaques and then machined to the ASTM D 638 [120] standard shape using a TensilKut model 10-21 specimen router. A schematic of the adhesive plaque preparation from which the 3 mm thick tensile specimens were cut out is shown in Figure 15.

The coupon joint specimens used to determine the fracture characteristics of the joints were machined from bonded joint panels made following the procedure detailed below. The adherends consisted of the carbon epoxy composite material mentioned before. These were prepared in 600×600 mm plates that were subsequently cut into 300×300 mm plates for bonding. The adhesive used again was PL731 commercial epoxy. Prior to bonding, the adherends were cleaned first using acetone and then alcohol and were then dried using cotton gauze. To bond the substrates, several small squares of 0.5 mm thick shim stock were cut and bonded around the perimeter of one substrate with a cyanoacrylate adhesive to control bondline thickness. 50 mm wide strips of Teflon (PTFE-Polytetrafluoroethylene) tape were bonded to the mating ends of the panels to provide initial debonds. Small 0.5 mm diameter wires were then placed along two opposite sides of the substrate to serve as trowel guides for controlling adhesive thickness. The resin and hardener were mixed at a 4:1 ratio using a MixPac MC 10-24



Figure 15. Schematic of the Adhesive Plaque Preparation

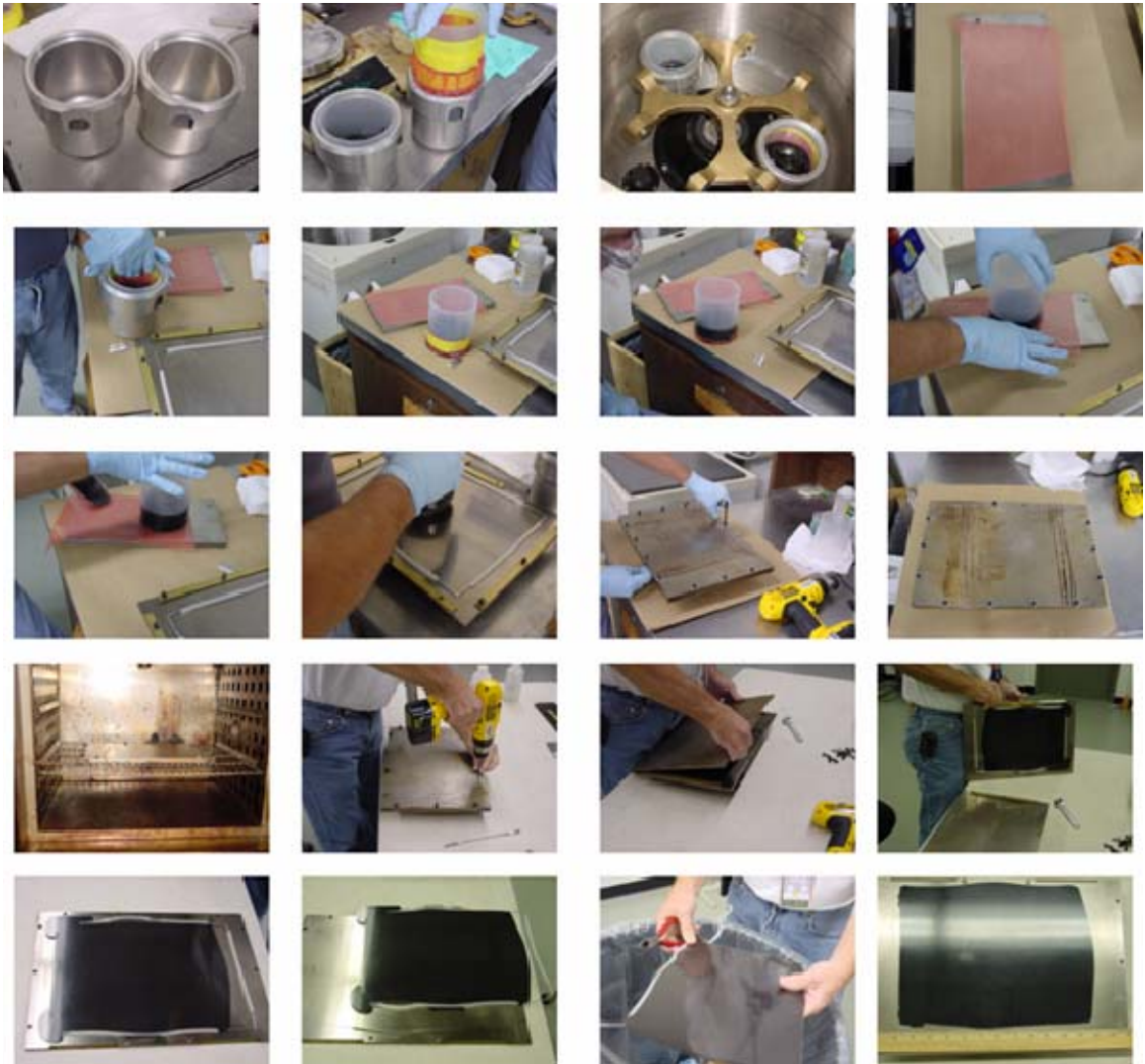


Figure 15. Continued

static mixer on a Profill pneumatic dispenser. Adhesive was dispensed onto both substrates and troweled onto both surfaces to ensure substrate wetting. A wide trowel riding on the guides was used to smooth the adhesive to a thickness of 0.5 mm on the first substrate, whereas a thinner layer of adhesive was deposited on the mating adherend. Having adhesive on both substrates assured good wetting and adequate squeeze-out.

Small pins placed into holes drilled in two corners of the substrates held the plates in alignment for handling until curing was complete. The bonded panel was then placed in a hot press for curing. The plates were clamped together and the assembly was held at 125°C for 60 minutes in a convection oven. The curing procedure was used based on discussions with the manufacturer. Panels were then sawn into 25.4 mm wide specimens. A schematic of the bonded joint panel preparation is shown in Figure 16. For those specimens tested using the driven wedge technique, the wedge was inserted between the two PTFE inserts, and pushed into a starting position. Panels from which the single lap shear and double notch shear test specimens were cut out were also made following a similar procedure with minor changes as per the test specimen geometry requirements.

2.6 Experimental Apparatus

An MTS servo-hydraulic machine (Load frame MN 204.61) equipped with a 11 kip actuator and two 1 liter/sec servo valves were used for the coupon testing at speeds up to 1 m/s. A 5000 N strain gage-based load cell was used to measure loads. Data were acquired at sampling rates of up to 33,000 per second using a PCI-6031E National

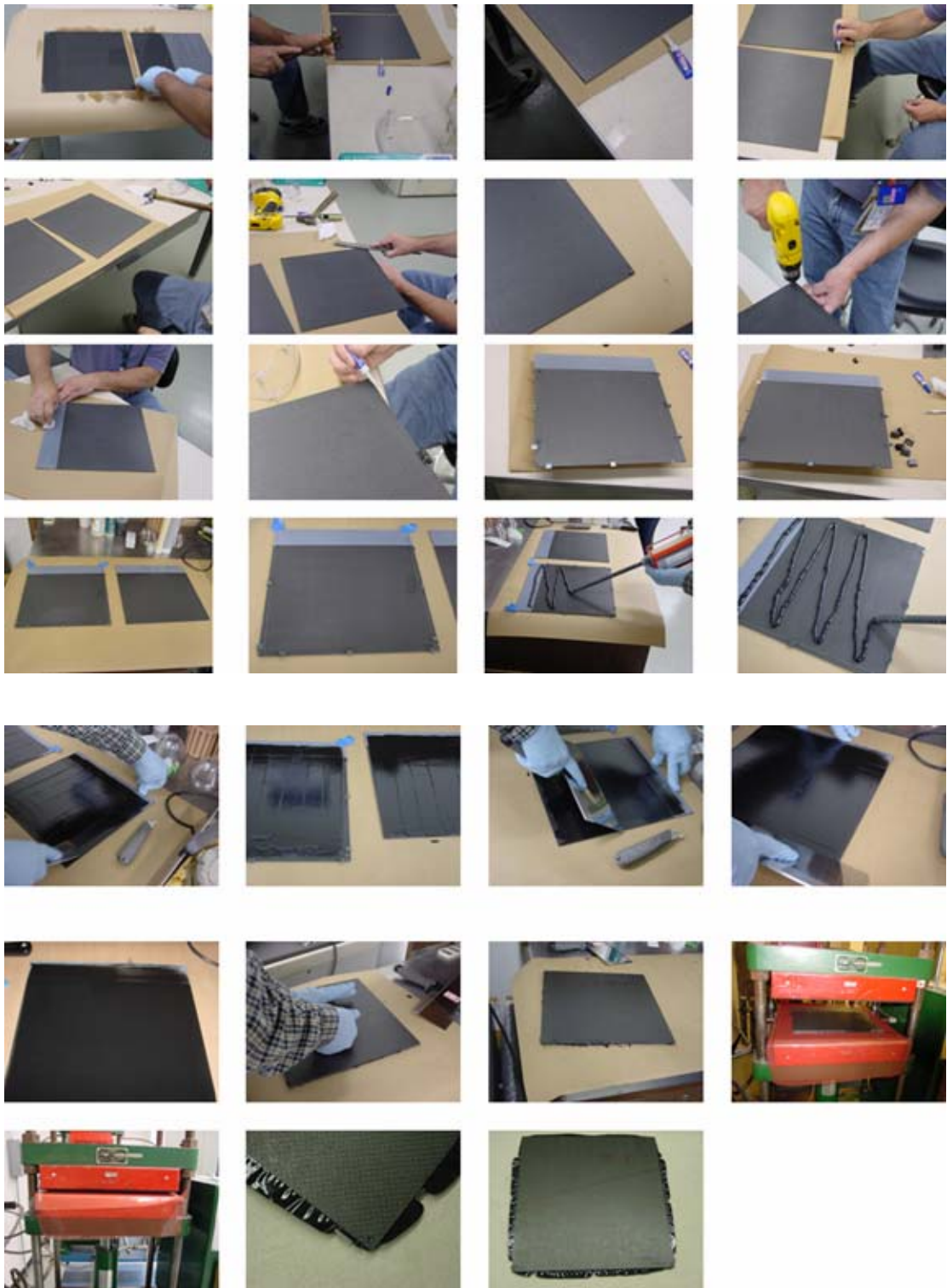


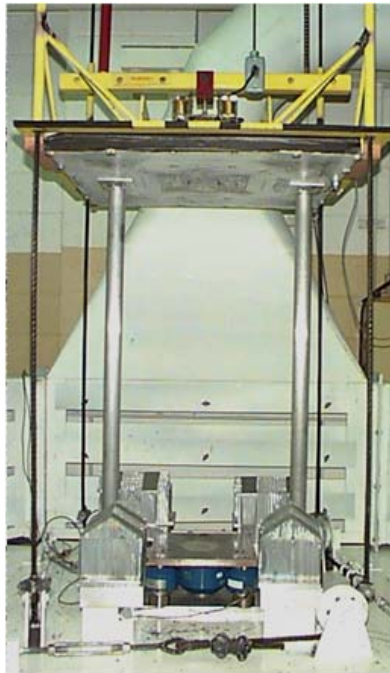
Figure 16. Schematic of the Bonded Joint Panel Preparation

Instruments data acquisition card in a computer running custom LabView (National Instruments, Austin) codes.

Component testing at dynamic rates of 0.05, 0.5, and 5 m/s on both unbonded and adhesively bonded tubes with various configurations of bond widths and bond thicknesses were done using the intermediate strain rate machine in ORNL called TMAC. Before the advent of the TMAC at ORNL, typical dynamic structural test capabilities existed at General Motors (GM) and Ford, which had the drop tower and the impact sled, respectively shown in Figure 17. The limitation of the drop towers and sleds was that they do not give constant velocities especially at lower loading rates. For example, to maintain 0.7 m/s crush velocity to within 10% over 125 mm crush requires 8600 kg, which is too large to be practical.

TMAC, a closed-loop servo-hydraulic machine, designed, engineered and manufactured by MTS, permits controlled, progressive crush experiments at programmable velocity profiles and high force levels. It was a result of collaborative efforts between ORNL and the ACC of United States Council for Automotive Research (USCAR). Total investment on the TMAC was about a millions dollars; the cost being shared by the Department of Energy (DOE) and the ACC. TMAC shown in Figure 18 had the following specifications:

- Physical
 - 490 KN Actuator Capacity (static)
 - 250 mm Stroke
 - Greater than 490 KN side-load capacity



**General Motors
Drop Tower**



**Ford Impact Sled with
Mounted Test Specimen**

**Figure 17. Typical Dynamic Structural Test Capabilities at General Motors and
Ford**



Figure 18. Test Machine for Automotive Composites (TMAC)

- Attachable 450 kg mass
- Operating
 - No Load: 115 mm travel at 8 m/s
 - 133 kN: 115 mm travel at 6 m/s
 - 267 kN: 115 mm travel at 4 m/s

CHAPTER 3

RESULTS AND DISCUSSION

3.1 Chopped Carbon Fiber Epoxy Composites

The ACC (Automotive Composite Consortium) is interested in investigating the use of random chopped fiber reinforced composites as crash energy absorbers primarily due to the low costs involved in their manufacture thus making them cost effective for automotive applications. The crashworthiness, measured in terms of SEA, fracture toughness, and flexure properties of various random chopped carbon fiber composite material systems were examined. The different random chopped carbon reinforced epoxy composite material systems studied were P4, HexMC, CCS150, and CCS100.

The CCS100 and CCS150 composite plates were manufactured from Toray T700 chopped carbon fiber with YLA RS-35 epoxy resin using compression-molding techniques. While YLA Incorporated supplied the molding compound, CCS Composites LLC compression molded the plates. The CCS100 (100 gsm Tow Size) and CCS150 (150 gsm Tow Size) composites had a fiber volume fraction of 50% and a fiber length of 1 inch. The random chopped carbon fiber epoxy resin HexMC composite plates, which had a fiber volume fraction of 57% and 2-inch fiber length, were compression molded by Hexcel Composites LLC. The compression molded P4 composite plates were manufactured from chopped carbon fiber having 2-inch lengths and 36% fiber volume fraction in an epoxy resin.

An experimental setup was developed for discerning the deformation behavior and damage mechanisms that occur during the progressive crushing of composite materials to determine their crashworthiness. The fixture was designed to isolate damage modes associated with frond formation (splaying mode) in composite tubes by testing plate geometries. Practical considerations related to the cost of production of the test specimens were of paramount importance in developing the test methodology. Composite plate specimens are very cheap to fabricate and it has been observed that plate specimens progressively crush in modes very similar to the damage modes that occur during progressive crushing of composite tubes. In addition, plates can be easily produced with consistently high quality. The design of the test fixture can accommodate different plate widths (up to a maximum of 50 mm), plate thicknesses ($3 \text{ mm} \pm 1.5 \text{ mm}$), contact profile shapes (profile block radius: 6.4 mm and 13 mm), and contact profile constraints (tight, loose and no constraint). An illustration of the test fixture is shown in Figures 19, 20, 21, and 22. Below is a list of the primary components of the fixture identified in Figure 19:

1. Top plate
2. Base plate
3. Profile block
4. Roller plate
5. Grip plate and insert
6. Linear shaft and bearing
7. Load cell

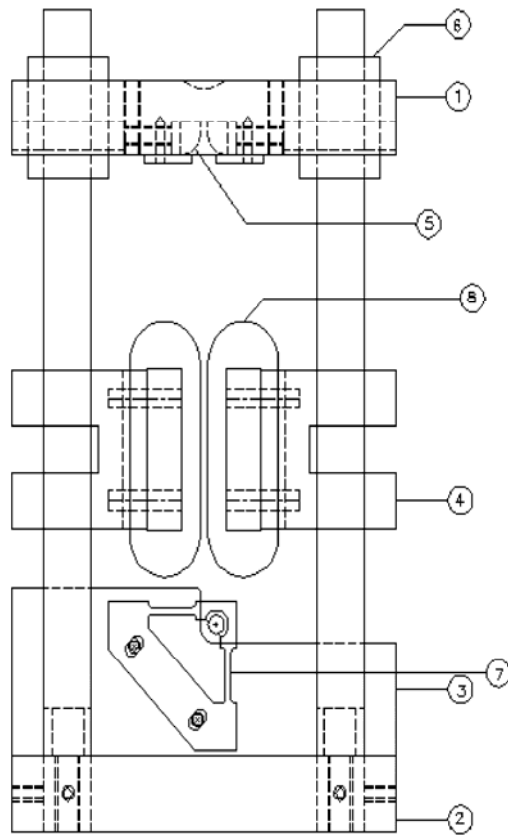


Figure 19. Schematic of Test Fixture Design

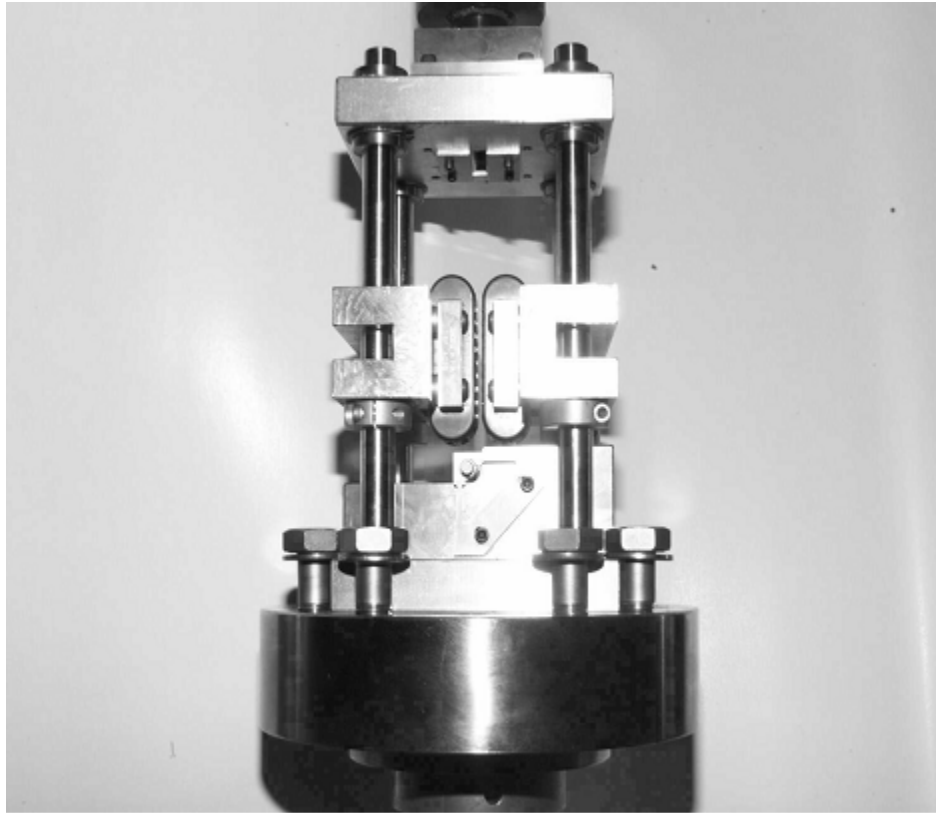


Figure 20. Test Fixture Assembly

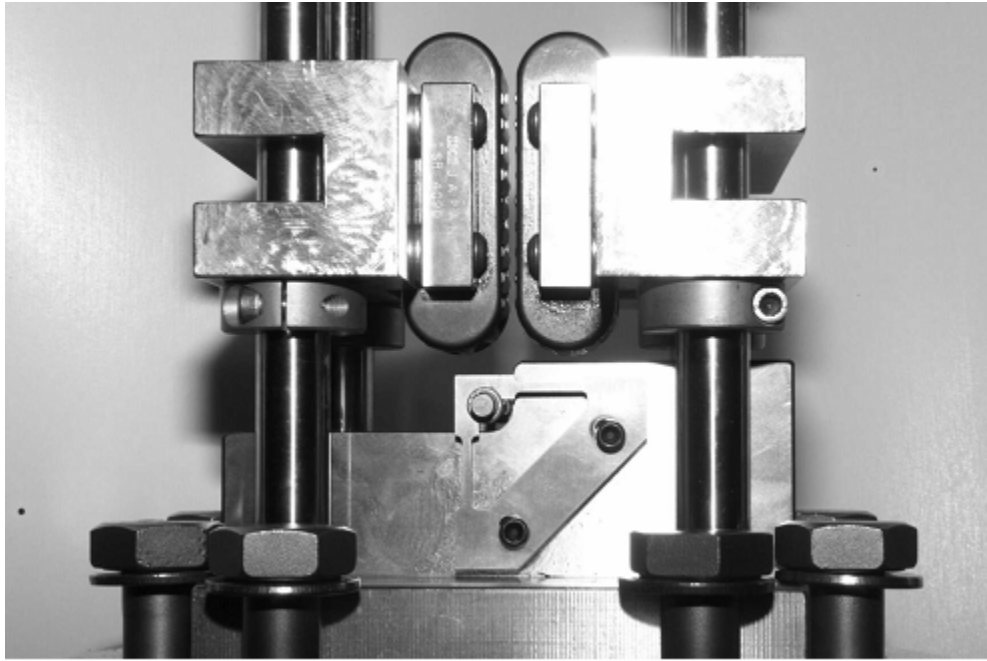
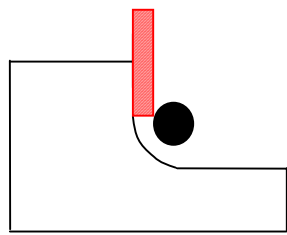
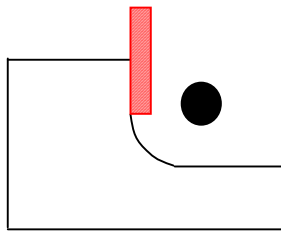


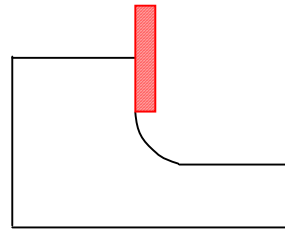
Figure 21. Roller Ways and Contact Profile Constraint



a) Tight constraint



b) Loose constraint



c) No constraint

Figure 22. Constraint Conditions

8. Roller way

Features incorporated into the design include an observable crush zone, long crush length (50 mm), interchangeable contact profile, frictionless roller for contact constraint, and out of plane roller supports to prevent buckling. The brackets on each side of the profile plate were designed to provide a method of constraining the specimen to deform along the path of the contact profile. The severity of the contact profile constraint was determined by the position of the brackets and was adjustable using slotted positioning holes. The objective of the profile constraint was to determine if different damage mechanisms could be activated depending on the position of the roller. Jacob et al. [2] provides more details of the fixture design and its validation.

The P4, CCS-100, and HexMC specimen plates had a nominal length of 178 mm (7 inches) and a width of 25.4 mm (1 inch). A 45° chamfer was used as the crush initiator. A diamond cut off wheel was used to cut the specimens from the composite panel. No coolant was used during cutting to prevent contamination of the test specimens. Quasi-static progressive crush tests were then performed on the above three random chopped carbon reinforced epoxy composite material systems using a servo-hydraulic test machine at a loading rate of 0.5 cm/min (0.2 inches/minute) to determine their crashworthiness. An MTS model 407 controller, which is a single channel, digitally supervised proportional, integral, derivative, feed forward (PIDF) servo controller, was used to provide complete control of one servo hydraulic channel/station in the MTS testing system. The load-deflection response was recorded using a computerized data acquisition system. The area under the load deflection curve was calculated for the total

energy absorbed and the initial peak load, maximum peak load and sustained crush load was identified. All the P4, HexMC, and CCS100 specimens tested generated load deflection curves that were similar to the ones generated during the progressive crushing of composite tubes. Figure 23 shows the load displacement traces obtained for the P4, HexMC, and CCS100 composite plate specimens. It had four stages, the first one being characterized by an initial rapid load increase. A rapid load drop occurred in the second stage of the load deflection curve followed by a gradual saturation of the load. The final stage was characterized by stable crushing at a constant mean load. The small load fluctuations and serrations in the fourth stage of the curve are characteristic of stable crushing. For all P4, HexMC and CCS100 specimens tested, local crushing took place at the chamfered end of the plates. Matrix cracking occurred at the ends of the fiber tows due to stress concentration at these ends. An illustration of matrix cracking occurring at the ends of fiber tows is shown in Figure 24. Fiber-matrix debonding also took place in a majority of the specimens that were tested.

On comparing the performance of the P4, HexMC, and CCS100 composite plates, the specific energy absorption of P4 composites was found to be greater than that of the HexMC and CCS100 composites. For a comparison of the specific energy absorbed by the P4, HexMC, and CCS100 composites please see Tables 6, 7, 8, and Figure 25. The P4 composites with a fiber volume fraction of 36% had the highest specific energy absorption when compared with that of the HexMC and CCS100 composites, which had higher fiber volume fractions. It is not always true, as one would normally think that an increase in the fiber content would necessarily improve the

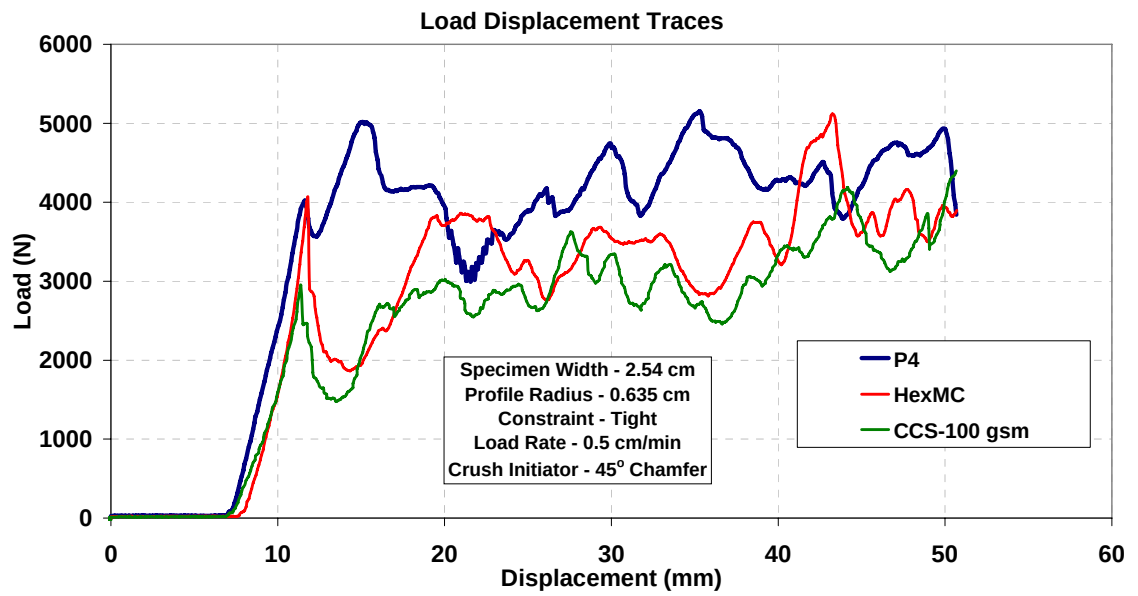


Figure 23. Load Displacement Traces for the Various Random Chopped Fiber Reinforced Composites

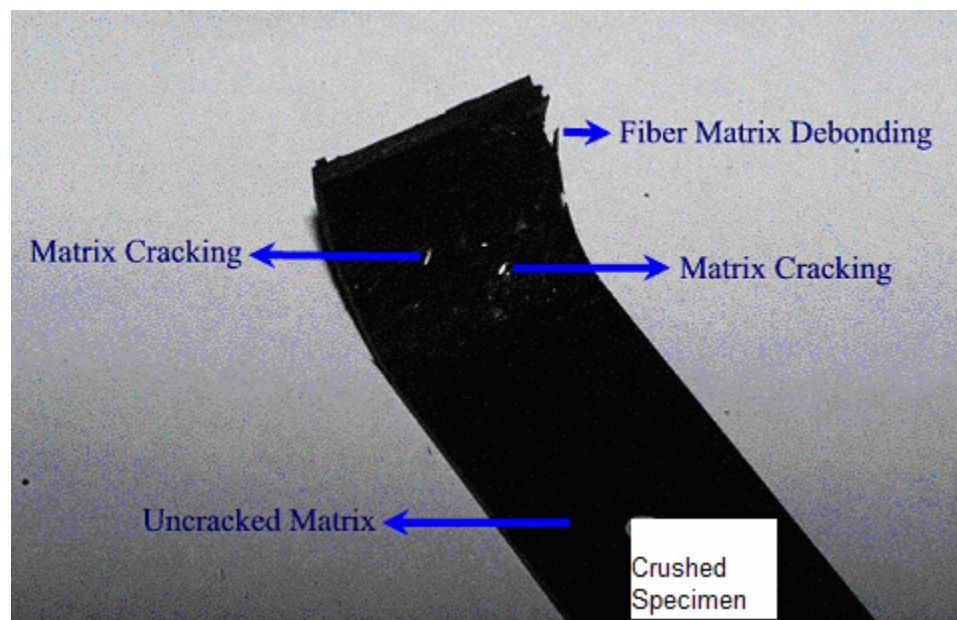


Figure 24. Matrix Cracking

**Table 6. Experimental Data from Tests Conducted with a Profile Block of Radius
0.635 cm at 0.5 cm/min Loading Rate on P4**

Spec. Numb.	Spec. Width (cm)	Profile Radius (cm)	Const- raint	Load Rate (cm/ min)	Initial Peak Load (N)	Max. Peak Load (N)	Susta- ined Crush Load (N)	SEA (J/g)
ENAB S0_2P 41	2.551	0.635	Tight	0.5	2342.7	4696.2	3136.3	30.76
ENAB S0_2P 42	2.556	0.635	Tight	0.5	2536.7	5515.4	3429.2	34.53
ENAB S0_2P 43	2.555	0.635	Tight	0.5	2987.0	4830.5	3642.6	36.77
ENAB S0_2P 44	2.550	0.635	Tight	0.5	2592.3	4787.1	3592.1	36.71
ENAB S0_2P 45	2.551	0.635	Tight	0.5	3156.6	4475.1	333.2	33.91
ENAB S0_2P 46	2.549	0.635	Tight	0.5	2860.9	4560.6	3503.2	35.55

Table 6. Continued

Spec. Numb.	Spec. Width (cm)	Profile Radius (cm)	Const- raint	Load Rate (cm/ min)	Initial Peak Load (N)	Max. Peak Load (N)	Susta- ined Crush Load (N)	SEA (J/g)
ENAB S0_2P 47	2.550	0.635	Tight	0.5	4016.6	5147.9	3997.7	40.19
ENAB S0_2P 48	2.560	0.635	Tight	0.5	2734.7	5038.1	3937.2	39.30
ENAB S0_2P 49	2.558	0.635	Tight	0.5	3063.0	5317.5	3873.1	38.50
ENAB S0_2P 410	2.545	0.635	Tight	0.5	3522.8	5131.7	3848.6	38.52
							Avg. SEA (J/g)	36.47

**Table 7. Experimental Data from Tests Conducted with a Profile Block of Radius
0.635 cm at 0.5 cm/min Loading Rate on HexMC**

Spec. Numb.	Spec. Width (cm)	Profile Radius (cm)	Const- raint	Load Rate (cm/ min)	Initial Peak Load (N)	Max. Peak Load (N)	Susta- ined Crush Load (N)	SEA (J/g)
ENAB SHEX MC1	2.542	0.635	Tight	0.5	5191.4	5191.4	3714.8	37.39
ENAB SHEX MC2	2.539	0.635	Tight	0.5	3233.9	4884.8	3132.4	31.52
ENAB SHEX MC3	2.541	0.635	Tight	0.5	4061.4	5115.4	3203.0	32.41
ENAB SHEX MC4	2.547	0.635	Tight	0.5	4188.9	4605.3	2972.5	30.05
ENAB SHEX MC5	2.540	0.635	Tight	0.5	4038.3	4298.8	2852.8	29.42
ENAB SHEX MC6	2.539	0.635	Tight	0.5	6334.9	6334.9	3738.0	37.63

Table 7. Continued

Spec. Numb.	Spec. Width (cm)	Profile Radius (cm)	Const- raint	Load Rate (cm/ min)	Initial Peak Load (N)	Max. Peak Load (N)	Susta- ined Crush Load (N)	SEA (J/g)
ENAB SHEX MC7	2.541	0.635	Tight	0.5	3635.4	3923.0	2798.3	28.35
ENAB SHEX MC8	2.544	0.635	Tight	0.5	3748.0	4658.2	3506.4	35.55
ENAB SHEX MC9	2.541	0.635	Tight	0.5	4898.3	5248.3	3912.9	39.64
ENAB SHEX MC10	2.542	0.635	Tight	0.5	4007.1	4007.1	2975.0	29.82
							Avg. SEA (J/g)	33.18

**Table 8. Experimental Data from Tests Conducted with a Profile Block of Radius
0.635 cm at 0.5 cm/min Loading Rate on CCS-100**

Spec. Numb.	Spec. Width (cm)	Profile Radius (cm)	Const- raint	Load Rate (cm/ min)	Initial Peak Load (N)	Max. Peak Load (N)	Susta- ined Crush Load (N)	SEA (J/g)
ENABS0 _2CCF1 001	2.540	0.635	Tight	0.5	3815.9	4174.0	3035.3	30.95
ENABS0 _2CCF1 002	2.538	0.635	Tight	0.5	3799.6	4873.9	2865.8	28.96
ENABS0 _2CCF1 003	2.538	0.635	Tight	0.5	3765.7	4799.3	3106.9	31.10
ENABS0 _2CCF1 004	2.555	0.635	Tight	0.5	3423.8	5803.1	3442.4	34.36
ENABS0 _2CCF1 005	2.550	0.635	Tight	0.5	2940.9	4395.1	2789.0	27.95
ENABS0 _2CCF1 006	2.538	0.635	Tight	0.5	2920.6	4054.6	3024.4	30.49

Table 8. Continued

Spec. Numb.	Spec. Width (cm)	Profile Radius (cm)	Const- raint	Load Rate (cm/ min)	Initial Peak Load (N)	Max. Peak Load (N)	Susta- ined Crush Load (N)	SEA (J/g)
ENABS0 _2CCF1 007	2.536	0.635	Tight	0.5	3090.1	5729.9	3473.9	34.87
ENABS0 _2CCF1 008	2.536	0.635	Tight	0.5	3687.0	5525.1	3449.1	34.75
ENABS0 _2CCF1 009	2.543	0.635	Tight	0.5	2534.0	4176.1	2402.7	25.26
ENABS0 _2CCF1 0010	2.538	0.635	Tight	0.5	2801.2	4568.7	3127.2	32.89
							Avg. SEA (J/g)	31.16

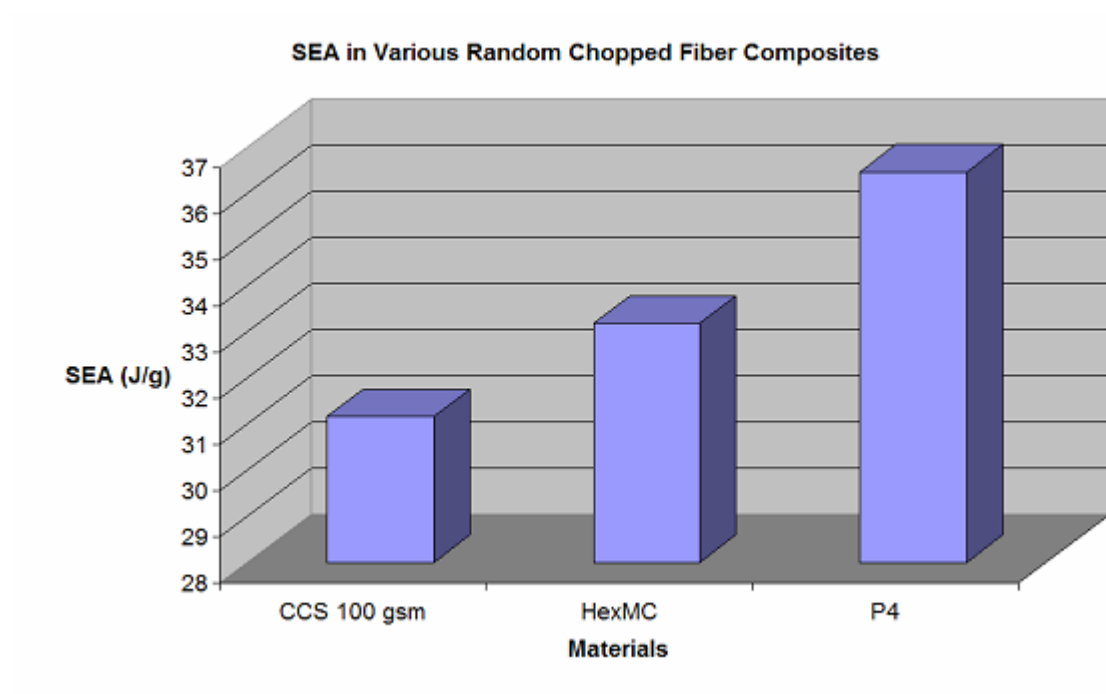


Figure 25. Specific Energy Absorption (SEA) in CCS 100, HexMC, and P4 Composites

specific energy absorption capability of a composite material. A possible explanation for the above statement is that as the fiber volume fraction increases, the volume of the matrix between the fibers decrease. This leads to a decrease in the interlaminar strength of the composite. As interlaminar strength decreases, interlaminar cracks form at lower loads, resulting in a reduction in the energy absorption capability.

