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BEARING STRENGTH OF SINGLE SHEAR CFRP JOINTS UNDER COMBINED LOADING

By

Trevor Harrison, B.Eng.

A thesis submitted to
The Faculty of Graduate Studies and Research
In partial fulfillment of
The requirements for the degree of

Master of Engineering

Department of Mechanical and Aerospace Engineering
Ottawa-Carleton Institute
For Mechanical and Aerospace Engineering

Carleton University

Ottawa, Ontario

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Thesis Supervisor

Co-Supervisor

Chair, Department of Mechanical and Aerospace Engineering

Carleton University
November 1998

ABSTRACT

The use of mechanically fastened joints is unavoidable in aerospace structures due to serviceability requirements. Because of the low efficiencies of mechanical joints, considerable effort is put into their design. Conventional design methodologies use uniaxial strength data and isotropic failure criteria to predict joint strength. However, these methodologies result in poor strength predictions when applied to fibre reinforced plastic laminates. To improve results, orthotropic failure criteria were developed, but the use of uniaxial allowables remained.

The primary objective of this thesis was to obtain bearing failure data under various states of plane stress to improve joint strength predictions. This was achieved through an experimental test program which investigated the effects of biaxial and bypass stress states on the bearing strength of a graphite/epoxy laminate. Results were compared against uniaxial data, and showed that the application of a biaxial load caused a decrease in the bearing strength. The addition of a bypass load was also found to lower the strength at which bearing failure began. The magnitudes of these strength losses were dependant on the biaxial stress state.

Further to the experimental program, 2-D and 3-D finite element analyses were performed to supplement experimental findings. The 3-D model was determined to be more representative of the present problem, as it demonstrated fastener and joint deformation consistent with that of single shear lap joints. Both 2-D and 3-D models overpredicted the theoretical peak bearing stress.

Experimental, numerical, and analytical results were used to develop an empirical failure prediction technique. This technique relied upon three factors which accounted for joint configuration, stress state, and fastener preload. Strength predictions for various load cases resulted in good agreement with experimental findings.

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NOMENCLATURE

Greek

σ	Stress
σ_o	Gross Section Stress
σ_{brg}	Average Bearing Stress
σ_N^∞	Notched Laminate Stress in an Infinite Plate
σ_{peak}	Peak Bearing Stress
σ_{su}	Ultimate Shear Stress
σ_θ	Tangential Stress
σ_{tu}	Ultimate Tensile Stress
σ_z	Clamp-up Stress
$\sigma_l, \sigma_2, \tau_{12}$	Longitudinal, Long Transverse, Short Transverse Principal Lamina Stresses
$\sigma_x, \sigma_y, \tau_{xy}$	Longitudinal, Long Transverse, Short Transverse Principal Laminate Stresses
β	Bypass Ratio
Δ	Compression Reduction Factor
ϕ	Preload Correction Factor
λ	Fastener Clearance
Ω	Electrical Resistance
θ_c	Contact Angle
$\nu_{12}, \nu_{13}, \nu_{23}$	Poisson's Ratio in the Lamina Coordinate System
$\nu_{xy}, \nu_{xz}, \nu_{yz}$	Poisson's Ratio in the Laminate Coordinate System
μ_s	Static Friction Coefficient
ψ_r, ψ_θ	Bearing, Tangential Stress Multipliers
γ	Puppo & Evensen Interaction Term
η	Efficiency
π	pi = 3.14159
∞	Infinity

Arabic

a_o	Average Stress Characteristic Distance
a	Crack Length
A_{eff}	Effective Area
d_o	Fastener Footprint Outer Diameter; Point Stress Characteristic Distance
d_i	Fastener Footprint Inner Diameter
d	Hole Diameter
e	Hole Centre to Plate Edge Distance
E_{11}, E_{22}, E_{33}	Longitudinal, Long Transverse, Short Transverse Young's Modulus in the Lamina Coordinate System
E_{xx}, E_{yy}, E_{zz}	Longitudinal, Long Transverse, Short Transverse Young's Modulus in the Laminate Coordinate System
f	Friction Force
f	Fringe Factor
F_{ij}	Tensor Interaction Terms; $i, j = 1 \dots \infty$
G_{12}, G_{13}, G_{23}	Shear Moduli in the Lamina Coordinate System
G_{xy}, G_{xz}, G_{yz}	Shear Moduli in the Laminate Coordinate System
K	Torque Coefficient, Optical Coefficient
K_T^∞	Isotropic Tangential Stress Concentration Factor for a Circular Notch
P_{joint}	Applied Joint Force
P_{Onset}	Predicted Bearing Failure Load After Application of Correction Factors
$P_{Predicted}$	Predicted Failure Load Before Application of Correction Factors
$P_{preload}$	Fastener Preload Force
P_{brg}	Bearing Force
P_{bypass}	Bypass Force

R	Hole Radius
t	Laminate Thickness
T	Torque
W	Plate Width
X', Y', S'	Longitudinal, Long Transverse, Short Transverse Principal Laminate Allowables
X_t, Y_t, S_t	Longitudinal, Long Transverse, Short Transverse Principal Lamina Tensile Allowables
X_c, Y_c, S_c	Longitudinal, Long Transverse, Short Transverse Principal Lamina Compressive Allowables
z	Short Transverse Position

Abbreviations

BFRP	Boron Fibre Reinforced Plastics
CFRP	Carbon Fibre Reinforced Plastics
CLPT	Classic Laminate Plate Theory
DSC	Digital Servo Controller
FEA	Finite Element Analysis
FEM	Finite Element Model
FWC	Finite Width Correction Factor
GFRP	Glass Fibre Reinforced Plastics
HAZ	Heat Affected Zone
LEFM	Linear Elastic Fracture Mechanics
LVDT	Linear Variable Differential Transformer
MTS	Material Testing System
NRC	National Research Council Canada
NSR	Notched Strength Reduction Factor
SF	Severity Factor
SMPL-IAR	Structures, Materials, and Propulsion Laboratory - Institute for Aerospace Research

UNITS

Keeping with the practices of the North American aircraft industry, the Imperial system of measurement is employed throughout this thesis.

1. INTRODUCTION

Aerospace structures comprise of many complex parts, each designed for maximum structural efficiency. The connections between these parts are important in aerospace design, as improperly designed joints increase weight and degrade structural integrity. Although fabrication techniques exist to eliminate joints, manufacturing limitations and serviceability requirements still demand their use.

Various techniques for joining structures have been developed, with each having its advantages and drawbacks. Welded, bonded, and mechanically fastened joints are most common, but only the latter satisfies the serviceability requirement. The low structural efficiency of mechanical joints has led to extensive research into their load transfer mechanisms. Because of this research, the behaviour of mechanical joints in isotropic materials is well understood for uniaxial and multi-axial stress states. Consequently, joint analysis is straightforward.

Current design methodologies for isotropic materials calculate the joint strength using uniaxial material allowables and isotropic failure criteria. But, when these methodologies are applied to orthotropic fibre reinforced plastic laminates, they result in poor strength predictions. This in turn produces inefficient joints.

Improvements in joint strength predictions have been achieved by developing failure criteria specific to composite laminates. However, the use of uniaxial allowables in the analysis continued. This resulted in satisfactory predictions for structures experiencing uniaxial loads, but not for structures subjected to multi-axial loads. Further improvements were achieved using empirical interaction terms derived from biaxial experiments. But, because of the cost and complexity associated with biaxial testing, this approach has remained largely uninvestigated.

As part of ongoing research on the study of mechanical joining technology, the objectives of this thesis are:

1. to identify the parameters which influence the behaviour of mechanically fastened joints,
2. to identify the present design approaches for mechanically fastened joints in composite laminates,
3. to obtain single shear bearing strength data for an orthotropic composite laminate in the four quadrants of plane stress,
4. to investigate the influence of stress state and bypass load on the bearing strength of the laminate,
5. to develop a finite element model (FEM) of the joint to supplement experimental data, and
6. to develop an empirical technique for predicting the onset of bearing failure.

The first chapters of this thesis provide a comprehensive background into the mechanisms that influence the strength of mechanically fastened joints, and the design methodologies used to analyze them. The subsequent chapters detail the above objectives and the programs undertaken to fulfil those objectives. Experimental results and discussions are presented in Chapter 8. Finite element analysis (FEA) and the bearing failure onset prediction technique are described and discussed in Chapters 9 and 10, respectively. Chapter 11 states the conclusions drawn from this program, and identifies areas for future work.

2. MECHANICAL JOINT BEHAVIOUR

Joints are important elements in aerospace structures as they are potential weak links. Although techniques exist to reduce the number of joints, manufacturing limitations and serviceability provisions demand their use.

2.1 Types of Joints

Many methods exist to connect two or more parts together. In aerospace structures, this is often accomplished through welding, bonding, or mechanical fastening, with each having its advantages and drawbacks. The following sections provide an overview of the above joining methods.

2.1.1 *Welded Joints*

Welded joints are created by fusing two or more parts together. This low cost approach results in high joint efficiencies because of a negligible weight increase and small loss in structural strength. Joint efficiency, η , is expressed as the strength of the joint, σ , divided by that of the material if the joint was not present, σ_o .

$$\eta = \frac{\sigma}{\sigma_o} \times 100\% \quad \text{eqn. 2.1}$$

Strength losses occur because the welding process causes localized heating adjacent to the weld bead which induces a phase change in the material. This region of reduced strength is referred to as the heat affected zone (HAZ). Within this zone, welded joints display poor resistance to fatigue and shock, thereby limiting their use in aerospace applications.

2.1.2 *Bonded Joints*

Bonded joints are formed when a thin film of adhesive is placed between two or more overlapping parts (adherents). Several different joint configurations are possible, each

producing different strengths (see Figure 2.1). However, bonded joints are typically limited to thin adherents since load eccentricities produce large peel stresses which cause premature adhesive failure[1]. Additionally, joint strength is sensitive to adherent surface preparation. Therefore, controlled fabrication procedures are required, making bonded joints expensive to manufacture.

In the aviation industry, bonded joints are further restricted to components in which joint failure does not compromise aircraft survivability[2]. Where a bonded joint would compromise survivability, it must be reinforced with mechanical fasteners.

2.1.3 Mechanical Joints

Mechanical joints are formed when two or more overlapping parts are connected by discrete fasteners through which the load is transferred. Such joints are easily disassembled and reassembled without causing damage to joined members. Additionally, mechanical joints are capable of connecting dissimilar parts which could not be joined by other methods. These advantages make mechanical joints suitable for serviceability requirements.

However, the use of mechanical fasteners structurally degrades the joint, and induces stress concentrations within joint members. These factors limit the efficiency of mechanical joints to less than 50%.

2.2 Load Transfer Mechanics

The predominant load transfer mechanism in mechanically fastened joints is through bearing (the local compression of joint members), and shear of discrete fasteners. Joint member loads are transferred to the fasteners by bearing. As the fastener opposes the bearing load, it transfers the load to the other member by shear, where it is reacted out in bearing again.

Another load transfer mechanism in mechanically fastened joints is that of friction. The application of a normal load to surfaces in contact generates a shear force which opposes relative in-plane movement. This force, known as friction, transfers loads directly between the contact surfaces. However, friction is not always present as it depends on the type of fastener, material surface finish, and the materials being joined.

2.3 Failure Modes of Mechanically Fastened Joints

Three predominant failure modes exist in mechanically fastened joints: shearout, net-section, and bearing. Joint failure by one of these modes is inevitable, with the joint design determining the dominate mode. The following subsections discuss the failure modes, and their mechanisms.

2.3.1 Shearout Failure

One mode of failure common to mechanical connections is that of shearout. Shearout failures are characterized by the extrusion of a plug from the bulk material (see Figure 2.2a). This occurs when the ultimate material shear strength, σ_{su} , between the hole and the plate edge is exceeded. Shearout failures in isotropic joints occur when the following condition is met

$$\frac{P_{brg}}{2 \cdot t \left(e - \frac{d}{2} \right)} \geq \sigma_{su} \quad \text{eqn. 2.2}$$

In the above equation, P_{brg} is the load transferred by the fastener, e is the distance from the plate edge to the hole centre, d is the hole diameter, and t is the joint member thickness.

In composite laminates, equation 2.2 is not always valid as layups containing excessive plies aligned with the bearing direction are at risk of failure by shearout. This higher risk results from a lack of cross-ply reinforcement. Due to their catastrophic nature, shearout failures are undesirable as they result in complete loss of load carrying ability.

2.3.2 Net-Section Failure

Net-section failures, otherwise known as tension-through-hole failure, occur when the net-section stress exceeds the ultimate material tensile strength, σ_{tu} . This failure mode produces a fracture passing through the hole, and extending perpendicular to the applied load (see Figure 2.2b). Net-section failures result from inadequate material in the net-section, and occur in isotropic materials if the following condition is satisfied

$$\frac{K_T^\infty \cdot P_{brg}}{t(W - d)} > \sigma_{tu} \quad \text{eqn. 2.3}$$

where K_T^∞ is the isotropic tangential stress concentration factor, and W is the plate width. Equation 2.3 assumes that the net-section stress is caused by the bearing load only; however, if part of the load is not transferred by the fastener, the following equation governs failure

$$\frac{K_T^\infty \cdot (P_{brg} + P_{bypass})}{t(W - d)} > \sigma_{tu} \quad \text{eqn. 2.4}$$

where P_{bypass} is the load not transferred by the fastener (i.e. bypass load).

Similar to shearout failures, laminates containing large percentages of plies oriented perpendicular to the bearing load are prone to net-section failure. Again, this is caused by a lack of reinforcement in the loading direction.

2.3.3 Bearing Failure

Bearing failures in mechanically fastened joints are localized compressive failures (see Figure 2.2c). These failures occur when the bearing load, P_{brg} , acting over an effective area, A_{eff} , exceeds the material bearing strength, σ_{brg}

$$\frac{P_{brg}}{A_{eff}} > \sigma_{brg} \quad \text{eqn. 2.5}$$

Unlike shearout and net-section failure modes, bearing failures result in a gradual elongation of the hole along the bearing direction, and consequently lack a well defined

failure point. Therefore, bearing failures are often considered to have occurred once a predefined hole elongation has been reached.

Most isotropic structural materials have greater bearing strengths than tensile or shear strengths. Therefore, according to equation 2.1, maximum joint efficiency occurs when the joint fails in bearing.

2.4 Joint Configurations

Joints exist in many configurations, but most are extensions of two types: single shear lap, and double shear lap. The selection of which configuration to use depends on structural, aerodynamic, weight, and aesthetic considerations.

2.4.1 Single Shear Lap Joints

A single shear lap joint consists of two partially overlapping plates which are held together by a single row, or multiple rows of fasteners (see Figure 2.3a). These joints are complex to design and analyze because of asymmetry which induces plate bending and fastener rotation. These reactions result in a non-uniform through-thickness bearing load, with higher bearing loads occurring at the contact surfaces (faying surfaces) as shown in Figure 2.4.

The major advantage of a single shear lap joint is that assembly is possible if access is limited to one side of the structure. This is important in bolted repair schemes. However, the single load path does not permit a fail-safe design, which is a concern in aerospace applications.

2.4.2 Double Shear Lap Joints

A double shear lap joint is shown in Figure 2.3b. Similar to single shear joints, this configuration consists of two members flanking a third, and held together by a single row, or multiple rows of fasteners. The symmetric configuration of double shear lap

joints eliminate the joint bending and fastener rotation observed in their single shear counterparts. However, since the loads are still eccentric, the fastener experiences bending, which increases the stresses at the faying surfaces. The stress increase is small compared to the nominal bearing stress, thus resulting in a small through-thickness stress variation. Therefore, the variation is often neglected[3].

2.5 Design Considerations

Achieving maximum efficiencies in practical mechanical joints is challenging since many parameters influence the design process. Simplifying assumptions are often applied to make the design and analysis more manageable. These assumptions are valid for homogeneous, isotropic materials, but tend to be less accurate for orthotropic materials. Prime design parameters which affect joint strength, and the assumptions associated with each, are discussed in the subsequent sections.