The CCS100 composite plates, which had a fiber length for 1 inch, recorded the lowest specific energy absorption when compared with the P4 and HexMC composites, which had a fiber length of 2 inches. This is in agreement with a previous study on the effect of fiber length on the energy absorption capabilities of composites that reported an increase in the specific energy absorption with increased fiber lengths [123]. However, previous work done by this author on random chopped carbon fiber composites with 1 inch and 2 inch fiber lengths found that greater fiber lengths caused decreased specific energy absorption [8]. Hence, there seems to be a lack of consensus about the influence of fiber length on the energy absorption in random chopped fiber composite materials. Therefore, more work needs to be done in pursuit of the above goal where in chopped carbon composites with varying fiber lengths but with other parameters identical to each other need to be thoroughly investigated. In addition, the use of random chopped fiber composite materials having a distribution of fiber lengths can be considered for crashworthy structures.

The specific energy absorption of the 3 random chopped carbon fiber composite material systems (P4, HexMC, and CCS100) was higher when compared with that of other fiber resin systems investigated by this author like graphite/epoxy cross-ply

laminates (CP#1 (25.58 Jg^{-1}) and CP#2 (17.62 Jg^{-1})) [5], a graphite/epoxy braided material system (17.23 Jg^{-1}) [5], and a glass-reinforced continuous strand mat (25.58 Jg^{-1}) [6]. The above results are very encouraging for the use of random chopped fiber reinforced composites as crash energy absorbers, as desired by the ACC due to the low costs involved in their manufacture thus making them cost effective for automotive applications.

The P4 composite material system was tested at additional rates of 15.2 cm/min (6 inches/min) and 762 cm/min (300 inches/min) to investigate their preliminary strain rate dependence. For the P4 composite plate specimens tested at higher loading rates the failure mechanisms observed (local crushing, matrix cracking, and fiber matrix debonding) were similar to that seen at the quasi-static rate (0.5 cm/min). However, it was observed that the matrix cracking at the fiber tow ends and the fiber matrix debonding was more in the P4 composite plate specimens tested at 0.5 cm/min than what took place at higher rates. All the P4 specimens tested at higher rates generated load deflection curves that were similar to the ones generated during the progressive crushing of composite tubes. Figure 26 illustrates the load displacement traces of the P4 composite plate specimens tested at three different loading rates. It had the same four stages as observed for the P4, HexMC, and CCS100 composites when tested at quasi-static rates that have been previously described in this document.

On loading the P4 composite plate specimens at higher rates of 15.2 and 762 cm/min respectively, the specific energy absorption at 762 cm/min was found to be greater than the specific energy absorption at 15.2 cm/min. A comparison of the specific

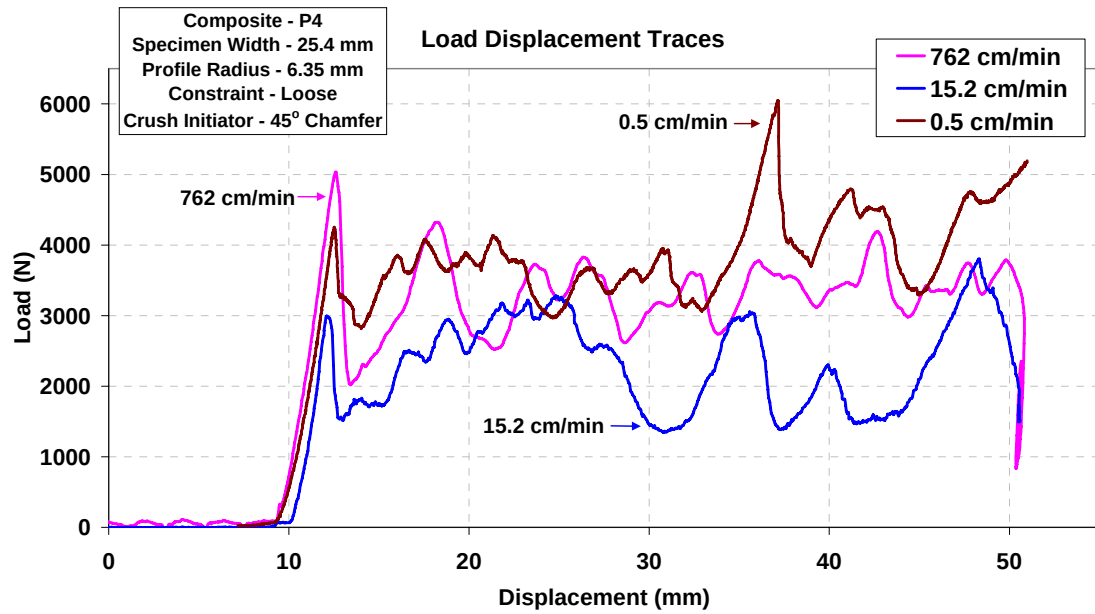


Figure 26. Load Displacement Traces for a Test Conducted on P4 in the Loose Constraint Condition at Loading Rates of 0.5, 15.2, and 762 cm/min

energy absorbed by the P4 composites at loading rates of 0.5, 15.2, and 762 cm/min can be obtained by referring to Tables 6, 9, 10, and Figure 27. The increase in specific energy absorption with increase in loading rate from 15.2 cm/min to 762 cm/min might be because of the increased fracture toughness of the P4 composite with increasing loading rate as. Increased fracture toughness of the composite means more resistance to crack formation. Therefore, more energy absorption will take place in the composite. The high specific energy absorption in the P4 composite plates loaded at 0.5 cm/min is due to greater matrix cracking at the fiber tow ends and fiber matrix debonding which contributed to the greater energy being absorbed.

Single Edge Notch Bend (SENB) fracture tests using a servo-hydraulic test machine at a loading rate of 0.15 cm/min (0.06 inches/minute) were also conducted on the chopped carbon fiber composites (P4, HexMC, CCS150, and CCS100) to determine their fracture toughness properties. Fracture toughness tests were run as per ASTM D5045 [122] and the load-deflection response was recorded using a computerized data acquisition system. On comparing the performance of the P4, HexMC, CCS150, and CCS100 composite plates, the fracture toughness of the HexMC composite was found to be the highest followed by CCS150, CCS100, and P4 in the order of decreasing fracture toughness. A comparison of the fracture toughness of P4, HexMC, CCS150, and CCS100 composites can be obtained by referring to Tables 11, 12, 13, 14, and Figure 28. The HexMC composites had a fiber volume fraction of 57% followed by the CCS150 and CCS100 composites (50%) and the P4 composite had the lowest fiber volume fraction (36%). This indicates the effect of fiber volume fraction on the fracture toughness

**Table 9. Experimental Data from Tests Conducted with a Profile Block of Radius
0.635 cm at 15.2 cm/min Loading Rate on P4**

Spec. Numb.	Spec. Width (cm)	Profile Radius (cm)	Const- raint	Load Rate (cm/ min)	Initial Peak Load (N)	Max. Peak Load (N)	Susta- ined Crush Load (N)	SEA (J/g)
ENAB S0_2P 412	2.543	0.635	Loose	15.2	3020.2	4247.8	2009.5	20.12
ENAB S0_2P 413	2.545	0.635	Loose	15.2	3576.2	4283.4	2534.6	25.42
ENAB S0_2P 414	2.542	0.635	Loose	15.2	2686.6	3469.4	1905.2	19.24
ENAB S0_2P 415	2.542	0.635	Loose	15.2	3429.4	3460.5	1699.4	17.13
ENAB S0_2P 416	2.546	0.635	Loose	15.2	3215.9	3215.9	2081.0	20.99
ENAB S0_2P 417	2.543	0.635	Loose	15.2	3367.1	4269.8	2361.4	22.75

Table 9. Continued

Spec. Numb.	Spec. Width (cm)	Profile Radius (cm)	Const- raint	Load Rate (cm/ min)	Initial Peak Load (N)	Max. Peak Load (N)	Susta- ined Crush Load (N)	SEA (J/g)
ENAB S0_2P 418	2.545	0.635	Loose	15.2	3233.7	3545.1	2229.1	21.81
ENAB S0_2P 419	2.546	0.635	Loose	15.2	2998.0	3798.6	2228.2	21.82
ENAB S0_2P 420	2.542	0.635	Loose	15.2	3687.4	3896.5	2279.1	22.42
							Avg. SEA (J/g)	21.30
							Std. Dev.	2.35
							C.V.	11.00

**Table 10. Experimental Data from Tests Conducted with a Profile Block of Radius
0.635 cm at 762 cm/min Loading Rate on P4**

Spec. Numb.	Spec. Width (cm)	Profile Radius (cm)	Const- raint	Load Rate (cm/ min)	Initial Peak Load (N)	Max. Peak Load (N)	Susta- ined Crush Load (N)	SEA (J/g)
ENAB S0_2P 421	2.547	0.635	Loose	762	5337.6	5604.5	3705.6	36.34
ENAB S0_2P 424	2.545	0.635	Loose	762	5026.2	5026.2	3112.5	30.06
ENAB S0_2P 425	2.547	0.635	Loose	762	4301.2	5604.5	3161.8	30.16
ENAB S0_2P 426	2.547	0.635	Loose	762	5159.7	5159.7	3216.5	31.23
ENAB S0_2P 427	2.550	0.635	Loose	762	4336.8	5426.6	2845.2	27.40
ENAB S0_2P 428	2.548	0.635	Loose	762	4581.4	4803.8	3254.9	30.91

Table 10. Continued

Spec. Numb.	Spec. Width (cm)	Profile Radius (cm)	Const- raint	Load Rate (cm/ min)	Initial Peak Load (N)	Max. Peak Load (N)	Susta- ined Crush Load (N)	SEA (J/g)
ENAB S0_2P 429	2.547	0.635	Loose	762	5204.2	5871.4	3079.1	29.11
ENAB S0_2P 430	2.547	0.635	Loose	762	5159.7	5159.7	3251.8	30.66
							Avg. SEA (J/g)	30.73
							Std. Dev.	2.57
							C.V.	8.36

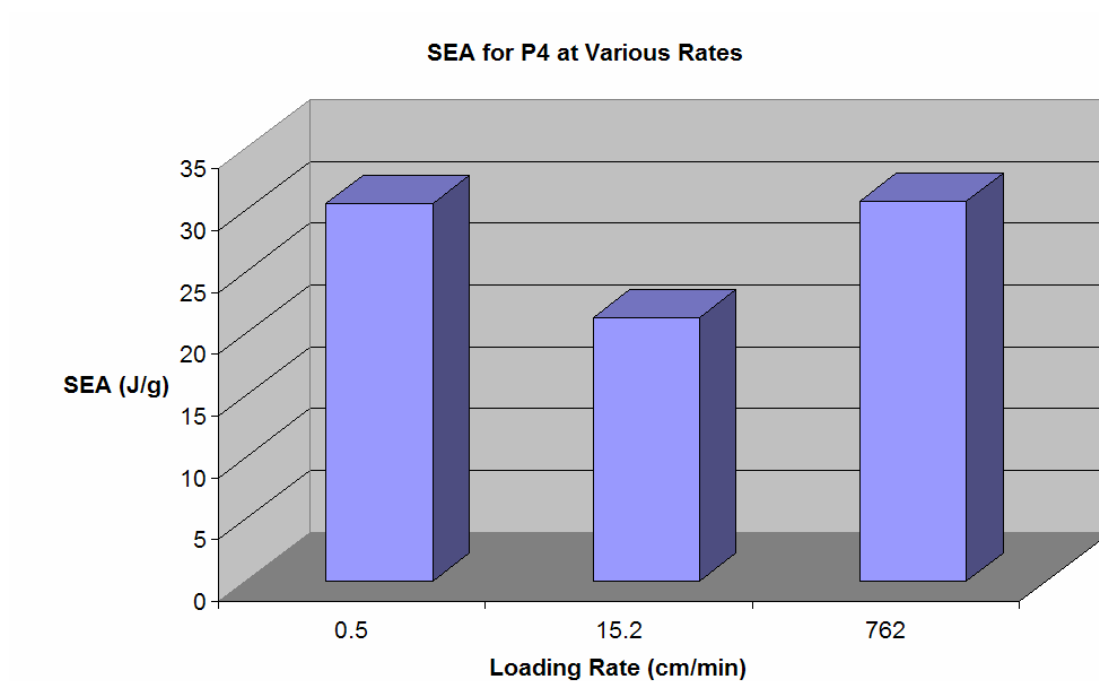


Figure 27. Specific Energy Absorption (SEA) in P4 Composites at Loading Rates of 0.5, 15.2, and 762 cm/min respectively

**Table 11. Fracture Toughness Data from Tests Conducted at 0.15 cm/min Loading
Rate on HexMC**

Specimen ID	Avg. Width W (mm)	Avg. Thickness b (mm)	a/W	f(a/W)	K_{IC} (MPa-m^{1/2})
HEXMC1	12.695	3.117	0.50	10.65	7.12
HEXMC2	12.690	3.100	0.50	10.65	7.03
HEXMC3	12.700	3.070	0.50	10.65	6.61
HEXMC4	12.675	3.133	0.50	10.65	6.07
HEXMC5	12.695	3.077	0.50	10.65	6.46
HEXMC6	12.705	3.130	0.50	10.65	6.26
HEXMC7	12.700	3.087	0.50	10.65	6.84
HEXMC8	12.705	3.117	0.50	10.65	6.70
HEXMC9	12.690	3.110	0.50	10.65	6.54
HEXMC10	12.690	3.123	0.50	10.65	6.82
Average					6.64

Table 12. Fracture Toughness Data from Tests Conducted at 0.15 cm/min Loading**Rate on CCS150**

Specimen ID	Avg. Width W (mm)	Avg. Thickness b (mm)	a/W	f(a/W)	K_{IC} (MPa-m^{1/2})
CCF1508	12.695	3.140	0.50	10.65	3.55
CCF1509	12.695	3.170	0.50	10.65	6.20
CCF15010	12.705	3.153	0.50	10.65	10.23
CCF15011	12.695	3.130	0.50	10.65	8.56
CCF15012	12.685	3.127	0.50	10.65	7.28
CCF15013	12.700	3.143	0.50	10.65	7.16
CCF15014	12.705	3.160	0.50	10.65	6.98
CCF15015	12.700	3.153	0.50	10.65	3.10
CCF15016	12.695	3.147	0.50	10.65	1.14
CCF15017	12.700	3.150	0.50	10.65	8.64
CCF15018	12.690	3.147	0.50	10.65	2.93
CCF15019	12.695	3.143	0.50	10.65	9.34
Average					6.26

**Table 13. Fracture Toughness Data from Tests Conducted at 0.15 cm/min Loading
Rate on CCS100**

Specimen ID	Avg. Width W (mm)	Avg. Thickness b (mm)	a/W	f(a/W)	K_{IC} (MPa-m^{1/2})
SENBA1	12.700	3.010	0.50	10.65	4.89
SENBA2	12.680	2.923	0.50	10.65	5.41
SENBA3	12.700	3.240	0.50	10.65	7.64
SENBA4	12.690	3.057	0.50	10.65	5.63
SENBA5	12.675	3.150	0.50	10.65	4.40
SENBF1	12.680	2.940	0.50	10.65	6.15
SENBF2	12.700	2.930	0.50	10.65	6.44
SENBF3	12.700	2.957	0.50	10.65	5.46
SENBF4	12.700	2.943	0.50	10.65	5.12
SENBF5	12.700	2.903	0.50	10.65	6.72
Average					5.79

**Table 14. Fracture Toughness Data from Tests Conducted at 0.15 cm/min Loading
Rate on P4**

Specimen ID	Avg. Width W (mm)	Avg. Thickness b (mm)	a/W	f(a/W)	K_{IC} (MPa-m^{1/2})
FRACTTOUG H0_06P41	12.700	3.587	0.50	10.65	3.38
FRACTTOUG H0_06P42	12.700	3.533	0.50	10.65	4.56
FRACTTOUG H0_06P43	12.700	3.497	0.50	10.65	2.23
FRACTTOUG H0_06P44	12.695	3.490	0.50	10.66	2.87
FRACTTOUG H0_06P45	12.695	3.510	0.50	10.66	2.31
FRACTTOUG H0_06P46	12.695	3.473	0.50	10.66	3.68
FRACTTOUG H0_06P47	12.690	3.460	0.50	10.66	3.50
FRACTTOUG H0_06P48	12.700	3.423	0.50	10.65	3.73
FRACTTOUG H0_06P49	12.695	3.417	0.50	10.66	2.77
FRACTTOUG H0_06P410	12.685	3.420	0.50	10.67	2.93
Average					3.19

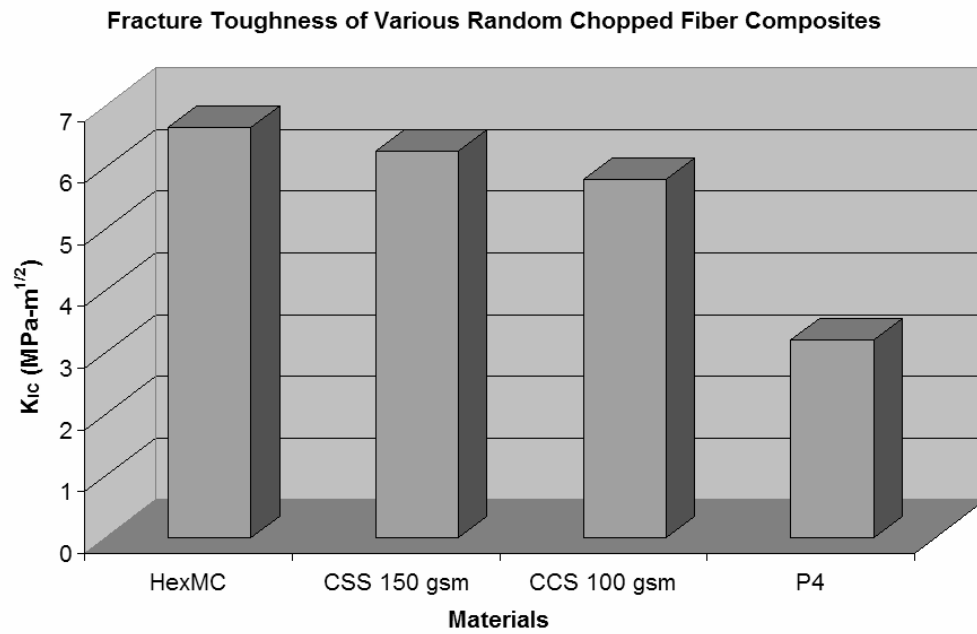


Figure 28. Comparison of Fracture Toughness for HexMC, CCS150, CCS100, and P4 Composites

properties of chopped carbon fiber composites, with increased fiber volume fracture leading to higher fracture toughness properties. The above is in agreement with what one would normally expect that an increase in the fiber content would improve the performance properties of a composite.

The fact that the CCS150 composite having a tow size of 150 gsm recorded a higher fracture toughness value than the CCS100 composite having a tow size of 100 gsm indicates a positive influence of an increased tow size on the fracture toughness properties of chopped carbon composites. The HexMC composites that displayed superior fracture toughness properties had a fiber length of 2 inches while the P4 composite, which recorded the least fracture toughness values also had a fiber length of 2 inches. Hence, not much can be read into effect of fiber length on fracture toughness properties of chopped carbon fiber composites from the above results.

Since the HexMC and the P4 composite material systems recorded the highest and lowest fracture toughness properties respectively, an attempt was made to also determine the flexural properties of these two random chopped carbon fiber composite materials systems to see how they compared and verify whether the same trend persisted. Four point flexure tests were run as per ASTM D6272 [124] and the load-deflection response was recorded using a computerized data acquisition system. The composite specimen plate was made to rest on two supports and was loaded at two points by means of two loading noses, each an equal distance from the adjacent support point. The alignment of the support and loading anvils were properly ensured. Flexure testing produces tensile stress in the convex side of the specimen and compression stress

in the concave side. This creates an area of shear stress along the midline. To ensure the primary failure comes from the tensile or compression stress the shear stress must be minimized. This was done by using a support span-to-depth of 16:1. The specimen was loaded until rupture occurred in the fibers. The flexure properties of P4 and HexMC composites are detailed in Tables 15 and 16, respectively. The maximum strain recorded for both the automotive composites was quite similar but the maximum stress recorded by the HexMC composite was much greater than that recorded by the P4 composite. This resulted in the HexMC composites recording much higher stiffness than the P4 composites. The superior flexural performance of the HexMC composite is in agreement with the fracture toughness property results also reported in this document. The above is also in agreement with some other flexure studies conducted by this author in the past [4] on a different chopped carbon fiber composite system where in also it was concluded that the higher fiber volume fraction resulted in higher flexural strengths and stiffnesses. An increase in the fiber content improves the performance properties of a composite.

The P4 composite material system was tested at additional rates of 15.2 cm/min (6 inches/min) and 762 cm/min (300 inches/min) to investigate the preliminary strain rate dependence of their fracture toughness properties. On loading the P4 composite plate specimens at 0.15, 15.2, and 762 cm/min, the fracture toughness properties of the P4 composite was found to increase with loading rate. A comparison of the fracture toughness of P4 composites at loading rates of 0.15, 15.2, and 762 cm/min can be obtained by referring to Tables 14, 17, 18, and Figure 29.

Initial efforts to obtain consistent mechanical properties on chopped carbon

Table 15. P4 4-Point Flexural Properties

Spec. Numb.	Spec. Width W (mm)	Spec. Thickness d (mm)	Support Span S (mm)	S/d	Max. Stress (MPa)	Max. Strain (%)	Stiffness (MPa)
FLEX0_06P41	12.70	3.23	50.80	16	48.58	1.164	5963.75
FLEX0_06P42	12.70	3.18	50.80	16	52.67	1.565	5573.59
FLEX0_06P43	12.70	3.19	50.80	16	44.24	0.925	5809.90
FLEX0_06P44	12.71	3.24	50.80	16	53.32	1.313	6696.97
FLEX0_06P45	12.70	3.20	50.80	16	52.04	1.026	8123.84
FLEX0_06P46	12.70	3.14	50.80	16	54.66	1.196	6010.74
FLEX0_06P47	12.70	3.16	50.80	16	44.38	1.229	5526.04
FLEX0_06P48	12.70	3.16	50.80	16	67.98	1.240	6855.54
FLEX0_06P49	12.70	3.22	50.80	16	62.35	1.637	5497.06
FLEX0_06P410	12.69	3.18	50.80	16	53.56	1.028	6347.46
FLEX0_06P411	12.74	3.23	50.80	16	48.29	1.371	5465.88
FLEX0_06P412	12.57	3.33	50.80	16	58.83	1.157	7016.07
FLEX0_06P413	12.72	3.21	50.80	16	50.22	0.920	6646.47
FLEX0_06P414	12.73	3.23	50.80	16	50.24	1.002	6710.25
FLEX0_06P415	12.72	3.24	50.80	16	51.78	1.431	6153.86
FLEX0_06P416	12.72	3.24	50.80	16	60.55	1.084	8517.53
FLEX0_06P417	12.73	3.26	50.80	16	49.59	1.187	6325.21
FLEX0_06P418	12.72	3.24	50.80	16	67.22	1.465	7619.67
FLEX0_06P419	12.73	3.24	50.80	16	68.05	1.394	8345.85
FLEX0_06P420	12.73	3.28	50.80	16	49.24	1.459	5558.08
FLEX0_06P421	12.73	3.21	50.80	16	78.14	1.827	7569.35
Average Value					55.52	1.268	6587.29

Table 16. HexMC 4-Point Flexural Properties

Spec. Numb.	Spec. Width W (mm)	Spec. Thickness d (mm)	Support Span S (mm)	S/d	Max. Stress (MPa)	Max. Strain (%)	Stiffness (MPa)
FLEXHEXMC1	12.71	3.13	50.80	16	427.56	0.965	44766.06
FLEXHEXMC2	12.70	3.14	50.80	16	595.90	1.072	55299.51
FLEXHEXMC3	12.70	3.13	50.80	16	608.00	1.435	52029.82
FLEXHEXMC4	12.71	3.18	50.80	16	519.20	1.299	45431.76
FLEXHEXMC5	12.71	3.21	50.80	16	445.28	1.086	42478.71
FLEXHEXMC6	12.71	3.21	50.80	16	534.50	1.262	46146.80
Average Value					521.74	1.187	47692.11

**Table 17. Fracture Toughness Data from Tests Conducted at 15.2 cm/min Loading
Rate on P4**

Specimen ID	Avg. Width W (mm)	Avg. Thickness b (mm)	a/W	f(a/W)	K_{IC} (MPa-m^{1/2})
FRACTTOUG H6P411	12.700	3.400	0.50	10.65	5.88
FRACTTOUG H6P412	12.695	3.387	0.50	10.66	6.72
FRACTTOUG H6P413	12.690	3.357	0.50	10.66	7.21
FRACTTOUG H6P414	12.695	3.363	0.50	10.66	6.27
FRACTTOUG H6P415	12.695	3.350	0.50	10.66	6.23
FRACTTOUG H6P416	12.690	3.340	0.50	10.66	8.53
FRACTTOUG H6P417	12.690	3.300	0.50	10.66	3.64
FRACTTOUG H6P418	12.695	3.333	0.50	10.66	8.80
FRACTTOUG H6P419	12.695	3.327	0.50	10.66	5.41
FRACTTOUG H6P420	12.700	3.340	0.50	10.65	6.52
Average					6.52

**Table 18. Fracture Toughness Data from Tests Conducted at 762 cm/min Loading
Rate on P4**

Specimen ID	Avg. Width W (mm)	Avg. Thickness b (mm)	a/W	f(a/W)	K_{IC} (MPa-m^{1/2})
FRACTTOUG H300P421	12.700	3.283	0.50	10.65	14.59
FRACTTOUG H300P422	12.695	3.287	0.50	10.66	15.17
FRACTTOUG H300P424	12.690	3.280	0.50	10.66	13.76
FRACTTOUG H300P425	12.700	3.263	0.50	10.65	14.87
FRACTTOUG H300P426	12.690	3.230	0.50	10.66	13.14
FRACTTOUG H300P427	12.700	3.230	0.50	10.65	14.87
FRACTTOUG H300P428	12.695	3.203	0.50	10.66	9.98
FRACTTOUG H300P429	12.695	3.200	0.50	10.66	14.42
FRACTTOUG H300P430	12.690	3.200	0.50	10.66	11.94
FRACTTOUG H300P431	12.690	3.183	0.50	10.66	13.36
Average					13.61

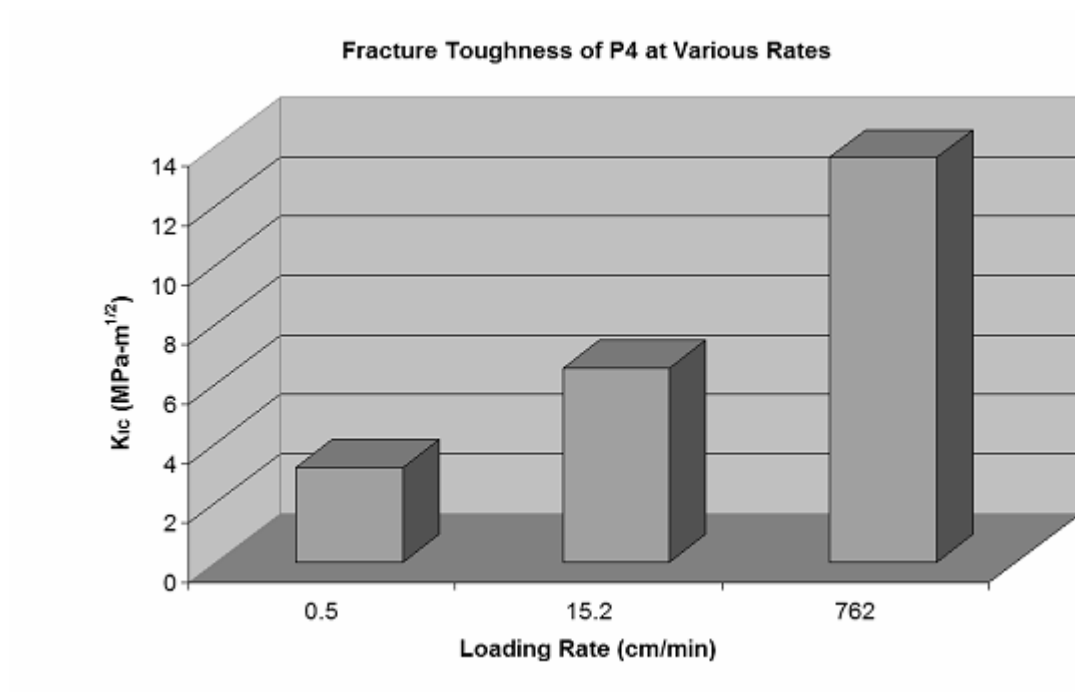


Figure 29. Comparison of Fracture Toughness for P4 Composites at Loading Rates of 0.15, 15.2, and 762 cm/min respectively

fiber materials were unsuccessful as can be inferred from all the above-mentioned observations. To focus on adhesive joint related issues, it is desirable to eliminate any variability or issues associated with the substrate material. Therefore, a braided carbon fiber composite was selected to replace the chopped carbon fiber material for this study.

3.2 Braided Carbon Fiber Epoxy Composite Substrate Material

In this section, the focus is on the carbon/epoxy composite substrate material where in their performance properties at quasi-static and dynamic loading rates are investigated.

3.2.1 Tension, Compression, and Shear

Tensile and compression testing at quasi-static rates of substrate materials that were machined from two different orthogonal directions in the panels indicated similar properties in both directions. Tensile results for five samples each from two different orthogonal directions in the panels resulted in average ultimate strength and modulus for the substrate being 485.2 MPa and 40.2 GPa, respectively. Coefficients of variability (COV) for the strength and modulus results were 3.6% and 1.3%, respectively. Compressive results for five samples each from two different orthogonal directions in the panels resulted in average ultimate strength and modulus for the substrate being 423.5 MPa and 38.3 GPa, respectively. COVs for the strength and modulus results were 3.9% and 3.2%, respectively. Figures 30 and 31 show typical tensile and compression stress strain curves for the substrate material, respectively. Shear tests using the V-notched beam method were conducted to determine the shear properties of the substrate material.

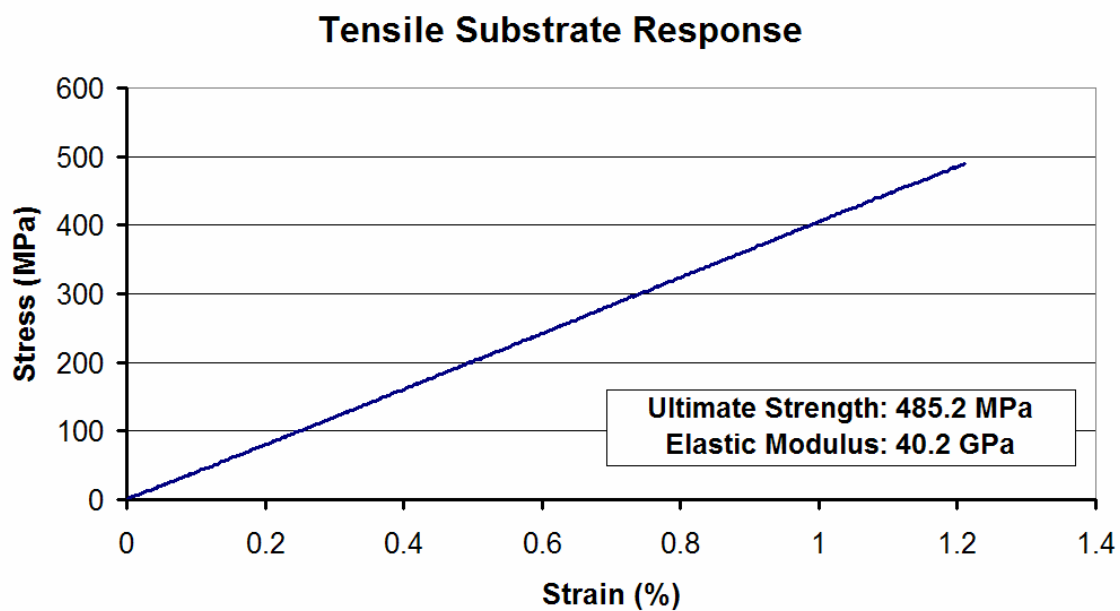


Figure 30. Typical Tensile Stress Strain Curve for the Substrate Material

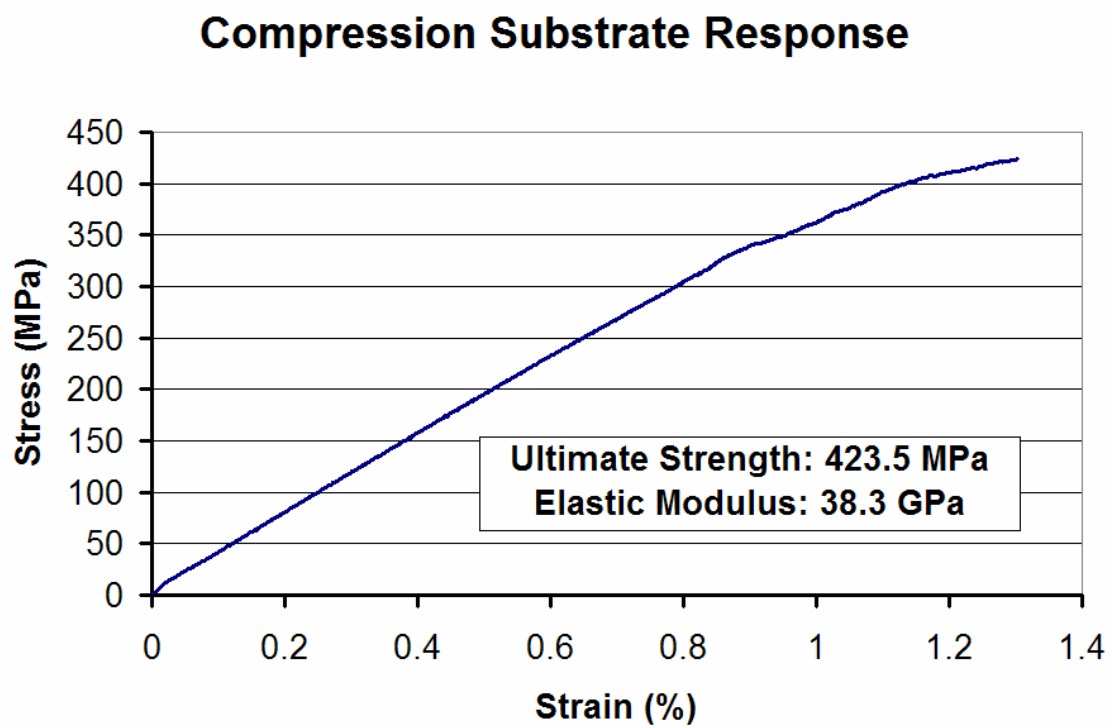


Figure 31. Typical Compression Stress Strain Curve for the Substrate Material

Tests were done in replicates of three recording failure shear stress, failure shear strain, and modulus of 186.3 MPa, 1.47%, and 13.53 GPa, respectively. Table 19 details the standard deviation and COVs for the various shear properties obtained.

Tensile tests for five substrate samples each conducted at 100, 500, and 1000 mm/sec resulted in a decrease in the average ultimate strength with increasing loading rate. A slack adapter or lost motion device shown in Figure 32 was included in the test set-up at higher rates to reach the desired velocities prior to the load application on the test specimen. The need for the slack adapter can be further understood from Figure 33. If the red line is the command signal and the white line the feedback, one can see that at lower rates these two lines overlap each other. This is to suggest that there is no difference between what one is asking the machine to do (command signal) and what the machine actually does (feed back signal). However, at higher rates one can observe a difference between the two signals, which suggests that there is a minimum time lapse before the machine can get up to speed and load the specimen at the desired rate. The slack adapter by providing the much-needed slack accounts for this additional time needed by the machine to get up to speed before loading the specimen. Figure 34 illustrates the variation of ultimate tensile strength of the substrate material with loading rate. Error bars are included in the figure to show the scatter in the data.

Table 19. Shear Test Results of the Substrate Material

Substrate			
	Failure Shear Stress (MPa)	Failure Shear Strain (%)	Modulus (GPa)
Average	186.3	1.47	13.53
Std. Dev.	19.62	0.13	0.33
COV	10.53	8.63	2.45

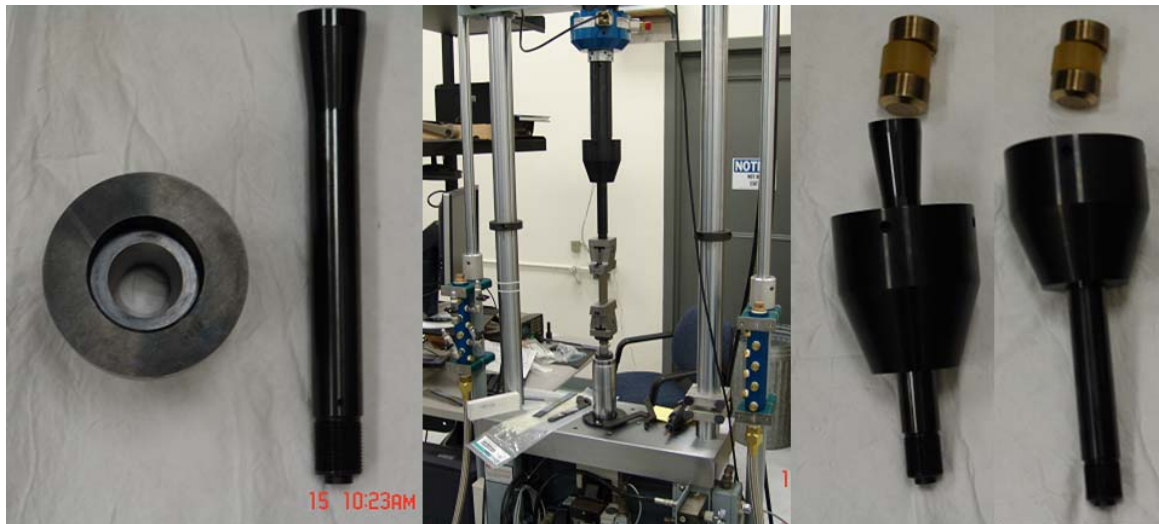


Figure 32. Slack Adapter

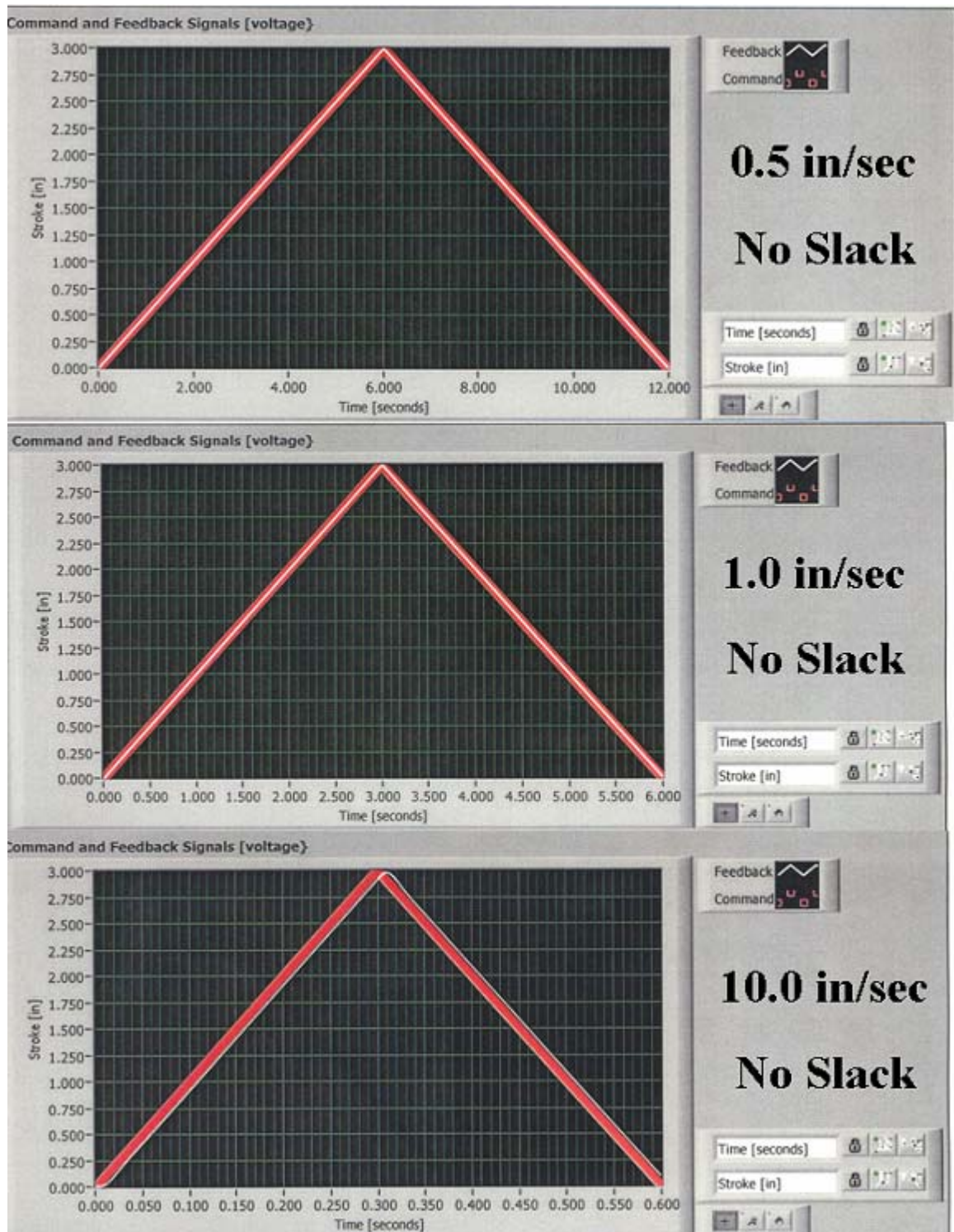


Figure 33. Illustration of the Need for a Slack Adapter

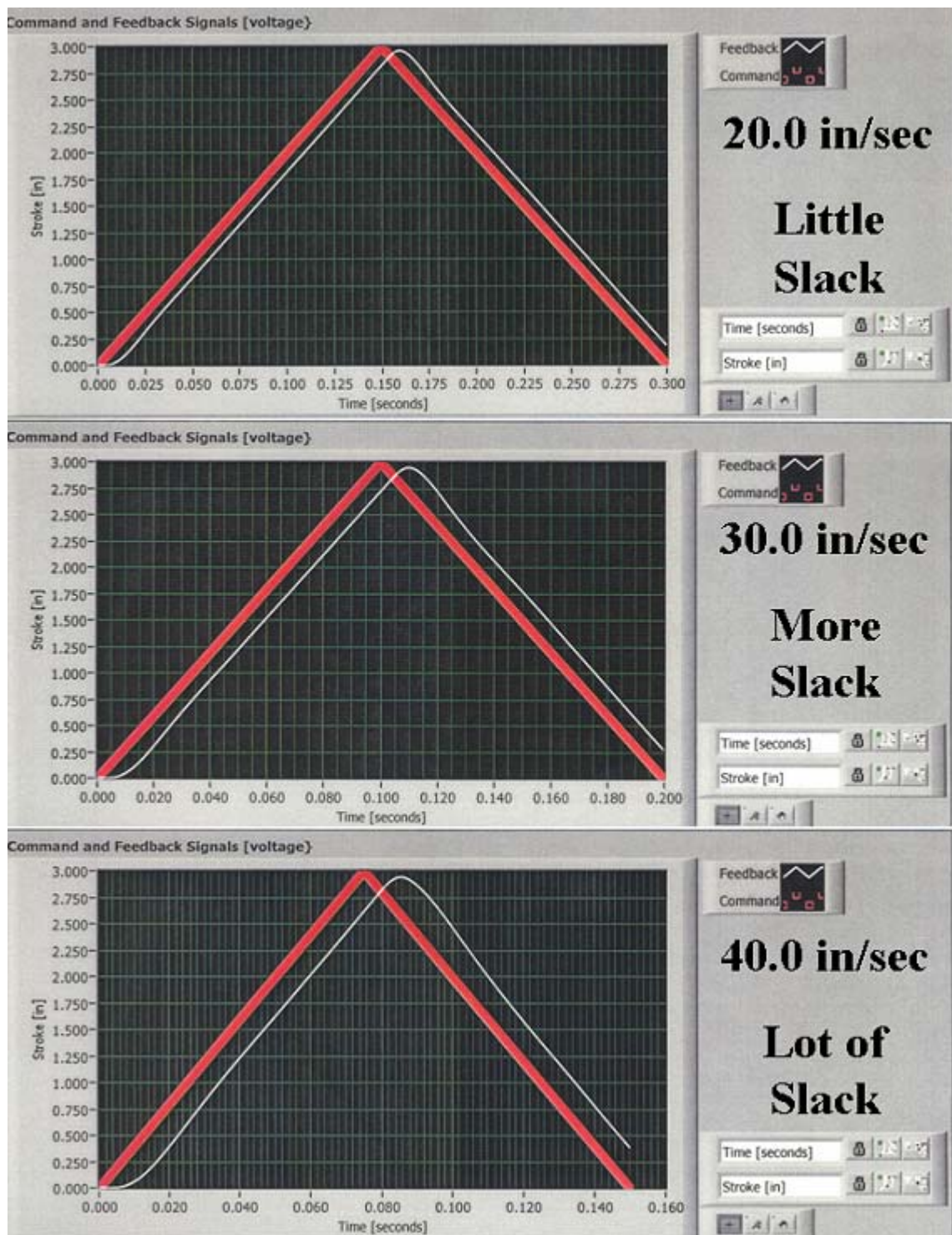


Figure 33. Continued

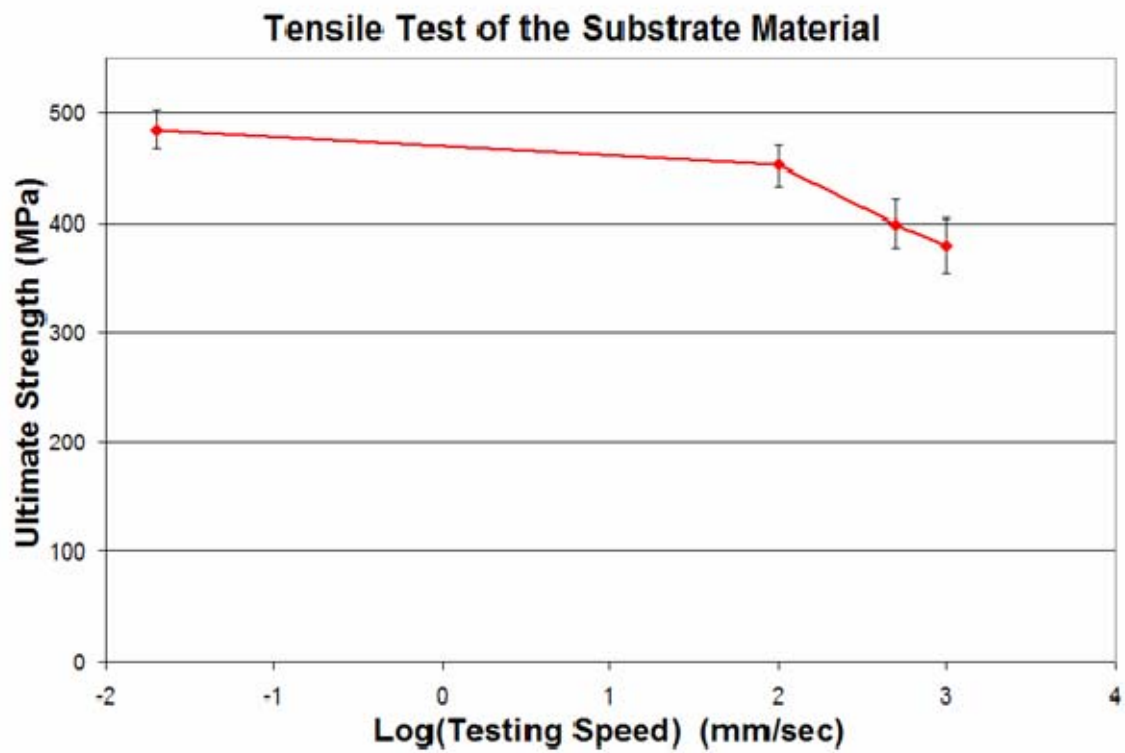


Figure 34. Variation of Ultimate Tensile Strength of Substrate with Loading Rate

3.3 Epoxy Bulk Adhesive

The results of various studies conducted on the epoxy bulk adhesive are reported in this section. This is another major section of our work where in the concentration is on the bulk adhesive.

3.3.1 Thermal Characterization

Fragments from the cured plaques of the bulk adhesive were examined with differential scanning calorimetry (DSC) to determine the glass transition temperature and, through a second heat, information about the degree of cure was obtained. The DSC result for a typical sample heated at a rate of $10^{\circ}\text{C}/\text{min}$ is shown in Figure 35. From DSC we were able to determine that the glass transition temperature of the as-produced specimen was 82°C , as defined by the midpoint method. Furthermore, additional curing was evident when the specimen was reheated following the first heat to 200°C . The glass transition temperature as measured during the second heat was found to be 88°C . Although this information provides convincing evidence that the as-produced specimens were not fully cured, no attempt was made to post-cure the adhesive samples, as the manufacturer recommends that optimum performance is obtained at less than full cure.

3.3.2 Tension and Compression

Characterization of the static tensile response of the bulk adhesive resulted in excellent consistency of the tensile properties among the specimens tested. The average ultimate strength and modulus for the adhesive were 49.4 MPa and 2.3 GPa, respectively. COVs for the strength and modulus results are 2.3% and 2.7%, respectively. Samples of

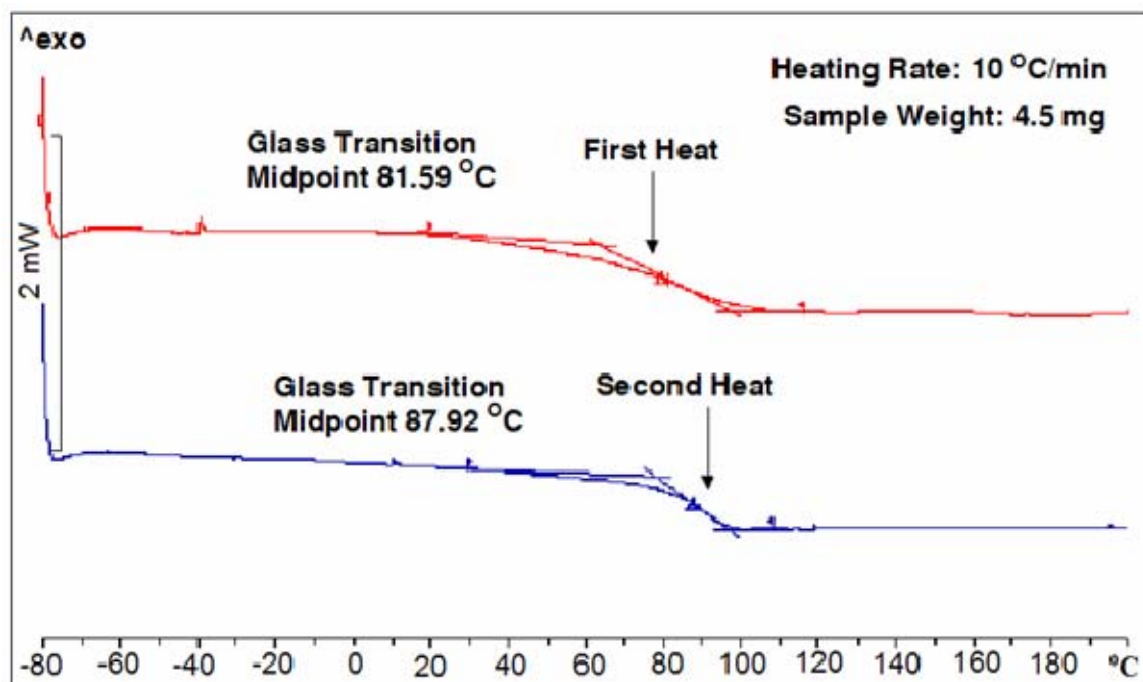


Figure 35. Differential Scanning Calorimetry Results for First and Second Heats

bulk adhesive were subjected to compressive loads at quasi-static rates up to strains of 30% without global failure. The average compressive modulus for the adhesive was 2.4 GPa, almost identical to the tensile value. Figures 36 and 37 show typical tensile and compression stress strain curves for the bulk adhesive, respectively. Compressive strength, defined as the stress just before the knee formation in the stress strain curve, averaged about 51.8 MPa with a COV of 8.4%.

Bulk adhesive samples tested in replicates of five under tensile loading from 0.02 to 500 mm/sec resulted in the average ultimate strength remaining constant over this range of rates. Getting to the desired velocities prior to load application was ensured by the inclusion of the slack adapter. Figure 38 illustrates the ultimate tensile strength of the bulk adhesive as a function of rate. Error bars are included in the figure to show the scatter in the data. At 1000 mm/sec, it was difficult to discern the loads generated by the test specimen because of contributions from the momentum of the 10-pound (4 kg) slack adapter. To eliminate these inertia effects typical static test set-ups need to be modified to produce valid results. One possible suggestion would be to repeat the tests at 1000 mm/sec after inverting the slack adapter to shorten and simplify the load train between the load cell and specimen.

3.3.3 Shear

Refusal of the bulk adhesive to fail in shear resulted in the need for innovative test method development to ultimately determine the shear properties. Preliminary shear tests by applying a torsional load on a solid cylindrical bulk adhesive rod (12 mm in diameter and 100 mm long) did not result in specimen failure. The

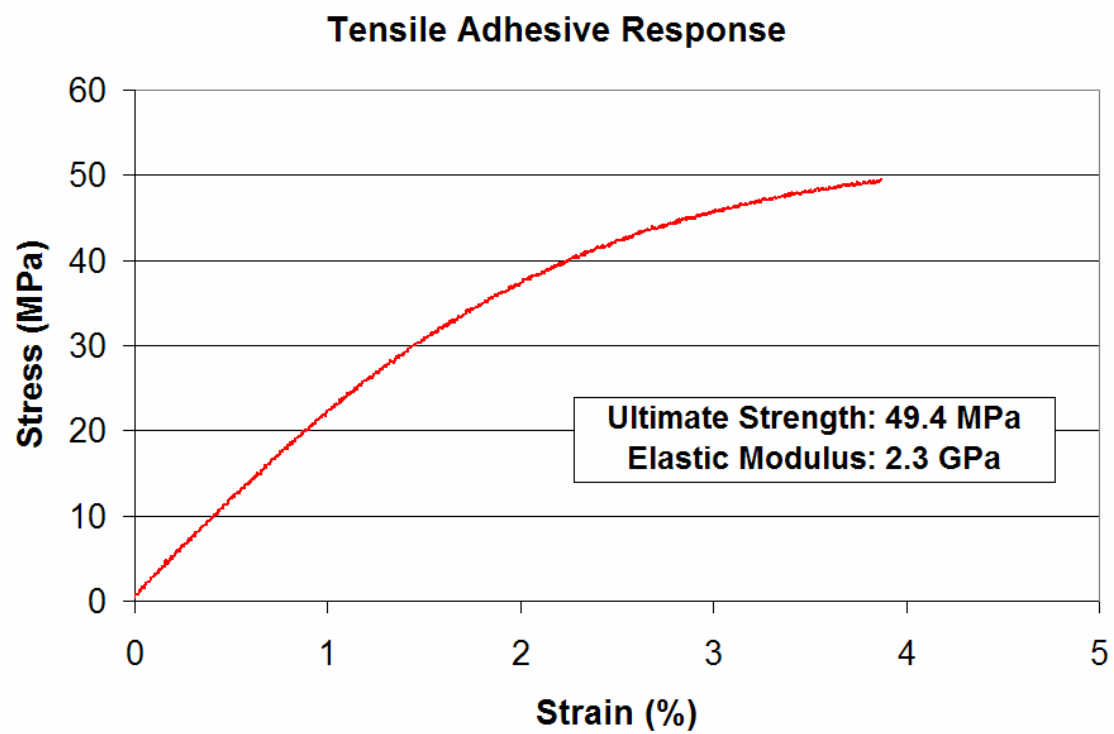


Figure 36. Typical Tensile Stress Strain Curve for the Bulk Adhesive

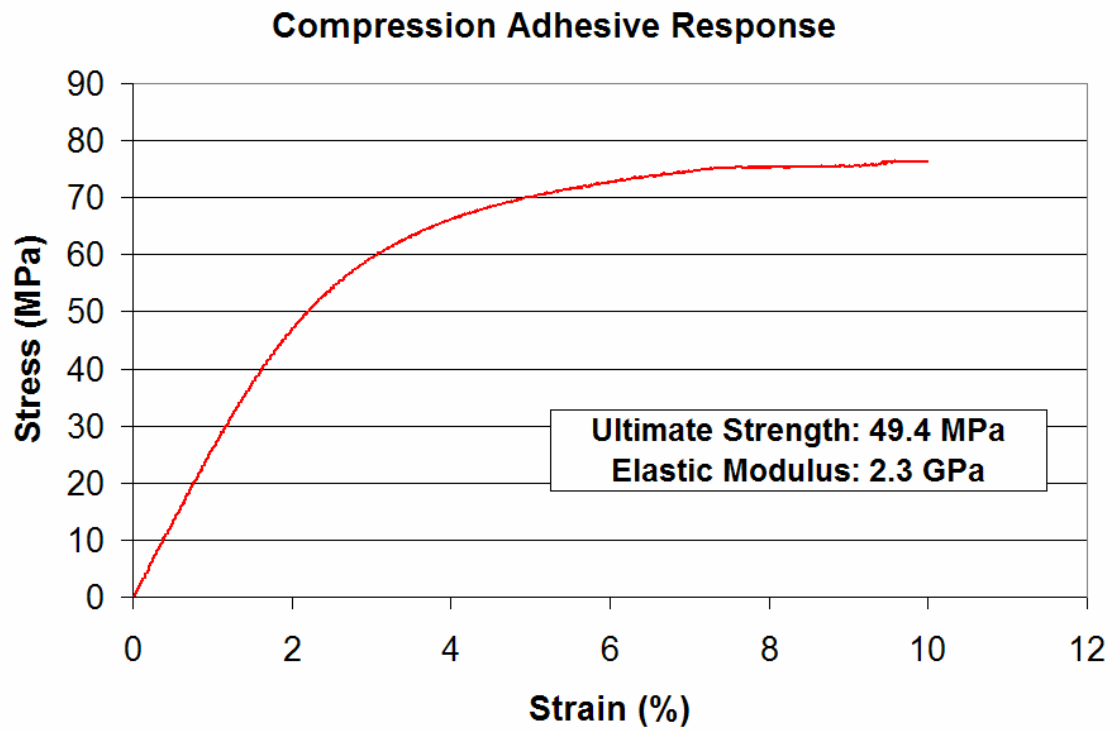


Figure 37. Typical Compression Stress Strain Curve for the Bulk Adhesive

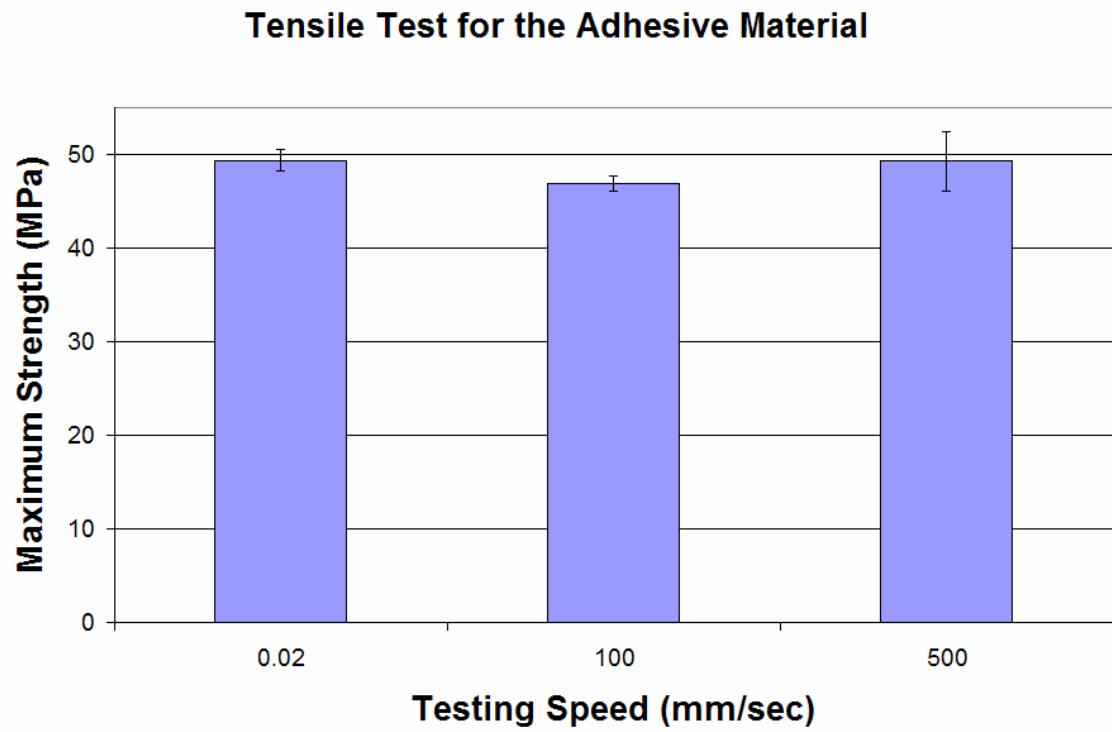


Figure 38. Variation of Ultimate Tensile Strength of Bulk Adhesive with Loading Rate

maximum shear strain of almost 13% corresponded to a 60° rotation of the specimen, which was the maximum capability of the test equipment. To achieve a more uniform shear stress distribution and possibly produce an ultimate failure the solid rod specimen geometry was modified to a hollow cylinder. The specimens were manufactured by drilling and reaming the solid rods. The hollow cylindrical specimen still did not fail before exceeding the maximum capability of the test equipment. Figure 39 shows the measured shear stress-strain response of the solid rod and hollow cylindrical bulk adhesive. Finally, shear tests using the V-notched beam method, typically used for composite materials, were conducted to determine the shear properties of the bulk adhesive, which resulted in specimen failure. Figures 40 and 41 illustrate the measured shear stress-strain response and modulus determination of the bulk adhesive, respectively. Bulk adhesive samples tested in replicates of three recorded failure shear stress, failure shear strain, and modulus of 30.99 MPa, 4.45%, and 0.977 GPa, respectively. Table 20 details the standard deviation and COVs for the various shear properties obtained. It is believed that the shear stress strain response obtained from the V-notched specimens is the most accurate based on the agreement of the measured shear modulus with the Young's modulus-shear modulus relationship. However, this method severely underestimates the failure shear stress and strain.

3.3.4 Fracture Toughness

Fracture toughness tests of the bulk adhesive were conducted on a conventional closed-loop servo-hydraulic machine at rates of 0.02, 2.5, 25, and 1000 mm/s with three specimens tested at each rate. From these initial tests, there appeared to

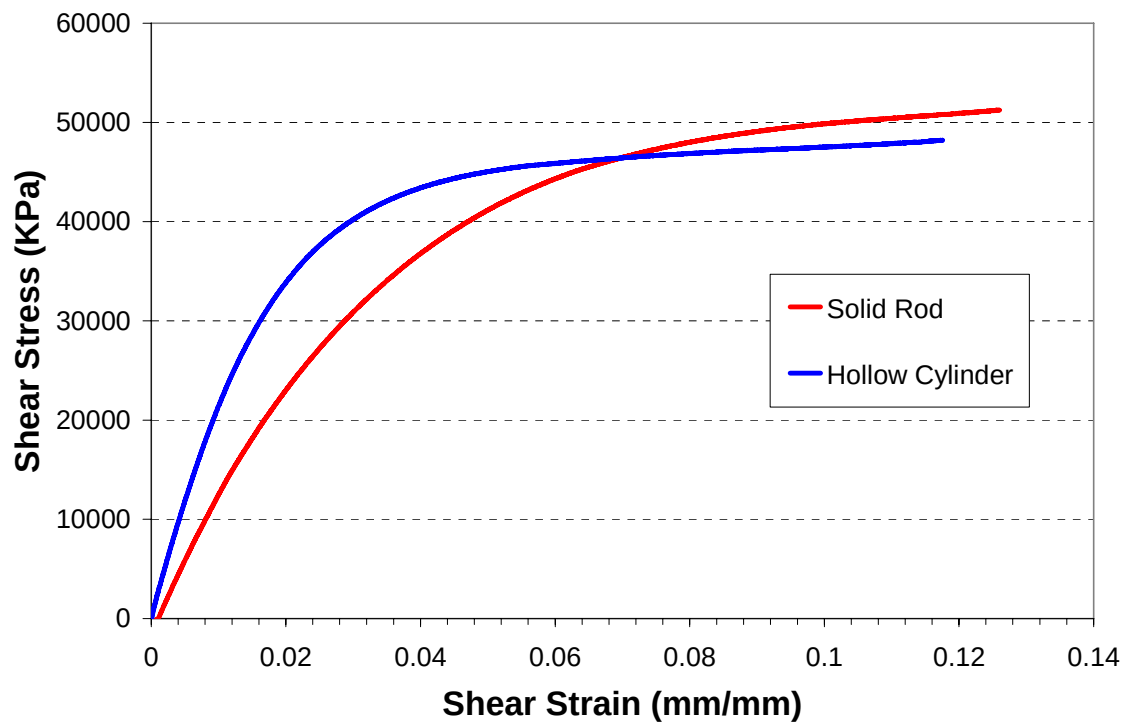


Figure 39. Measured Shear Stress-Strain Response of the Solid Rod and Hollow Cylindrical Bulk Adhesive

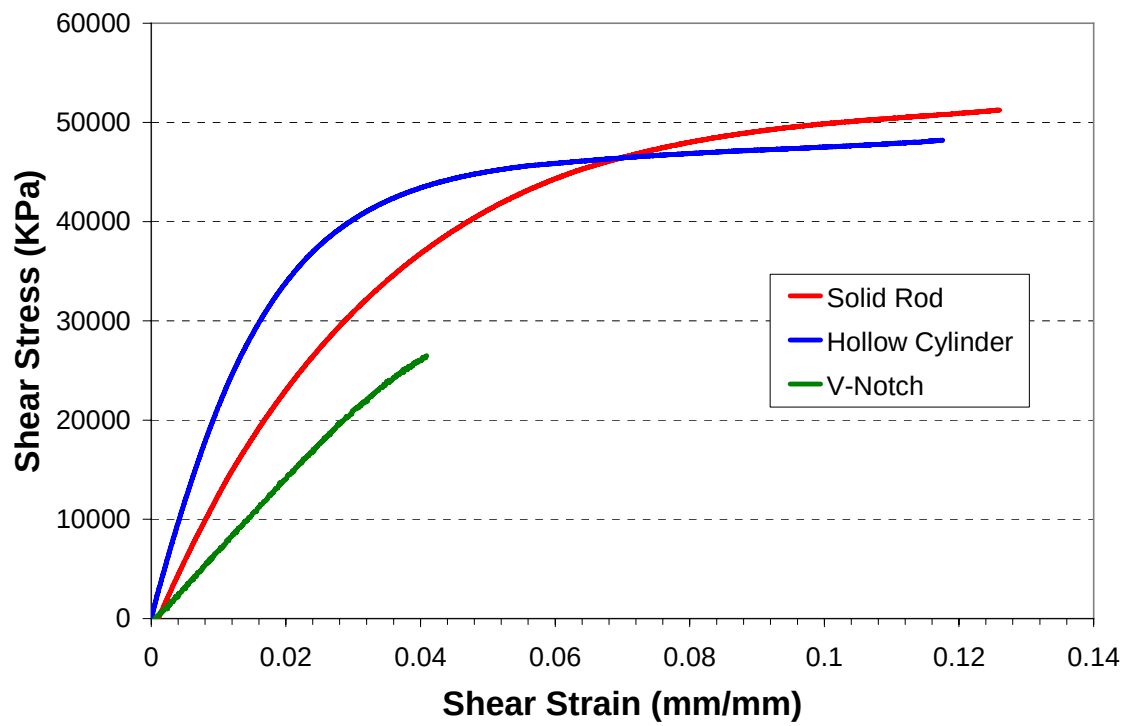


Figure 40. Shear Stress-Strain Response of the V-notched, Solid Rod, and Hollow Cylindrical Bulk Adhesive