2.5.1 Contact Angle

The contact angle, θ_c , is the angle from the hole centreline over which the fastener is in direct contact with the hole face (see Figure 2.5). Often a constant contact angle is assumed to simplify analysis; however, studies have shown that contact angle varies with clearance and load[4,5].

Fastener clearances of 0 to 2% of the hole diameter are typical in the aerospace industry when working with composite materials[4]. Figure 2.6 shows that looser fits produce smaller contact angles for orthotropic graphite/epoxy laminates. For a constant bearing load, increasing the clearance from a snug fit ($\lambda = 0$) to a clearance of 0.01 inches reduces the contact angle from 90° to 77°.

Figure 2.7 shows that for a given clearance, the allowable bearing stress increases exponentially with increasing contact angle.

2.5.2 Influence of Fastener Diameter on Bearing Strength

The average bearing stress, σ_{brg} , is defined as the fastener bearing load divided by the effective area, A_{eff} , upon which it acts,

$$\sigma_{brg} = \frac{P_{brg}}{A_{eff}} \quad \text{eqn. 2.6}$$

The effective area is the projection of the contact angle, θ_c , on the hole diameter multiplied by the material thickness, and is expressed mathematically as

$$\sigma_{brg} = \frac{P_{brg}}{t \cdot (d \cdot \sin(\theta_c))} \quad \text{eqn. 2.7}$$

For engineering applications, a contact angle of 90° is assumed, thereby making the effective area equal to the hole diameter times the material thickness. Thus, without any significant loss of accuracy, the definition of the average bearing stress becomes

$$\sigma_{brg} = \frac{P_{brg}}{t \cdot d} \quad \text{eqn. 2.8}$$

The peak bearing stress, σ_{peak} , is then defined as

$$\sigma_{peak} = \frac{4}{\pi} \cdot \sigma_{brg} \quad \text{eqn. 2.9[6]}$$

The inverse proportionality of equation 2.8 indicates that lower bearing stresses are achieved by increasing the hole size.

2.5.3 Through-Thickness Stress Variations

In metallic structures, the design of mechanically fastened joints assume a uniform through-thickness stress distribution. The magnitude of this stress is calculated by multiplying the average bearing stress (eqn. 2.8) by a severity factor (SF) thereby accounting for the through-thickness stress variation arising from joint configuration (see Figure 2.8).

In composite materials, the assumption of a uniform through-thickness stress distribution is less appropriate as lamina stresses can vary significantly from those of the bulk laminate. Therefore, applying a SF to the average bearing stress may yield inadequate safety margins. To compensate for this, the SF is multiplied by an arbitrary factor of 1.5 to 2.0 until experimental results can confirm the actual stress intensity[7].

2.5.4 Stacking Sequence Influence on Joint Strength

2.5.4.1 Minimum Stacking Requirements for Acceptable Joint Strength

The stacking sequence defines the number, order, and orientation of plies within the laminate, and is important in determining laminate strength (see Figure 2.9)[7]. Since maximum joint strength is achieved when failure is by bearing (section 2.3.3), several design guidelines exist to ensure bearing failure first. Hart-Smith suggests that for $[0_k/\pm 45_l/90_m]_{ns}$ laminates, the strongest stacking sequences contain a minimum of 12.5%, but not more than 37.5%, of plies in each of the 0° , $\pm 45^\circ$, or 90° orientations (see Figure 2.10)[8].

2.5.4.2 Influence of Stacking Sequence on the Stress Distribution Along a Hole Perimeter

The stress distribution around a hole is dependant on the plate orthotropy, which for composite laminates, is a function of stacking sequence. Crews et al. reported that for quasi-isotropic laminates, the peak radial stress ratio was near unity[9]. For a 0° unidirectional laminate, the peak radial stress ratio was 2, while for a 90° laminate it was 1.4. The quasi-isotropic and 0° laminates demonstrated a peak radial stress aligned with the bearing direction, while for the 90° laminate, the peak radial stress was almost perpendicular to the bearing load.

2.5.4.3 Placement of Plies within the Stacking Sequence

Not only is the number of plies within the laminate important to bearing strength, but so is the order in which they occur[10,11]. Quinn demonstrated that for double shear glass fibre laminates containing equal ply percentages, failure mode and strength were

dependant on the stacking sequence[10]. Experimental results showed that a $[0/\pm 45/90]_s$ laminate yielded a lower bearing strength than a $[90/\pm 45/0]_s$ layup .

2.5.5 Fastener Preload

The application of a through-thickness pressure around a hole increases joint bearing capacity. Figure 2.11 shows that a finger-tight preload doubles the bearing strength over a pin-loaded case. This increase in bearing strength is achieved through two mechanisms. The first is the constraint of the hole edge by the fastener head and footprint. This prevents damaged material from mushrooming outwards, thereby limiting load redistribution within the laminate. The lack of such constraint would otherwise lead to rapid damage progression. The second mechanism is an increase in the friction between the joint members, and is discussed in the following.

The most common fastener types used in the aerospace industry are rivets and bolts. Of these two, only bolts permit variable preloads as determined by the amount of torque applied. Bolt preload, $P_{preload}$, is calculated from

$$P_{preload} = \frac{T}{K \cdot d} \quad \text{eqn. 2.10}$$

where K is the torque coefficient, and is a function of bolt type and lubrication. T is the applied torque. The application of a preload induces a through-thickness clamp-up stress, σ_z , expressed by

$$\sigma_z = \frac{P_{preload}}{\frac{\pi}{4}(d_o^2 - d_i^2)} \quad \text{eqn. 2.11}$$

where d_i and d_o are the inner and outer fastener footprint diameters, respectively. The amount of clamp-up depends on the purpose of the joint; however, values above 3,200 psi do not produce significant increases in bearing strength[12].

In aerospace applications, preloads above finger-tight are often disregarded during analysis as the ability to retain preload throughout service is questionable. This is because under fatigue loading, the visco-elastic properties of composite laminates alleviate fastener preload[13]. In addition, joints relying on the increased bearing strength from preloading are at risk of premature structural failure if fasteners are incorrectly preloaded. Therefore, as a matter of safety, high preload contributions are generally neglected.

2.5.6 Influence of Bypass Load on Joint Strength

Bypass loads are created when only a portion of the applied load is reacted by the fastener (see Figure 2.12). This occurs in multi-fastener joints in which load transfer is shared between fasteners, or when faying surface friction is present. The bypass ratio, β , is defined as

$$\beta = \frac{(P_{joint} - P_{brg})}{P_{joint}} \quad \text{eqn. 2.12}$$

where, P_{joint} is the joint load, and P_{brg} is the portion of load transferred by the fastener.

The application of a bypass load influences failure mode by altering the net-section stress, with greater bypass loads resulting in higher net-section stresses. For finite width plates, a bypass limit exists (see Figure 2.13). Bypass values exceeding this limit result in failures occurring in the net-section rather than bearing.

2.5.7 Friction Between Faying Surfaces

Friction between joint faying surfaces create a bypass load which reduces the loads transferred by the fastener. The amount of friction, f , within a joint is a function of fastener preload, $P_{preload}$, and the coefficient of static friction, μ_s , such that

$$f = \mu_s \cdot P_{preload} \quad \text{eqn. 2.13}$$

The static friction coefficient is a function of the materials in contact, their surface roughness, and the lubrication between faying surfaces. Values of 0.1 to 1.0 are typical for the friction coefficient[14]. Increasing joint friction causes a greater proportion of the applied load to bypass the fastener; thereby reducing bearing loads. However, under fatigue loading, wear debris between faying surfaces acts as a lubricant which reduces friction.

2.5.8 Joint Flexibility

Studies have shown that joint flexibility influences its strength[10,15,16,17,18]. These works suggest that several key parameters, namely joint configuration, material properties, and fastener stiffness, influence joint strength.

2.5.9 Fastener Diameter to Plate Thickness Ratio

General design practices for composite materials recommend the use of large fastener diameter to plate thickness ratios to minimize non-uniform through-thickness stresses[2]. Collins observed that for double shear joints, bearing strength decreased with higher d/t values[19]. However, results obtained by Ramkumar & Tossavainen showed that increasing d/t improved bearing strength for single shear joints[20]. This was because thicker laminates produce larger load eccentricities which increases fastener rotation, and hence lowers bearing strength. Since composite materials are typically thicker than metals of equivalent strength, the use of large d/t ratios become significant in reducing high bearing stresses[7].

2.5.10 Joint Geometry Effects on Joint Strength

2.5.10.1 Plate Width Effect

The ratio of plate width to hole diameter, W/d , is an important parameter in determining the failure mode, and hence joint strength. Since most materials have greater bearing strengths than tensile or shear strengths, then the maximum possible joint strength occurs when the failure mode is bearing. Depending on the W/d value, two failure modes are

possible: bearing and net-section. The value of W/d at which the failure mode changes from net-section failure to bearing is the point of optimum joint strength. In Figure 2.14, this point occurs at a W/d ratio of 6 for $\pm 45^\circ$ laminates, 5 for $[0/\pm 45]_3$ laminates, and 4 for $[0_2/\pm 45_2]$ laminates. Furthermore, experiments showed that for quasi-isotropic laminates, the maximum tangential stress doubled when W/d decreased from ∞ to 2[9]. The radial stress appeared insensitive to W/d .

In addition to failure mode, W/d ratios influence the net-section stress. For W/d ratios greater than 8, the difference between gross and net-section stresses is small and considered negligible[21]. However, smaller values require the application of a finite width correction (FWC) factor to account for the increased stress caused by edge effects. For isotropic materials, the following closed form is used

$$FWC = \frac{2 + \left(1 - \frac{d}{W}\right)^3}{3 \cdot \left(1 - \frac{d}{W}\right)} \quad \text{eqn. 2.14}$$

No such relationship exists for orthotropic materials, but the isotropic case has been found to be reasonably accurate when applied to some orthotropic laminates[22].

2.5.10.2 Edge Effect

The edge distance from a hole, e , influences the failure mode in a similar manner to that seen for plate width (see Figure 2.15). Depending on the value of e/d , one of two failure modes is possible. Below a specific value of e/d , shearout failures result, while increasing the ratio beyond that value switches the failure mode to bearing. The value of this switch-over point is dependant on the lamina properties and stacking sequence.

Finite element studies performed by Crews et. al. examined the effect of e/d on pin-loaded, quasi-isotropic laminates[9]. Assuming an infinitely wide plate, the authors showed that for increasing ratios of e/d , the peak tangential stress decreased.

Furthermore, the location of the peak tangential stress was perpendicular to the bearing

load at $e/d = \infty$, and increased in angle as e/d decreased. Similar to the finite plate width parameter, the radial stress varied slightly around the hole perimeter, thereby appearing insensitive to edge effect.

2.5.11 Influence of Biaxial Load on Joint Strength

The application of lateral loads alter the stress fields surrounding a hole. Figure 2.16 shows the variation in stress field for a notched isotropic plate under different conditions of plane stress[23]. Compared to a uniaxial load, a tensile biaxial load reduces the tangential stress concentration, K_T^∞ , at point A, while doubling and changing sign at B. Hoa studied the effect of tensile biaxial loads on the bearing strength of carbon fibre reinforced plastic (CFRP) laminates[24]. The author observed that under a biaxial load, a bearing strength loss resulted, in some cases by as much as 50%, over uniaxially loaded specimens.

2.6 Discussion

The design and analysis of mechanically fastened joints are complex due to the numerous parameters involved. Several simplifying assumptions exist, but these are based on isotropic materials and hence are inaccurate when applied to orthotropic laminates. Considerable effort, both experimental and analytical, has been expended to establish the behaviour of composite laminates subjected to uniaxial bearing/bypass loads. However, little data is available for biaxially loaded specimens. Since the stress state under a biaxial load is significantly different than that for a uniaxial load, then it must be considered during the design of notched structures. Therefore, further work in this area is required.

3. DESIGN METHODOLOGY

To include all parameters in the design of mechanically fastened joints renders the problem impractical; therefore, joints are simplified using conservative engineering assumptions and design rules. Such rules are well established for isotropic materials, but tend to be inadequate for composite materials because of their anisotropic, non-homogeneous, and brittle nature.

The design methodology for mechanical joints is schematically represented by Figure 3.1 and consists of four basic steps: overall structural analysis, fastener load distribution, detailed stress analysis, and strength prediction[25].

3.1 Overall Structural Analysis

Overall structural analysis establishes internal loads which allows for detailed stress analysis and part optimization. Often the influences of joints are ignored to simplify the analysis[25]. However, in situations where the joint is an integral part of the structure and its omission would compromise analysis accuracy, it must be considered. For example, the wing pivot pin on the B-1 bomber is a critical part of the structure, and therefore must be included in the overall analysis. In such cases, the design becomes more complex requiring the use of advanced equations and modelling techniques.

3.2 Fastener Load Distribution

After structural loads have been calculated, joint configurations are determined and fastener loads calculated. Closed form solutions are used for preliminary load analysis, but are limited to simple joint configurations[26,27]. To handle more complex joints, various in-house computer programs such as JOINT[28], A4EJ[29], SAMCJ[30], and BOLTFAST[31] were developed. These programs employ a variety of techniques to determine fastener loads and perform detailed stress analysis. However, the advantage of these programs is not their analysis capabilities, but rather their ability to handle multi-

fastener joints. Venkayya & Tischler provide a comprehensive description of each program[32].

3.2.1 JOINT

JOINT is an empirical program that optimizes uniaxially loaded joints by calculating stress concentration factors based on experimental data[28]. Member stresses are then obtained by substituting the factors into empirical equations. The simplicity of this program permits rapid joint optimization once laminate properties are known. However, this simplicity makes JOINT inconvenient for laminate optimization since it requires experimental data on each laminate.

3.2.2 A4EJ

A4EJ analyzes multi-row joints using a continuum mechanics approach. By assuming a bilinear elastic material response, load reactions are established by satisfying both equilibrium and compatibility requirements[29]. This approach for analyzing uniaxial mechanical joints is faster than finite elements, but requires substantial user input, including an initial guess of the fastener reactions. Furthermore, A4EJ does not account for joint bending or fastener rotation, which limits its use to double shear analysis.

3.2.3 SAMCJ

SAMCJ calculates fastener load distributions amongst multi-row joints subjected to a two-dimensional stress field[30]. Using special finite elements, fastener loads are calculated through the application of a unit load and compliance with compatibility equations. To simplify analysis, a cosinusoidal fastener load distribution, bilinear in behaviour, is assumed. The most significant limitation of SAMCJ is that it is slow compared to other programs.

3.2.4 BOLTFAST

Unlike the previous programs, BOLTFAST is a post-processing program which calculates fastener loads and the associated stress fields, but is incapable of analyzing joint strength[31]. Using data from a NASTRAN output file, the program determines

which fasteners are connected to composite laminates, and obtains the average stress within adjoining plate elements. Stress reduction factors are then calculated to compensate for the difference between open hole and pin-loaded cases. This program does not output the results directly. Rather it acts as the front end to another program, CFAN (section 3.3.2), which analyzes the data to establish failure loads and modes.

3.2.5 Other Methods

Other analytical programs such as those developed by Xiong[33], and Fan & Qiu[34] are useful for determining fastener loads and joint strength. However, like most applications, they are limited to specific conditions. A more versatile approach is that of finite element analysis. The recent increase in computational power has made FEA a viable technique for obtaining load distributions within complex multi-fastener joints[35]. The drawback to FEA is that reliable material data is required. This data is readily available for metallic structures, but limited for composite materials because of the numerous stacking sequences and material systems possible. Because of material variability between theoretical and actual properties, test programs often accompany the finite element analysis to ensure model accuracy. Blackie & Chutima[36] summarize various approaches taken in the modelling of multi-fastener joints.

3.3 Detailed Stress Analysis

Once individual fastener loads have been calculated, analytical and computational methods are applied to calculate the stress field around the fastener. Many of the techniques listed in section 3.2 are applicable to this section since they are often extensions of those listed herein which are limited to single fastener analysis. Camanho & Matthews[5] provide a detailed summary of analytical and numerical methods used in the analysis of single fastener joints.