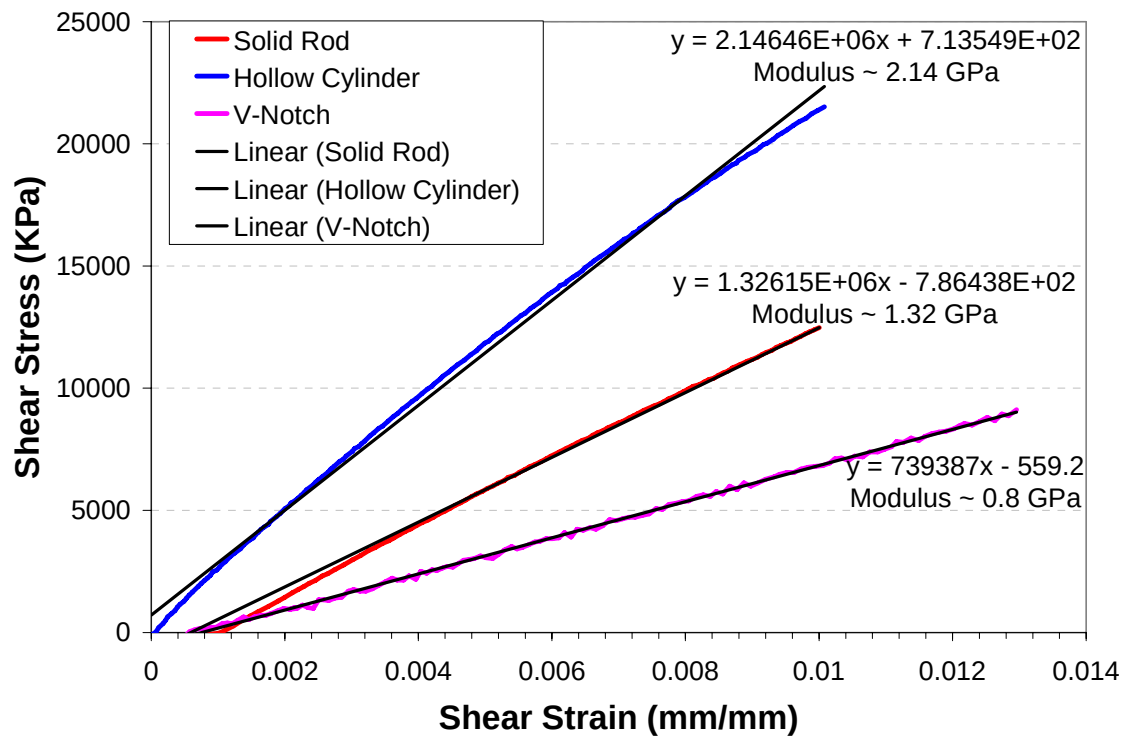


Figure 41. Modulus Determination of the Bulk Adhesive

Table 20. Shear Test Results of the Bulk Adhesive

Bulk Adhesive			
	Failure Shear Stress (MPa)	Failure Shear Strain (%)	Modulus (GPa)
Average	30.99	4.54	0.977
Std. Dev.	1.67	0.63	0.037
COV	5.37	13.78	3.76

be a drop in the fracture toughness as a function of loading rate but the values appeared to be much greater than typical values for an epoxy system. The experimental data and test methodology was evaluated for their validity and it was determined that the target velocities were not achieved prior to load application. Consequently, the test set-up was modified to include a commercial slack adapter within the load frame and the tests were repeated at 0.02, 100, 500, and 1000 mm/sec. Checkout tests were completed and it was determined that the target velocities could be achieved prior to the load application. There was a drop in fracture toughness as loading rate was increased from .02 to 100 mm/sec. At rates of 500 and 1000 mm/sec, it was difficult to detect the relatively small loads generated by the test specimens because of contributions from the momentum of the 10-pound (4 kg) commercial slack adapter as can be seen in Figure 42. Hence an alternate much smaller and much lighter slack adapter shown in Figure 43 was fabricated in house by cutting and threading a #2 Morse taper (Standard Extension Drill Socket: 1-5161-015). The device provided sufficiently smooth loading profiles, especially when a small amount of lubrication was added to prevent premature sticking. Figure 44 illustrates the smooth load profile obtained for a string pulled in tension at 1000 mm/sec. Fracture toughness tests were successfully repeated at various rates starting from 0.001 to 1000 mm/sec using the new setup with three specimens tested at each rate. The new setup, shown in Figure 45, boasted of a smaller load cell, shorter and simplified load train between the load cell and specimen, and reduced weight on the specimen because of the lighter adapter. In order to minimize the mass of the load train between the specimen and load cell, a short section of threaded rod was used to attach a small aluminum clevis

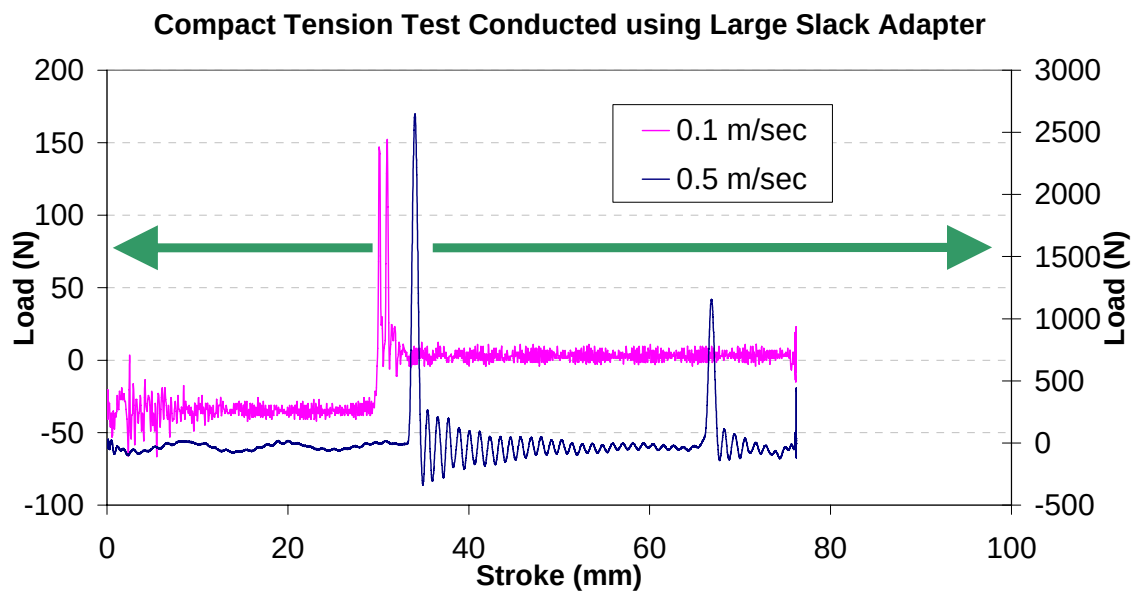


Figure 42. Load Displacement Curves from Compact Tension Test using Large Slack Adapter



Figure 43. Simple Slack Adaptor Fabricated from a #2 Morse Taper Extension

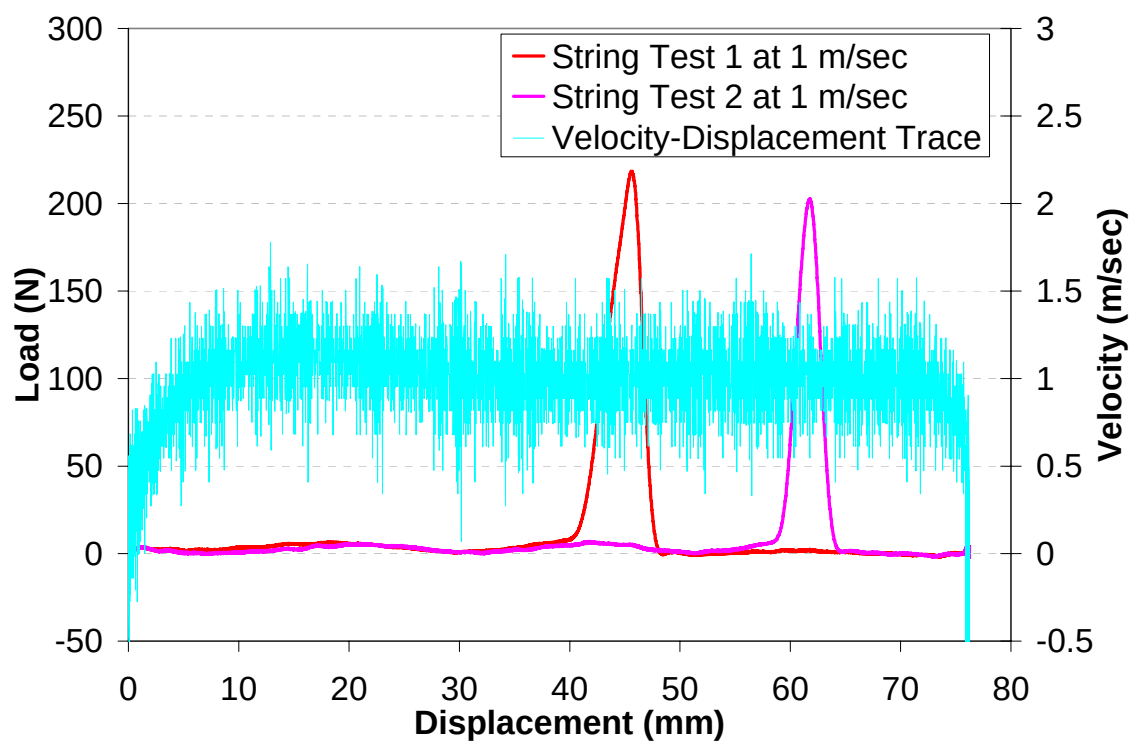


**Figure 44. Smooth Load Profile obtained for a String pulled in Tension at 1.0 m/sec
using the Small Slack Adapter**

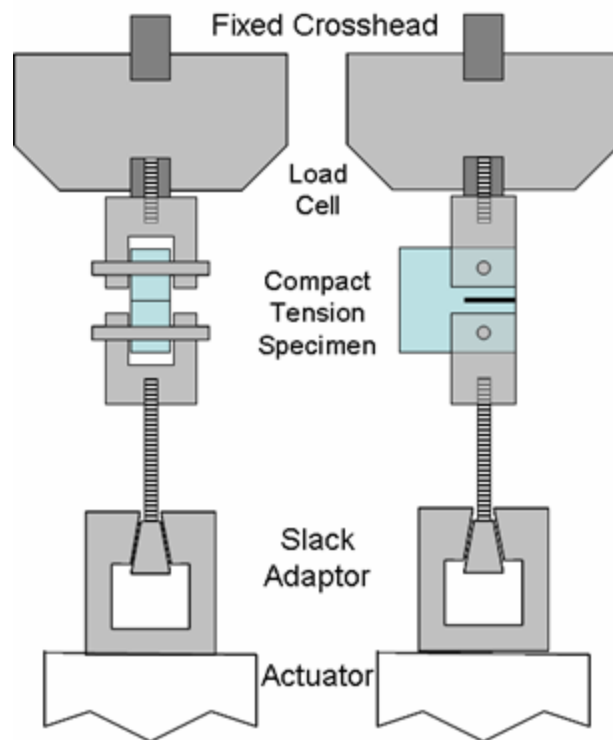


Figure 45. Simplified Load Train used for Compact Tension Tests at Higher Loading Rates

directly to the load cell, which was in turn mounted to the upper, stationary crosshead. The slack adapter was attached to the lower clevis.

Figure 46 illustrates the typical load displacement traces for several CT specimens tested at room temperature over the range of loading rates from 0.001 to 1000 mm/s. As can be seen from Figure 46 the loading profiles looked very clean for all loading rates. For some specimens that were tested at the highest crosshead displacement rate of 1000 mm/sec shoulders were seen on the load traces. Figure 47 illustrates the range of behavior that was observed at this highest loading rate. Fracture toughness was found to decrease as a function of loading rate resulting in a drop in fracture toughness at 1000 mm/sec to about 20% of the value at lower rates. The variation of fracture toughness with loading rate is shown in Figure 48. Error bars, which have been included in the figure, show that the data obtained were quite consistent at each condition. At the lower rates, there was extensive stress whitening and stable crack growth. Stress whitening was significantly reduced at higher testing speeds suggesting that plastic deformation at the crack tip was severely limited at the higher loading rates. The failure was brittle at higher rates. While the plane strain fracture toughness, K_{IC} , was determined from the intercept of a 95% slope with the load trace for the slower rates, the peak load was used to determine the fracture toughness at the higher rates. The dashed line in Figure 48 illustrates fracture toughness values obtained from using the peak load values at the slower rates. An interesting observation was that higher testing speeds resulted in catastrophic failure; the fracture surface rotating to nearly a 45-degree angle, similar to shear failure. Inertial effects or alignment issues could be possible reasons for the above

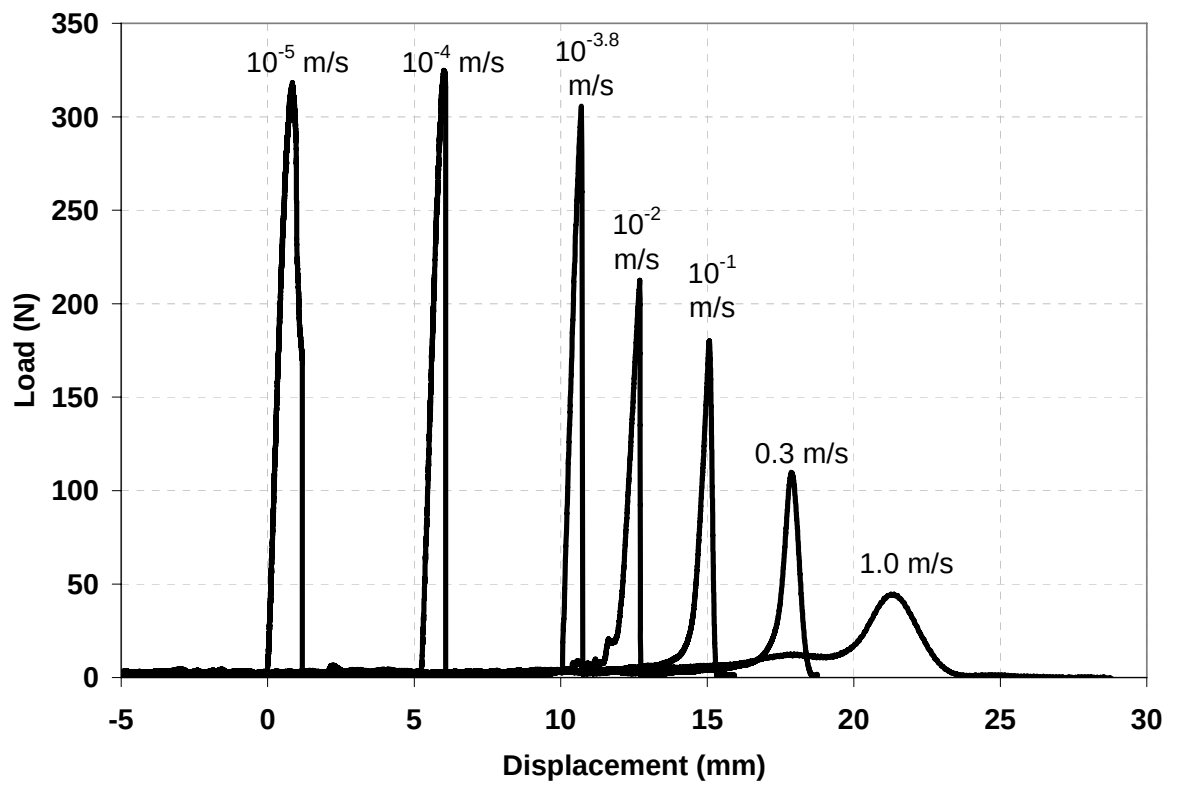


Figure 46. Typical Load Displacement Response for Compact Tension Tests at Various Loading Rates

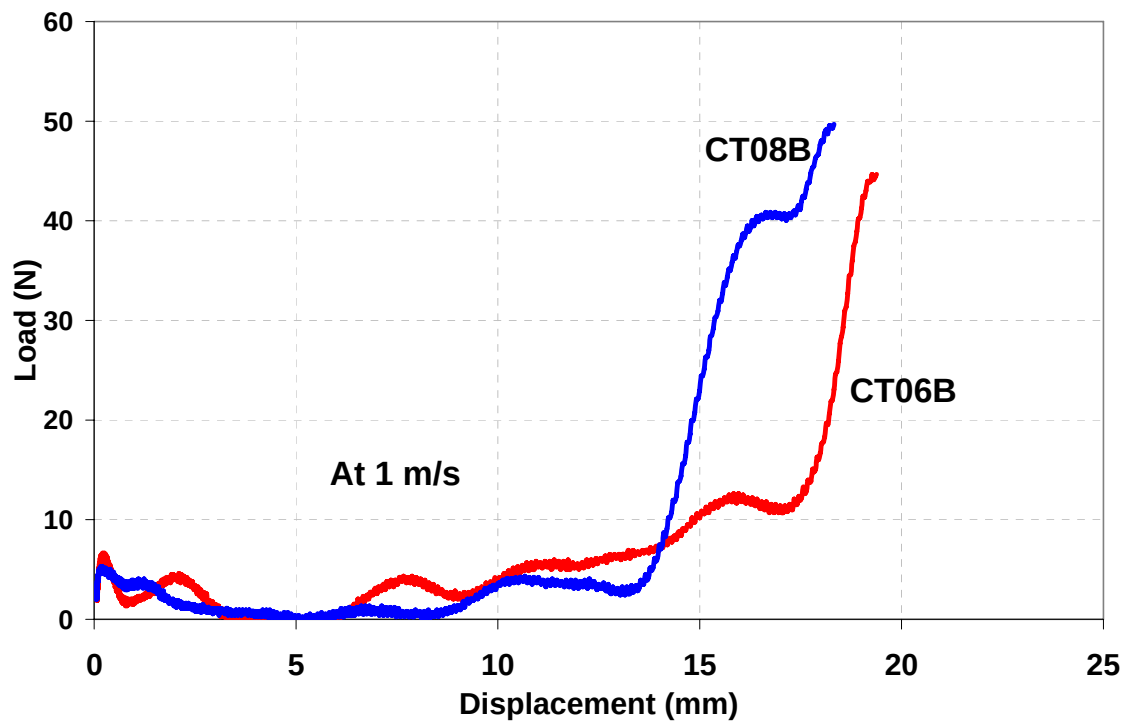


Figure 47. Comparison of Load Displacement Traces at the Highest Loading Rate

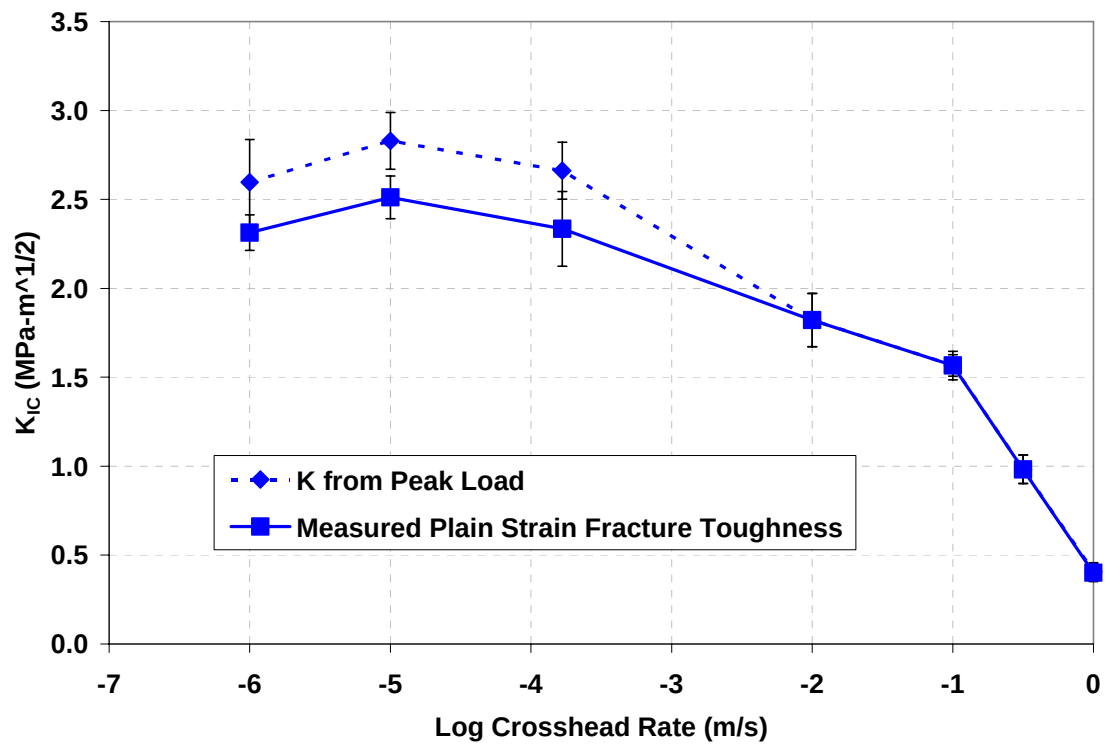


Figure 48. Measured Fracture Toughness Values as a Function of Crosshead Rate

interesting observation. Figures 49 and 50 have the failed specimens placed in the order of increasing loading rate from left to right depicting a comparison of the stress-whitened, plastic zones and the 45-degree crack rotation observed at higher rates.

Predicting a high temperature dependence of fracture toughness properties further tests were done at sub-ambient temperatures. These tests were conducted to determine if time temperature superposition (TTSP) could be used to predict behavior at even higher loading rates. After all, there was no plateau in fracture toughness indicated at higher test speeds in Figure 48. Tests conducted at slower rates at temperatures as low as -115°C showed somewhat lower fracture toughnesses than obtained at room temperature, but the effect was less pronounced than expected from a TTSP perspective. Figures 51 and 52 illustrate the fracture toughness values obtained at all subambient temperature levels over the full range of testing rates and its comparison to tests conducted at room temperature. Subambient testing at temperatures -100°C below room temperature lowered fracture toughness values to only 55% of the ambient values at a given crosshead rate. This is in contrast to the high rate loading, which resulted in up to 80% drops in fracture toughness. The TTSP failed to account for the fracture toughness values obtained at subambient conditions. This failure could be due to the complexities of this fully formulated adhesive, which could exhibit multiphase and thermorheologically complex behavior. The failure of TTSP to account for the fracture toughness values obtained at subambient conditions is not without precedent [125].

Figures 53 through 56 show scanning electron microscope (SEM) images of the fracture surface of the CT specimens tested at slow, intermediate and the high rates.

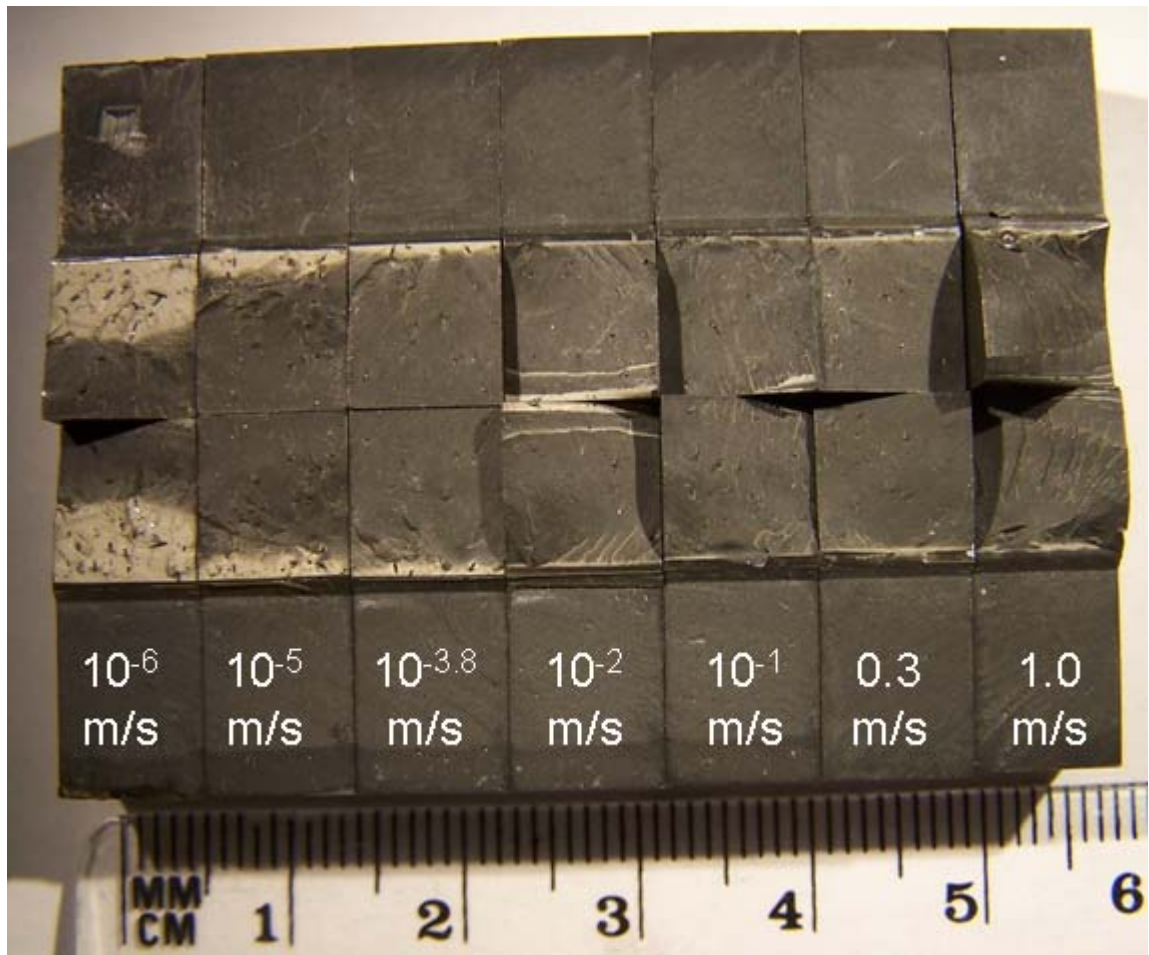


Figure 49. Illustration of Typical Stress Whitening Extent for Range of Crosshead Rates

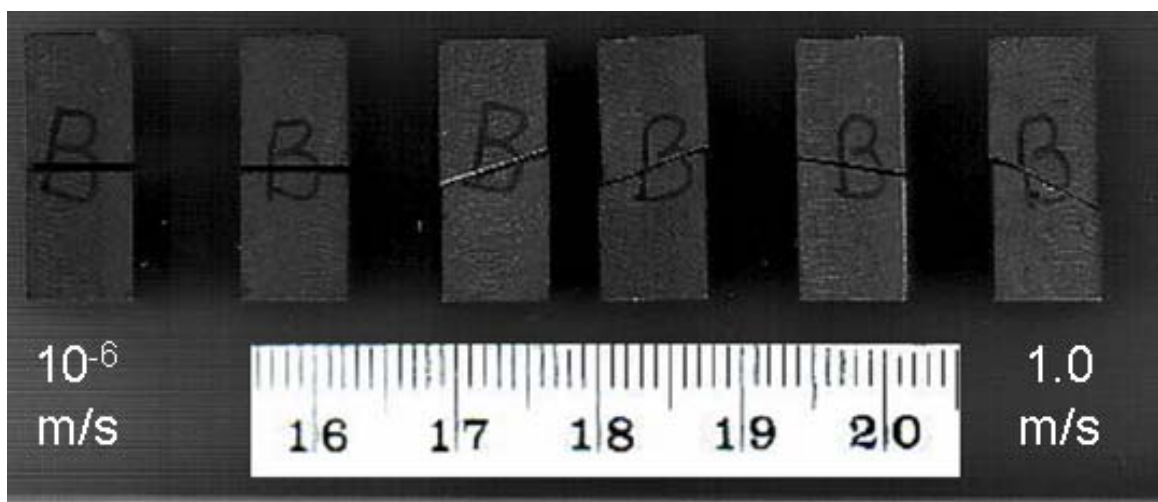


**Figure 50. Illustration of Intersection of Crack Plane with Outer Edge of Specimen
showing Crack Plane Rotation at Higher Crosshead Rates**

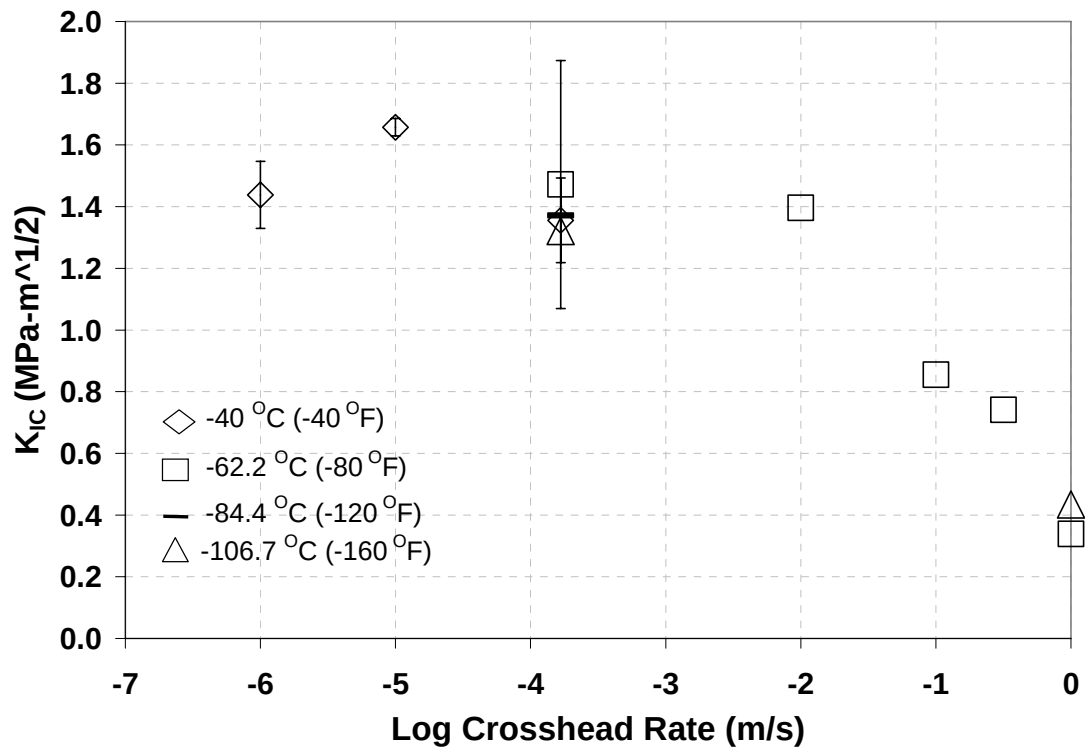


Figure 51. Summary of Compact Tension Tests Conducted at Subambient Temperature Levels at Various Crosshead Rates

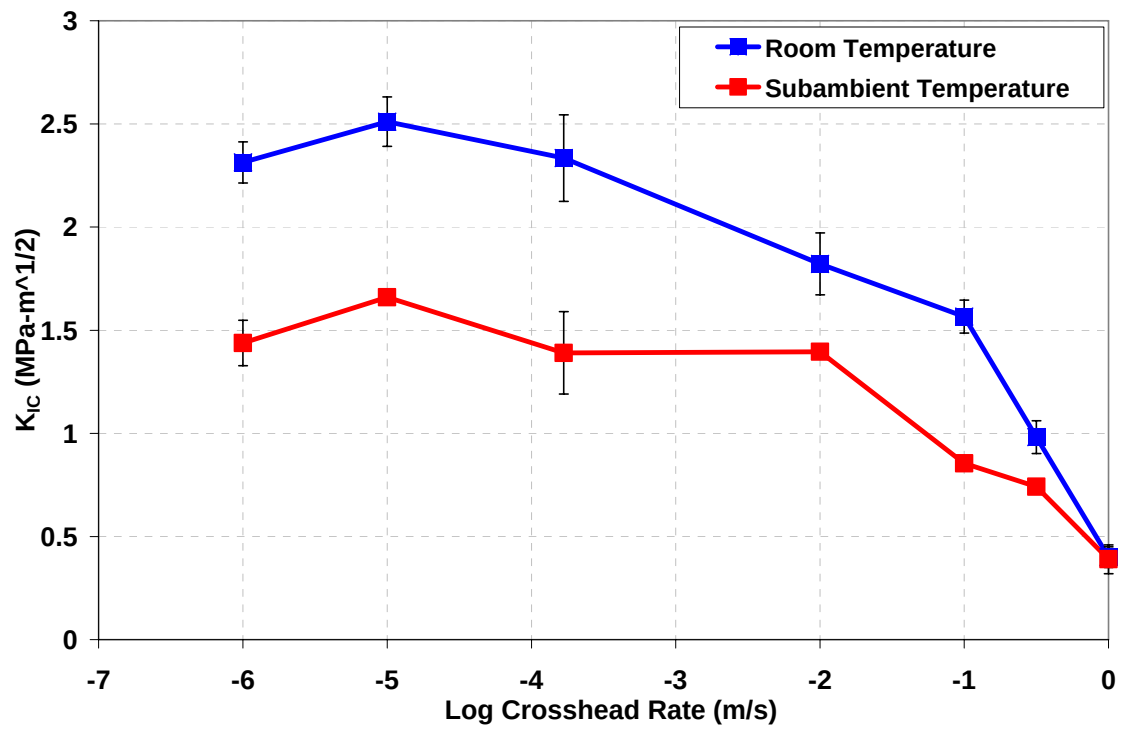


Figure 52. Variation of Fracture Toughness with Crosshead Rate at Room Temperature and Subambient Temperature Testing

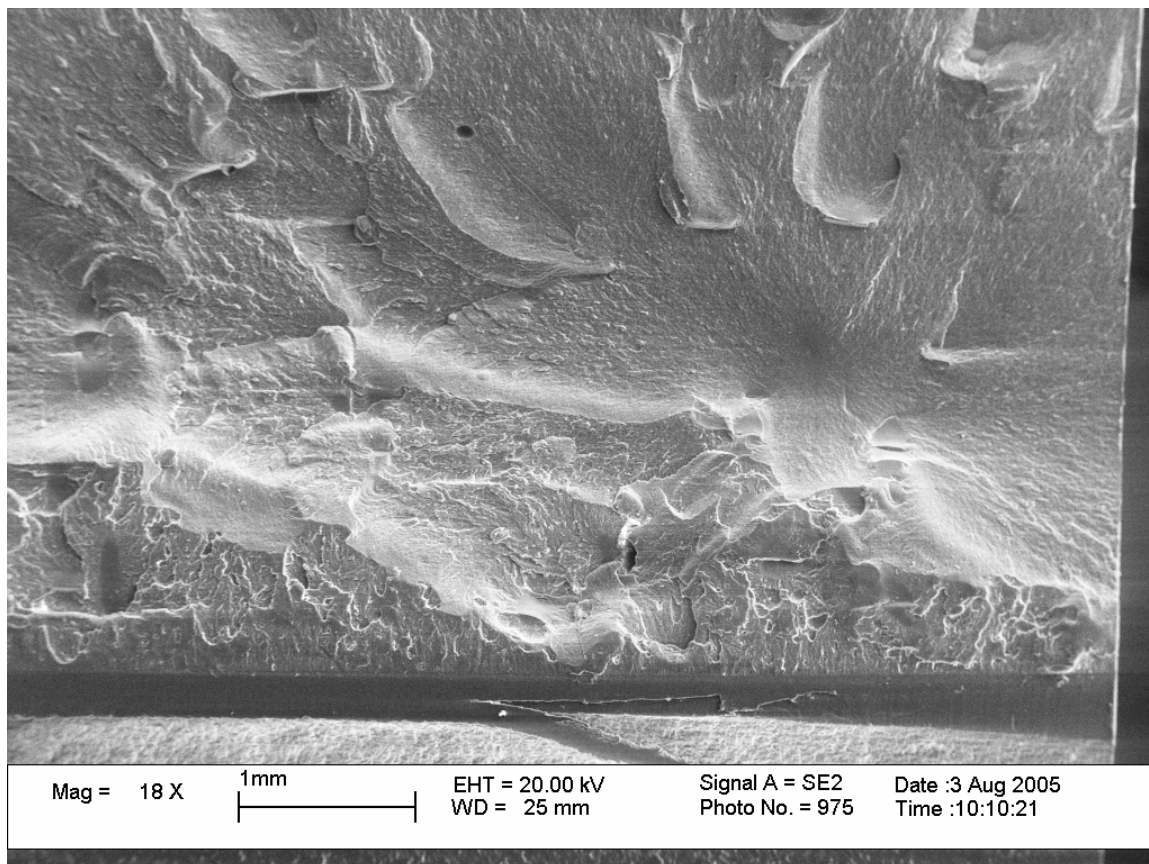


Figure 53. SEM Image of the Fracture Surface of a Compact Tension Specimen

Tested at a Slow Rate of 10^{-5} m/s

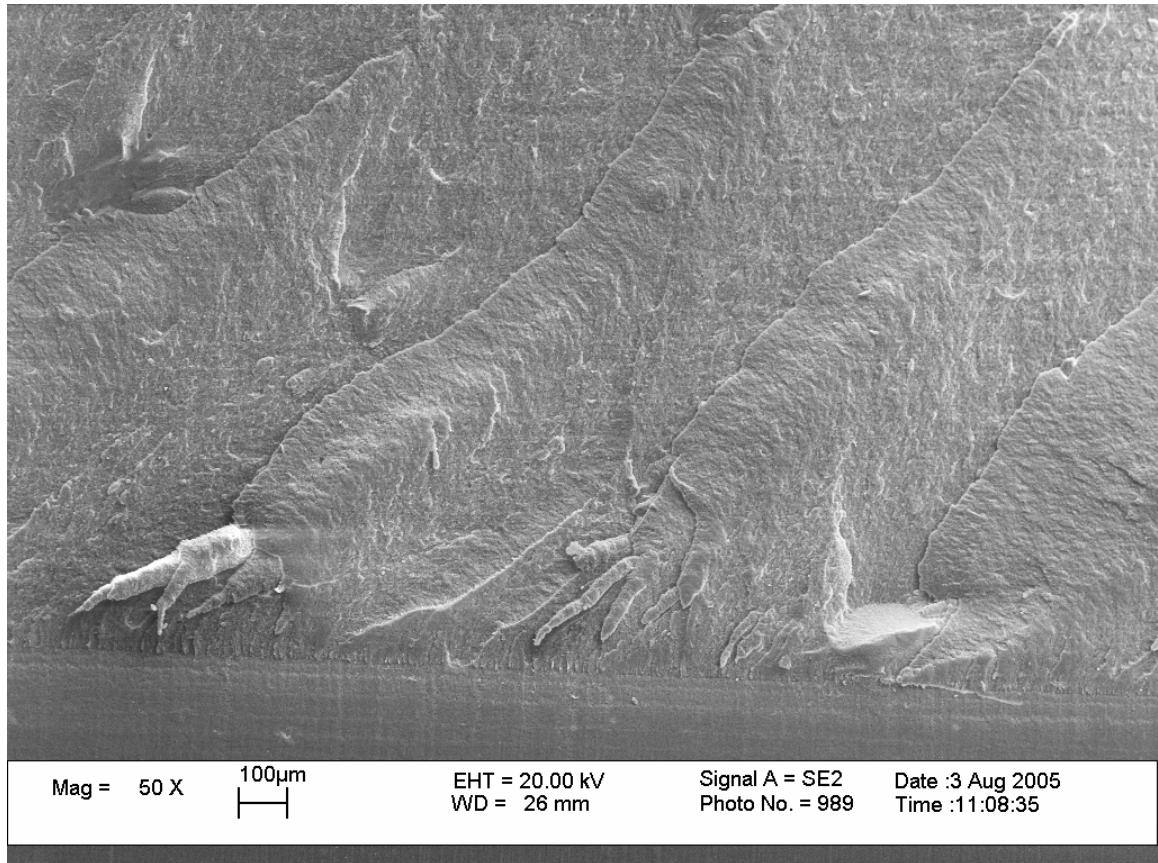


**Figure 54. SEM Image of the Fracture Surface of a Compact Tension Specimen
Tested at an Intermediate Rate of 0.01 m/s**

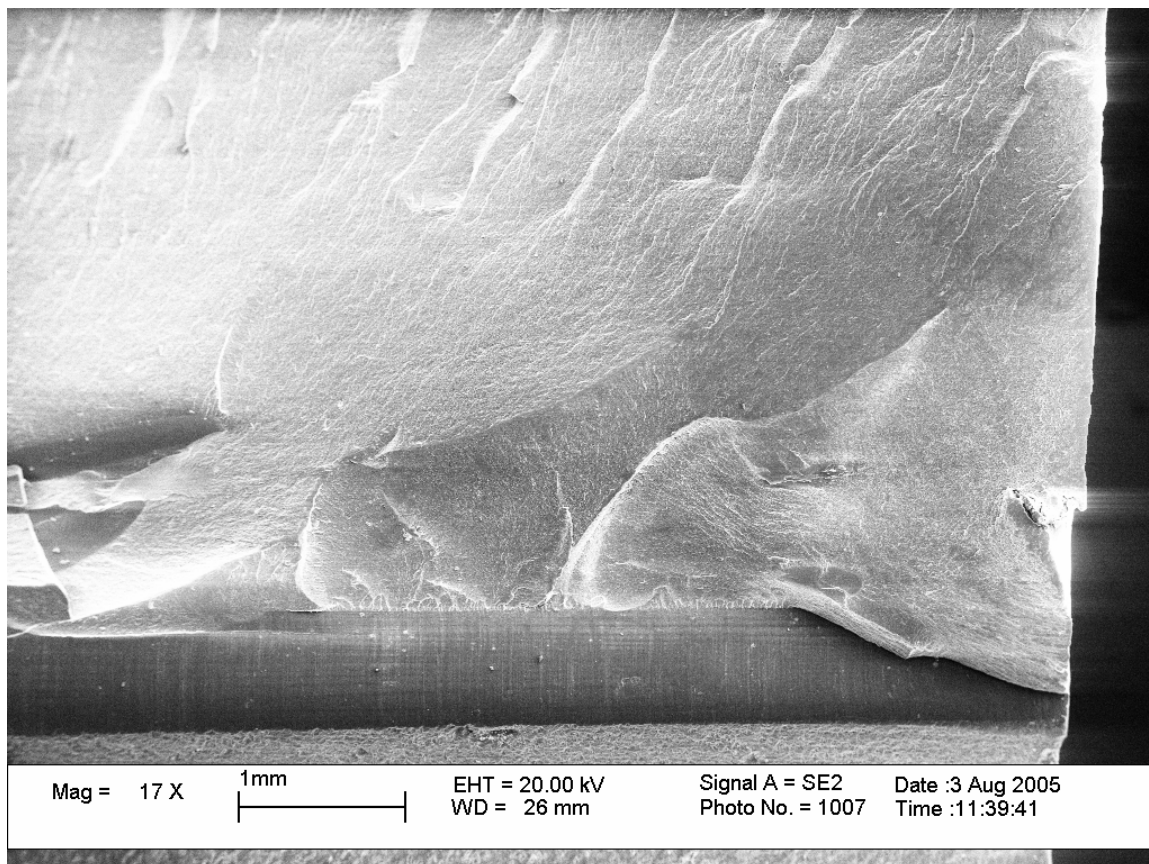


**Figure 55. SEM Image of the Fracture Surface of a Compact Tension Specimen
Tested at the Highest Rate of 1 m/s**

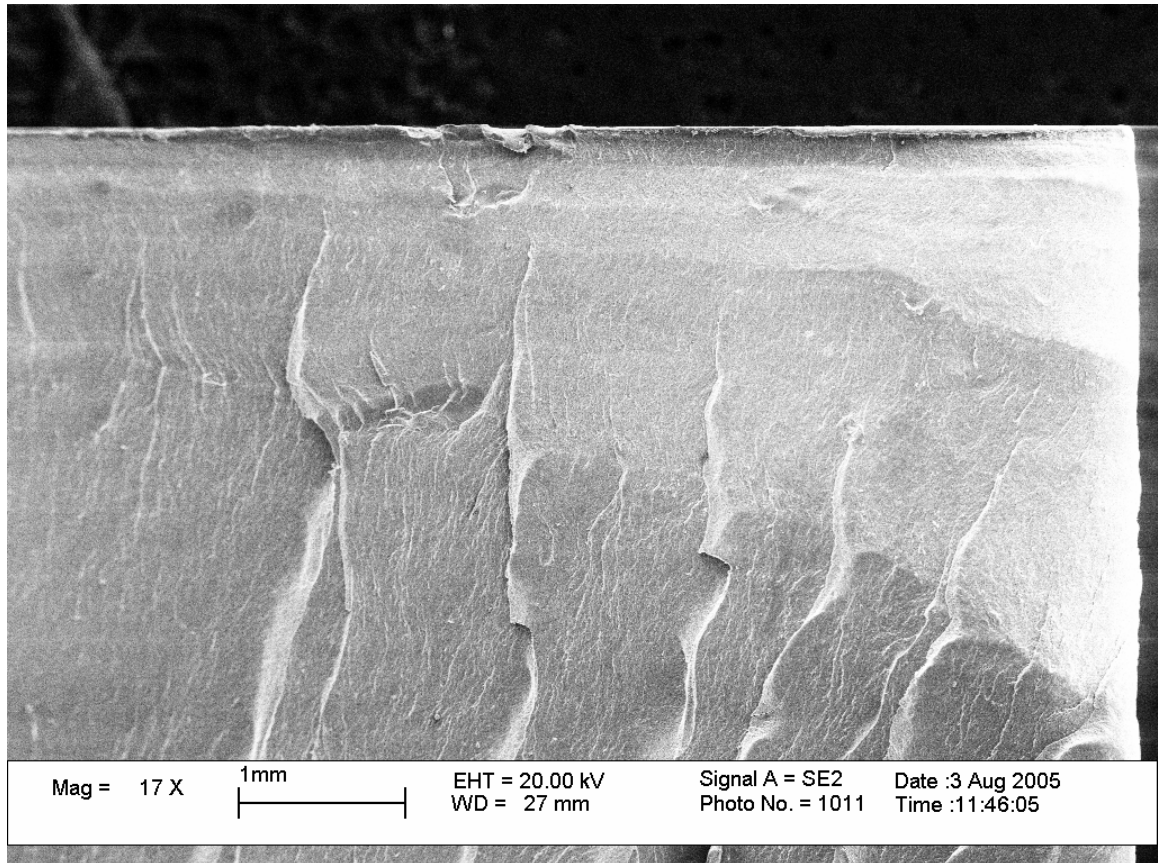


**Figure 56. SEM Image of the Fracture Surface of a Compact Tension Specimen
Tested at the Highest Rate of 1 m/s Showing the Formation of a Lip**

Figure 53 shows the stress whitening taking place ahead of the crack tip due to plastic deformation for a specimen tested at 10^{-5} m/s. Figure 54 illustrates the striations or chevrons on the fracture surface of a specimen tested at an intermediate rate of 0.01 m/s. Note that the plastic deformation ahead of the crack tip is severely limited at this intermediate rate causing visible stress whitened zone to be significantly reduced. Figure 55 illustrates the fracture surface of a CT specimen tested at the highest rate of 1 m/s. Finally Figure 56 illustrates the fracture surface of the CT specimen tested at the highest rate of 1 m/s rotating to nearly a 45 degree angle showing the formation of a lip (change in plane), similar to shear failure.

In summary, the fracture toughness of the PL731 was characterized using compact tension specimens tested over a range of temperatures (as low as -115°C) and loading rates (up to 1 m/s). At room temperature, the fracture toughness of the bulk adhesive at higher test speeds was found to be only 20% of its value at lower rates. The above observation does not bode well for the performance of this material in the event of a crash at high speeds or in vehicles operating in very cold environments in subzero temperatures. An attempt was made to predict the behavior of the bulk adhesive at even higher loading rates by conducting tests at subambient temperatures and applying the time temperature superposition principle (low temperatures can be correlated with high speed testing through TTSP). Slight reductions in fracture toughness were found at intermediate loading rates for the subambient tests, but temperature effects on fracture toughness were much less than expected from TTSP considerations. It is suggested that alternate specimen geometries like the single edge notch bend (SENB) also be considered

to determine fracture toughness of the bulk adhesive. This fracture toughness work on the bulk adhesive will contribute to our current efforts to develop a comprehensive experimental methodology to quantify the strain rate dependencies in composite bonded structures.

3.4 Adhesive Joint

In this section, an effort is made to focus on the adhesive joints at the coupon level and to investigate rate effects on the performance of the joints.

3.4.1 Single Lap Shear and Double Notch Shear

Preliminary single lap shear (SLS) tests were conducted at 0.02 mm/sec on specimens with PL731 adhesive thickness and overlap length of 0.6 mm and 12.7 mm, respectively. The average shear strength value recorded for the SLS specimen was 22.56 MPa. Thereafter, more SLS and double notch shear (DNS) test specimens using the PL731 adhesive with both 0.5 and 1.0 mm bond thickness and 12.7 mm overlap length were tested in replicates of 5 at rates of 0.02, 100, 500, and 1000 mm/sec. Figure 57 shows the load-displacement traces at various loading rates for an SLS test specimen. The slack adapter was included in the test set-up at higher rates to reach the desired velocities prior to the load application.

There was an apparent decrease in shear strength with increase in loading rate from quasi-static (0.02 mm/sec) to 1000 mm/sec for both the SLS and DNS test specimen with both bond thicknesses. For the SLS and DNS test specimens with both bond thicknesses there was not much difference in the shear strength between the loading

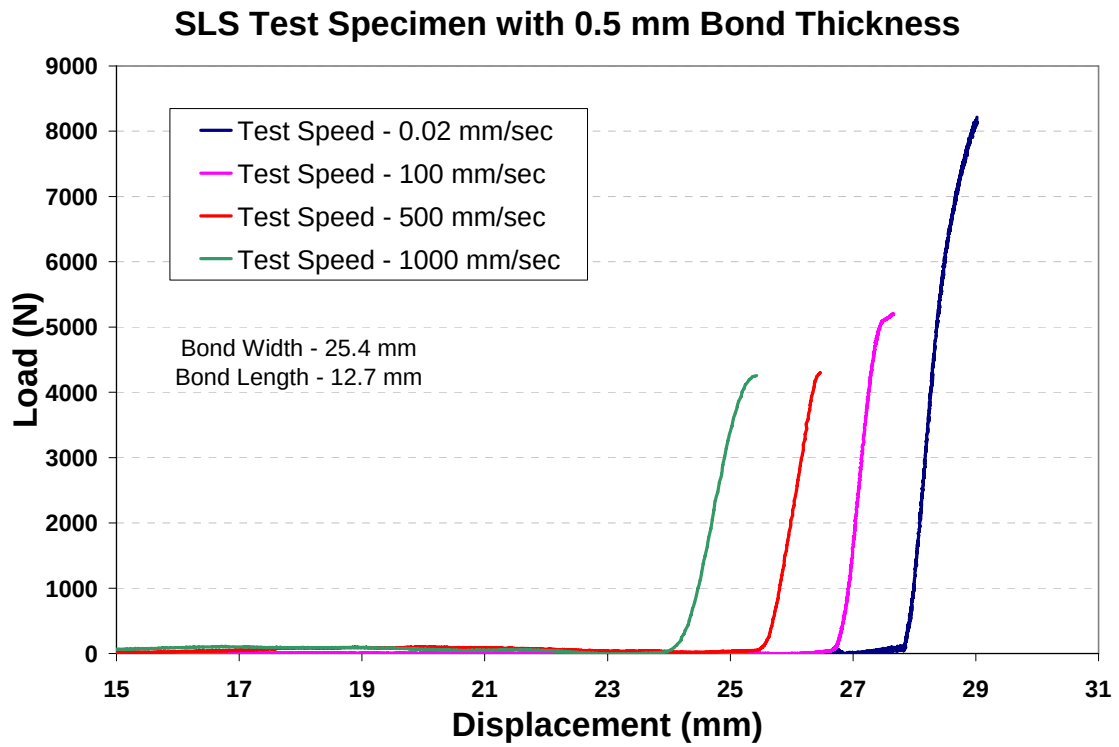


Figure 57. Load Displacement Traces at Different Loading Rates for an SLS Test Specimen

rates from 500 to 1000 mm/sec. This is not unexpected considering the viscoelastic effect of increasing the loading rate from 500 to 1000 mm/sec is equivalent to decreasing the temperature by only 2 to 3 °C using typical time temperature superposition for polymer systems. Figure 58 illustrates the variations in shear strength with loading rate for the DNS and SLS test specimens. There was an apparent decrease in the failure load with increasing bond thickness at the quasi-static loading rate for the SLS test specimens. Figure 59 illustrates the variation of failure load with bond thickness. Error bars are included in Figures 58 and 59 to show the scatter in the data. Failure in both the SLS and DNS test specimens at all the rates tested occurred close to the interface. There was occasional fiber removal and some times the adhesive was left behind. Figure 60 shows two test specimens run in the SLS configuration. The bottom part of Figure 60 illustrates the fiber removal while the top part displays the adhesive being left behind. The figure also shows the composite tabs that were used in the test grips.

The left hand side of Figure 61 illustrates the load train used for the SLS and DNS tests whose results have been reported above. An effort was made to determine the effect of a simplified and shortened load train between the load cell and the specimen, as illustrated in the right hand side of Figure 61, on the load displacement response of these coupon joints. Hence, SLS and DNS tests on specimens with 0.5 mm bond thickness, 12.7 mm bond length, and 25.4 mm bond width were conducted at 0.5 and 1.0 m/s after inverting the slack adapter to minimize the mass of the load train between the specimen and load cell. There was no significant improvement in the load profile as a result of

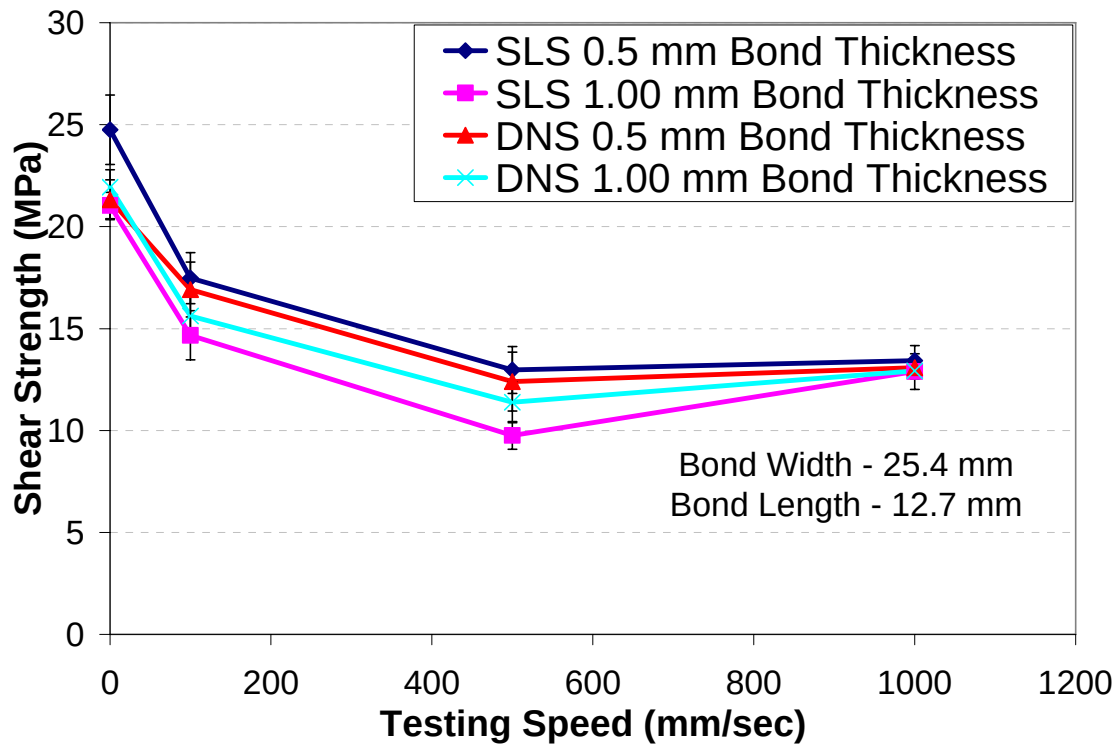


Figure 58. Variations in Shear Strength with Loading Rate for the SLS and DNS Test Specimens

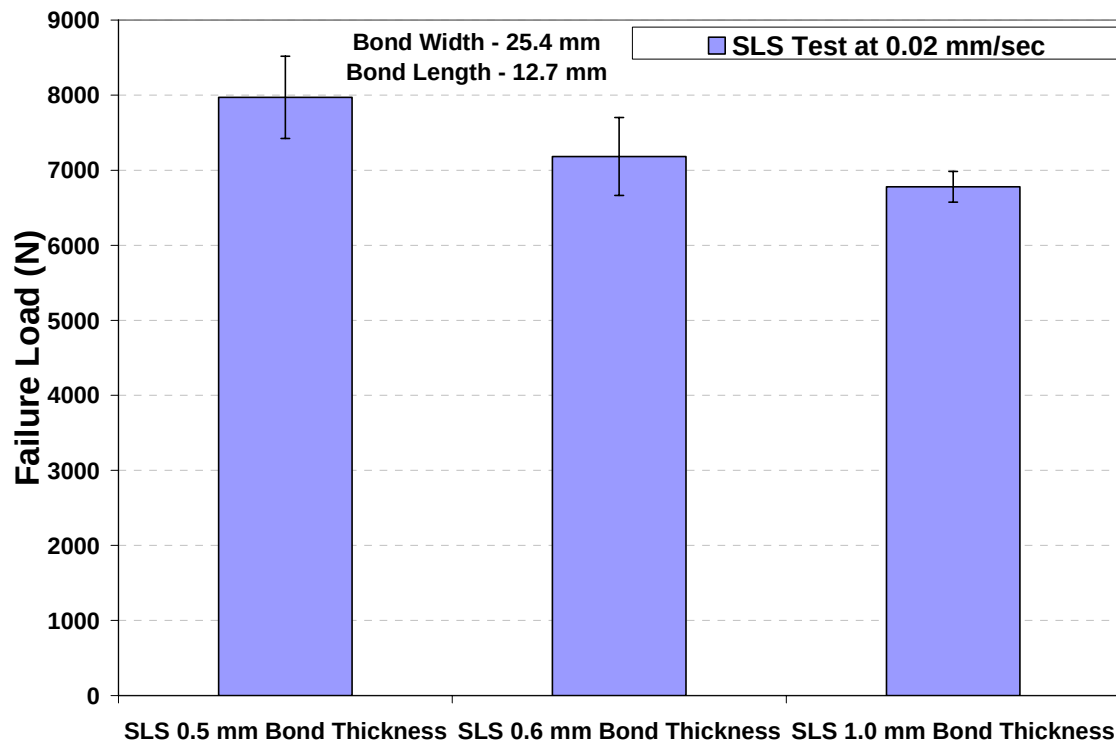


Figure 59. Variation of Failure Load for SLS Test Specimens with Bond Thickness

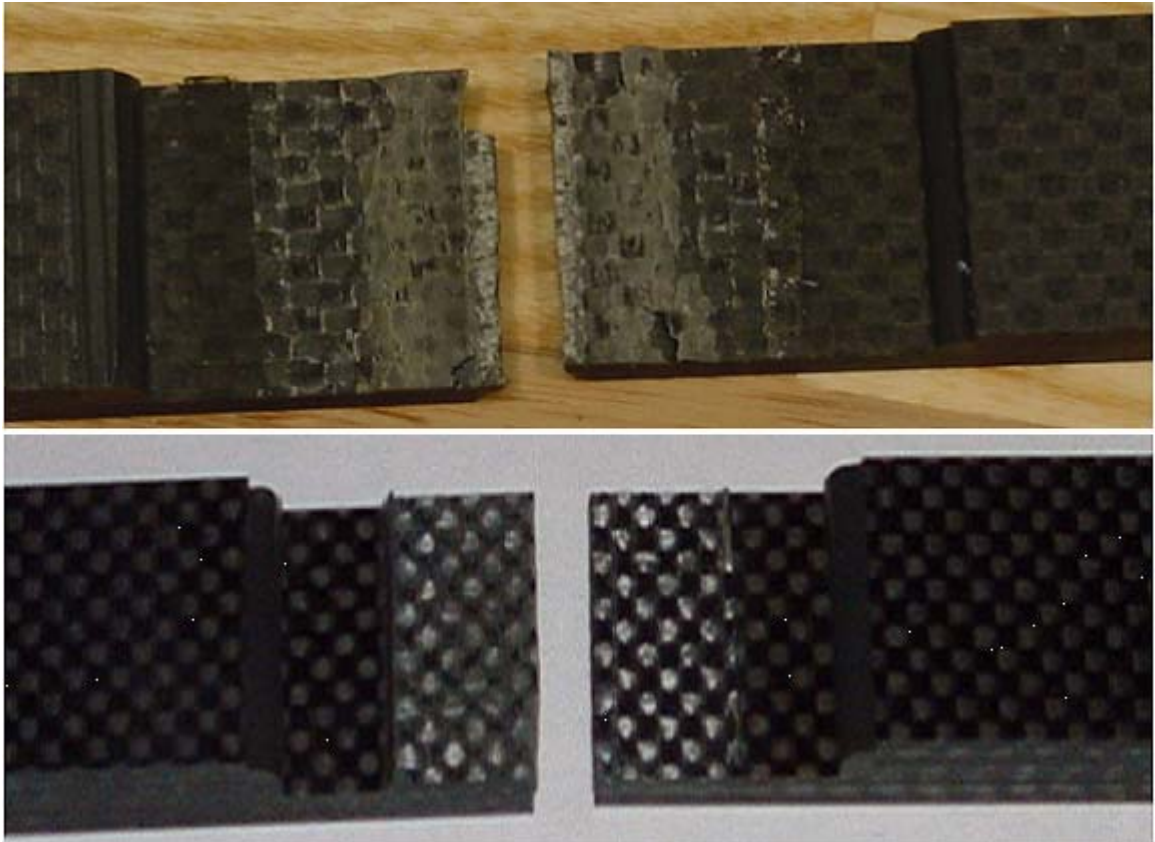


Figure 60. Failure in the Joints

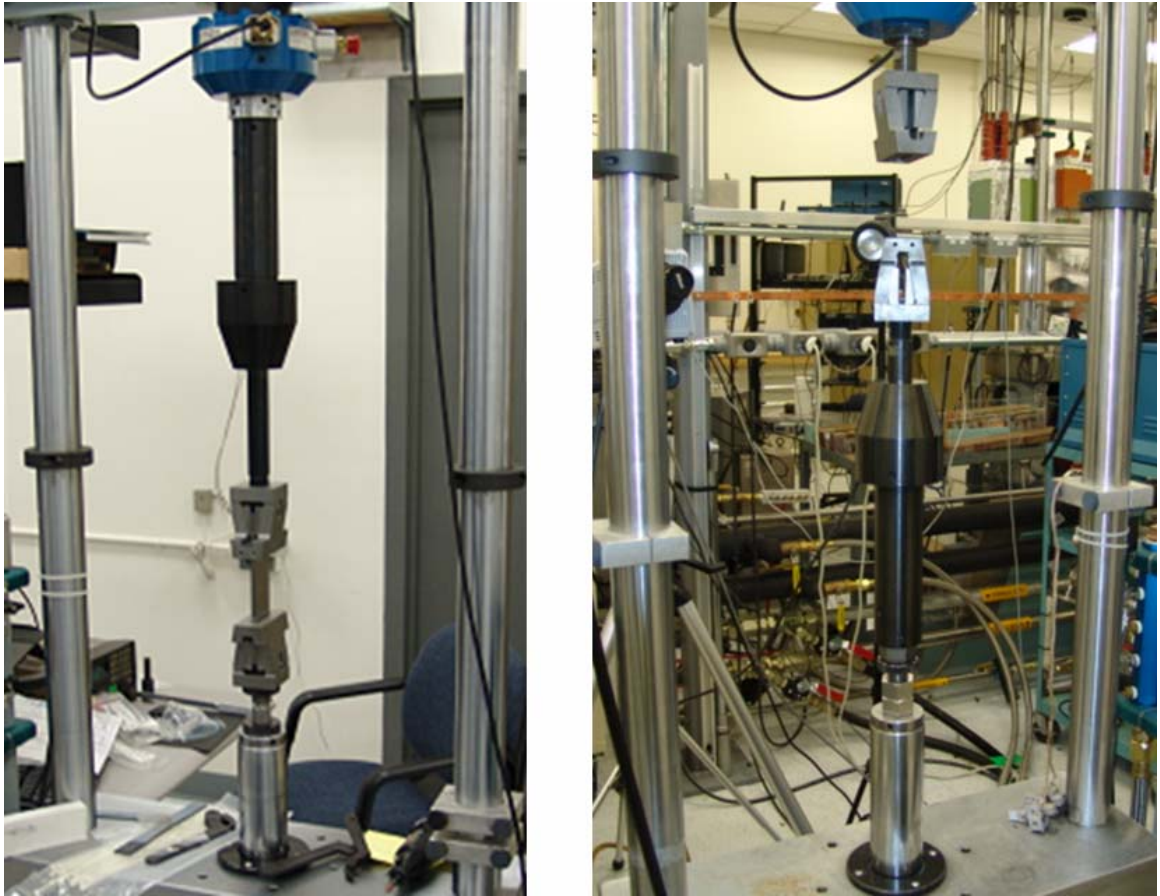


Figure 61. Different Load Trains used for SLS and DNS Tests at Higher Loading Rates

inverting the slack adapter as illustrated in Figure 62 which compares the load displacement responses recorded from the two set ups.

3.4.2 Fracture Toughness

3.4.2.1 Double Cantilever Beam Testing

Mode I fracture tests performed on 11 ply and 36 ply adhesively bonded standard Double Cantilever Beam (DCB) type specimens at loading rates of 0.01, 0.1, and 1 m/s resulted in decreasing fracture energies with increasing rates. The fracture energies were calculated using the Simple Beam Theory (SBT), Corrected Beam Theory (CBT), and the Experimental Compliance Method (ECM). Figures 63 and 64 illustrates the variation of fracture energy calculated using the CBT with loading rate for the 11 ply and 36 ply DCB specimens, respectively. Note that SERR in Figures 63 and 64 is an acronym used for the strain energy release rate. The decrease in fracture energy with increase in rate is expected because the material behaves in a more brittle fashion at higher rates resulting in a decrease in the energy needed to grow the crack. A comparison of the load displacement traces obtained for an 11 ply DCB specimen tested at rates of 0.01 and 0.1 m/s is shown in Figure 65. Figures 66 and 67 display the 11 and 36 ply DCB specimens, respectively tested at various loading rates. The brittle behavior of the material at higher loading rates causes easier crack propagation with less energy stored in the crack thus resulting in smaller crack (debond) jumps for both the 11 ply and 36 ply at these higher rates. The white thumb marks are less prominent at higher loading rates suggesting that the plastic deformation at the crack tip was severely limited at the higher rates. In all standard DCB tests loaded at the specimen ends, the debond jump

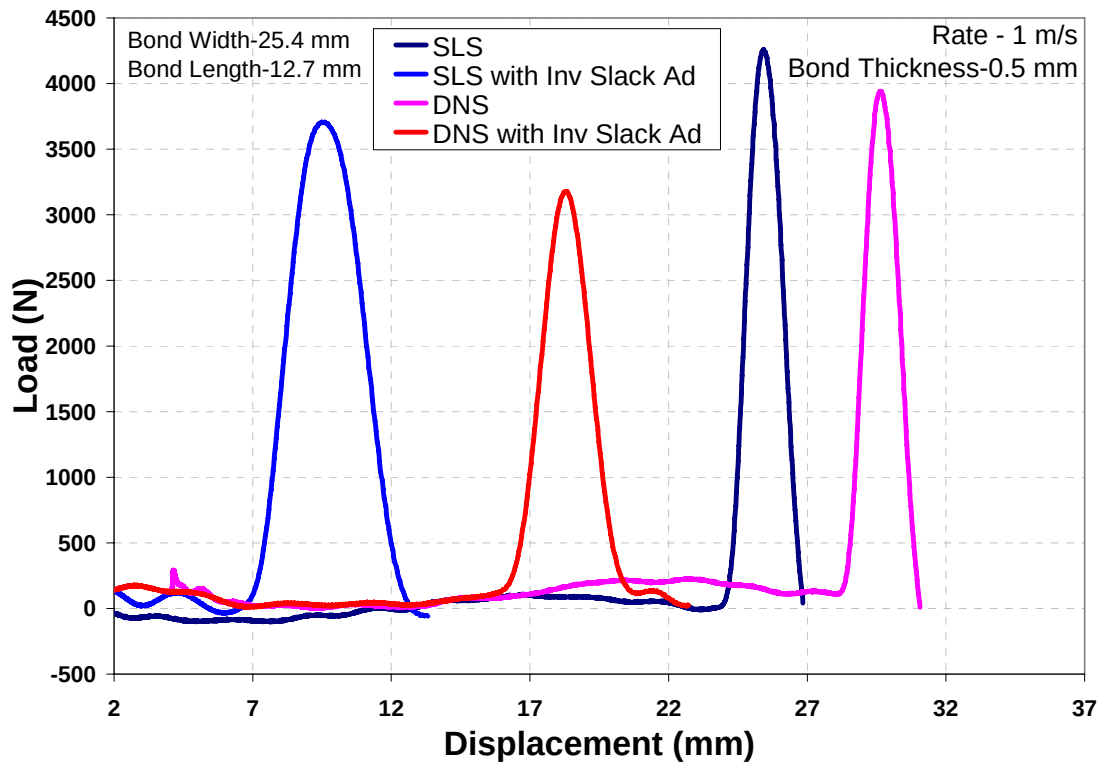


Figure 62. Comparison of the Load Displacement Responses Recorded from Different Test Setups for SLS and DNS Tests

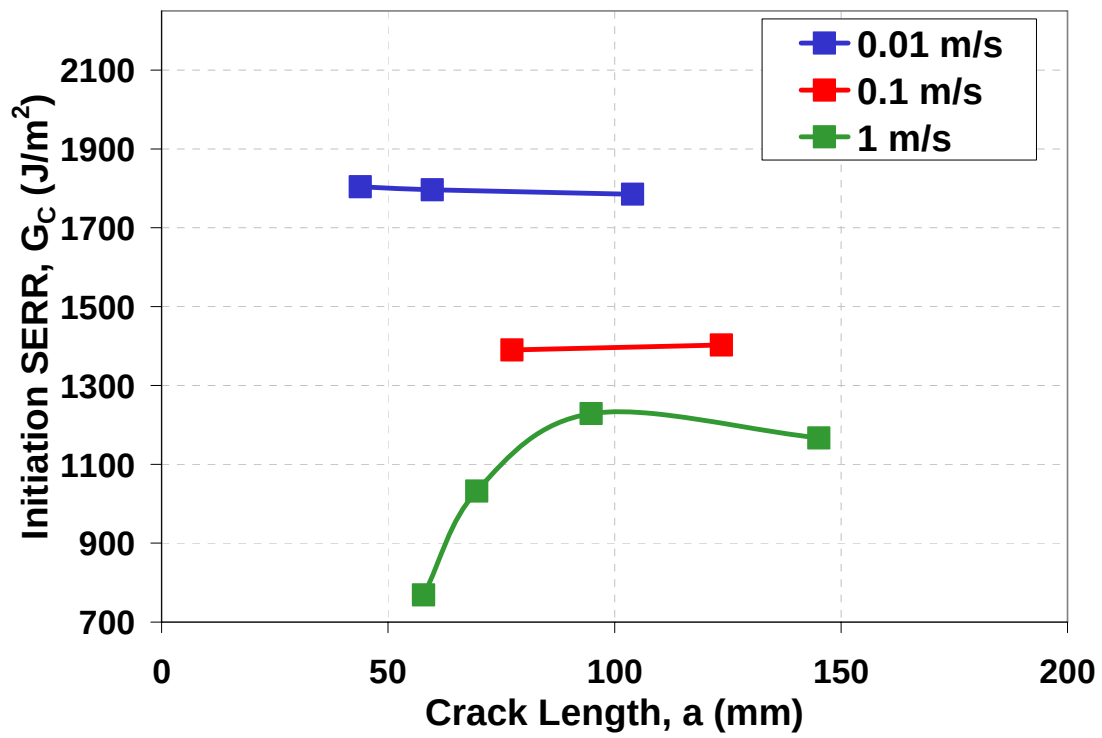


Figure 63. Variation of Fracture Energy with Loading Rate for the 11 ply DCB Specimens

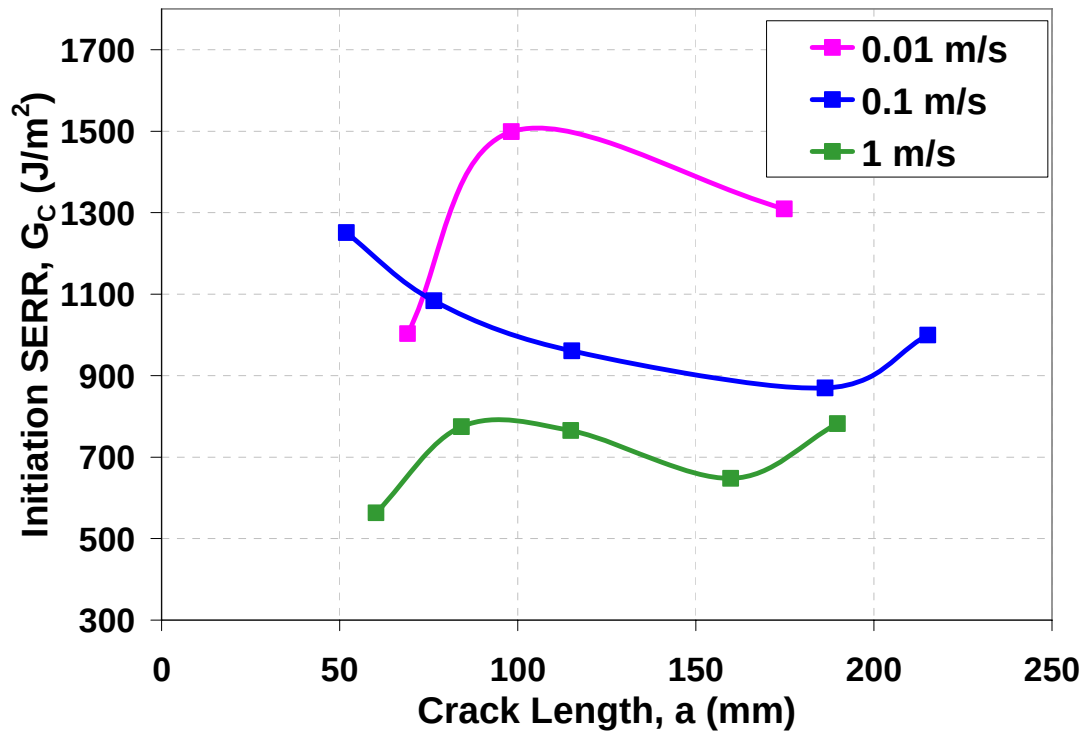


Figure 64. Variation of Fracture Energy with Loading Rate for the 36 ply DCB Specimens