Closed form solutions such as those developed by Lekhnitskii[37], Savin[38], De Jong[39], and Klang & Hyer[40] use complex variable theory to describe the stress state surrounding pin-loaded holes. Although complex variable methods are efficient and

accurate, they increase in complexity with the degree of realism. To simplify the analysis, fasteners are assumed to be rigid and of snug fit, while the bearing loads are assumed to have a cosinusoidal pressure distribution.

Stress states for complex loading configurations, such as bearing/bypass, are obtained by using the principle of elastic superposition. This principle suggests that for materials loaded within their elastic regime, a complex load can be represented by the sum of the individual loads. For example, a bearing/bypass load can be represented by a notched plate under uniaxial loading, and a plate under pure bearing (see Figure 3.2). Each problem is solved individually. The stress resultants are then combined to yield the solution to the original problem.

Programs such as SASCJ[30], BJSFM[32], and CFAN[31] make use of the above concepts to simplify single fastener analysis.

3.3.1 SASCJ and BJSFM

SASCJ and BJSFM are single fastener analysis codes that use anisotropic elasticity theory to calculate the stress fields of a joint subjected to biaxial loading. The two-dimensional stress field is obtained using an Airy stress function, $F(x,y)$, which satisfies force and displacement equilibria throughout the joint. Fastener loads are then applied assuming a cosinusoidal distribution along the hole boundary. Mapping functions and boundary collocation techniques are applied to account for finite geometry effects and reduce computational time.

3.3.2 CFAN

CFAN is an analysis program which modifies Hart-Smith's C-factor theory to predict joint strength under biaxial loading. Originally proposed for uniaxial pin-loaded joints, the C-factor theory calculates an effective stress concentration along the hole edge in terms of the open hole tangential stress concentration factor[41]. Using superposition, CFAN breaks complex load cases into simpler ones for which known solutions exist.

Each case is solved individually, and then combined to yield an overall solution which is compared to net-section allowables. Failure occurs when an allowable is exceeded. The failure mode is determined from the angle at which failure occurred. Since this method is based on tangential stress and not radial, this program is more accurate when predicting net-section failures.

3.3.3 Finite Element Analysis

Finite element analysis has been extensively used to analyze and predict the behaviour of mechanically fastened joints. Crews et al.[9] examined geometric effects on the stress distribution around a loaded hole, while Rowlands et al.[42], and Rahman et al.[43] examined friction, bolt clearance, and finite geometry. Matthews et al.[44], and Smith et al.[45] examined the effect of preload on bolted joints, while non-linear material behaviour was studied by others[46,47,48]. Results were comparable to other techniques; however, the versatility of FEA permits the inclusion of many parameters which can not be handled by other techniques.

3.4 Strength Prediction

Once the stresses surrounding the fastener are known, failure analysis is performed to define the safe operating region of the joint. Nahas[23], Rowlands[49], and Camanho & Matthews[5] surveyed over 30 failure theories applicable to isotropic and composite materials.

3.4.1 Failure Criteria

Most failure criteria are derivatives of homogeneous isotropic theories, modified to account for the non-homogeneous and anisotropic nature of composite materials. Presently, only a handful of theories are commonly used, and are discussed in the following sections.

3.4.1.1 Maximum Strength Failure Criteria

Maximum Strength Criteria use lamina strength allowables, often the ultimate material strengths, to define the failure envelope. Maximum Stress[23] is one such criterion, and defines the envelope as follows

$$\begin{aligned}\sigma_1 &= X, \quad \text{or} \quad X_c \\ \sigma_2 &= Y, \quad \text{or} \quad Y_c \\ \tau_{12} &= S\end{aligned}\tag{eqn. 3.1}$$

where σ_1 , σ_2 , and τ_{12} are the lamina principal stresses. X, Y, and S are the lamina allowables, with the subscripts denoting tensile or compressive.

Ply failure occurs when a principal stress exceeds its allowable, whether in tension or compression. The failure envelope is a square with unequal distributions in each of the four quadrants of plane stress. If the compressive and tensile lamina properties are the same, the square is centred about the origin. Since this method does not consider an interaction between applied stresses, it permits the distinction between fibre and matrix failures.

Other theories categorized as *Maximum Strength Criteria* are Maximum Shear Stress[23], and Maximum Strain[23], and are of similar form to that of equation 3.1.

3.4.1.2 Interaction Failure Criteria

Interaction Failure Criteria suggest that failure is governed by a combination of the principal stresses, and not individual stresses as implied by the *Maximum Strength Criteria*. Most interaction theories predict ply failure when the sum of its terms exceeds unity.

One such criterion is that proposed by Tsai-Hill[50]. By extending the Von-Mises-Hencky maximum distortional energy theory to anisotropic materials, Tsai-Hill arrived at the following equation

$$\left(\frac{\sigma_1}{X}\right)^2 + \left(\frac{\sigma_2}{Y}\right)^2 - \left(\frac{1}{X^2}\right) \cdot \sigma_1 \sigma_2 + \left(\frac{\tau_{12}}{S}\right)^2 \leq 1 \quad \text{eqn. 3.2}$$

Since this criterion does not differentiate between tensile and compressive strengths, the failure envelope is a continuous curve centred about the origin. Furthermore, the interaction term complicates the determination of failure mode.

Another criterion based on the Von-Mises-Hencky maximum distortional energy theory is that proposed by Norris[51], which provides a more generalized theory. Fischer[52] modified Norris' theory to account for lamina anisotropy, and obtained the following

$$\left(\frac{\sigma_1}{X}\right)^2 + \left(\frac{\sigma_2}{Y}\right)^2 - \left(\frac{E_{11}(1+\nu_{21}) + E_{22}(1+\nu_{12})}{2\sqrt{E_{11}E_{22}}(1+\nu_{12})(1+\nu_{21})}\right) \cdot \frac{\sigma_1 \sigma_2}{XY} + \left(\frac{\tau_{12}}{S}\right)^2 \leq 1 \quad \text{eqn. 3.3}$$

The interaction term is multiplied by a correction factor which is a function of the lamina elastic constants E_{11} , E_{22} , ν_{12} , and ν_{21} . For isotropic materials, this factor becomes unity, making the Fischer criteria identical to that proposed by Norris.

An attempt to include different lamina compressive and tensile strengths was proposed by Hoffman[53].

$$\frac{\sigma_1^2 - \sigma_1 \sigma_2}{X_c X_t} + \frac{\sigma_2^2}{Y_c Y_t} + \left(\frac{X_c - X_t}{X_c X_t}\right) \cdot \sigma_1 + \left(\frac{Y_c - Y_t}{Y_c Y_t}\right) \cdot \sigma_2 + \left(\frac{\tau_{12}}{S}\right)^2 \leq 1 \quad \text{eqn. 3.4}$$

Hoffman's criterion differentiates between lamina strengths by incorporating the compressive and tensile stresses directly into the equation. This produces a continuous curve which bounds the failure region, but is offset from the origin.

3.4.1.3 Tensor Polynomial Failure Criteria

Tensor Polynomial Criteria are further refinements of the *Interaction* category, but use biaxial data to obtain better representations of failure. Similar to the *Interaction Criteria*, *Tensor Polynomial Criteria* only predict ply failure, not the mode. Few theories are available, but the most popular are those by Gol'denblat & Kopnov[23], Tsai-Wu[54], and Tennyson et al.[55].

Gol'denblat & Kopnov formulated one of the first generalized anisotropic failure theories. By equating strength tensors to engineering strengths, their criterion took on the form

$$\begin{aligned} & \frac{1}{2} \left(\frac{1}{X_t} - \frac{1}{X_c} \right) \cdot \sigma_1 + \frac{1}{2} \left(\frac{1}{Y_t} - \frac{1}{Y_c} \right) \cdot \sigma_2 \\ & + \sqrt{\frac{1}{4} \left(\frac{1}{X_t} - \frac{1}{X_c} \right)^2 \cdot \sigma_1^2 + \frac{1}{4} \left(\frac{1}{Y_t} - \frac{1}{Y_c} \right)^2 \cdot \sigma_2^2 + 2 \cdot F_{12} \cdot \sigma_1 \sigma_2 + \left(\frac{\tau_{12}}{S} \right)^2} \leq 1 \end{aligned} \quad \text{eqn. 3.5}$$

The biaxial interaction term, F_{12} , is material and stacking sequence dependant, and hence must be obtained experimentally.

Tsai-Wu developed a simplified quadratic tensor to predict the behaviour of anisotropic materials. Using this theory, ply failure did not occur as long as the following condition was satisfied

$$F_1 \cdot \sigma_1 + F_2 \cdot \sigma_2 + F_{11} \cdot \sigma_1^2 + F_{22} \cdot \sigma_2^2 + F_{12} \cdot \sigma_1 \sigma_2 + F_{66} \cdot \tau_{12}^2 \leq 1 \quad \text{eqn. 3.6}$$

where,

$$\begin{aligned} F_1 &= \frac{1}{X_t} + \frac{1}{X_c} \quad , \quad F_2 = \frac{1}{Y_t} + \frac{1}{Y_c} \\ F_{11} &= \frac{1}{X_t X_c} \quad , \quad F_{22} = \frac{1}{Y_t Y_c} \\ F_{66} &= \frac{1}{S} \\ F_{12} &= \text{experimentally determined} \end{aligned} \quad \text{eqn. 3.7}$$

Similar to Gol'denblat & Kopnov, the F_{12} tensor must be established from experimental data, and thus restricts its use for laminate optimization.

Empirical relationships have been proposed for F_{12} to resolve this limitation, but studies report little difference for various values of F_{12} [56]. Most criteria can be expressed in tensor form, with the only difference between them being the treatment of the F_{12} term.

For example, setting $F_{12}=1/(2X_cX_c)$ transforms equation 3.6 into the criterion proposed by Hoffman (equation 3.4).

To improve failure predictions, Tennyson et al.[55] proposed a third order polynomial tensor criterion. Although this approach resulted in an improvement over other tensor polynomial criteria, the additional biaxial interaction terms makes this criterion inconvenient.

3.4.1.4 Laminate Failure Criteria

Early prediction theories focused on the strength and failure of individual lamina to establish laminate failure. To improve failure prediction, Guess-Gerstle[57], and Puppo & Evensen[58] proposed strength theories based on laminate rather than lamina properties. Guess-Gerstle proposed a failure theory identical to that of Norris except that laminate strengths were used instead of lamina strengths. Puppo & Evensen adopted a similar theory, but assumed the laminate to be homogeneous and anisotropic. Their criterion was

$$\begin{aligned} \left(\frac{\sigma_x}{X'}\right)^2 - \gamma \cdot \left(\left(\frac{X'}{Y'} \cdot \frac{\sigma_x}{X'} \cdot \frac{\sigma_y}{Y'} \right) + \left(\frac{\sigma_y}{Y'} \right)^2 \right) + \left(\frac{\tau_{xy}}{S'} \right)^2 &\leq 1 \\ \left(\frac{\sigma_y}{Y'}\right)^2 - \gamma \cdot \left(\left(\frac{Y'}{X'} \cdot \frac{\sigma_x}{X'} \cdot \frac{\sigma_y}{Y'} \right) + \left(\frac{\sigma_x}{X'} \right)^2 \right) + \left(\frac{\tau_{xy}}{S'} \right)^2 &\leq 1 \end{aligned} \quad \text{eqn. 3.8}$$

where,

$$\gamma = \frac{3 \cdot (S')^2}{X'Y'} \quad \text{eqn. 3.9}$$

σ_x , σ_y , and τ_{xy} and X' , Y' , and S' are the laminate stresses and laminate strength allowables, respectively. Equation 3.8 implies that no single continuous closed surface can adequately describe the failure zone; rather, it is comprised of the intersection of several surfaces in stress space.

3.4.1.5 Fracture Mechanics Failure Criteria

Waddoups, Eisenmann, & Kaminski[59] applied the concept of linear elastic fracture mechanics (LEFM) to predict the failure of notched laminates. The authors assumed an intense energy region extended from the hole, perpendicular to the loading direction. This energy region was modelled as a crack of length 'a' (see Figure 3.3). The notched strength reduction (NSR) factor was defined as follows

$$\frac{\sigma_N^\infty}{\sigma_o} = \frac{1}{f(a/R)} \quad \text{eqn. 3.10}$$

where σ_N^∞ , and σ_o are the notched and unnotched laminate strengths, and $f(a/R)$ is a stress intensity parameter based on the works of Paris & Sih[60]. The value of 'a' was found to be constant for different hole sizes, and thus assumed a material characteristic. However, the parameter 'a' varies with orthotropy, and therefore must be obtained experimentally for each laminate.

Whitney & Nuismer[61,62] developed two uniaxial criteria similar to that proposed by Waddoups et al.. The first approach was the Point Stress Criterion which assumed that laminate failure occurred when the stress at a characteristic distance, d_o , exceeded the unnotched laminate strength. This produced an NSR in the form

$$\frac{\sigma_N^\infty}{\sigma_o} = \frac{2}{2 + \xi_1^2 + 3 \cdot \xi_1^4 - (K_T^\infty - 3)(5 \cdot \xi_1^6 - 7 \cdot \xi_1^8)} \quad \text{eqn. 3.11}$$

where,

$$\xi_1 = \frac{R}{R + d_o} \quad \text{eqn. 3.12}$$

Their second criterion was the Average Stress Criterion, which considers that failure is caused by the average stress over a characteristic distance, a_o , exceeding the laminate strength. The NSR was thus expressed as

$$\frac{\sigma_N^\infty}{\sigma_o} = \frac{2(1 - \xi_2)}{2 - \xi_2^2 - \xi_2^4 + (K_T^\infty - 3)(\xi_2^6 - \xi_2^8)} \quad \text{eqn. 3.13}$$

where,

$$\xi_2 = \frac{R}{R + a_0} \quad \text{eqn. 3.14}$$

In both cases, K_T^∞ represents the open hole stress intensity factor, and is expressed by the following equation

$$K_T^\infty = 1 + \sqrt{2 \cdot \left(\sqrt{\frac{E_{yy}}{E_{xx}}} - \nu_{xy} \right) + \frac{E_{yy}}{G_{xy}}} \quad \text{eqn. 3.15}$$

where E_{xx} , E_{yy} , G_{xy} , and ν_{xy} are the laminate engineering constants. For isotropic materials, equation 3.15 equals 3, and hence cancels out of equations 3.11 and 3.13. Similar to Waddoups et al., the characteristic distances were assumed material constants, but dependent on laminate stacking sequence. The Average Stress Criterion has been used by numerous authors in the study of mechanically fastened joints[63,64,65,66,67].

Modifications of the previous failure criteria have been performed to improve on deficiencies. Poe & Sova[68] accounted for the nonlinear behaviour of various laminates by developing a LEFM strain failure criterion. Others such as Karlak[69], and Pipes et al.[70], modified the Point Stress Criterion after observing that the characteristic length, d_0 , was not a material constant.

3.4.2 Laminate Failure Analysis

The failure criteria discussed in section 3.4.1 fall under two categories: lamina failure criteria, and laminate failure criteria. The primary difference between lamina and laminate failure criteria is how the laminate failure load is obtained.

3.4.2.1 Lamina Failure Criteria

Lamina failure criteria are those discussed in sections 3.4.1.1 to 3.4.1.3. These theories are based on the concept that laminate failure is a function of the individual plies, and not the collective laminate. Two approaches presently exist for predicting laminate failure: first ply, and progressive (last ply).

The first ply approach assumes that laminate failure occurs when the first ply fails. For laminates experiencing uniform through-thickness loads, this is a suitable approach, since the failure of a single ply implies failure of all plies of that orientation. However, under certain circumstances, this approach underpredicts laminate strength. An example of this is when the orientation of the first failed ply does not make up a significant portion of the laminate. Another example is single shear lap joints in which a non-uniform through-thickness stress exists. In these cases, the laminates are able to sustain additional loading after first ply failure.

To improve strength predictions, a progressive failure approach was adopted. This method assumes that upon failure of a ply, its contribution to the laminate properties is completely removed. Loads are progressively increased, and the laminate properties are recalculated for each failed lamina. Laminate failure then results when the last ply fails. However, depending on the ply failure mode, ply contributions to the laminate properties may still exist. Therefore, this approach can result in conservative strength predictions.