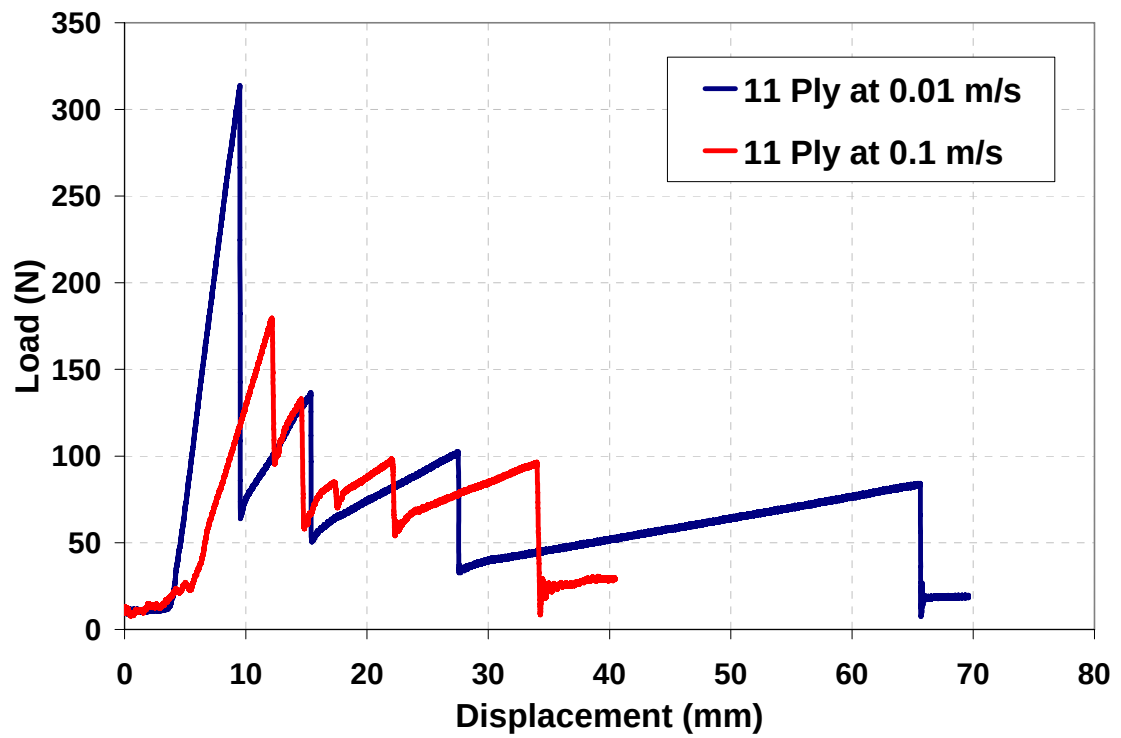


Figure 65. Comparison of Load Displacement Traces for 11 ply DCB Specimens at Different Loading Rates

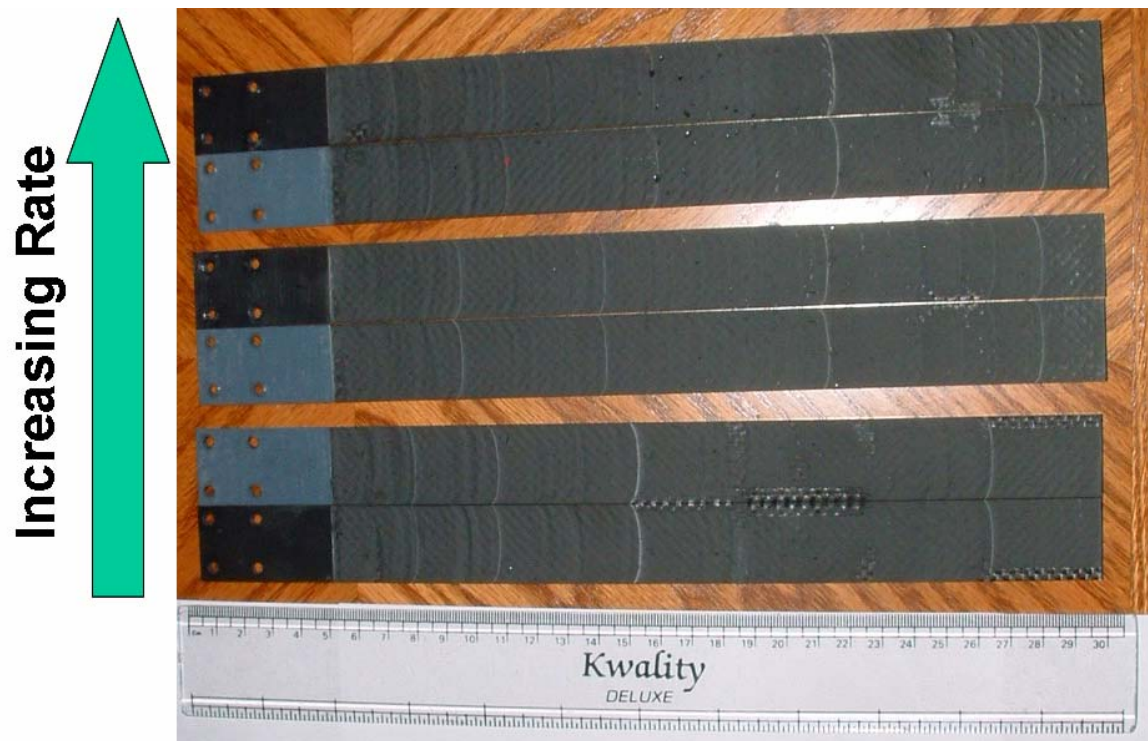


Figure 66. 11 ply DCB Specimens Tested at Various Loading Rates



Figure 67. 36 ply DCB Specimens Tested at Various Loading Rates

becomes increasing longer as the test progresses. This is because the stored energy within the deformed adherends available to drive the crack once initiated is proportional to the distance between the loading point and the crack tip. Since the loading point is fixed in a standard DCB test, as the crack grows further away from the point of loading, it has more stored energy resulting in bigger debond jumps as the test progresses.

The effective rate of loading at the crack tip can also be used to describe the rate dependence of the fracture behavior. In fracture mechanics, the effective fracture loading rate, $\frac{dK}{dt}$, is often used, where K is the stress intensity parameter. Since the strain energy release rate is a more common fracture parameter for adhesive joints involving dissimilar materials, a similar quantity, $\frac{d\sqrt{G}}{dt}$, can be used. Here the finite difference relationship, $\frac{\Delta\sqrt{G}}{\Delta t} = \frac{\sqrt{G_c} - \sqrt{G_a}}{t_c - t_a}$, is used. Here, the critical and arrest values are as determined for a specific stationary crack position, and t_c and t_a represent the elapsed time between the arrest and propagation for that crack. Figures 68 and 69 illustrate the variation of fracture energy calculated using the CBT as a function of the above mentioned $\frac{d\sqrt{G}}{dt}$ for the 11 ply and 36 ply DCB specimens, respectively.

3.4.2.2 Driven Wedge Test Method

The DCB test method though a standardized test method for Mode I loading yielded very few useful data points per specimen when tested at higher rates. Hence, an

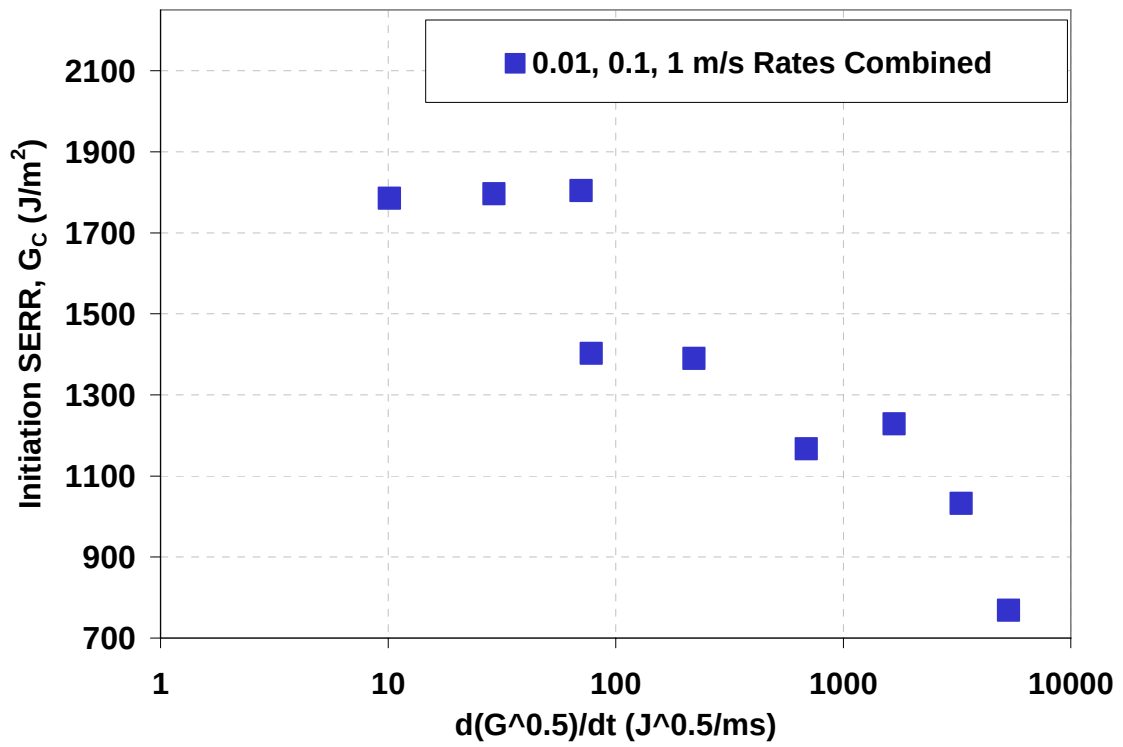


Figure 68. Variation of Fracture Energy with $\frac{d\sqrt{G}}{dt}$ for the 11 ply DCB Specimens

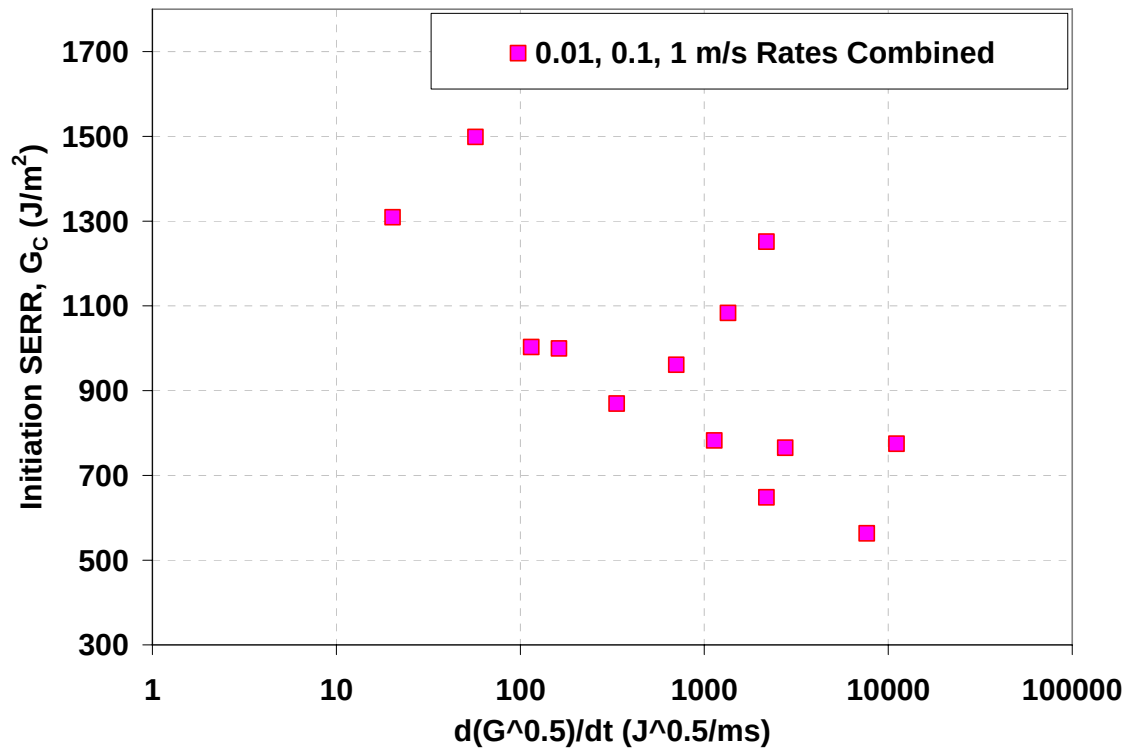


Figure 69. Variation of Fracture Energy with $\frac{d\sqrt{G}}{dt}$ for the 36 ply DCB Specimens

alternate test method still loading the specimen in Mode I, for evaluating the fracture energies of adhesively bonded specimens, called the driven wedge test method, was developed to get more fracture energy data points per specimen and used over a wide range of loading rates. If the crack propagates in a stable fashion, the DCB specimens can also result in the collection a number of fracture data points from an individual specimen, thereby aiding in obtaining statistically meaningful results from a limited number of specimens. However under high rates of loading or when unstable, stick-slip propagation occurs (time dependent materials displaying a region of decreasing fracture toughness (adiabatic heating of the crack tip region softens the material, allowing cracks to run more easily or rapidly moving cracks permit less crack tip blunting due to viscoelastic deformation, resulting in more brittle behavior) are inherently prone to stick-slip behavior, as the accelerating crack requires less of the available energy, which is converted to kinetic energy that can continue to drive the crack beyond where the static analysis would suggest) the adherend flexibility results in high kinetic energy terms, and long advances during slip events due to the significant elastic energy that is stored within the long debonded adherends resulting in the limited data points. Figure 70 is an illustration of the driven wedge test method.

As can be seen from Figure 70, a specimen with a wedge inserted at one end was placed in the load frame, with the base of the specimen held lightly in place on the movable crosshead. The wedge, in contact with the crack faces, is driven along the length of the specimen, splitting the specimen apart. A crosshair was mounted on the wedge support fixture to correspond with the wedge tip location enabling easy

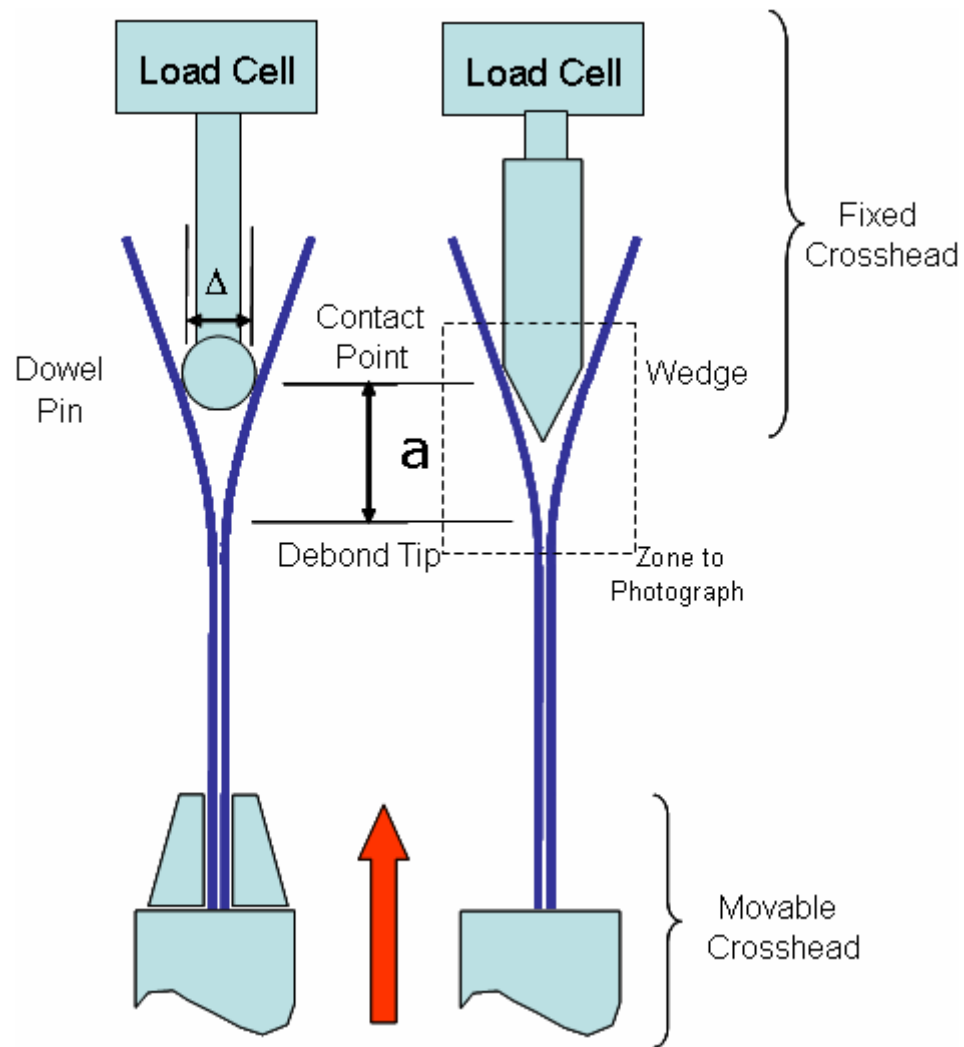


Figure 70. Illustration of the Driven Wedge Test Method

determination of the wedge position at discrete fracture events from video images that were captured during the tests using a high-speed camera. Figure 71 illustrates whereas DCB tests resulted in only 2-3 fracture events, driven wedge testing of similar specimens could result in a very significant increase in the amount of useful data. Figure 72 further illustrates the large number of fracture points that can be obtained per specimen using the driven wedge test method at various loading rates. Figure 73 displays typical initiation and arrest fracture energies obtained during 75 mm crosshead travel.

In a typical DCB specimen, for reasons already explained in the previous section, the debond length may grow from 75 to 250 mm as the test progresses resulting in the capture of just 2-3 fracture events per specimen. The thinking behind the development of the driven wedge test method was that if the crack lengths as measured from the point of loading to the crack tip can be shortened significantly many additional data points could be obtained from each specimen tested. Hence, by using a relatively thin wedge, the crack lengths ahead of the wedge could be kept quite small. Additionally since the wedge (load application point) is moving relative to the upward movement of the specimen in compression, the crack lengths ahead of the wedge remain consistent rather than snowballing as they do in conventional DCB testing, where the point of load application remains fixed. For these reasons there is a significant increase in the number of fracture points using this technique.

Figures 74, 75, and 76 show the results obtained from the driven wedge test conducted on an 11, 20, and 36 ply bonded specimen, respectively. Note that all the data points shown in each of the Figures 74, 75, and 76 were obtained from a single specimen

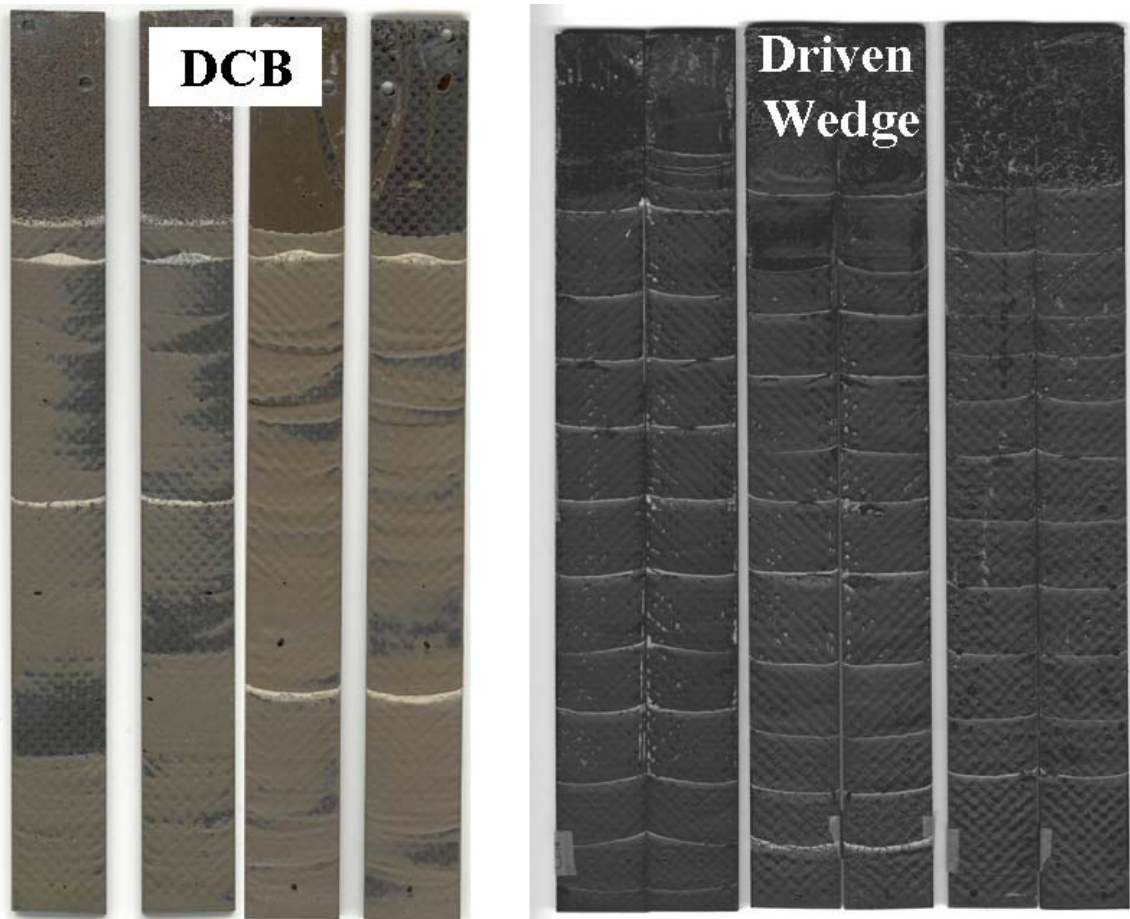


Figure 71. DCB Specimen vs. Driven Wedge Specimen

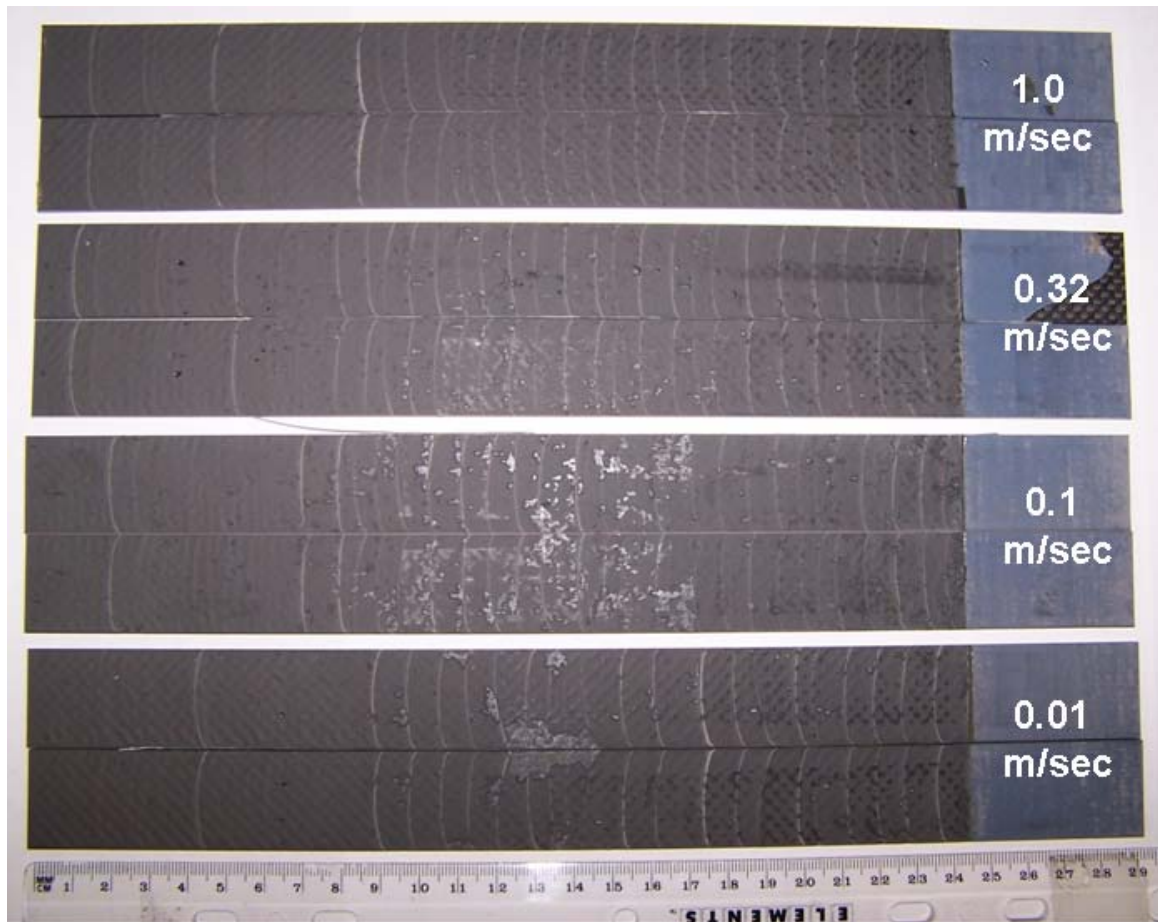


Figure 72. Illustration of the Large Number of Fracture Points obtained Per Specimen using the Driven Wedge Test Method at Various Loading Rates

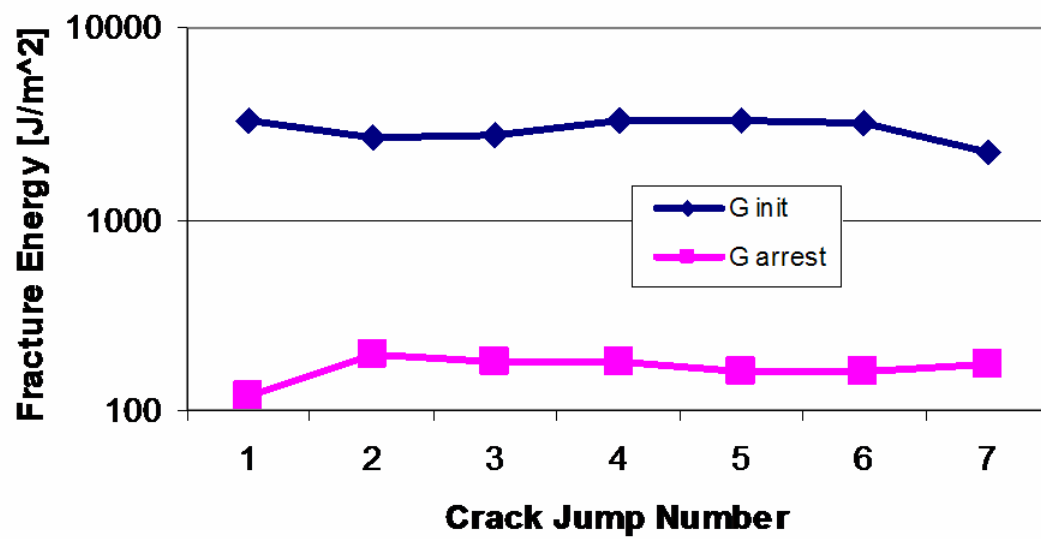


Figure 73. Typical Initiation and Arrest Fracture Energies Obtained During 75 mm
Crosshead Travel

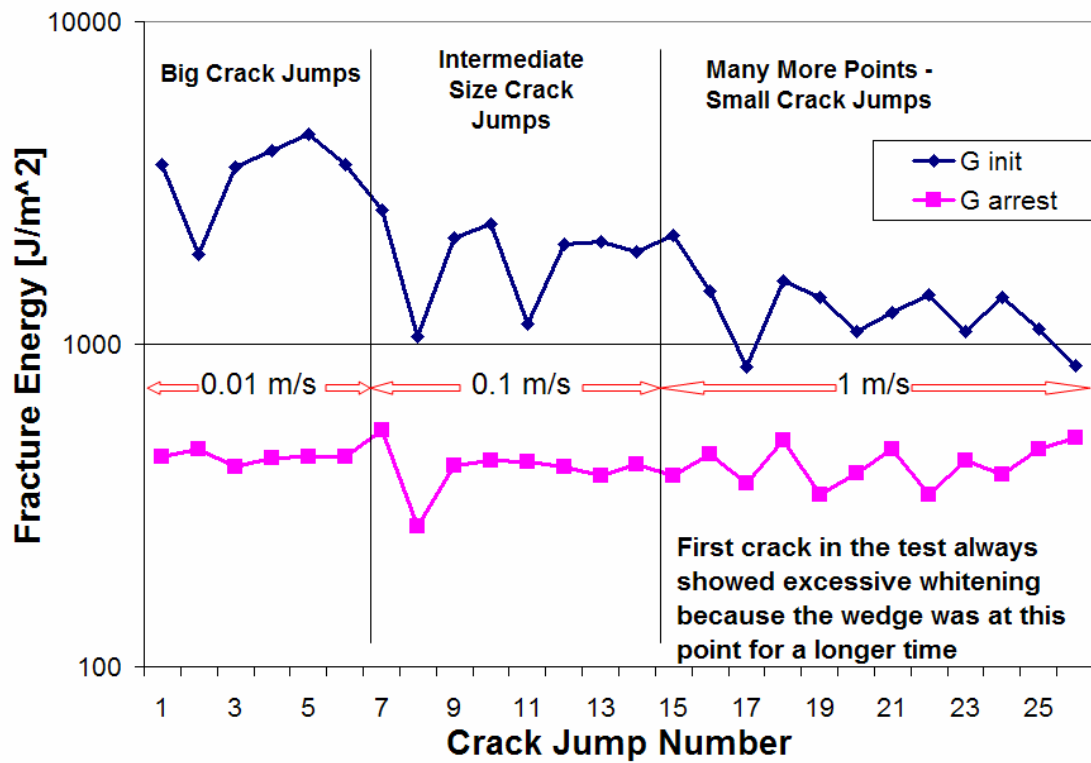


Figure 74. Results Obtained from the Driven Wedge Test Conducted on an 11 Ply Bonded Specimen

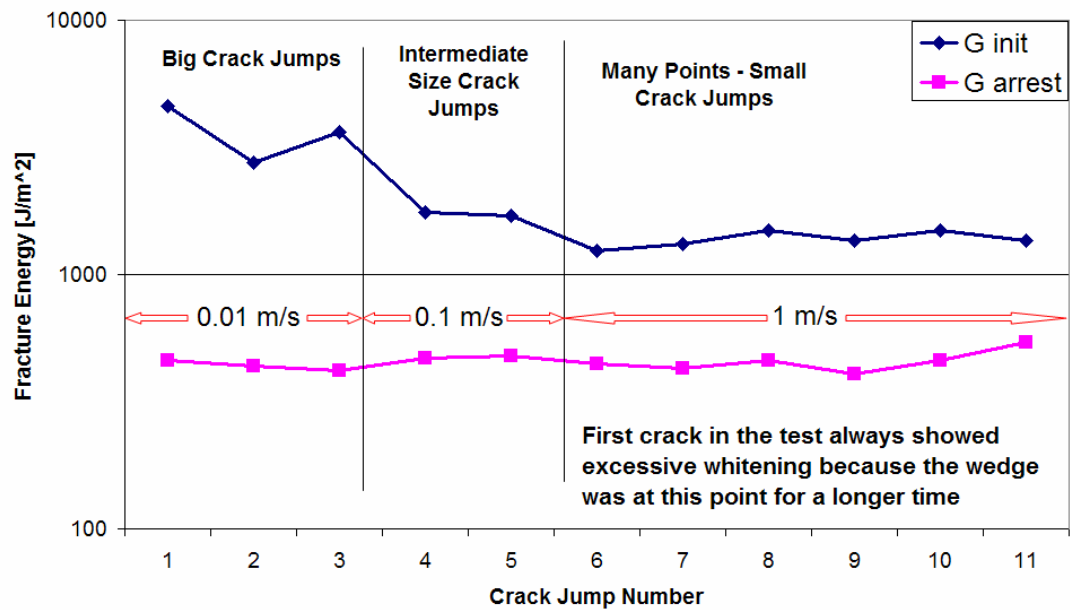


Figure 75. Results Obtained from the Driven Wedge Test Conducted on a 20 Ply Bonded Specimen

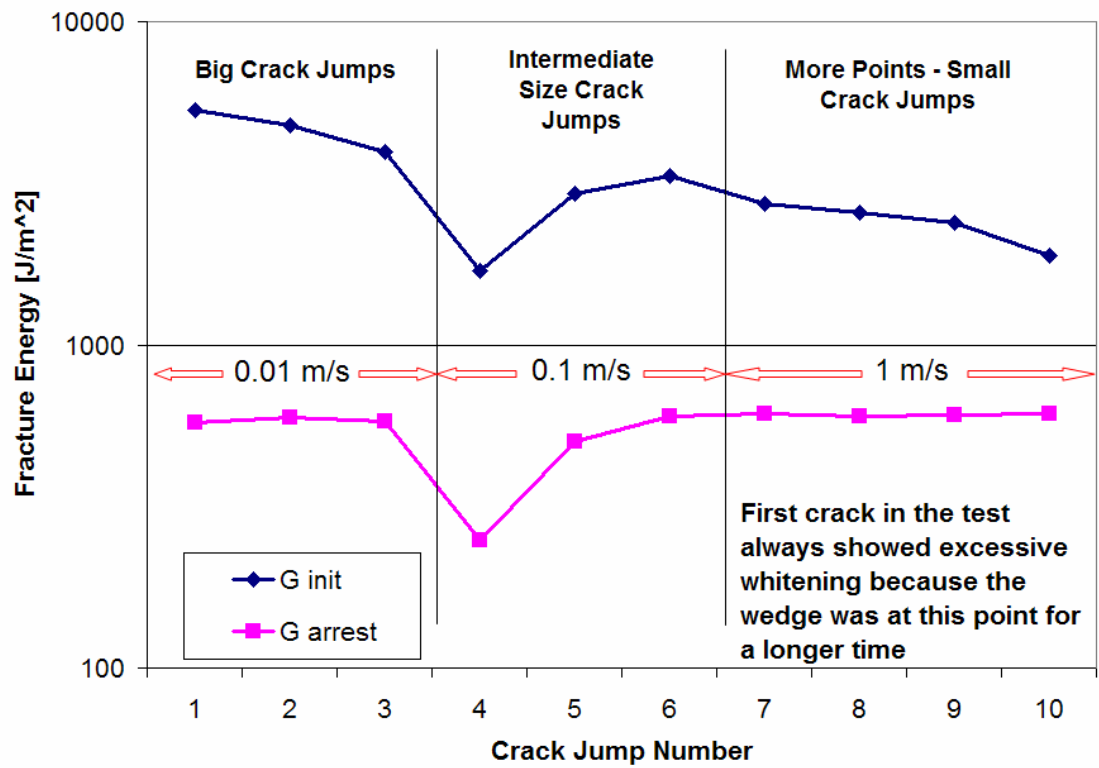


Figure 76. Results Obtained from the Driven Wedge Test Conducted on a 36 Ply Bonded Specimen

loaded at three different rates of 0.01, 0.1, and 1.0 m/s as illustrated in the figures respectively. There was excessive whitening at the point the wedge was before the start of every test because of the of large plastic deformation taking place at that point as a result of the wedge being there for longer times during test preparations. Figures 77, 78, and 79 shows the variation of fracture energy with loading rate for an 11, 20, and 36 ply bonded specimen, respectively. The 11, 20, and 36 ply specimens all showed similar trends where in the fracture energy decreased with increasing loading rate. The decrease in fracture energy with increase in rate is expected because the material behaves in a more brittle fashion at higher rates resulting in a decrease in the energy needed to grow the crack. Figure 80 shows scanning electron microscope (SEM) images of the fracture surface of a driven wedge specimen tested at a slow and fast rate. As can be seen in Figure 80, the magnitude of the crack jump depicted by the space between the two faint white lines in the images becomes less with increasing rate, thus resulting in the largest number of fracture points at the highest rate, as can be observed in Figures 74, 75, and 76. This is due to the brittle behavior of the material at higher loading rates causing easier crack propagation with less energy stored in the crack thus resulting in smaller crack (debond) jumps at these higher rates. A comparison of the load displacement traces obtained for an 11 ply driven wedge test specimen loaded at rates of 0.01, 0.1, and 1.0 m/s is shown in Figure 81.