3.4.2.2 Laminate Failure Criteria

Laminate failure criteria are represented by criteria from sections 3.4.1.4 and 3.4.1.5. These failure theories assume that laminate failure occurs when the criteria fails to be satisfied.

3.5 Discussion

The design methodology for mechanical fasteners involves four key stages. The complexity of mechanically fastened joints requires the use of simplifying assumptions to make the problems more manageable. These assumptions, although valid for isotropic materials, tend to be less than accurate for composite materials. Therefore, when analyzing joints involving composite materials, one must ensure the assumptions are valid.

Numerous failure criteria exist, and are classified into several categories. Some rely upon lamina failure to predict that of the laminate, while others use a fracture mechanics approach to determine failure. However, the vast majority are derived from theories of isotropic origin, and typically require correlation against experimental data to obtain reliable results. Therefore, once correlated, these criteria are unique to the material system and stacking sequence. This makes them inconvenient for use in laminate optimization.

4. PROJECT DEFINITION

4.1 Problem Statements

Current design methodologies use uniaxial strength data and orthotropic failure criteria to predict the failure of composite laminates. Such methodologies are sufficient for uniaxially loaded structures, but not for those experiencing multi-axial stress fields. Empirical interaction terms exist to improve predictions for biaxial stress fields, but due to the cost and complexity of biaxial testing, this data is limited.

4.2 Objectives

The objectives of the work undertaken are as follows:

1. to obtain single shear bearing strength data for orthotropic laminates in the four quadrants of plane stress,
2. to investigate the influence of stress state and bypass load on the laminate bearing strength,
3. to develop a finite element model to supplement experimental findings, and
4. to develop an empirical technique for predicting the onset of bearing failure.

4.3 Approach

The approach adopted to satisfy the first two objectives was to simulate a wing skin subjected to a mechanically fastened repair. The specimen design, previously used in biaxial bearing testing, permitted the application of in-plane biaxial loads. Bearing loads were furnished by a single titanium blind rivet which connected a titanium patch to the specimen. This configuration was typical of bolted structural repair.

A test matrix, requiring the application of various bearing, bypass, and biaxial loads was developed. To increase the sampling size, specimens were tested more than once using progressively larger fasteners. This approach required that bearing failure be detected as

early as possible. Several uniaxial tests were also performed to serve as a baseline for comparison.

The third objective was achieved by performing 2-D and 3-D finite element analysis of the specimen and joint configuration. Using this information, along with experimental data, the fourth objective was completed with the development of a preliminary empirical failure prediction method.

5. SPECIMEN SELECTION

Biaxial testing is a complex process in which results are dependent on the test method and specimen geometry. Since the specimen contributes to the quality of data obtained, the following features are desirable:

1. the gauge section produces a uniform stress under any loading combination,
2. the specimen can be loaded under various conditions of plane stress,
3. failure initiates in the centre of the gauge section,
4. a large test section is available to minimize edge effects, and
5. a flat test section.

Over the past several decades, numerous specimen designs have evolved, with some being more successful than others[71,72,73,74]. The most common configurations are the off-axis, tubular, and cruciform specimens.

5.1 Specimen Configurations

5.1.1 Off-Axis Specimen

Off-axis specimens are used because of their simplicity and cost effectiveness in obtaining pseudo-biaxial data. By applying unidirectional loads offset from the principal fibre axis, a biaxial stress state is induced from shear stresses (see Figure 5.1). Although popular, off-axis specimens are restricted to a few biaxial ratios within the tension-tension quadrant. Furthermore, they have small gauge areas due to loading constraints and geometry effects. However, loading constraints and geometry effects can often be minimized by increasing the specimen length between grips.

5.1.2 Tubular Specimen

Tubular specimens are commonly used for biaxial testing since they can be tested in all four quadrants of plane stress and at any biaxial ratio. Additionally, they are suitable for compression testing due to their inherent stability[75,76]. However, the curvature which gives the specimen its stability, limits its use to thin laminates because of through-thickness stress variations (see Figure 5.2). Tubular specimens are loaded through a combination of axial load and internal pressure. End caps are used to close the pressure vessel, but this induces stress concentrations which cause failure outside the gauge area. This problem has been addressed by Swanson & Christoforou[77], and Duggan & Bailie[78] through the development of a specialized cap/specimen interface.

5.1.3 Cruciform Specimen

Flat cruciform specimens, regardless of thickness, can be tested at any biaxial ratio or plane stress quadrant. However, difficulty occurs in obtaining a uniform stress state over a sufficient area. This was overcome by Daniel[79], Monch & Galster[80], and Lucking[24,81]. Both Daniel, and Monch & Galster machined axial slots into the specimen arms to achieve a reduced lateral stiffness while maintaining axial strength (see Figure 5.3). This produced a large region of uniform stress; however, failure occurred in the arms, not the test section as desired. Lucking's approach reduced the lateral stiffness of the arms by truncating all non-axial plies and replacing them with an omni-directional adhesive. This improved the uniformity of the stress state within the test section, while minimizing axial strength loss.

5.2 Specimen Selection

Based on Lucking's design, a 48 ply, 17" flat hybrid cruciform with an 8" x 8" test section, was fabricated from graphite/epoxy prepreg (AS4/3501-6) and FM300K film adhesive. The constituent material properties are listed in Table 5.1, while the specimen configuration and stacking sequence is shown in Figure 5.4. The 8" x 8" test section used 48 plies of AS4/3501-6, while the arms were a combination of FM300K and AS4/3501-6.

The arms consisted of two regions - a gripping region, and a load transfer region. The gripping region was designed to withstand the forces generated by the gripping mechanism and hence used only AS4/3501-6 plies. The load transfer region contained axially oriented AS4/3501-6 plies and omni-directional FM300K adhesive. This arrangement decreased lateral stiffness while maintaining maximum strength in the longitudinal direction. The film adhesive layers maintained section thickness and ply planarity, and were assumed to contribute negligible longitudinal strength.

Properties	AS4/3501-6 [82]	FM300K [24]
E_{11}	21.32E6 psi	0.71E6 psi
$E_{22} = E_{33}$	1.31E6 psi	0.71E6 psi
$G_{12} = G_{13}$	0.73E6 psi	57.86E3 psi
G_{23}	0.44E6 psi	57.86E3 psi
$\nu_{12} = \nu_{13}$	0.3	0.3
ν_{23}	0.42	0.3

Table 5.1 : Lamina Engineering Properties

5.3 Specimen Manufacturing

5.3.1 Fabrication

The original procedure for specimen fabrication, developed by Lucking[24,81], was unsuitable since it used a hot press for curing which was unavailable for this project. Therefore, an autoclave curing procedure, more representative of airframe construction in industry, was developed.

In a collaborative effort between SMPL-IAR, Carleton University, and Spar Aviation Services (formerly CAE Aviation in Edmonton), a reliable procedure was developed for

the fabrication of cruciform specimens. The complete fabrication and curing procedures are described in Appendix A.

A total of 27 specimens were manufactured over a period of 12 months. The first 3 were used to develop and validate the layup procedure. Batch I (#945 to #950) was a pre-production group in which the bagging sequence was refined to obtain the desired laminate quality. Batch II (#951 to #959) was the first of two production groups, and consisted of 9 specimens. The final group (Batch III - #960 to 968) was fabricated using a different procedure than Batch II due to a shortage of 0.05 psf adhesive material. This procedure involved substituting two 0.05 psf plies with one 0.08 psf ply.

5.4 Specimen Quality and Verification

Specimen quality was compared against ASTM standards and the material manufacturer's requirements (Hercules Inc.). Furthermore, test requirements dictated that a uniform test section of adequate size be obtained. Parameters found to influence specimen quality were thickness and planarity. Variations in either of these induced bending stresses within the test section.

5.4.1 Thickness Tolerances

ASTM D5687/D5687M-95 specifies that thickness variations of 0.005 inches within a 1 inch radius are permissible[83]. In addition, the manufacturer requires a laminate thickness between 0.230" and 0.269" to achieve optimum mechanical properties[84].

Thickness measurements were made using a deep throat micrometer with 1/4" diameter spherical tips. All but two specimens met the thickness requirements as specified by Hercules Inc. (see Figure 5.5). Batch I specimens were near the upper bound of the thickness limit due to inadequate bleed during cure. Specimens from Batch II had an average laminate thickness in the middle of the limits, while Batch III specimens were close to the lower limit because of the change in layup procedure.

Likewise, all but two specimens met the ASTM thickness requirements. These failures were caused by local indentation within the test section. Although these indentations normally constitute specimen rejection, their location was remote from the area of interest. Hence, a decision was made to test these specimens.

5.4.2 Planarity Tolerances

ASTM D5687/D5687M-95 states that the planarity tolerance is a function of laminate thickness, requiring smaller tolerances for thinner laminates. A planarity tolerance of $\pm 0.02''$ is suggested for a $0.25''$ thick laminate to achieve negligible bending[83]. Therefore, a tolerance of $\pm 0.02''$, or $\pm 8\%$ of laminate thickness, was selected. Planarity measurements were obtained by measuring the specimen height above a fixed reference, and then subtracting the respective thickness from the height.

The three pre-batch specimens (#942 to #944) were produced to resolve fabrication problems. These specimens exhibited an average planarity tolerance of $0.034''$, or 13% of the laminate thickness. Since this was in excess of the 8% limit, the bagging procedure was modified to bleed from both sides during cure. This improved the planarity for the remaining production specimens. Specimens #945 to #968 showed typical planarity tolerances of $0.017''$, which is within the 8% requirement (see Figure 5.6). Although some specimens were outside of these tolerances, they were still tested due to the limited number of specimens available.

5.4.3 Verification of Stress Uniformity Within the Test Section

Specimen verification was performed to ensure the new fabrication procedure did not alter specimen performance from that of the original. Using experimental and numerical methods to establish test section uniformity, results were compared against those available in literature.

5.4.3.1 Experimental Verification

Verification of the test section for strain uniformity was conducted on specimen #944 using a photoelastic coating and a biaxial strain gauge. Using a 6" x 6" x 0.080" PS-1B photoelastic coating ($K=0.15$ and $f=925 \mu\epsilon/\text{fringe}$), a global strain field was obtained for uniaxial and biaxial loads of 10 kips (see Figure 5.7a and b). A biaxial strain gauge was located at the test section centre, and was used to confirm photoelastic results.

Photoelastic results for specimen #944 are listed in Table 5.2. Strain gauge readings yielded a principal strain difference of $857 \mu\epsilon$ and $93 \mu\epsilon$ under uniaxial and biaxial loading, respectively.

Dimensions of Photoelastic Area Analyzed	Principal Strain Difference for Uniaxial Load	Principal Strain Difference for Biaxial Load
1" x 1"	$725 \pm 4\% \mu\epsilon$	$175 \pm 14\% \mu\epsilon$
2" x 2"	$725 \pm 4\% \mu\epsilon$	$175 \pm 14\% \mu\epsilon$
3" x 3"	$725 \pm 4\% \mu\epsilon$	$175 \pm 14\% \mu\epsilon$
4" x 4"	$725 \pm 7\% \mu\epsilon$	$175 \pm 29\% \mu\epsilon$
5" x 5"	$725 \pm 17\% \mu\epsilon$	$175 \pm 43\% \mu\epsilon$

Table 5.2 : Photoelastic Survey for Specimen #944 at 10 kips

Using a similar setup, Hoa examined the test section uniformity of Lucking's cruciform design. Hoa obtained strain variations of $\pm 3.7\%$ for a 2" centrally located square, and $\pm 11\%$ for a 4" square, under uniaxial loading[24]. No results were reported for biaxial loads in reference 24.

5.4.3.2 Finite Element Verification

A finite element model was developed for an unnotched cruciform specimen to compare theoretical strains against those obtained experimentally. The model used 2-D QUAD 4 isoparametric shell elements assigned composite properties (see Figure 5.8). These properties are reported in Table 5.1. Both uniaxial and biaxial load cases were modelled,

with boundary conditions representative of the photoelastic analysis (Specimen #944). Results obtained from the finite element analysis are presented in Table 5.3.

Hoa's finite element model was similar to that used in this program, but was modelled as doubly symmetric. Therefore, only one quarter of the total specimen geometry was modelled. Under a uniaxial load, Hoa reported strain variations of $\pm 2.1\%$ and $\pm 5.1\%$ for 4" and 6" widths, respectively[24].

Dimensions of FEM Area Analyzed	Principal Strain Difference for Uniaxial Load	Principal Strain Difference for Biaxial Load
1" x 1"	$728 \pm 0\% \mu\epsilon$	$90 \pm 2\% \mu\epsilon$
2" x 2"	$728 \pm 2\% \mu\epsilon$	$90 \pm 6\% \mu\epsilon$
3" x 3"	$728 \pm 3\% \mu\epsilon$	$90 \pm 9\% \mu\epsilon$
4" x 4"	$728 \pm 5\% \mu\epsilon$	$90 \pm 12\% \mu\epsilon$
5" x 5"	$728 \pm 7\% \mu\epsilon$	$90 \pm 12\% \mu\epsilon$

Table 5.3 : FEA Survey for Cruciform Specimen at 10 kips

5.4.4 Discussion

Comparison between experimental, numerical, and analytical results are shown in Table 5.4. The values in brackets are the percent difference from the strain gauge results which were used as the baseline for comparison.

Load Case	Strain Gauge	Photoelastic	CLPT	FEA
Uniaxial*	857 $\mu\epsilon$	725 $\mu\epsilon$ (-15.4%)	749 $\mu\epsilon$ (-12.6%)	728 $\mu\epsilon$ (-15.1%)
Biaxial*	93 $\mu\epsilon$	175 $\mu\epsilon$ (88.2%)	111 $\mu\epsilon$ (19.3%)	90 $\mu\epsilon$ (-3.2%)

Table 5.4 : Comparison of Test Data for Specimen #944 at 10 kips at Centre of Test Section

* Values are the difference between the principal strains.

Values for the principal strain difference were in good agreement for the uniaxial case, with errors between methods around 15%. Biaxial results agreed with Classic Laminate Plate Theory (CLPT) and finite element analysis. However, larger errors existed between the baseline and photoelastic results. The source of this error lies in that photoelasticity indicated a 5° inclination of the principal axis to the 0° laminate orientation. Because strain gauge, CLPT, and FEA data were obtained along the 0° and 90° laminate axes, they underpredicted the strain.

The stress state uniformity of the present specimen was compared against results available in literature[24,81]. Photoelastic analysis of Specimen #944 demonstrated a strain variation of 7% within a 4" square region when subjected to a uniaxial load. For a biaxial load, a 14% strain variation was observed within a 3" square region. Beyond these dimensions, the variations increased rapidly. Uniaxial results agreed with those obtained by Hoa, who obtained a 12% strain variation within a 4" square[24]. Hoa did not evaluate the specimen under a biaxial stress state, and so no comparison exists.

Finite element analysis of Specimen #944 was also performed for evaluation purposes. Numerical results showed that within a 5" square, strain variations did not exceed 7% for uniaxial loads, while variation up to 12% existed over the same region for biaxial loads. In comparison, Hoa's uniaxially loaded model demonstrated better uniformity throughout the test section, obtaining half the strain variation of the present model[24]. Again, no biaxial results were presented. The difference between models is likely due to mesh refinement, and lamina properties. Although Hoa's mesh was similar, subtle differences in element density and biasing existed, specifically in the transition region between the arms and the test section. Furthermore, the lamina properties used by Hoa varied from those in this report by 3-12%. The effect of material property variations on stress uniformity is unknown, but appears to be significant.

Uniaxial experimental results showed greater strain variation than those from FEA. This was expected since the model assumes perfect ply alignment and bonding. The differences between experimental and FEA results are small, except for the 5" square in which experimental variations doubled those of the FE model. Since the 5" dimension is 83% of the photoelastic coating width, then some difference is attributable to edge effects.

6. BEARING FAILURE ONSET DETECTION

To establish bearing failure for single shear CFRP joints requires that bearing failure be clearly defined. Unlike net-section and shearout failure modes which are defined by catastrophic failure, bearing failures are more subtle. Several definitions exist in literature for establishing bearing failure. The following section discusses the various definitions.

6.1 Ultimate Sustainable Load

The simplest definition is that of ultimate sustainable load, in which bearing failure occurs at the maximum load obtained from a load/displacement plot (see Figure 6.1, point a).

Wichorek[85] applied this definition when analyzing single bolt joints in quasi-isotropic laminates. No interpretation of the failure point was required. Because of the considerable specimen damage, the failure mode was determined from the examination of the specimen and the load/displacement data collected.

6.2 First Peak Load

The first peak load definition considers specimen failure once the load-displacement plot peaks for the first time (see Figure 6.1, point b), and is analogous to the yield point observed in ductile materials.

This definition was successfully applied by Quinn & Matthews[10] in their study of pin-loaded glass fibre-reinforced plastics (GFRP). The authors concluded that this definition was repeatable, but the failure point was associated with gross damage onset. Stockdale & Matthews[86] obtained similar conclusions when using the first peak load definition to determine the effect of clamping pressure on bolted 0°/90° GFRP laminates.

6.3 Percent Hole Elongation

The percent hole elongation definition is the most widely used. This definition states that bearing failure corresponds to a specific amount of hole elongation as measured by a parallel offset in the load/displacement plot (see Figure 6.1, point c).

MIL-HDBK-17-1E defines bearing failure as a 4% elongation of the original hole diameter. However, work by Johnson & Matthews[27] found that bearing damage began in a $[0/\pm 45/90]_s$ GFRP laminate around 0.4 % hole elongation, while Althof and Müller[87] suggested 0.5% and 1% for CFRP and boron fibre-reinforced plastic (BFRP) materials, respectively. In addition, test from Johnson & Matthews[27] indicate that at 0.4% hole elongation, the amount of damage was greater for a $[0/90_2/0]_s$ than a $[0/\pm 45/90]_s$ laminate. Therefore, the amount of elongation which defines failure appears to be a function of material system and orthotropy.

6.4 Peak Acoustic Activity

Before any significant hole elongation is observed, the brittle matrix fractures, producing compression waves which propagate through the laminate. The presence of these waves is referred to as acoustic activity, with bearing failure defined as the point of peak acoustic activity. However, two definitions exist for peak acoustic activity. The first is the load at which the largest amplitude is observed, and the second is the load at which the maximum number of consecutive “hits” occurs within a given time. Furthermore, the location of this point on Figure 6.1 is unknown as it is dependant on equipment setup.

Hoa[24] relied upon peak acoustic activity, identified as the maximum “hits”, to determine the influence of lateral loads on the bearing strength of CFRP laminates. Using this approach, Hoa obtained repeatable results. However, corresponding laminate damage was significant, often in excess of 25% of the hole diameter.

6.5 Bearing Strain

A novel definition for bearing failure onset is based upon a change in laminate stiffness arising from lamina failure[88]. When a ply fails, it causes a decrease in the local stiffness. For a constant stress, this results in a local strain increase. Meanwhile, the gross section stiffness, and hence strain, is unaffected by the local failure. The location of this point on Figure 6.1 is dependant on the ability to detect this change in stiffness, and is a function of the technique used, the failure mode, lamina moduli, and stacking sequence.

Since bearing failures are localized in nature, the detection technique must be a local method, and not global. This suggests the use of strain gauges. However, the gauge must be sufficiently small to approximate a point response. Furthermore, the gauge must be close to the failure region, and aligned with the direction of changing stiffness. Methods not satisfying these requirements result in the local stiffness change being blended in with the global stiffness. Therefore, for bearing failures, the use of a small gauge located at the hole edge, and oriented along the bearing axis, would yield the best results.

The ability to detect bearing failure is also influenced by the laminate failure mode, which alters the behavior and magnitude of the stiffness change. The common failure modes are matrix failures, fibre failures, and ply delaminations. Matrix and fibre failures occur instantaneously, and consequently produce instantaneous stiffness changes. Conversely, ply delaminations are a slow growth process, thereby resulting in a gradual stiffness change. The difference between matrix and fibre failures is the magnitude of the stiffness change. Typically a matrix failure causes a small stiffness change in the bearing direction, while a fibre failure significantly alters the stiffness. Therefore, bearing failures are commonly identified from fibre failures.

The bearing strain approach was used by O'Brien[88] to obtain the failure of laminates under fatigue loading. A similar strain behaviour was observed by Snell & Burkitt[89] in

their testing of bearing/bypass interactions on CFRP laminates. In most cases, the authors observed instantaneous bearing strain changes just preceding the first peak load (see Figure 6.2, arrow). This was because the gauges were remote from the bearing region, and hence were detecting global stiffness changes. In some cases, however, the strain change occurred before any notable slope change in the deflection plot.

6.6 Discussion of Bearing Onset Failure Detection Methods

There are many definitions available to determine bearing failure within composite laminates, with each representing a different stage of failure. Out of the five definitions discussed previously, only the Percent Hole Elongation and Bearing Strain definitions were suitable for detecting the onset of bearing failure. The other definitions were associated with gross specimen damage, and consequently were considered as final failure definitions.

Literature suggests that for the Percent Hole Elongation definition, the amount of hole elongation is dependant on material system and stacking sequence. As no value exists for the laminate under study, it had to be determined experimentally. Furthermore, the only available method for measuring hole elongation was the actuator LVDT. This approach was questionable due to the long gauge length, material variability within this length, the loading arrangement, and the biaxial stress state. It was uncertain how these factors might influence the bearing failure onset detection.

The Bearing Strain definition is considered suitable for detecting bearing failure onset since it theoretically detects the first ply failure. However, the reliability and use of this definition is unproven for bearing failures. This limitation could be overcome by comparing results against other definitions, and by performing post-test inspections to confirm damage. Another problem was that the intended test configuration prevents the strain gauges from being located near the maximum bearing region. This was of concern as it influences the ability to detect subtle failures. To counteract this problem, small strain gauges were required.

The Bearing Strain approach was selected for use as the primary detection technique since its limitations could be managed. This was accomplished by selecting a secondary method to detect bearing failure onset, by using non-destructive inspection to confirm damage, and smaller strain gauges. For this program, the secondary method was the Percent Hole Elongation.

7. TEST PROGRAM

The purpose of the test program was to obtain bearing failure data for CFRP mechanically fastened joints subjected to biaxial stress states. Using biaxial loads in conjunction with bearing/bypass loading, experimental data in all four quadrants of plane stress was collected. Bearing damage was confirmed through visual and non-destructive radiographic inspection. In some instances, destructive fractography was performed to obtain through-thickness damage profiles.

7.1 Test Apparatus

7.1.1 Loadframe

Biaxial testing was conducted at the Structures, Materials, and Propulsion Laboratory - Institute for Aerospace Research in Ottawa, Canada. A Material Testing System (MTS) biaxial loadframe was modified for the test program (see Figure 7.1). The original hydraulic, wedge-type grips were replaced with mechanical grips designed to float perpendicular to the actuator loading axis (see Figure 7.2). This eliminated transverse shear stresses arising from specimen movement relative to the loadframe centre, and prevented the application of damaging side loads to the actuators.

The four hydraulic actuators were independently controlled which permitted the application of differential loads in the bearing (0°) and lateral (90°) axes of the specimen. This setup was advantageous as it allowed bearing and bypass loads to be generated in any direction relative to the specimen axes. Actuator force and displacement information was provided by a load transducer and a LVDT. The application of differential actuator loads produced the desired bearing, bypass, and biaxial loads, while bearing reactions were provided by a complex arrangement which simulated joint stiffness (see Figure 7.3).

7.1.2 Controller

Actuators were controlled using a 4-channel, 4-station, MTS FlexTest Digital Servo Controller (DSC) (see Figure 7.4). Each channel was capable of operating an actuator in either stroke or force feedback. For testing, force feedback was employed to maintain bypass and biaxial ratios. Static conditions were simulated using bearing load rates of 10 lbf/sec and 5 lbf/sec for the 0% and 50% bypass cases, respectively.

7.1.3 Data Acquisition

While testing, experimental parameters were recorded using an MTS TestStar II controller running TestWare-SX software. Capable of recording 24 external channels at rates up to 70 kHz, only twelve channels, sampling at 120 Hz, were used for this program. Actuator force and displacement (LVDT) data was collected from each of the four channels. Raw signal data was conditioned by the FlexTest controller and then input into the TestStar II system for acquisition.

In addition to force and LVDT data, four 350 Ω foil strain gauges measured near and far field strains for failure detection and comparison against the finite element analysis. Figure 7.5 shows the location of the strain gauges, with the positions reported in Table 7.1. Two near field gauges (#2 and #3), indicated failure initiation by observing a characteristic change in strain rate. Because of the high strain gradient in the near field, small gauge lengths of 0.062" were used. The far-field gauges (#1 and #4) obtained the gross section strain, and since the strain gradient was small, larger 0.250" gauges were employed.

Strain gauge locations were governed by two physical limitations. The first was the titanium patch which damaged faying surface gauges and bridged their circuits. This required that the gauges be mounted on the outer surface. The second limitation was the size of the blind rivet head which required gauge placement away from the hole edge.

Strain signals were amplified by Pacific amplifiers, model 8250, calibrated to 1000 $\mu\epsilon$ per volt. Excitation voltages were limited to 2 volts to minimize signal drift caused by inadequate heat dissipation. Amplifier outputs were fed to the TestStar II controller for acquisition.

Hole Diameter	Gauge Centre Relative to the Hole Centre (see Figure 7.5)			
	Gauge L1	Gauge L2	Gauge L3	Gauge L4
0.200"	2.500"	-0.275"	0.275"	2.500"
0.260"	2.500"	-0.275"	0.275"	2.500"
0.310"	2.500"	-0.400"	0.400"	2.500"

Table 7.1 : Strain Gauge Placement

7.2 Fasteners

Fastener selection was representative of those used for field repair to aircraft skins. Originally NAS1671-4L-7 Jo-Bolt blind fasteners were selected for the test program. However, preliminary testing revealed these fasteners to be unacceptable due to low shear strength. Therefore, Huck UNIMATIC blind rivets were selected. These fasteners, recommended by Huck International Ltd., had a high shear strength and were designed for joining composite parts.

Three sizes of UNIMATIC fasteners were selected for this program: 0.200", 0.260", and 0.312". Fastener specifications are shown in Table 7.2.

Fastener holes in the composite specimens were dry machined on a 5-axis variable speed milling machine. Solid carbide tri-flute drills allowed one step machining within the required tolerances. Reaming was not necessary. The use of carbide tools increased their life and performance. Holes were drilled at a spindle speed of 530 rpm and a manual feed

rate. To minimize fibre splitting and breakout, Plexiglas™ sheets were firmly clamped to both sides of the laminate while drilling.

Part Number	SB100-EV06-07	UB100-EV08-07	UB100-EU10-07
Nominal Diameter	0.200"	0.260"	0.312"
Head Type	100° Flush Countersink	100° Flush Countersink	100° Flush Countersink
Grip Length	-7	-7	-7
Footprint Diameter	0.238"	0.315"	0.373"
Sleeve Material	A286 Cres Steel	A286 Cres Steel	A286 Cres Steel
Pin Material	Titanium	Titanium	A286 Cres Steel
Locking Mechanism	Mechanical	Mechanical	Mechanical
Minimum Tensile Strength	1,400 lbf	2,100 lbf	4,170 lbf
Minimum Shear Strength	2,925 lbf	5,005 lbf	7,200 lbf
Minimum Preload	140 lbf	210 lbf	417 lbf

Table 7.2 : Huck UNIMATIC Fastener Specifications

7.3 Test Program

The objective of the experimental program was to obtain bearing data for single shear bolted joints under biaxial stress fields. Since no baseline data existed for this layup, uniaxial bearing/bypass tests were also performed.

7.3.1 Test Matrices

Test matrices for uniaxial and biaxial tests are shown in Table 7.3 and Table 7.4, respectively. All bearing/bypass loads were applied inline with the specimen 0° orientation (see Figure 5.4), with positive numbers indicating a tensile load.

	Load Case	0° Axis Load	0° Bypass Load	Fastener Sizes	No. of Tests
UNIAXIAL	1	1	0%	0.200"	2
				0.260"	3
				0.312"	0
	2	1	50%	0.200"	1
				0.260"	3
				0.312"	0
	3	-1	0%	0.200"	2
				0.260"	0
				0.312"	0
	4	-1	50%	0.200"	1
				0.260"	0
				0.312"	0

Table 7.3 : Test Matrix for Uniaxial Bearing Program

	Load Case	0° Axis Load	0° Bypass Load	Biaxial Ratio (90°/0°)	90° Bypass Load	Fastener Sizes	No. of Tests
BIAXIAL	5	1	0%	1	100%	0.200"	3
						0.260"	2
						0.312"	2
	6	1	50%	1	100%	0.200"	4
						0.260"	3
						0.312"	2
	7	-1	0%	1	100%	0.200"	3
						0.260"	2
						0.312"	2
	8	-1	50%	1	100%	0.200"	3
						0.260"	2
						0.312"	2
	9	-1	0%	-1	100%	0.200"	4
						0.260"	3
						0.312"	2
	10	-1	50%	-1	100%	0.200"	4
						0.260"	3
						0.312"	2
	11	1	0%	-1	100%	0.200"	0
						0.260"	2
						0.312"	2
	12	1	50%	-1	100%	0.200"	0
						0.260"	2
						0.312"	2

Table 7.4 : Test Matrix for Biaxial Bearing Program

7.3.2 Test Procedure

Due to the cost of specimen fabrication, only 24 test specimens were made. To permit both testing in the four quadrants of plane stress, and obtain a suitable sampling size, required specimen re-use. To achieve this, hole sizes were progressively increased after each test.

Since this approach destroyed the experimental evidence, careful documentation of pre- and post-test damage was required. Pre-test radiographic inspections were performed to verify that no damage was present after machining. Any damage was evaluated as to what effect it could have on subsequent tests. Post-test damage was recorded using radiographic imaging.

The procedures for specimen installation and removal are provided in Appendix B.

8. EXPERIMENTAL OBSERVATIONS AND RESULTS

The purpose of the test program was to establish the effect of biaxial plane stress on bearing failure onset. Testing spanned 14 months, and involved 68 tests of both cruciform and uniaxial CFRP specimens.

Throughout the test program, radiographic and fractographic analyses were performed to confirm the damage. This was done for a variety of loading configurations and hole sizes.

8.1 Bearing Failure Onset Detection

Bearing failure onset was established using the bearing strain criterion as described in section 6.5. Failure onset was typically indicated by an instantaneous increase in the bearing strain slope. This characteristic step was associated with the bearing failure of a lamina or group of laminae. For several specimens this step was difficult to observe or did not appear to exist.

During post-processing, plots of bearing load versus LVDT were examined for regions of non-linearity in accordance with the definition of hole elongation. These plots were seldom linear as reported in literature. This rendered the use of LVDT data for cross-checking difficult. Only at high loads did a noticeable slope change occur consistently; however, this was typically above the failure onset point.

8.2 Biaxial Test Results

The bearing strains under bearing/bypass and lateral loading for single shear lap joints produced a characteristic plot having four distinct stages (see Figure 8.1). The first stage (I) consisted of a zero shift in bearing strain. This shift was due to grip alignment and system preloading. Stage II consisted of a steep strain slope which gradually transitioned into a region of constant slope (stage III). The steep slopes were caused by fastener

contact with the hole edge and load transfer between joint members taking place. The constant slope which followed resulted when the contact angle and load transfer stabilized. The fourth stage (IV) was failure progression, in which the magnitude of the bearing strains increased rapidly. The response of stage IV varied with fastener size. Failure initiation occurred within stage III, and was usually indicated by a sudden increase in the bearing strain slope. This step is not visible in Figure 8.1 because of the large scale, but can be seen in Figure 8.18a (arrow head). The step was often identified by applying piecewise linear trend lines to the data. The start of the trend line discontinuity identifies the step locations.

Damage was confirmed through physical observation, radiographic inspection, and destructive fractography. These techniques were not intended for quantitative measurements, but rather as qualitative measurements for confirming bearing failure. Because of the small magnitude of damage at failure onset, radiographic and fractographic images required magnification to clearly observe bearing damage.