Figure 82 displays the 11, 20, and 36 ply driven wedge specimens tested at loading rates of 0.01, 0.1, and 1.0 m/s. Note that each of the 11, 20, and 36 ply

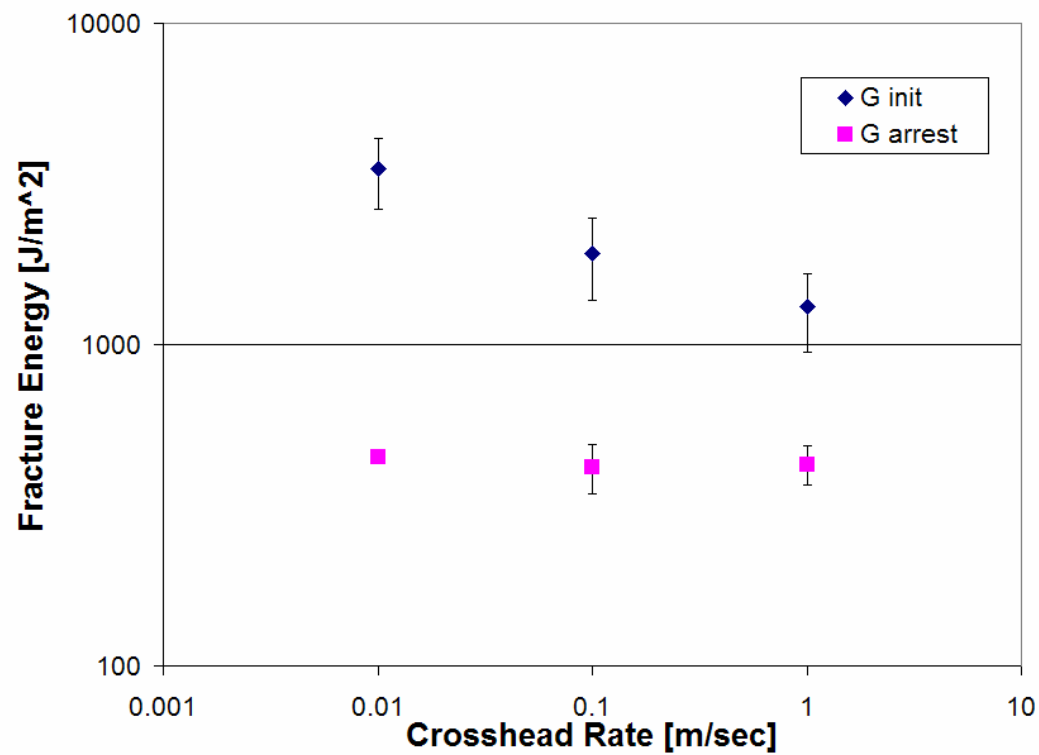


Figure 77. Variation of Fracture Energy with Loading Rate for an 11 Ply Bonded Specimen

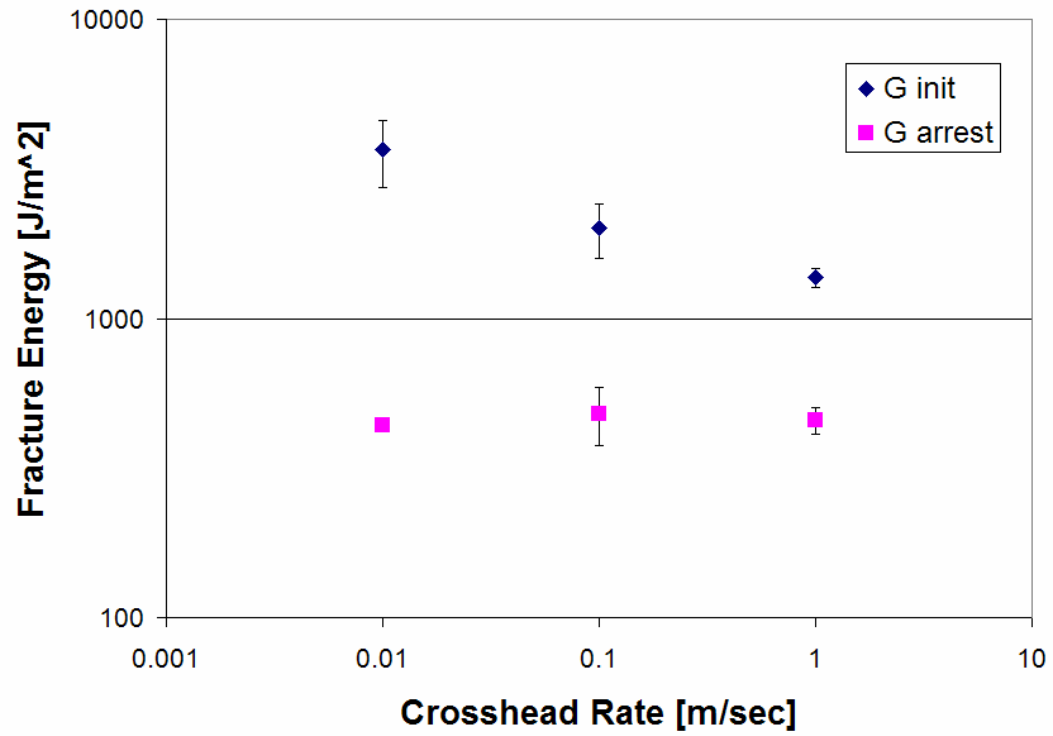


Figure 78. Variation of Fracture Energy with Loading Rate for a 20 Ply Bonded Specimen

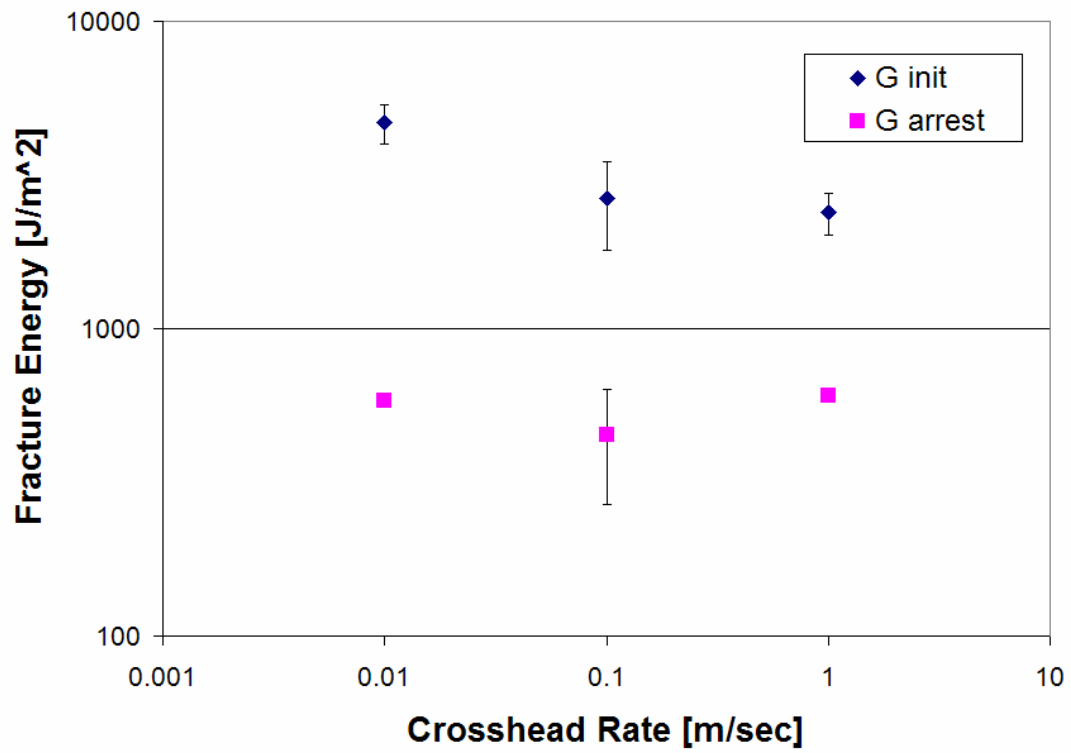


Figure 79. Variation of Fracture Energy with Loading Rate for a 36 Ply Bonded Specimen

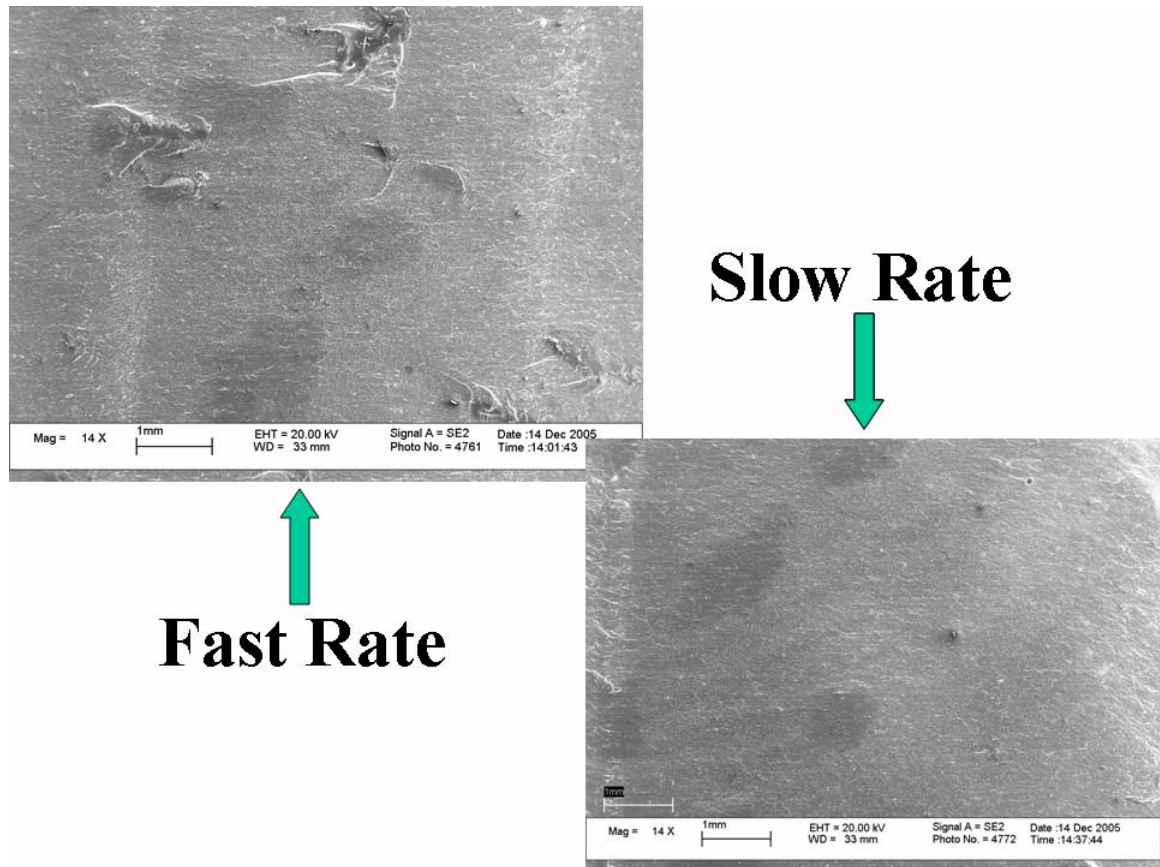


Figure 80. SEM Images of the Fracture Surface of a Driven Wedge Specimen Tested at a Slow and Fast Rate

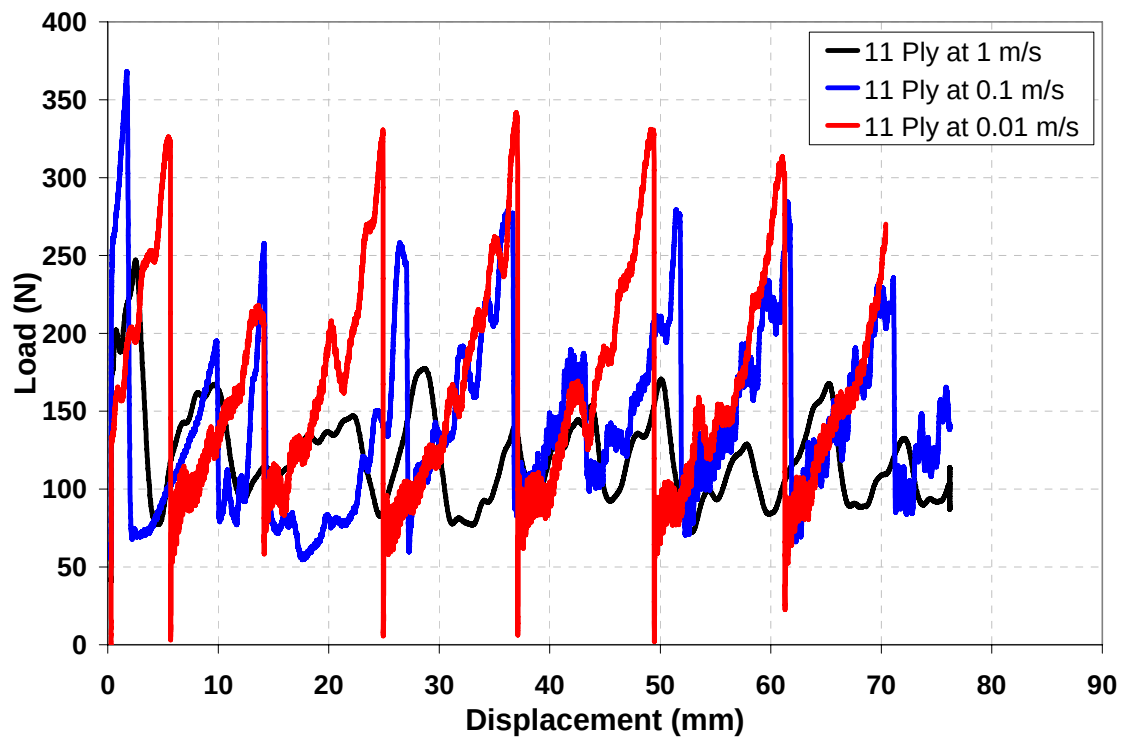


Figure 81. Comparison of Load Displacement Traces for an 11 ply Driven Wedge Test Specimen at Different Loading Rates

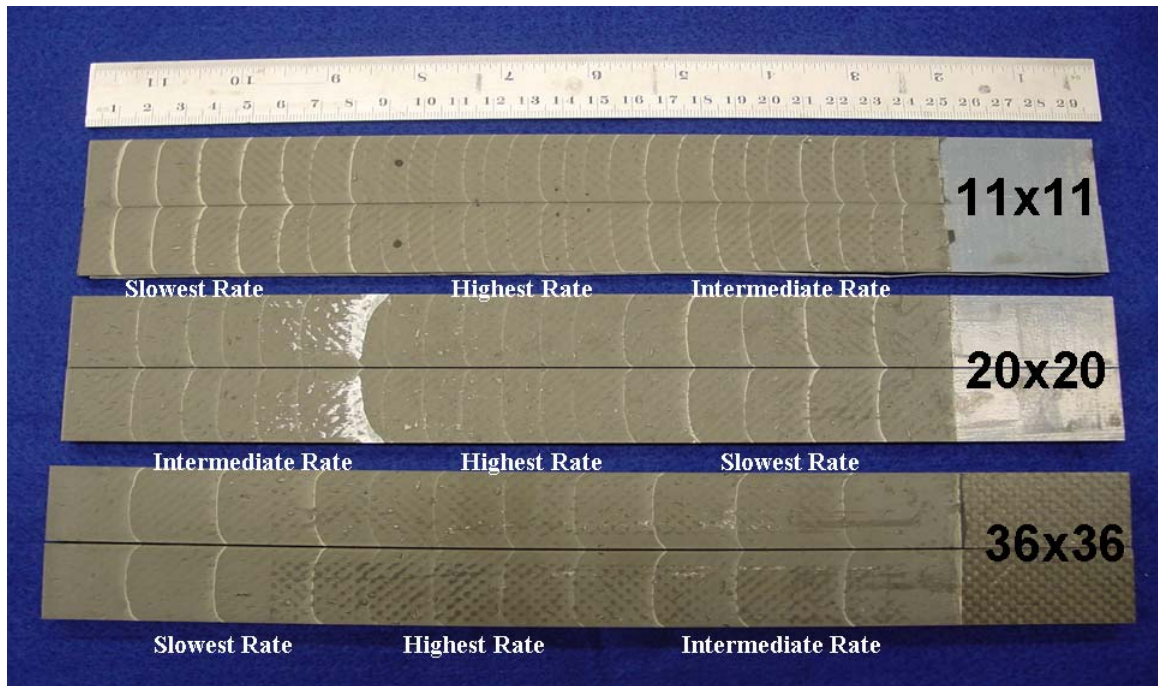


Figure 82. 11, 20, and 36 Ply Driven Wedge Specimens Tested at Loading Rates of 0.01, 0.1, and 1.0 m/s

specimens shown in Figure 82 were loaded at three different rates of 0.01, 0.1, and 1.0 m/s and parts of these specimens loaded at the different rates can be identified from the figure. The white thumb marks are less prominent at higher loading rates suggesting that the plastic deformation at the crack tip was severely limited at the higher loading rates while that at the lower and intermediate rates are more prominent suggesting more plastic deformation taking place at these rates. Figure 83 depicts the SEM images that focus on the extend of the stress whitened zones on the fracture surfaces of a driven wedge specimen loaded at a slow and fast crosshead rate. As obvious to the naked eye in Figure 82, it can also be clearly seen in the SEM images as well that the extent of the stress whitened zone is significantly larger for the slowly loaded specimen.

As mentioned in the previous section on DCB testing, the effective rate of loading at the crack tip in the form of quantity $\frac{d\sqrt{G}}{dt}$ can also be used to describe the rate dependence of the fracture behavior of driven wedge test specimens. Figures 84, 85, and 86 illustrate the variation of fracture energy as a function of $\frac{d\sqrt{G}}{dt}$ for the 11, 20, and 36 ply driven wedge specimens, respectively.

Figures 87 and 88 compare the magnitude of crack jump recorded in the 11 and 36 ply DCB and driven wedge test specimens respectively during the course of the test. For DCB specimens the magnitude of the crack jump increase as the test progresses for reasons that have already been explained earlier in the document. By contrast, the magnitude of crack jumps for the driven wedge test specimens do not increase as the test

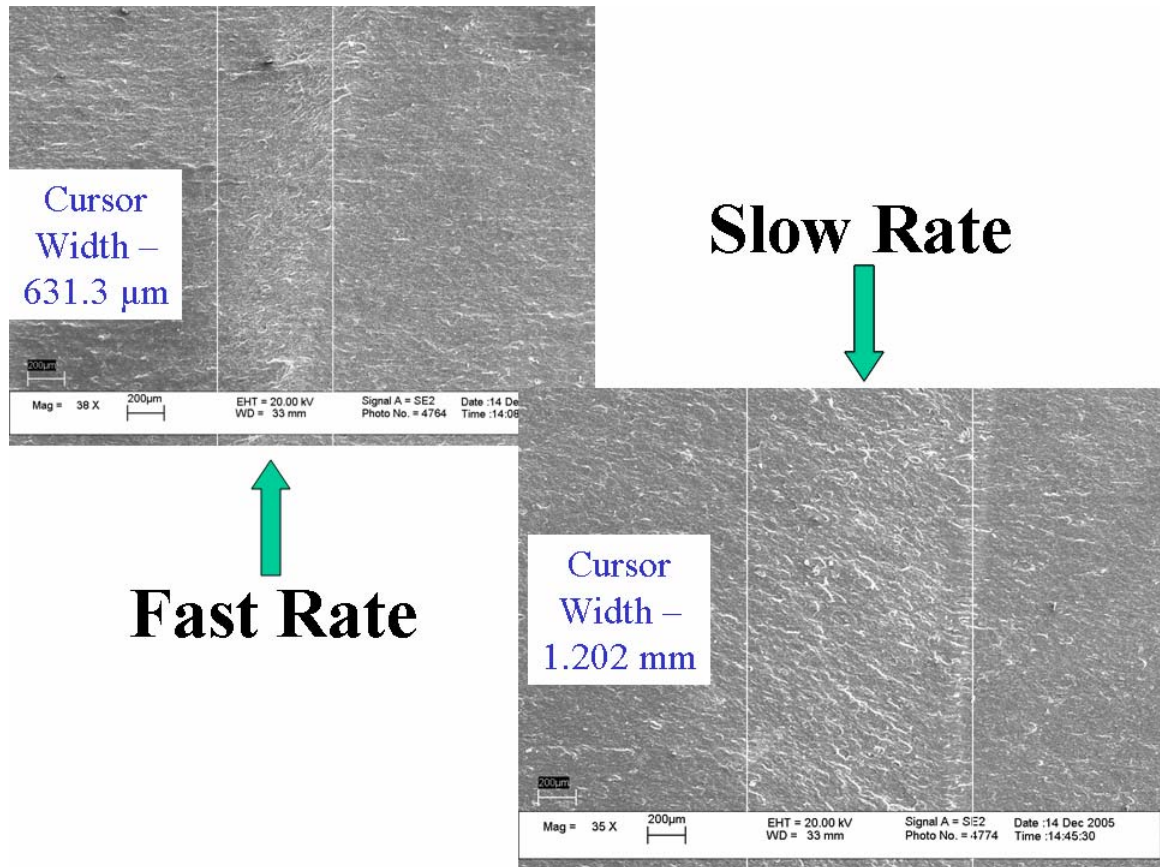


Figure 83. SEM images focusing on the Extend of the Stress Whitened Zones on the Fracture Surfaces of a Driven Wedge Specimen Loaded at a Slow and Fast Rate

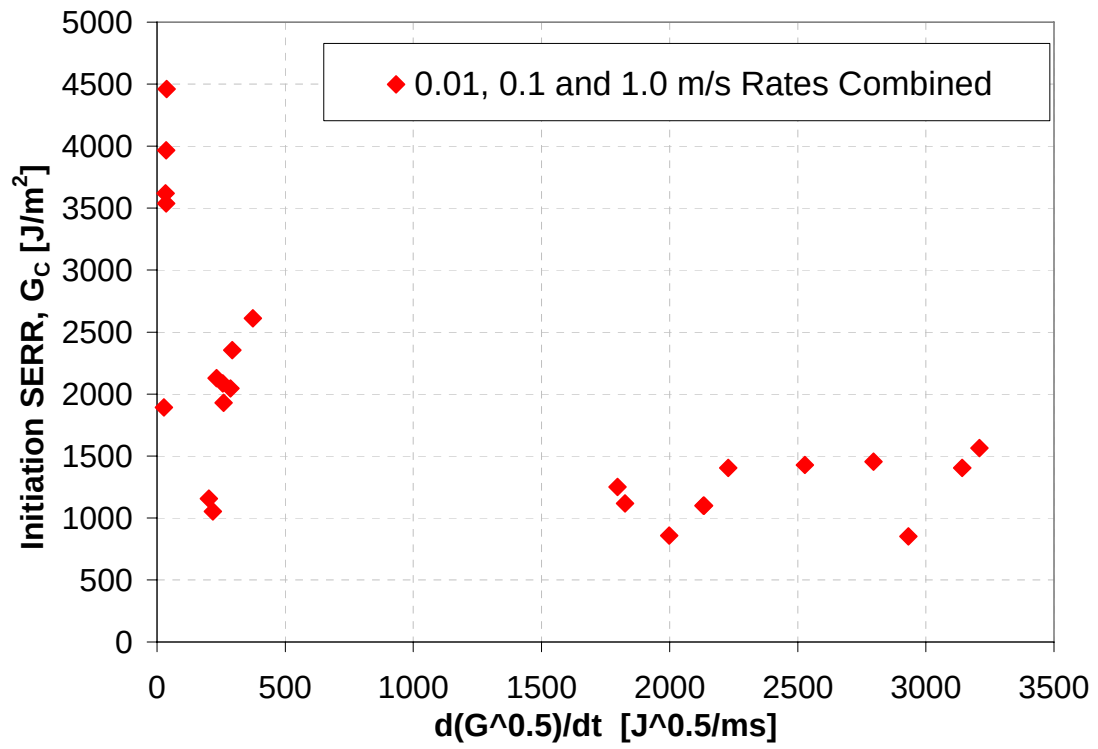


Figure 84. Variation of Fracture Energy with $\frac{d\sqrt{G}}{dt}$ for the 11 ply Driven Wedge

Test Specimens

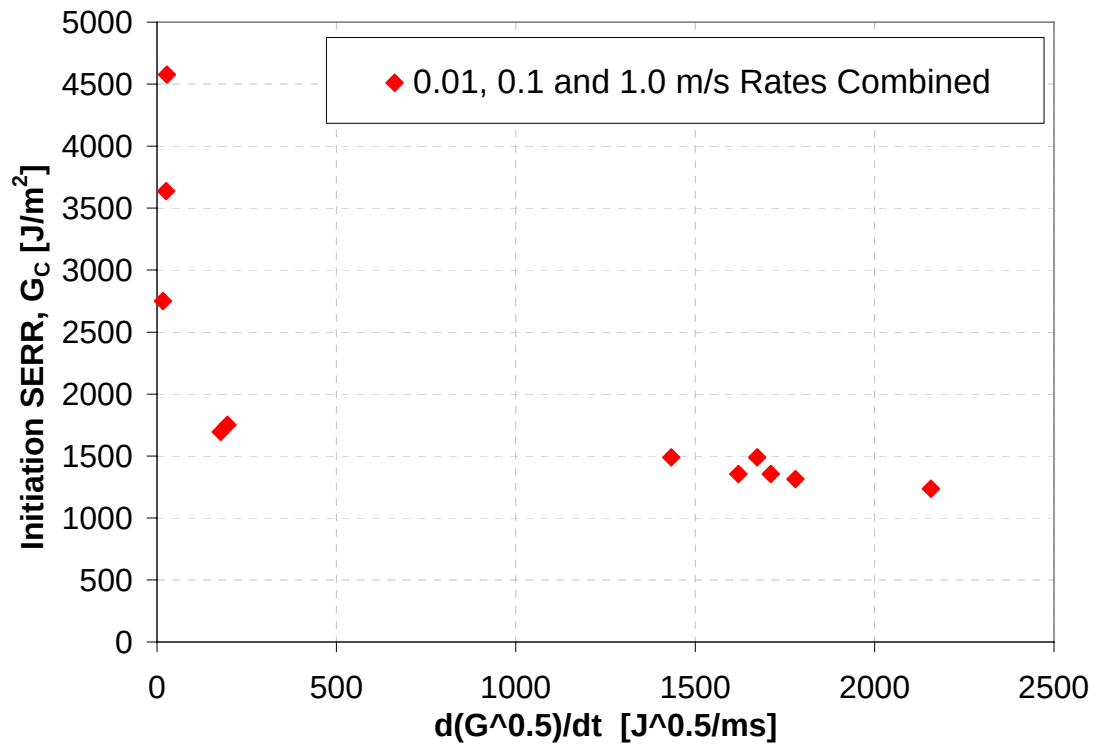


Figure 85. Variation of Fracture Energy with $\frac{d\sqrt{G}}{dt}$ for the 20 ply Driven Wedge

Test Specimens

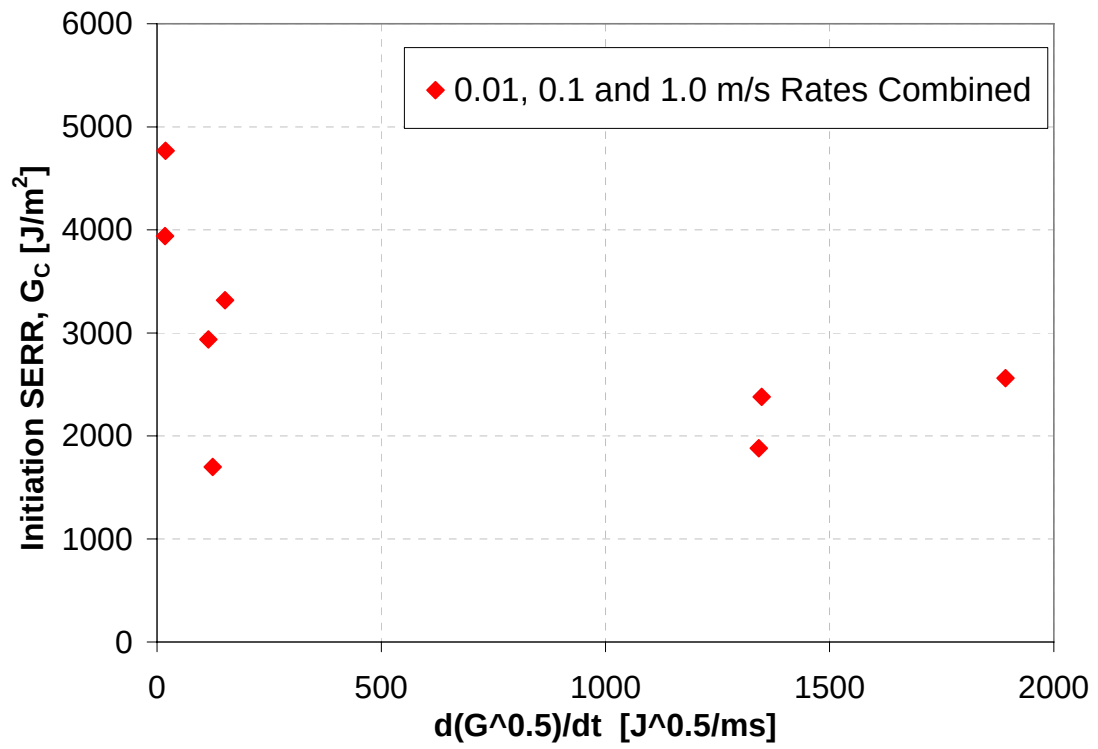


Figure 86. Variation of Fracture Energy with $\frac{d\sqrt{G}}{dt}$ for the 36 ply Driven Wedge

Test Specimens

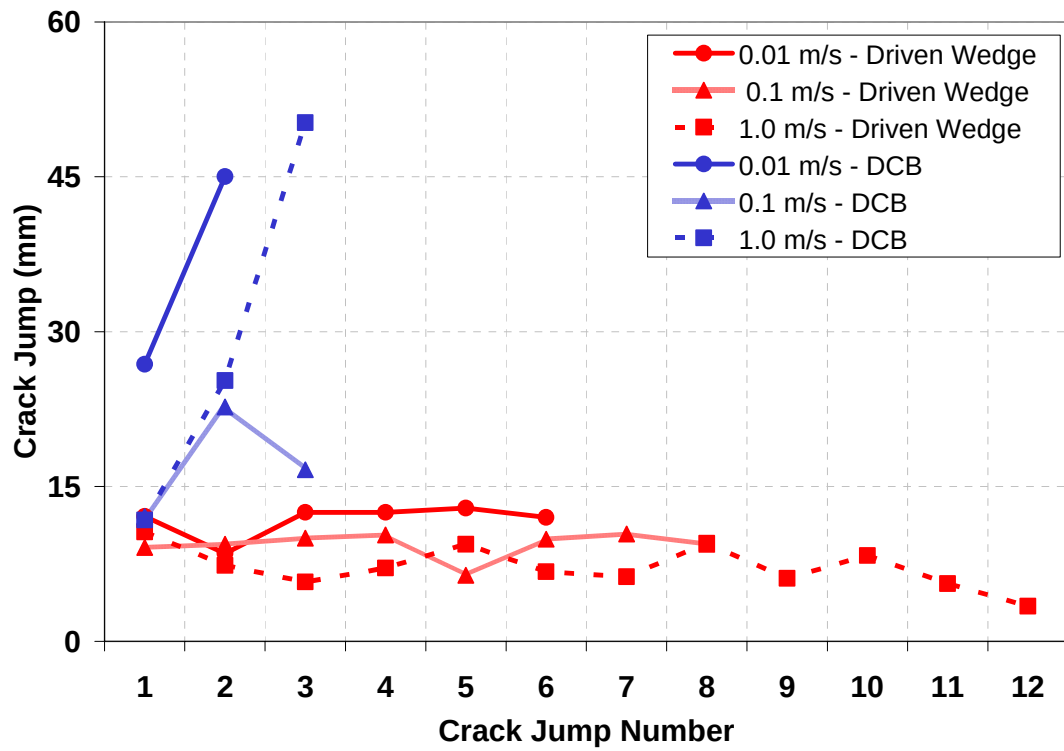


**Figure 87. Comparison of the Magnitude of Crack Jump Recorded in the 11 Ply
DCB and Driven Wedge Test Specimens**

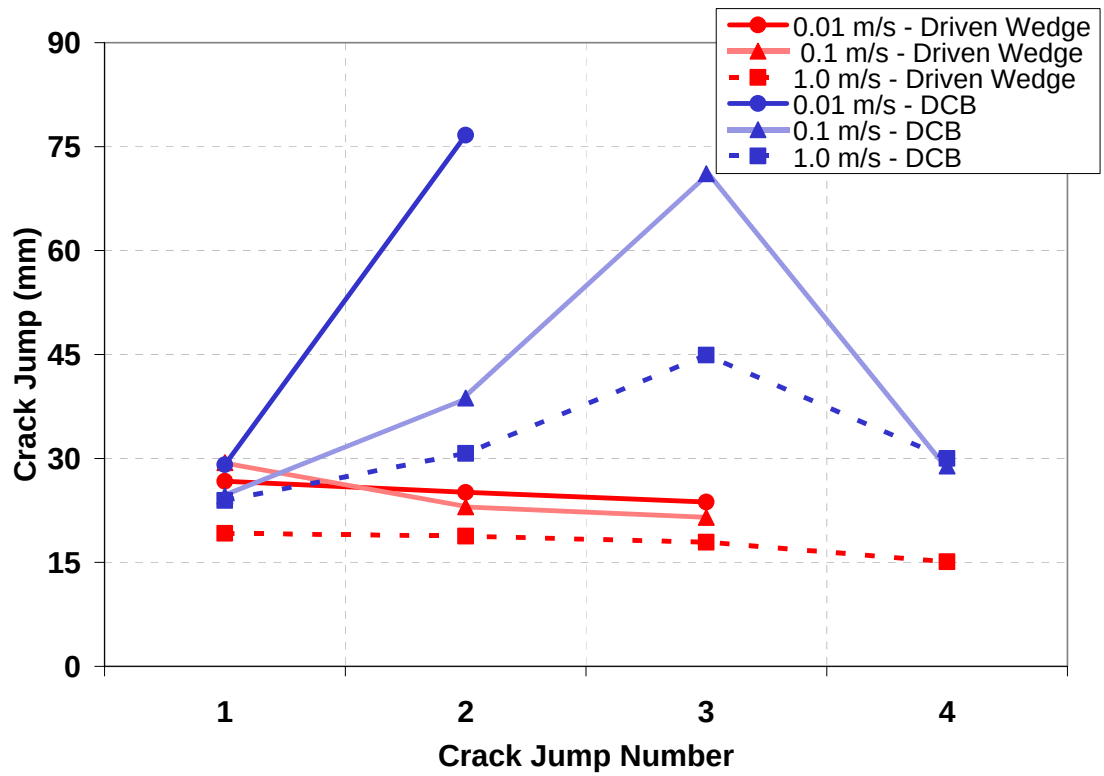


Figure 88. Comparison of the Magnitude of Crack Jump Recorded in the 36 Ply DCB and Driven Wedge Test Specimens

progresses but remains somewhat constant. As can be seen from Figures 87 and 88, again for reasons that have already been explained earlier in the document, an increase in rate during both the DCB and driven wedge tests results in an increased number of fracture points while the magnitude of crack jumps decrease.