For the remaining sections, a simplified notation will be used to denote the applied biaxial stress state. The load applied along the 0° specimen axis will be followed by the load along the 90° axis. The two fields will be separated by a forward slash, '/'. Bearing and bypass loads were only applied along the 0° axis. For example, a load case of biaxial tension with 50% bypass is denoted as: Tension/Tension, 50% bypass.

On the bearing load versus strain plots, the failure points are identified by an arrow. Radiographic images are oriented such that the specimen 0° axis is always towards the top of the page. Fractographic images, unless otherwise noted, are of the cut surface along the hole centreline in the 0° specimen direction, with the specimen outer surface oriented towards the top of the page.

8.2.1 Tension/Tension Plane Stress

8.2.1.1 Tension/Tension, 0% Bypass (Figures 8.2 to 8.8)

Strain responses were typical of the characteristic curve shown in Figure 8.1, with the onset of bearing failure identified by the characteristic step. Failure onset values displayed low scatter, varying within 10% (see Table 8.1). Non-bearing strains for the 0.200" holes demonstrated a strain reversal. This was caused by fastener rotation placing a compressive load on the non-bearing side. The 0.312" diameter fasteners were loaded beyond first peak and short of ultimate sustainable load to obtain an overall strain plot.

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{††}
959	0.200"	1,370 lbf	-27,390 psi	8%
957	0.200"	1,386 lbf	-27,722 psi	10%
953	0.200"	1,345 lbf	-26,892 psi	6%
	Average	1,367 lbf	-27,335 psi	
943	0.260"	2,271 lbf	-34,945 psi	7%
951	0.260"	2,440 lbf	-37,546 psi	7%
	Average	2,356 lbf	-36,246 psi	
954	0.312"	3,568 lbf	-45,742 psi	50%
959	0.312"	3,486 lbf	-44,693 psi	125%
	Average	3,527 lbf	-45,218 psi	

Table 8.1 : Experimental Results for Tension/Tension, 0% Bypass

Radiographic images showed bearing damage as a dark band of decreasing intensity extending from the hole edge in the bearing direction. This was most visible on the

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{††} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

0.200" diameter specimens. The bands occurred along the contact region of -90° to $+90^\circ$; however, the actual contact angle can not be established. For the 0.312" holes, damage was visible in various forms: bearing damage, matrix cracking, and ply delaminations. For Specimen #954-312, the bearing damage was nearly half the hole diameter (see Figure 8.7b), while that for Specimen #959-312 was approximately 1.25 times the hole diameter (see Figure 8.8b). Often a constant diameter halo was visible around the hole. This halo is the fastener footprint, caused by fastener preload. Figure 8.9 is a radiographic image, and Figure 8.10 a surface photograph, of fastener induced damage.

Surface damage to Specimen #954-312 due to bearing was clearly visible (see Figure 8.11). Examination of the sectioned surfaces revealed significant bearing damage to the first eight plies from the faying surface (see Figure 8.12). At 5,200 lbf, considerable interlaminar cracks existed, originating from ply 9 and progressing diagonally to the outer surface. One of these cracks is visible on Figure 8.12.

Fractographic evidence of Specimen #959-312 showed the damage resulting from near ultimate loads under Tension/Tension, 0% bypass. Examination of the non-bearing hole face indicates minor ply damage at the outer and faying surfaces (see Figure 8.13). The damage at the outer surface is fastener induced, while that at the faying surface resulted from sectioning and matrix cracking. On the bearing face, two predominant failure modes exist. The first is the bearing failure of the plies closest to the faying surface, which encompasses plies 1 through 17 (see Figure 8.14). The second failure mode is that of through-thickness shear as indicated by the fracture along the 45° axis.

8.2.1.2 Tension/Tension, 50% Bypass (Figures 8.15 to 8.23)

The strain signals for specimen subjected to a tensile bypass load of 50% followed the standard profile described in section 8.2. Typically, the bearing strains in stage III were constant, showing little variation until failure initiation (see Figure 8.17). However, there are some exceptions like Specimen #949-200, which showed an increasing bearing strain (see Figure 8.16). A tensile strain on the bearing gauge is only possible when

experiencing fastener rotation. As the fastener rotates, the ratio of bearing strain to bypass strain decreases until it becomes unity. At this point, the bearing strain, as read from a surface mounted gauge, becomes tensile. However, at the faying surface, the bearing strain is still compressive.

Radiographs revealed the presence of bearing damage, but only on the bearing face. Matrix cracking and ply delaminations were observed, especially for the 0.312" holes.

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{††}
967	0.200"	1,281 lbf	-25,620 psi	2%
951	0.200"	1,141 lbf	-22,817 psi	6%
947	0.200"	1,099 lbf	-21,978 psi	5%
949	0.200"	NONE	NONE	N/A
	Average	1,174 lbf	-23,472 psi	
949	0.260"	1,870 lbf	-28,773 psi	5%
950	0.260"	1,685 lbf	-25,929 psi	7%
954	0.260"	NONE	NONE	N/A
	Average	1,778 lbf	-27,351 psi	
953	0.312"	3,386 lbf	-43,416 psi	11%
957	0.312"	3,478 lbf	-44,587 psi	11%
	Average	3,432 lbf	-44,002 psi	

Table 8.2 : Experimental Results for Tension/Tension, 50% Bypass

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{††} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

8.2.2 Compression/Compression Plane Stress

8.2.2.1 Compression/Compression, 0% Bypass (Figures 8.24 to 8.32)

The bearing strains resulting from a compressive biaxial load were similar to those observed under tensile loading. Again, the characteristic four stages were identifiable. The 0.200" hole size typically demonstrated a non-bearing strain slope reversal. The point of this reversal corresponds with the characteristic step, usually preceding it by couple hundred pounds.

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{**}
943	0.200"	-1,305 lbf	-24,347 psi	6%
950	0.200"	-1,124 lbf	-22,472 psi	7%
954	0.200"	-1,427 lbf	-28,543 psi	6%
964	0.200"	-1,043 lbf	-20,863 psi	3%
	Average	-1,196 lbf	-23,916 psi	
955	0.260"	-1,673 lbf [†]	-25,742 psi	7%
957	0.260"	-1,764 lbf [†]	-27,140 psi	NONE
967	0.260"	-1,909 lbf [†]	-29,371 psi	???
	Average	-1,782 lbf	-27,418 psi	
945	0.312"	-3,464 lbf	-44,416 psi	11%
943	0.312"	-3,638 lbf	-46,642 psi	???
	Average	-3,551 lbf	-45,529 psi	

Table 8.3 : Experimental Results for Compression/Compression, 0% Bypass

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{**} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

[†] These numbers are suspect since bearing damage was not visible on the radiographs, yet the proper failure indicators were observed. The actual failure load is believed to be around -2,350 lbf.

Damage was mostly in the form of local ply crushing, with some matrix cracking and delaminations present. For the 0.312" diameter holes, matrix and fibre damage was present along the surface 45° plies, implying fastener induced damage.

Examination of the 0.260" fasteners demonstrates that the characteristic step approach works, but is subject to error. The bearing strain signals showed several steps during loading. Using the bearing strain step as the sole means of failure identification, initial failure load predictions are as reported in Table 8.3. The damage visible in Figure 8.29b, in the bearing direction, was consistent with that induced by fastener preload. Figure 8.30b, which is of a specimen loaded below -2,350 lbf, does not demonstrate any bearing damage. Cross-checking the bearing strain steps against non-bearing strain and displacement signals suggests that the bearing failure onset point occurs at -2,350 lbf, and not 1,909 lbf as originally thought.

8.2.2.2 Compression/Compression, 50% Bypass (Figures 8.33 to 8.41)

The addition of a 50% compressive bypass load produced similar strain signals as observed under pure bearing.

Bearing damage was assessed from radiographic images which indicated that the damage was isolated to the bearing direction, primarily along the 0° and $\pm 45^\circ$ axes to the bearing direction. In addition to bearing damage, ply delaminations were observed. Unlike the biaxial tension cases, matrix cracks were often not visible, especially for the 0.200" hole size.

Several specimens were sectioned for the Compression/Compression, 50% bypass load case at different stages of failure and hole sizes. After fastener removal, the faying surface revealed bearing damage (see Figure 8.42). Fractography of Specimen #958-312 showed the extent of through-thickness damage. Again, both bearing and through-thickness shear failures were observed (see Figure 8.43). Figure 8.44 indicates bearing

damage to the first ten plies. The shear failure originated from ply 10, and extended to the outer surface. The damage to plies 11 through 16 resulted during the uplift of plies 11 to 48. Along the non-bearing hole face, damage is limited to the +45° plies.

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{**}
968	0.200"	-1,158 lbf	-23,152 psi	3%
956	0.200"	-1,017 lbf	-20,337 psi	3%
955	0.200"	-945 lbf	-18,898 psi	3%
952	0.200"	-1,104 lbf	-22,070 psi	3%
	Average	-1,056 lbf	-21,114 psi	
964	0.260"	-2,218 lbf	-32,742 psi	5%
956	0.260"	-2,173 lbf	-33,425 psi	6%
952	0.260"	-2,100 lbf	-32,302 psi	6%
	Average	-2,164 lbf	-32,823 psi	
958	0.312"	-3,284 lbf	-42,107 psi	N/A
949	0.312"	-3,196 lbf	-40,974 psi	6%
	Average	-3,240 lbf	-41,541 psi	

Table 8.4 : Experimental Results for Compression/Compression, 50% Bypass

Fractographic images of Specimen #968-200 shows minor bearing damage to plies 3 and 4, with a ply delamination between plies 4 and 5 (see Figure 8.45). However, the damage to plies 3 and 4 may be a result of matrix cracking and sectioning damage, as similar marks are visible around ply 20. Figure 8.46 shows the beginning of bearing damage on Specimen #964-260. This damage is isolated to plies 3, 4 and 5. As measured from the

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{**} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

fractographic image, the damage size corresponds to a maximum of 6.5% of the hole diameter compared to the 5.0% obtained from the radiographic images.

8.2.3 Compression/Tension Plane Stress

8.2.3.1 Compression/Tension, 0% Bypass (Figures 8.47 to 8.53)

Bearing failure onset results were in good agreement displaying low scatter (see Table 8.5). Unlike previous load cases, the non-bearing strains became negative for the 0.200" diameter fasteners. Since this was not observed for other fastener sizes, then the difference can not be attributed to the load case. Therefore, it must be a function of fastener diameter. For the 0.260" fasteners, the occurrence of bearing damage is suspect as even radiographic images are inconclusive. The 0.312" fastener results are similar to those of previous test cases.

Bearing damage was visible in the bearing direction for the 0.200" diameter holes, and extended 45° to either side of the bearing axis. The majority of the damage was confined to bearing, although some matrix cracking and ply delaminations were visible. Matrix cracks and ply delaminations were the predominant failure modes for the 0.260" fasteners. Because of fastener induced damage for tests with the 0.312" fasteners, bearing damage was partially obscured. However, the darker intensity between -45° to 25° than at -135° suggests the presence of bearing damage (see Figure 8.52b).

Specimen #952-312 was sectioned to verify that bearing damage had occurred, as indicated by the radiographic image. Figure 8.54 is of the hole after sectioning, and clearly shows bearing damage at the lower left corner. The damage at the outer surface resulted from fastener installation. Closer examination of the bearing face indicates damage to the first 9 plies (see Figure 8.55), while the non-bearing face displays incidental damage (see Figure 8.56).

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{††}
960	0.200"	-963 lbf	-19,262 psi	7%
962	0.200"	-1,000 lbf	-20,000 psi	7%
966	0.200"	-1,029 lbf	-20,577 psi	6%
	Average	-997 lbf	-19,946 psi	
959	0.260"	-1,967 lbf	-30,264 psi	3%
953	0.260"	-1,943 lbf	-29,880 psi	7%
	Average	-1,955 lbf	-30,072 psi	
952	0.312"	-3,516 lbf	-45,072 psi	11%
947	0.312"	-3,205 lbf	-41,091 psi	9%
	Average	-3,361 lbf	-43,082 psi	

Table 8.5 : Experimental Results for Compression/Tension, 0% Bypass

8.2.3.2 Compression/Tension, 50% Bypass (Figures 8.57 to 8.63)

Table 8.6 lists the loads at which bearing failure onset occurred. Non-bearing strains were similar to those observed under pure bearing, especially for the 0.200" diameter fasteners which had negative values. The negative strain resulted from low fastener stiffness which permitted fastener rotation. Fastener contact with the non-bearing hole edge resulted, causing a compressive strain to be registered on the gauge. For the other sizes of fasteners, their stiffness was sufficient to minimize rotation. For Specimen #950-312, a sharp decrease in bearing strain occurred at -2,600 lbf, with no corresponding change in the non-bearing or displacement signals (see Figure 8.63a). This decrease does not constitute a characteristic step since it was of decreasing magnitude. However, it does imply ply delamination or matrix crack formation. The signal beyond this point appeared normal.

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{††} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

Radiographic images indicated bearing damage along the 0° axis for most specimens. However, minor evidence of bearing damage existed on the 0.260" fastener radiographs, while those of the 0.312" fasteners clearly displayed bearing damage.

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{††}
961	0.200"	-817 lbf	-16,346 psi	3%
963	0.200"	-1,100 lbf	-22,000 psi	8%
965	0.200"	-919 lbf	-18,379 psi	3%
	Average	-945 lbf	-18,908 psi	
963	0.260"	-1,748 lbf	-26,890 psi	3%
947	0.260"	-1,625 lbf	-25,007 psi	5%
	Average	-1,687 lbf	-25,948 psi	
951	0.312"	-3,398 lbf	-43,562 psi	11%
950	0.312"	-3,090 lbf	-39,614 psi	10%
	Average	-3,244 lbf	-41,588 psi	

Table 8.6 : Experimental Results for Compression/Tension, 50% Bypass

Fractographic analysis of Specimen #951-312 is in good agreement with the corresponding radiographic image, as damage was observed along the bearing face (see Figure 8.64). Examination of the bearing face shows that the bearing damage is concentrated to the first seven plies (see Figure 8.65). However, of greater interest is the through-thickness shear crack which formed. This crack originates at ply 18, and progresses towards the outer surface at a 45° angle. The formation of this crack is likely the cause of the near vertical bearing strain increase prior to first peak load (see Figure

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{††} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

8.62a). The non-bearing face demonstrates the typical fastener damage at the outer surface, while the faying surface shows the damage to the 45° plies (see Figure 8.66).

Sectioning of Specimen #963-260 clearly shows bearing damage to plies 3, 4, 5, and 6 (see Figure 8.67). This damage was not caused by sectioning as the severity is greater than that observed elsewhere along the bearing face. Therefore, it must be bearing related. Furthermore, examination of the bearing face shows a gradual change in slope towards the faying surface (see Figure 8.68).

8.2.4 Tension/Compression Plane Stress

8.2.4.1 Tension/Compression, 0% Bypass (Figures 8.69 to 8.72)

No information was collected for the 0.200" fastener sizes for this loading condition because of the limited number of specimens available. However, tests using the other two fastener sizes displayed results typical of previous tests. Experimental scatter was within 13% (see Table 8.7).

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{††}
966	0.260"	2,457 lbf	-37,804 psi	3%
961	0.260"	2,217 lbf	-34,106 psi	7%
	Average	2,337 lbf	-35,955 psi	
948	0.312"	3,219 lbf	-41,269 psi	15%
956	0.312"	3,671 lbf	-47,059 psi	7%
	Average	3,445 lbf	-44,164 psi	

Table 8.7 : Experimental Results for Tension/Compression, 0% Bypass

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{††} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

Bearing damage was observed from radiographic images. The specimens showed damage concentrated along the 0° and -45° axes to the bearing direction. In the 0.260" fastener size, failure modes were isolated to bearing, while the 0.312" fastener cases exhibited bearing damage, matrix cracking, and delaminations due to the final loads being near first peak loads.