3.4.2.3 End Load Split Test

Mode II fracture tests were conducted to calculate the fracture energies in adhesively bonded standard End Load Split (ELS) specimens at various loading rates in an effort to determine their rate dependence. Figure 89 illustrates the test set up. The fracture energies were calculated using the Simple Beam Theory (SBT), Corrected Beam Theory (CBT), and the Experimental Compliance Method (ECM). Both the CBT and ECM incorporate correction factors for large deflections (mainly occurring in 11 ply), end block stiffness, and compliance. Two types of calibration techniques were used: Clamp Calibration and the Cracked Specimen Compliance Calibration. In the Clamp Calibration technique, also called the invert ELS test, the clamp correction term for the ELS specimens of different adherend thicknesses were determined by evaluating the compliance of representative bonded specimens prior to the propagation of a debond. The pre-cracked end was placed under a clamp and the clamp was moved to allow for compliance testing of various values of beam length. Figure 90 shows the compliance plots. The abscissa intercepts in the compliance plots obtained for the ELS specimens of different adherend thicknesses were plotted as a function of the specimen thickness as illustrated in Figure 91 to obtain the resulting slope of 1.72 for the material system studied herein. Hence, the clamp correction term to be used in the calculation of fracture

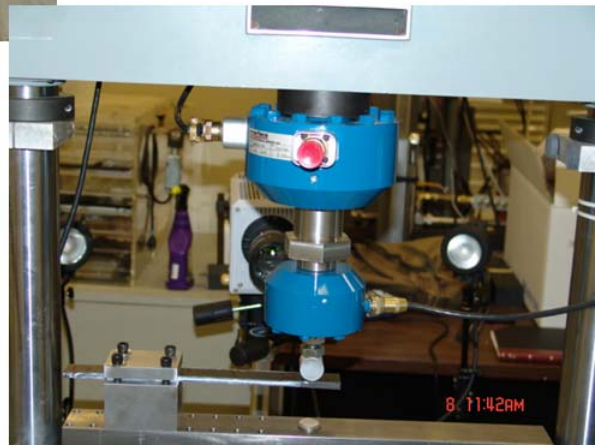


Figure 89. Mode II Fracture Toughness Test Setup

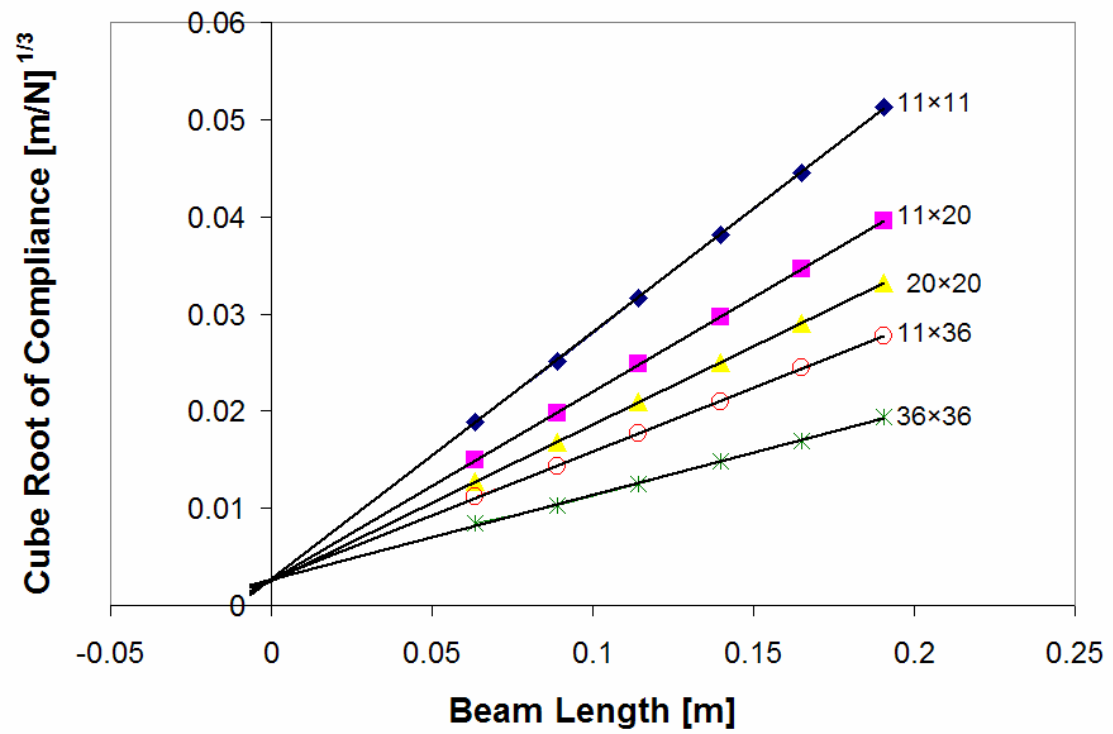


Figure 90. Compliance Plots obtained using the Clamp Calibration Technique

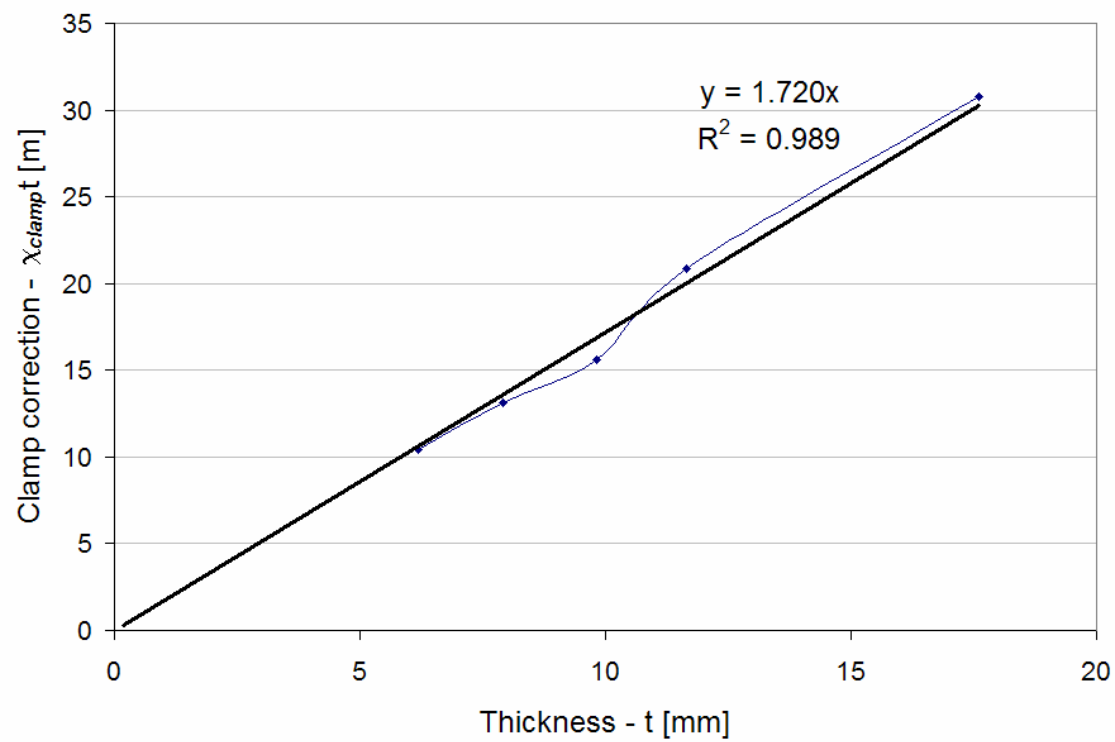


Figure 91. Determination of the Clamp Correction

energy was obtained by multiplying the above slope with the specimen thickness.

In the Cracked Specimen Compliance Calibration technique the specimen was precracked in Mode I and the approximate crack position on the specimen was marked. The crack length was varied by changing the loading point on the specimen and the compliance was measured at each crack length. A plot of compliance versus the cube of crack length as illustrated in Figures 92 and 93 was used to determine the slope and the intercept, which was finally used in the appropriate equations. Preliminary results calculated using nine methods detailed in Table 21 show that the fracture energies in the adhesively bonded standard ELS specimens decreased with increasing rate. It is suggested that the above tests be done at rates higher than 0.1 m/s to determine the effect on the Mode II fracture energy.

3.5 Bonded and Unbonded Tube Structures

Following up on the coupon tests, component testing at dynamic rates of 0.05, 0.5, and 5 m/s were conducted on both unbonded and adhesively bonded tubes with various configurations of bond widths and bond thicknesses using the TMAC at ORNL. All tubes tested at the above-mentioned rates indicated a very well behaved progressive crush response critical for good crashworthiness with little difference between the bonded and unbonded tube responses. Figure 94 illustrates the load displacement response for an unbonded tube crushed at 5 m/s, which is very similar to the ones generated during the progressive crushing. It had four stages, the first one being characterized by an initial rapid load increase. A rapid load drop occurred in the second stage of the load deflection

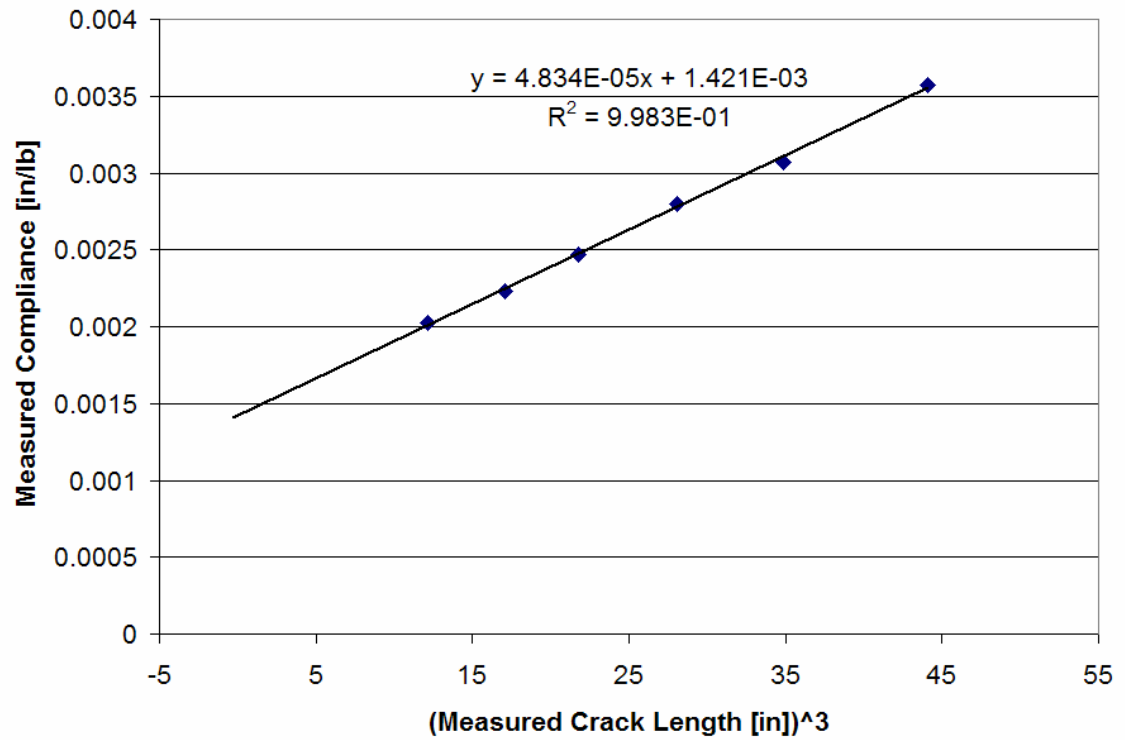


Figure 92. Plot of Compliance versus Cube of Crack Length for the 20 Ply in the Cracked Specimen Compliance Calibration Technique

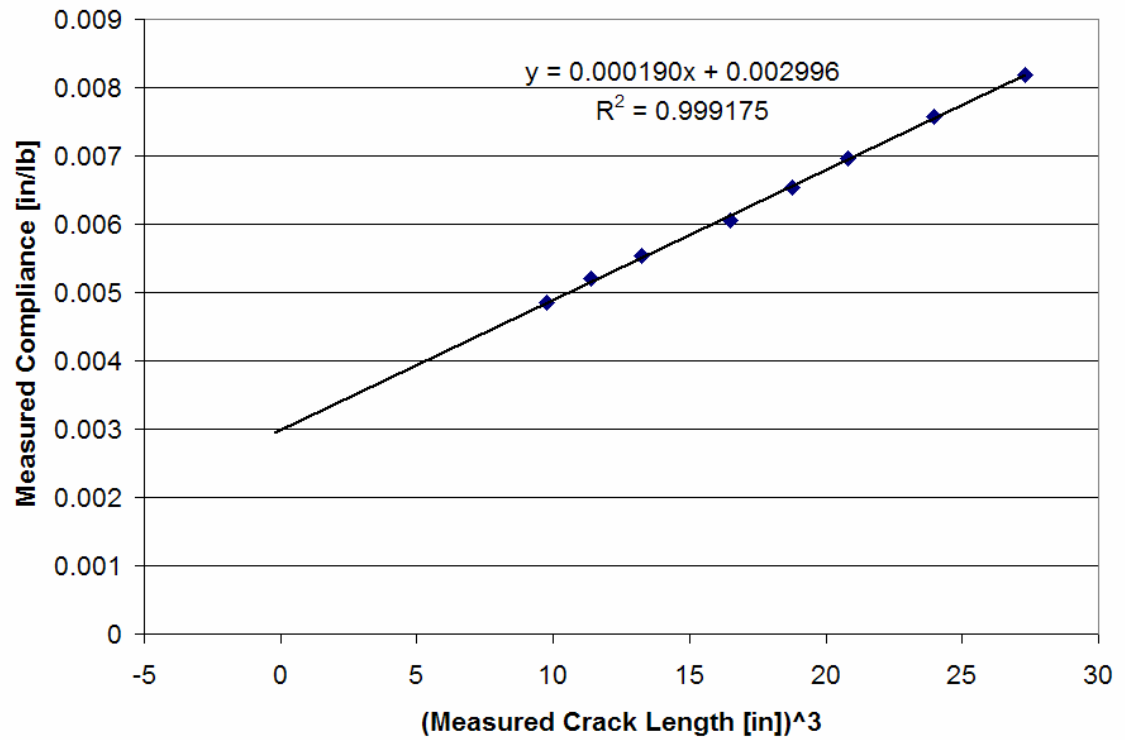


Figure 93. Plot of Compliance versus Cube of Crack Length for the 11 Ply in the Cracked Specimen Compliance Calibration Technique

Table 21. ELS Mode II Fracture Test Results

Lami- nates	Rate (m/s)	SBT Load Based G_{II} (J/m ²)	SBT Work Based G_{II} (J/m ²)	SBT Disp Based G_{II} (J/m ²)	CBT Load Based G_{II} (J/m ²)	CBT Work Based G_{II} (J/m ²)	CBT Disp Based G_{II} (J/m ²)	ECM Load Based G_{II} (J/m ²)	ECM Work Based G_{II} (J/m ²)	ECM Disp Based G_{II} (J/m ²)
11×11	10 ⁻⁴	4745	6850	9890	5100	5835	6675	5710	6510	7415
11×11	10 ⁻⁴	4515	6605	9655	4795	5795	7005	5435	6360	7440
20×20	10 ⁻⁴	5175	7895	12045	5740	6325	6975	6930	8055	9370
20×20	10 ⁻⁴	4940	7170	10415	5375	5935	6545	6615	7285	8020
20×20	0.01	4720	7375	11525	5200	5965	6855	6320	7515	8935
20×20	0.01	3300	4750	6835	3505	4150	4915	4415	5795	7605
20×20	0.01	2830	5065	9065	3085	4175	5650	3790	5145	6990
20×20	0.1	3205	5060	7990	3495	4180	4995	4290	5140	6160
20×20	0.1	2945	4190	5955	3155	3620	4150	3945	4235	4540

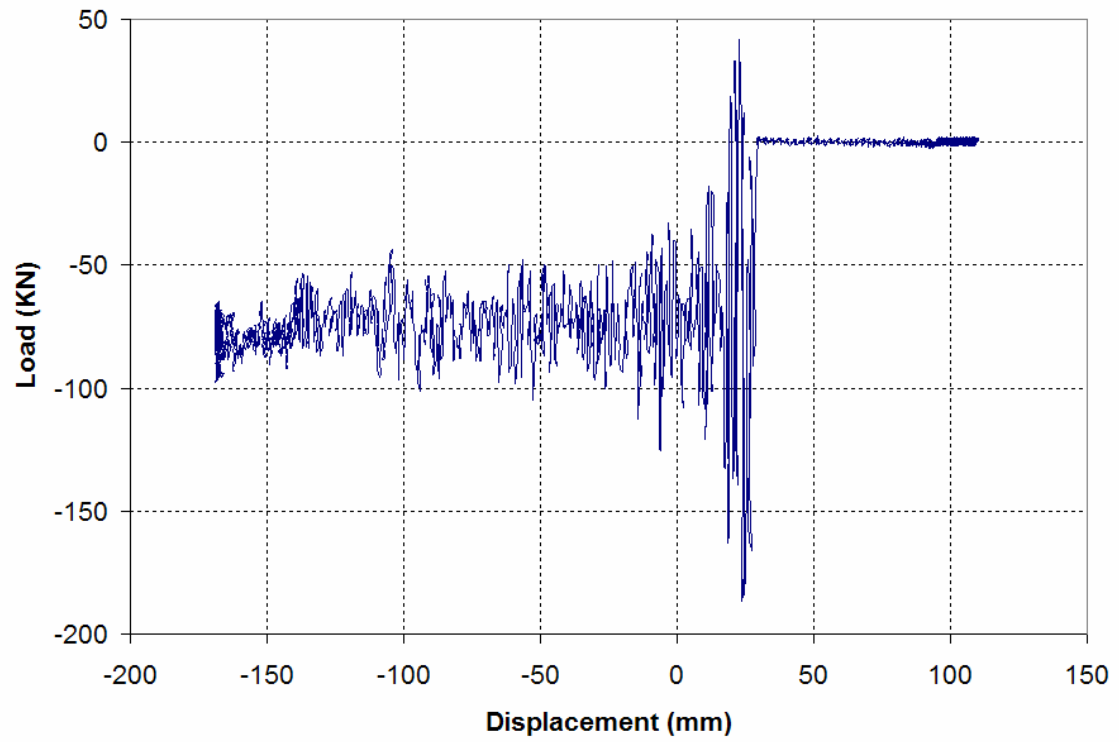


Figure 94. Load Displacement Response for an Unbonded Tube Crushed at 5 m/s

curve followed by a gradual saturation of the load. The final stage was characterized by stable crushing at a constant mean load. The small load fluctuations and serrations in the fourth stage of the curve are characteristic of stable crushing. Figure 95 depicts the progressive crush response of bonded and unbonded tubes each tested at 0.5 and 4 m/s. The tubes failed in a brittle fashion and literally exploded into tiny pieces. Figure 96 shows an unbonded tube before and after crushing at the highest rate.

Complete tubes recorded the highest SEA at all the loading rates. In fact, the SEA recorded by the tubes was twice as much displayed by materials such as Mild Steel [11] and Aluminum [11], which bodes well for the use of these carbon composites as crash energy absorbers. There was an apparent decrease in the SEA with increase in loading rate from 0.05 to 5 m/s for the complete tubes and all the bonded tubes with various joint configurations. The thinner bond thickness exhibited superior SEA, as was the case with the coupon joint tests (SLS and DNS tests). Bond thickness had more influence on SEA than bond width. Figure 97 illustrates the variation of SEA with rate for the unbonded and bonded tubes with various configurations of bond widths (BW) and bond thicknesses (BT). Figure 98 further depicts the rate dependence of the average SEA in the bonded and unbonded tubes that is calculated by taking into account the energy absorption of all tubes crushed at a given rate.

It is postulated that the decrease in SEA of the bonded and unbonded tubes with increasing rate is because of the decreased fracture toughness of matrix within the composite with increasing loading rate. A decrease in the fracture toughness with increasing loading rate means less resistance to crack formation. Therefore, there is less

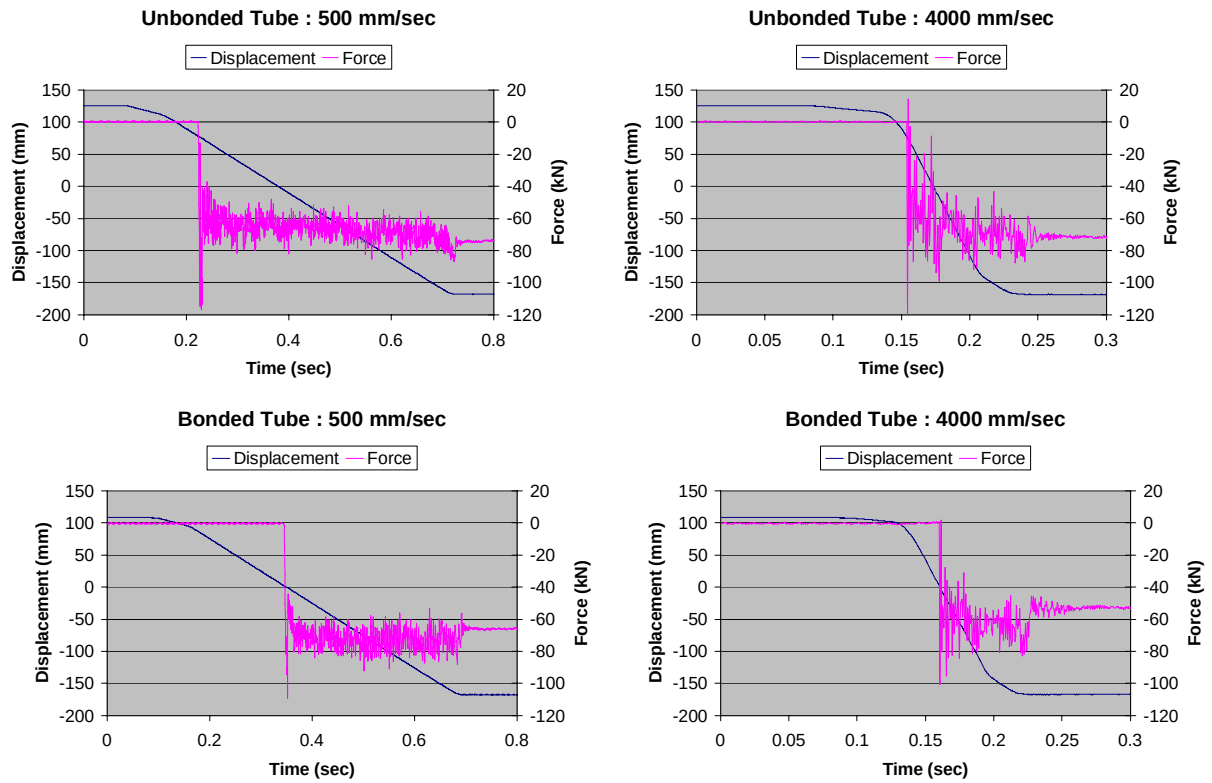


**Figure 95. Progressive Crush Response of Bonded and Unbonded Tubes each
Tested at 0.5 and 4 m/s**

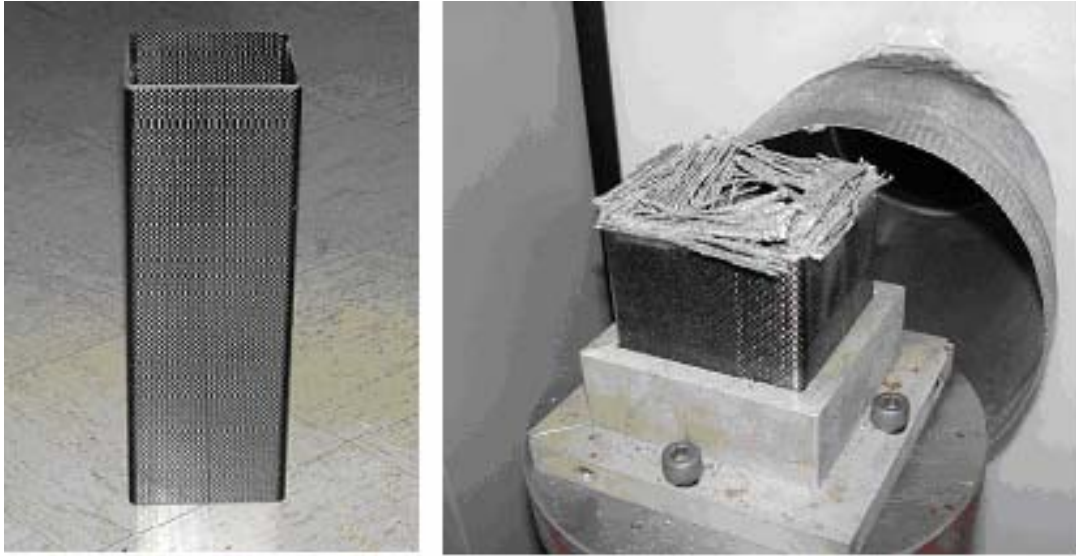


Figure 96. Unbonded Tube Before and After Crushing

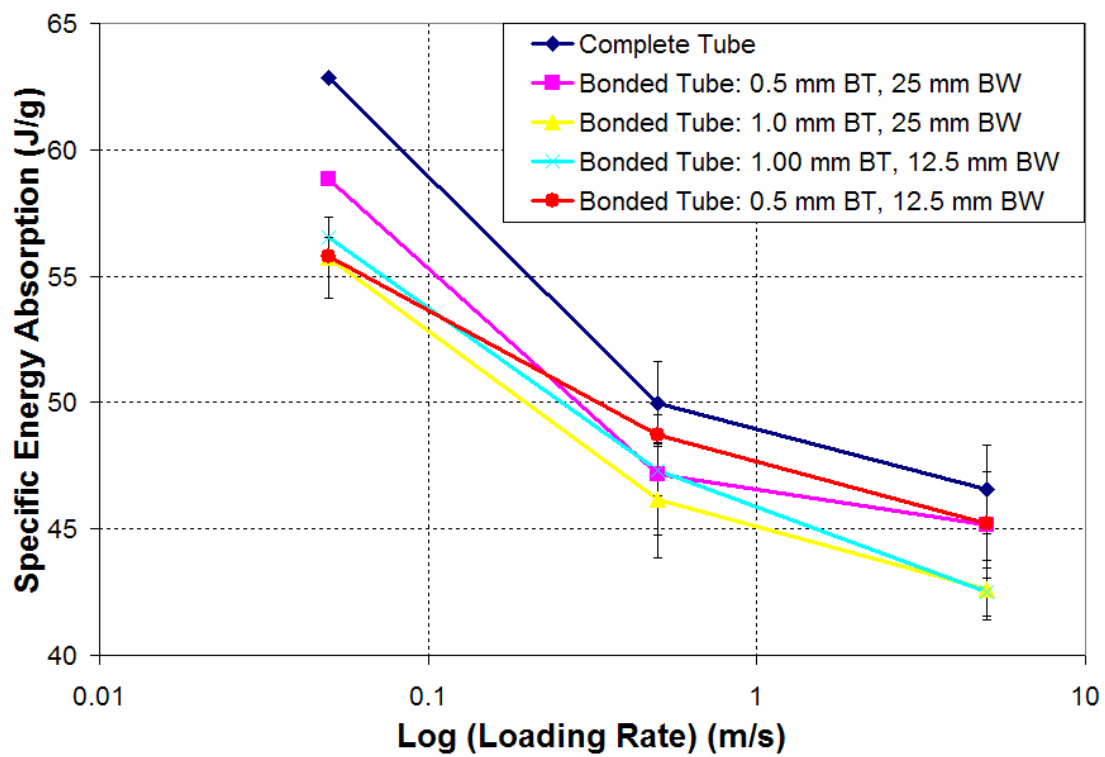


Figure 97. Variation of SEA with Rate for the Chamfered Unbonded and Bonded Tubes with Various Configurations of Bond Widths (BW) and Bond Thicknesses (BT)

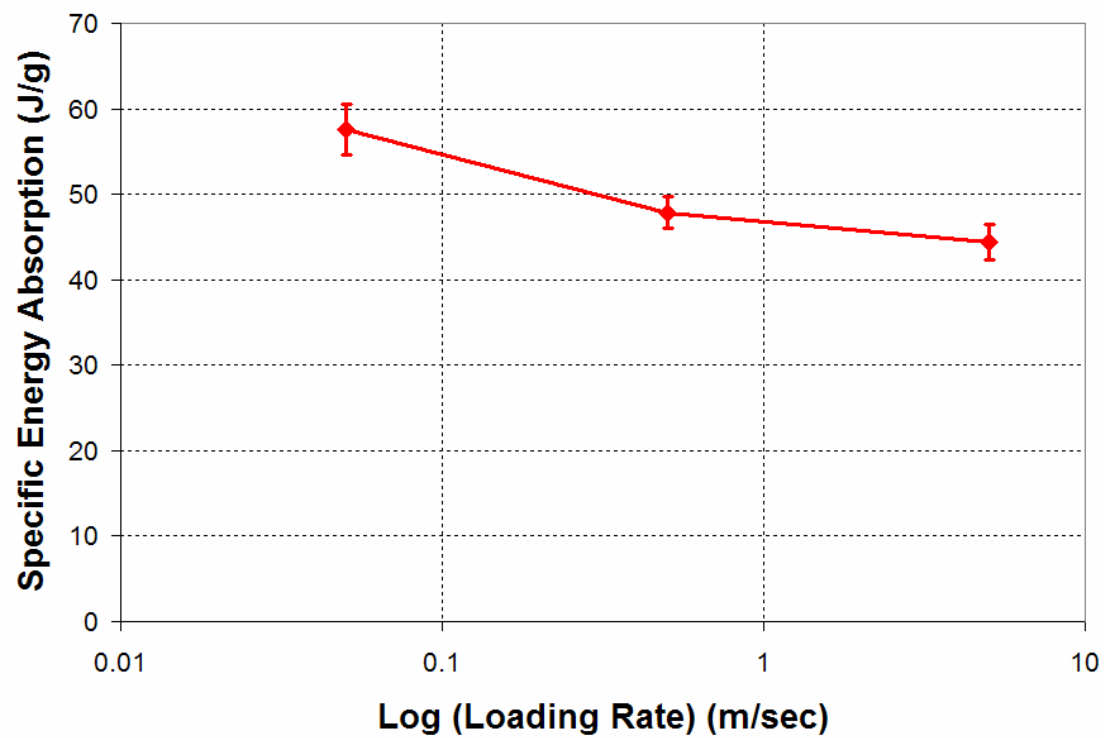


Figure 98. Variation of SEA of Tubes with Loading Rate

energy absorption in the composite tube at higher loading rates. As mentioned previously in this document that investigating rate effects on the fracture toughness of the PL731 epoxy bulk adhesive found the fracture toughness at higher test speeds to be only a fraction of its value at lower rates. While future tests should be planned to study rate effects on the fracture toughness of the composite material and also one can expect differences in the behavior of a resin in the bulk and within the matrix of a composite, the above observed effect of rate on fracture toughness of the epoxy bulk adhesive can be an indication to what might be happening in the composite tube at higher rates. Our experimental work in tube testing at various loading rates indicate more fiber dominated failure occurring at lower rates while at higher rates it is more matrix dominated failure that occurs. The matrix serves to effectively transfer the load between the fibers. If the matrix powders or fails early (volume of matrix between the fibers decrease) the load carrying capability/capacity of the fibers decrease (no load transfers to the fibers are possible) thus causing a decrease in the SEA of the composite structure.

It is suggested that future testing of the bonded and unbonded tubes be done at some additional rates (slower and some intermediate rates) and include also lateral impact loads. Some of these tests can be repeated using scaled geometries to get an initial look at scale effects. The results obtained from the complete and bonded tube tests show different SEA between them but not too much. Testing of adhesively bonded tubes having debonded regions within the bond line are being planned to further investigate the effect of bonded joints. The unbonded tests will serve to establish a baseline, and finally the results from all the substrate, bulk adhesive, and coupon-level/component level joints

characterization tests will guide the joint design such that bonded sections will be built to either fail or not fail in the joint.

CHAPTER 4

CONCLUSIONS AND FUTURE WORK

4.1 Conclusions

In determining how best fiber composite structures can be bonded together in the pursuit of designing the most crashworthy adhesively bonded structure a comprehensive experimental methodology to analyze and design adhesively bonded automotive composite structures made of carbon fiber reinforced polymer composites to sustain axial, off-axis and lateral crash/impact loads was developed and their performance dependence on strain rate was characterized. Initial efforts to obtain consistent mechanical properties on chopped carbon fiber materials, preferred by the ACC for use as crash energy absorbers primarily due to the low costs involved in their manufacture thus making them cost effective for automotive applications, were concluded unsuccessful. To focus on adhesive joint related issues, it is desirable to eliminate any variability or issues associated with the substrate material. Therefore, a braided carbon fiber composite was selected to replace the chopped carbon fiber material for this study. Both at the coupon and component level, during quasi-static and dynamic loading, it was concluded that the bond thickness had more influence on the joint performance properties, the thinner bond thickness of 0.5 mm exhibiting superior performance properties up to about 20%. Most importantly, the performance properties of the substrate, bulk adhesive and joints, both at the coupon and component level, dropped at the higher rates, some times precipitously to as low as 20% of the values obtained at slower loading rates.

Quantifying this performance and understanding rate effects is important to the crashworthiness design process. The SEA recorded by the carbon fiber composite substrate tubes was twice as much displayed by materials such as Mild Steel and Aluminum, which bodes well for the use of these carbon composites as crash energy absorbers. Among the many experimental tasks involved in the successful execution of the above research, new test method development figured prominently as and when it was required. Limitations in the existing typical dynamic structural test capabilities resulted in the advent of TMAC (Test Machine for Automotive Composites), a first of its kind intermediate strain rate servo-hydraulic test machine at ORNL. The experimental results from this work are being used to provide the building blocks for model developments – first the coupon level, then progressing in complexity to component level. Correlation with experimental results will provide the basis for which the analytical developments including development of constitutive laws, materials models, damage algorithms and new finite elements, are made.

4.2 Future Work

A disturbing but important observation from the above concluded work was the drop in the performance properties of the substrate, bulk adhesive and joints at the higher rates, some times to as low as 20% of the values obtained at slower loading rates. At the time this work was conducted, the capability to test coupon type specimens only up to 1 m/s existed at the lab. However, with the advent of a new high rate machine at ORNL that can test coupon type specimens up to 18 m/s, an effort is being made to

conduct CT, SLS, DNS, DCB, and other coupon tests at even higher rates, thus attempting to fully cash in on this recently available capability. The 18 m/s test machine custom designed between ORNL and MTS is shown in Figure 99 and has the following specifications:

- Velocity=18.5 m/sec over approximately 100 mm range
- Load Capacity : 40 KN static, 25 KN dynamic guaranteed by MTS
- Total Stroke: 400 mm
- Working Stroke: 175 mm approx. with slack adapter in the load train
- Control: MTS 407 servo - hydraulic controllers, with external command signal (drive file)
- 50 Gallon Accumulators
- 400 GPM Main Servo Valve
- 15 GPM Position Servo Valve
- Software: All software was developed in house using LabView 7.1
- and NiDAQ MX 7.5 drivers (based on TMAC experience)



Figure 99. New 18 m/s Test Machine at ORNL

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