Photographs of the faying surface of Specimen #961-260 indicate a lip on the bearing edge of the hole (see Figure 8.73), while fractography shows bearing damage to plies 3 and 4, plus a fracture running diagonally through plies 5 and 6 (see Figure 8.74).

8.2.4.2 Tension/Compression, 50% Bypass (Figures 8.75 to 8.78)

The addition of a 50% bypass load resulted in similar strain plots as those observed in section 8.2.4.1. Using the characteristic step to identify the failure point, the onset of bearing failure was obtained (see Table 8.8). While Specimen #962-260 indicated a step in the bearing strain, Specimen #965-260 did not. This implies that bearing failure did not occur for Specimen #965-260 (see Figure 8.76a).

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{††}
962	0.260"	1,941 lbf	-29,869 psi	6%
965	0.260"	NONE	NONE	N/A
	Average	1,941 lbf	-29,869 psi	
955	0.312"	3,002 lbf	-39,487 psi	13%
946	0.312"	3,079 lbf	-39,479 psi	6%
	Average	3,041 lbf	-39,483 psi	

Table 8.8 : Experimental Results for Tension/Compression, 50% Bypass

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{††} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

Radiographic inspection confirms bearing damage in all but Specimen #965-260, which only displays damage consistent with that induced by fastener clamp-up.

The sectioning of Specimen #946-312 revealed that bearing damage is present, as seen from Figure 8.79. The extent of the damage is small, approximately 6%, which is why the damage is difficult to observe using radiography.

8.3 Uniaxial Test Results

Limited uniaxial testing was performed to obtain a basis for comparison against biaxial data. Tests were conducted such that the laminate 0° orientation was aligned to the bearing direction. Failure onset was identified using the characteristic step technique described in section 8.1. Cross-checking against the non-bearing and displacement signals was performed to improve accuracy.

Similar to the biaxial load cases, characteristic plots for the bearing strain, non-bearing strain, and displacement were observed when plotted against bearing load. Four stages existed, with Stages I, II, and IV identical to those of section 8.2. Stage III demonstrated a constant slope; however, for some specimens, the slope was positive, while others were negative. Since the decreasing bearing strain was accompanied by a decreasing non-bearing strain, then this implies that fastener rotation was taking place..

8.3.1 Uniaxial Tension Plane Stress

8.3.1.1 Uniaxial Tension, 0% Bypass (Figures 8.80 to 8.84)

Bearing failure onset loads for uniaxial tension with 0% bypass are reported in Table 8.9. Although most specimens conformed to the typical strain pattern, some did not. Specimen #971-200 displayed an uncharacteristic bearing strain in that the values indicated a tensile bearing strain (see Figure 8.80a). This implies the occurrence of considerable fastener rotation, which transferred all the bearing load towards the faying

surface. The lack of a bearing load at the outer surface, which is where the gauge is located, resulted in the tensile strain.

Post-test radiographic images showed damage along the bearing edge. This damage was concentrated at 0° , $\pm 45^\circ$, and 90° , which corresponds to the laminate fibre directions.

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{**}
971	0.200"	1,680 lbf	-33,600 psi	5%
972	0.200"	1,840 lbf	-36,800 psi	4%
	Average	1,760 lbf	-35,200 psi	
969	0.260"	2,841 lbf	-43,706 psi	3%
970	0.260"	2,465 lbf	-37,925 psi	4%
974	0.260"	2,470 lbf	-37,996 psi	5%
	Average	2,592 lbf	-39,876 psi	

Table 8.9 : Experimental Results for Uniaxial Tension, 0% Bypass

8.3.1.2 Uniaxial Tension, 50% Bypass (see Figures 8.85 to 8.88)

Application of a 50% bypass load resulted in strain and displacement signals similar to those observed for pure bearing. Table 8.10 shows the bearing onset failure load associated with the characteristic step. Specimen #971-260 demonstrated a 27.5% greater onset load, and significantly different strain signals than similar specimens. The reason for this difference is unknown, but could be due to higher fastener preload, or a different sequence of laminae failure.

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{**} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{††}
970	0.200"	1,454 lbf	-29,080 psi	4%
	Average	1,454 lbf	-29,080 psi	
972	0.260"	1,966 lbf	-30,242 psi	5%
973	0.260"	1,955 lbf	-30,071 psi	6%
971	0.260"	2,500 lbf	-38,462 psi	3%
	Average	2,231 lbf	-32,925 psi	

Table 8.10 : Experimental Results for Uniaxial Tension, 50% Bypass

8.3.2 Uniaxial Compression Plane Stress

8.3.2.1 Uniaxial Compression, 0% Bypass (Figures 8.89 to 8.90)

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{††}
974	0.200"	-1,892 lbf	-37,838 psi	6%
973	0.200"	-2,016 lbf	-40,323 psi	10%
	Average	-1,954 lbf	-39,081 psi	

Table 8.11 : Experimental Results for Uniaxial Compression, 0% Bypass

Bearing strain, non-bearing strain, and displacement signals were similar to those observed in section 8.2 (see Table 8.11). However, at the end of stage II the non-bearing strain slope was negative. This behaviour was observed for some biaxial cases which used 0.200" diameter fasteners, and was attributed to the low d/t ratio and fastener stiffness. Additionally, a compressive strain registered on the non-bearing strain gauge.

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{††} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

Bearing damage was present on both specimens as determined by radiographic inspection. For Specimen #974-200, the greatest damage occurred in the bearing direction (0°), and decreased towards 90° (see Figure 8.89b).

8.3.2.2 Uniaxial Compression, 50% Bypass (Figure 8.91)

Only one test was performed using a 50% bypass; it was used to establish a trend in failure initiation. Damage onset was identified using the characteristic step (see Figure 8.91a). Uncertainty existed in the identification of this step due to its small size; therefore, the non-bearing and displacement signals were used as cross-checks. Since both signals indicated a slope discontinuity at the same load as the bearing step, then this was taken as the bearing failure onset load.

Specimen No.	Hole Size	Bearing Failure Onset Load[†]	Bearing Stress	Bearing Damage^{**}
969	0.200"	-1,556 lbf	-31,126 psi	7%
	Average	-1,556 lbf	-31,126 psi	

Table 8.12 : Experimental Results for Uniaxial Compression, 50% Bypass

8.4 Discussion

The use of the bearing strain signal for detecting the onset of bearing failure was successful. However, several cases existed where the characteristic step response was not seen or multiple steps were present. These types of signals required considerable interpretation, and still resulted in mispredictions of the failure initiation load. To improve accuracy and confidence in bearing failure identification, non-bearing strain and LVDT signals were cross-checked for similar discontinuities. Out of the 68 tests

[†] Reported bearing failure onset loads are normalized to a thickness of 0.250" to make results comparable.

^{**} Bearing damage was obtained from radiographic images, and is expressed as a percent of hole diameter.

performed, three specimens are known to have been mispredicted (Compression /Compression, 0% bypass), with another five being questionable. This yields a confidence level of 88% in correctly identifying failure onset.

Use of LVDT data to determine hole elongation was found to be impractical due to the lack of a clearly defined linear region from which to base an offset value. Furthermore, the expected behaviour reported in section 6 did not occur, except at loads associated with significant damage. This was likely due to the long gauge length which measured global displacement rather than local. The observed LVDT signals were consistently non-linear, showing instantaneous slope increases.

Bearing damage was observed in most specimens, whether by visual observation of the bearing edge, radiographic inspection, or fractography. Physical observations of the bearing surface revealed a burr along the bearing edge. This burr was formed from the relative movement between the titanium patch and the specimen, resulting in a hole misalignment. The two external +45° plies then mushroomed outwards due to the lack of through-thickness reinforcement.

Radiographic images were useful in establishing the presence of bearing damage. However, other failure modes, such as matrix cracking, ply delaminations, and fastener induced damage, obscured the bearing damage. Using radiographic techniques required the magnification of the hole region to see the bearing damage. Unfortunately, this caused some images to be blurry, making qualitative evaluations difficult.

Fractography was the definitive technique in confirming the existence of bearing failure; however, being a destructive technique, it was used sparingly. Although some ply damage occurred from sectioning, it was distinguishable from bearing induced damage.

Comparison of bearing failure onset values at different bypass ratios revealed that bearing strength decreased with the presence of a bypass load (see Figures 8.92 to 8.95). This

behaviour agrees with that observed in literature[4,8,24,29,51]. Strength losses of 2.7% to 25%, but mostly 10-16%, were obtained. The greatest losses due to the application of a 50% bypass load occurred with the Tension/Compression cases, and the smallest for biaxial compression. Because of the constant 1:1 biaxial ratio of the applied loads, the biaxial ratio of the bearing load to lateral load changed with bypass. For example, with a 0% bypass, the ratio was 1:1, but with a 50% bypass, the ratio became 1:2. Originally, the strength loss resulting from a bypass load was attributed to this increased biaxial ratio. However, uniaxial testing revealed strength decreases of 20% with the application of a bypass load. As no biaxial load existed, the strength loss was due to the bypass load. Therefore, for the biaxial cases, the strength losses between the 0% and 50% bypass cases resulted from the bypass load, and not the increased biaxial ratio.

Experimental data indicated that for a constant bypass ratio, the stress field influenced the bearing strength (see Figures 8.96 and 8.97). Biaxial specimens loaded in tension along the 0° specimen axis displayed a trend of higher bearing strengths than those in compression. Tensile loads applied to the 0° axis caused hole elongation in the loading direction, causing the plate material to wrap around the fastener and increase the contact angle. Likewise, the application of a compressive load to the specimen 90° axis produced a trend of higher bearing strengths over those with tensile 90° loads. This is because a compressive load in the 90° axis elongates the hole along the 0° axis thereby increasing contact angle. For a given bypass ratio, this explains the difference in bearing strengths as changes in contact angle alter the bearing capacity of the joint.

Three different fastener sizes were tested to observe the influence of fastener diameter on the bearing strength. Results indicated that the bearing strength increased as d/t increased (see Figure 8.98). This linear increase results from smaller fastener rotation which arises from increasing the diameter. The 0.312" diameter fastener produced a slightly greater increase in bearing strength due to material differences between fastener sizes. Unlike the two smaller fasteners which used titanium pins, the 0.312" fasteners used a stiffer steel pin. This increased stiffness provided a more uniform through-thickness stress

distribution. Additionally, the installation damage, unique to the 0.312" fastener, implies a higher clamping pressure. Both of these effects improved the bearing strength for the 0.312" fasteners.

Specimens with the 0.312" fastener size were loaded to various levels of failure to reveal the damage progression of the laminate under single shear loading. Failure initiation began with either the bearing failure of the -45° plies closest to the faying surface (plies 3 and 4), or the fracture of the 0° plies (plies 5 and 6) (see Figures 8.46a and 8.74a). Damage progression occurred with the bearing failure of plies 7 through 10 as the fastener rotated under load (see Figure 8.54a). This rotation induced shear forces on the remaining laminate, causing a shear fracture through plies 11 to 48 (see Figure 8.64a). The failure of the laminate along this fracture line resulted in substantial damage, as plies 11 to 48 uplifted (see Figure 8.14a and 8.43a). This point coincides with the first peak load on the bearing load/strain plot.

9. FINITE ELEMENT ANALYSIS

Finite element analysis is a numerical method useful in analyzing complex structures. Its use is valuable in lowering the costs associated with purely experimental programs. To support the experimental program, a finite element model of the cruciform specimen was developed. This model was intended to supplement test data and provide greater insight into the failure behaviour of single shear lap joints in CFRP laminates.

9.1 Finite Element Method

The computational intensity of FEA originally limited its use to simple structures, but the introduction of microcomputers has extended its use to more complex problems. Such problems can now be solved in a fraction of the time, and at lesser cost than conventional methods. Since FEA is a mathematical approximation of a physical object, model validation is important to ensure an accurate representation of the physical problem. Models can be validated through experimental testing or by comparison against known theoretical values.

The theory behind FEA is beyond the scope of this work, so the following is only intended as a brief introduction. Additional information on finite element analysis is available in literature[90,91,92].

9.2 Basic Methodology

The finite element method begins by discretizing the structure as a group of smaller components using points called *nodes* (see Figure 9.1). Nodes do not have any structural properties, and so the interaction between adjacent nodes is provided by structural units called *elements*. By assigning material properties to these elements, the stiffness between

nodes is defined. Connecting the elements together at common nodes produces a web which defines the behaviour of the overall structure.

Two methods exist to solve these discretized systems: displacement method, and force method. Both are useful in solving structural problems, but the displacement method is better suited for microprocessors, and hence, is frequently employed in FEM/FEA software applications.

9.2.1 Displacement Method

The displacement method uses nodal displacements as the unknowns. For each element, nodal displacements are equated to the nodal forces multiplied by the element stiffness. Nodal equations are then combined to produce a global stiffness matrix, which relates the nodal displacements to the external forces. Applying boundary conditions to reduce the number of unknowns, the equations are solved simultaneously to obtain a solution for the nodal displacements. Once the displacements are known, the strains within connecting elements can be found. Using stress-strain relationships, stresses are obtained, thus completing the analysis.

9.2.2 Force Method

The force method is similar to the displacement method, but uses elemental forces as the unknowns instead of nodal displacements. Internal forces are then associated with applied loads, and solved in a similar fashion as the displacement method.

9.3 Specimen Modelling

2-D and 3-D models of the cruciform specimen were developed to supplement experimental data and obtain a better understanding of the reactions within a bolted joint. Modelling was carried out using MSC's PATRAN/NASTRAN FEA software package running on an SGI Challenge at SMPL-IAR.

9.3.1 2-Dimensional Model

The 2-D cruciform model described in section 5.4.3.2 was further developed to simulate a mechanically fastened lap joint by modelling it as a pin-loaded plate. Two approaches exist for modelling the pin within the specimen. The most common, used by Crews & Naik[4], Agarwal[64,93], and Waszczak & Cruse[94], assumes a cosinusoidal pressure distribution along the hole boundary. This approach results in a constant contact angle of 90°, which is accurate for snug fit fasteners in isotropic materials. However, it is less so for orthotropic materials, since the contact angle varies with orthotropy. The second approach uses spring or gap elements connected between a fixed pin and the hole edge. Because of the mathematical difference between the two, spring elements require that the contact angle be obtained iteratively, while gap elements do not. Gap elements were used by Eriksson[16], and Bellinger & Poon[95] to model fastener interaction and simulate the contact problem. Since the influence of biaxial load on the contact angle was unknown, gap elements were selected for the present model.

9.3.1.1 Modelling Assumptions

Literature indicates that two parameters are key in the modelling of a 2-D pin-loaded plate; namely, the friction between the pin and the hole face, and pin clearance. Wilkinson et al.[96], and Tsujimoto & Wilson[97] used 2-D models of a pin-loaded flat plate which assumed a rigid pin with friction. Others chose to ignore the effects of friction to keep their models simple[98,39]. Similarly, hole clearances were ignored by most authors[36,99,100]. Pins were exclusively modelled, since the 2-D representation does not include through-thickness effects. Therefore, the model assumed a snug fit, frictionless pin to maintain model simplicity.

9.3.1.2 Mesh Generation

The composite panel and the titanium pin were meshed with a combination of QUAD 4 and QUAD 8 shell elements, both in uniform and biased configurations (see Figure 9.2). This combination increased accuracy while minimizing computational time. QUAD 8 elements were used around the hole boundary where accuracy was important.

Panel elements were assigned composite shell properties, while those of the titanium pin were assigned isotropic shell properties. The stacking sequences for each section of the panel were input into the a MSC/PATRAN composite spreadsheet and assigned to their respective elements. Material constants for the lamina and titanium are shown in Table 9.1.

Engineering Constants	AS4/3501-6 [82]	FM300K [24]	Ti-6Al-4V
E_{11}	21.32E6 psi	0.71E6 psi	16.0E6 psi
$E_{22} = E_{33}$	1.31E6 psi	0.71E6 psi	16.0E6 psi
$G_{12} = G_{13}$	0.73E6 psi	57.86E3 psi	6.2E6 psi
G_{23}	0.44E6 psi	57.86E3 psi	6.2E6 psi
$\nu_{12} = \nu_{13}$	0.3	0.3	0.29
ν_{23}	0.42	0.3	0.29

Table 9.1 : Lamina Properties Used for Finite Element Analysis

The contact region between the fastener and panel was approximated using 2 node, 2-D bar elements assigned gap properties. Elements had a high stiffness in the closing direction and negligible stiffness in the opening direction. This permitted only axial loads to be transferred when the elements were closing, thereby automatically calculating the contact angle.

The specimen geometry was meshed using both element biasing and selective mesh refinement to increase accuracy. Mesh density was highest around the hole boundary to improve the radial stress results. The high mesh density also minimized element distortion which improved model accuracy. Element biasing was effective for reducing the number of elements in regions where the stress gradient was low, thereby shortening computational time.

9.3.1.3 Boundary Conditions

The modelling of the complete specimen eliminated many boundary conditions. However, to achieve a bearing reaction and fix the specimen in space, selected nodes were restrained. All pin nodes were restrained in the x, y, and z axes for both translation and rotation. For the specimen, a single node along the bearing centreline was fixed for all but translation in the loading axis (see Figure 9.3). This fixed the model in space, allowing the solution to converge.

Loads were applied evenly to nodes on the ends of the specimen arms (see Figure 9.4). The magnitude of each load was obtained by dividing the desired load by the number of nodes. Bearing loads were obtained by applying different loads on opposing arms. No direct loads were applied to the pin.

9.3.1.4 Model Limitations

The 2-D model suffered from several limitations, the first of which was that it was plane stress. This type of analysis did not account for any through-thickness stress variations arising from bending or fastener rotation. Another limitation was that the fastener was modelled as a rigid pin, unlike the real fastener which exhibited bending and through-thickness clamping pressures. The 2-D model also prevented the load eccentricity from being introduced, which is a significant factor in the analysis of single shear lap joints. Therefore, this model was more representative of a double shear lap joint. All of these factors influence the joint stiffness, and hence the resultant stresses.

9.3.2 3-Dimensional Model

A 3-D model was developed to overcome the limitations of the 2-D model. By modelling the load eccentricity present in single shear lap joints, the through-thickness phenomena was simulated. This provided a more realistic analysis of the problem. To minimize the computational time, specimen symmetry about the x-axis, and a reduced modelling region, were employed.

9.3.2.1 *Modelling Assumptions*

Similar to the 2-D model, several assumptions were adopted to keep the model simple. Again, friction effects and fastener clearance between the pin and hole face were ignored [9, 100,101,102].

Since NASTRAN does not have 3-D composite elements, several approaches exist to simulate laminate behaviour. The first approach involves modelling each ply as a layer of orthotropic elements. Each layer is assigned properties based on the lamina orientation which it represents. However, this becomes computationally expensive as laminate thickness increases. The second approach, adopted by Matthews et al.[44], models a repeating group of lamina as a single layer of elements. These elements are then assigned the collective properties of that group.

The first approach is not convenient due to the thickness of the laminate (48 plies). Furthermore, the stacking sequence of the laminate, short of being symmetric, does not contain repeating groups, making the approach used by Matthews et al. unsuitable. Therefore, the model used 3-D orthotropic elements, assigned bulk laminate properties, to obtain the representative stiffness.

9.3.2.2 *Mesh Generation*

The mesh underwent several iterations for optimization purposes. The final mesh used HEX 20 isoparametric brick elements for the specimen, titanium patch, and fastener (see Figure 9.5). Specimen elements were assigned 3-D orthotropic properties containing material constants derived from 3-D laminate theory (see Table 9.2). Fastener and titanium patch elements were assigned material constants for titanium Ti-6Al-4V (see Table 9.1).

Laminate Constants	AS4/3501-6 [45 ₂ /-45 ₂ /0 ₂ /±45/ [90 ₂ /±45] ₂ /0 ₂ /±45/90/0 ₃] _s
E _{xx}	8.87E6 psi
E _{yy}	7.37E6 psi
E _{zz}	1.53E6 psi
G _{xy}	3.11E6 psi
G _{xz}	0.59E6 psi
G _{yz}	0.57E6 psi
ν _{xy}	0.34
ν _{xz}	0.28
ν _{yz}	0.31

Table 9.2 : Laminate Material Properties Used in 3-D FEA Model

The contact region between the fastener and plates was approximated using gap elements. Similar meshing techniques to those described in section 9.3.1.2 were employed.

9.3.2.3 Boundary Conditions

The use of model symmetry required the application of specific boundary conditions to obtain results representative of the actual problem. Along the cut surface (parallel to the x-axis), translation along the y-axis, and rotations about the x and z-axes were restrained. These boundary conditions are shown of Figures 9.5b, c, and d for the specimen, pin, and titanium plate, respectively. Fastener ends were restrained from z-axis translation and x and y-axes rotations (see Figure 9.5c). The titanium patch was held in place by fixing the left face against x-axis displacement and y and z-axes rotations (see Figure 9.5e).

The titanium plate and composite specimen were further restrained along the hole boundaries from z-axis translation and x and y-axes rotations to prevent separation (see Figure 9.5b and d). These conditions partially simulated fastener preload. A better

representation of fastener preload would be to apply an external pressure over an area equal to that of the fastener footprint.

Loads were simulated by the application of a uniform pressure distribution along the ends of the composite specimen (see Figure 9.5a). Applied pressures were determined based on the bearing load, bypass load, and biaxial ratio desired.

9.3.2.4 *Model Limitations*

The 3-D model is more representative of the actual specimen; however, there are several limitations which degrade its accuracy. The use of bulk properties does not yield lamina strains, thus producing a smooth through-thickness stress distribution, which is not the case with laminated materials. Therefore, CLPT must be applied to establish lamina stresses and strains.

The fastener used for experimental testing had a 100° countersink which penetrates half the thickness of the titanium plate. The model did not consider the countersink nor the reduced stiffness resulting from it. Furthermore, the fastener preload was not accurately modelled.

9.4 Results

2-D and 3-D models were compared using a pure bearing load of 2,335 lbf with no applied bypass or biaxial load. Both models exhibited in-plane deformation in the direction of the applied load, while only the 3-D model displayed an additional out-of-plane displacement. The out-of-plane displacement consisted of a clockwise rotation of the fastener and joint members about the midpoint of the fastener (see Figure 9.6).

The peak bearing stress, σ_{peak} , obtained from 2-D FEA was -53,000 psi (see Figure 9.7), as compared to -142,000 psi for the 3-D model (see Figure 9.8). For the 3-D model, the peak stress occurred at the faying surface, and decreased to a near uniform stress of

-41,700 psi by mid-laminate. This resulted in an average through-thickness peak stress of -56,587 psi for the 3-D model. The severity factor calculated from the 3-D analysis was 3.41* .

Using equation 2.9, the theoretical peak bearing stress for an applied load of 2,335 lbf was calculated as -45,739 psi. For a single shear lap joint, the respective SF from Figure 2.8 was 2, yielding a theoretical single shear peak bearing stress of -91,478 psi.

9.5 Discussion

Evaluation of finite element models confirmed that the 3-D model better represented the physical behaviour of a single shear joint. Both fastener and joint rotation, along with a non-uniform through-thickness bearing stress, were demonstrated by the 3-D model. As expected, the 2-D model did not demonstrate the above behaviour; therefore, it was unsuitable for single shear joint analysis. However, the 2-D model is suitable for analysis of double shear lap joints since fastener and joint rotation are small.

Comparison of the peak bearing stress resultants for the two models indicated that neither agreed with the theoretical peak, as both models overpredicted the theoretical stress. This is because the theoretical stress is based on isotropic materials, while the models are orthotropic. Recalling from section 2.5.4.2, orthotropic materials exhibit greater peak radial stresses than isotropic materials; hence, the overprediction.

Averaging the bearing stress resultants over the laminate thickness for each model produced results which were comparable. The averaged peak bearing stress for the 3-D model was 7% greater than that of the 2-D model. This implies that, although the

* The severity factor was calculated by dividing the peak stress at the faying surface by that at the outer surface. However, as will be discussed in section 10, a more accurate calculation would be the peak stress at the faying surface divided by the theoretical peak stress (eqn. 2.9). This would yield a SF of 3.10 instead of 3.41.

through-thickness stress distributions are considerably different, the use of bulk laminate properties satisfactorily represents the laminate.

The 3-D finite element model predicts a severity factor of 3.41 for uniaxial tension, while Figure 2.8 suggests a value of 2 for the t/d ratio of 0.96. This is a considerable difference, and results from the theoretical SF being derived for isotropic materials. As discussed in section 2.5.4.2, increasing orthotropy increases the peak radial stress, and is likely the cause for the difference. However, confirmation of this requires performing further FEA studies, and is beyond the scope of this work.

10. FAILURE ANALYSIS

An empirical failure prediction technique was developed to establish the onset of bearing failure for mechanically fastened single shear lap joints. Using experimental data and finite element analysis, the technique was tailored for two different load cases. The modified technique was then applied to the remaining experimental cases for validation.

10.1 Prediction Technique Development

The approach taken for predicting bearing failure onset was a first-ply failure analysis of the stress field at the hole boundary in the bearing direction. The stresses in each ply were calculated using superposition and CLPT, and compared against the allowables using various failure theories.

10.1.1 Stress Field Calculation

Analysis began by calculating the stress field at the hole edge of a biaxial loaded plate subjected to pure bearing. Using the principle of superposition, the problem was broken down into a pin-loaded plate subjected to pure bearing, and a uniaxially loaded plate containing a circular notch (see Figure 10.1).

The stress field for the pin-loaded plate was obtained using 2-D finite element analysis of the specimen (section 9.3.1) [103]. Non-dimensional tangential and radial stress multipliers, ψ_θ and ψ_r , respectively, were then calculated at the hole boundary using the following equation

$$\begin{aligned} (\psi_r)_{pin} &= \frac{(\sigma_{brg})_{FEA}}{\frac{P_{brg}}{d \cdot t}} \\ (\psi_\theta)_{pin} &= \frac{(\sigma_\theta)_{FEA}}{\frac{P_{brg}}{d \cdot t}} \end{aligned} \quad \text{eqn. 10.1}$$

where, σ_{brg} was obtained from a hole edge node, inline with the bearing direction. The calculated stress multipliers for a 0.260" diameter hole under pure bearing are shown in Table 10.1.

$(\psi_{\theta})_{pin}$	$(\psi_r)_{pin}$
0.62	-0.96

Table 10.1 : Stress Multipliers for an Orthotropic Pin-Loaded Plate

The stress field for the notched plate was solved using the isotropic stress concentration factor for an open hole, which yielded the following stress multipliers

$$\begin{aligned} (\psi_r)_{open} &= \frac{0}{\frac{P_{applied}}{W \cdot t}} \\ (\psi_{\theta})_{open} &= \frac{K_T^{\infty} \cdot \frac{P_{applied}}{W \cdot t}}{\frac{P_{applied}}{W \cdot t}} \end{aligned} \quad \text{eqn. 10.2}$$

Equation 10.2 is based on the nominal plate stress. In order to sum the stress multipliers of equations 10.1 and 10.2, equation 10.2 needs to be referenced to bearing stress. So, the open hole stress multipliers, with respect to bearing stress, are

$$\begin{aligned} (\psi_r)_{open} &= 0 \\ (\psi_{\theta})_{open} &= K_T^{\infty} \cdot \frac{d}{W} \end{aligned} \quad \text{eqn. 10.3}$$

For a 0.260" diameter hole under tensile loading, the open hole multipliers were calculated, and are shown in Table 10.2.

$(\psi_{\theta})_{open}$	$(\psi_r)_{open}$
0.10	0

Table 10.2 : Isotropic Stress Multipliers for a Tensile Loaded Circular Notched Plate

Multipliers from equations 10.1 and 10.3 were summed to obtain the overall stress multipliers.

$$\begin{aligned}\psi_r &= (\psi_r)_{pin} + (\psi_r)_{open} \\ \psi_\theta &= (\psi_\theta)_{pin} + (\psi_\theta)_{open}\end{aligned}\quad \text{eqn. 10.4}$$

Table 10.3 shows the stress multipliers for the various stress states tested.

			0% Bypass		50% Bypass	
0° Load	90° Load	Biaxial Ratio	ψ_θ	ψ_r	ψ_θ	ψ_r
Tension	---	0	0.62	-0.96	0.59	-0.96
Compression	---	0	0.62	-0.96	0.65	-0.96
Tension	Tension	1	0.72	-0.96	0.78	-0.96
Tension	Compression	-1	0.52	-0.96	0.39	-0.96
Compression	Compression	1	0.52	-0.96	0.46	-0.96
Compression	Tension	-1	0.72	-0.96	0.85	-0.96

Table 10.3 : Stress Multipliers for a Snug Fit, 0.260" Diameter Fastener
Subjected to Various States of Plane Stress[†]

10.1.2 Ply Stress Calculations

The calculation of the ply principal stresses was complicated by the single shear joint configuration. Through-thickness effects were determined using the 3-D finite element model described in section 9.3.2. By applying boundary conditions similar to those of the joint, radial stress information along the bearing face was obtained. This information was normalized against the outer surface bearing stress to obtain a Severity Factor (SF).

[†] These values of stress multipliers are valid for a particular FE model. Different mesh refinement, and selection of another node from which σ_{bng} is obtained, will yield different stress multipliers.

Applying the least squares curve fitting method to the SF values, a fourth order polynomial passing through the origin yielded the best fit. For the load case of Tension/Compression, 0% bypass, the polynomial was of the form

$$SF = 4793.20 \cdot z^4 - 1736.30 \cdot z^3 + 222.63 \cdot z^2 - 10.08 \cdot z + 1 \quad \text{eqn. 10.5}$$

where z is the through-thickness position referenced from the outer laminate surface.

Z Position	Bearing Stress	SF
0"	-28,681 psi	1.00
0.0624"	-25,483 psi	0.89
0.1248"	-28,592 psi	1.00
0.1872"	-40,444 psi	1.41
0.2498"	-113,498 psi	3.96

Table 10.4 : Through-Thickness Bearing Stresses and Severity Factors for Tension/Compression, 0% Bypass, with a 0.260" Diameter Fastener

The SF was calculated for each ply by interpolating equation 10.5 using the respective through-thickness position. For the Tension/Compression, 0% bypass case, the severity factor distribution indicated a steep gradient at the faying surface, which leveled off to unity by mid-laminate.

Ply radial and tangential stresses, σ_{brg} , and σ_{θ} , respectively, were then calculated for each ply by assuming an initial value for P_{brg} , and substituting into the following equations

$$\begin{aligned} (\sigma_{brg})_i &= SF_i \cdot \psi_r \cdot \frac{P_{brg}}{d \cdot t} \\ (\sigma_{\theta})_i &= \psi_{\theta} \cdot \frac{P_{brg}}{d \cdot t} \end{aligned} \quad \text{eqn. 10.6}$$

The subscript 'i' denotes the ply number. This yielded the stress field in global coordinates for each ply at the hole boundary along the bearing axis. Stresses were then converted to principal lamina stresses using CLPT.

10.1.3 Ply Failure Analysis

Once the stress fields were expressed in terms of lamina principal stresses, failure analysis was performed to determine the onset of bearing failure. Since the analysis was conducted on a ply-by-ply basis, only lamina failure criteria were considered. Using various criteria from sections 3.4.1.1 to 3.4.1.3 and the lamina properties listed in Table 10.5, plies were checked for failure using the lamina stresses obtained from CLPT. If no failures occurred, the load was incremented and new radial and tangential stresses calculated. Conversely, if more than one ply failed, the load was decreased. The onset of bearing failure was defined as the first instance that a ply within the laminate failed. Table 10.6 shows the failure predictions for plies 1 to 10 of a Tension/Compression, 0% bypass case at 1,550 lbf bearing. The Tsai-Hill failure criterion was used as the baseline to establish the onset of bearing failure. As seen in Table 10.6, Tsai-Hill predicts the first instance of failure, as defined by a value greater than unity, to occur in ply 5. This ply is of 0° orientation.

Lamina Constants	AS4/3501-6
X_t	290.65 ksi
X_c	-173.61 ksi
Y_t	7.69 ksi
Y_c	-29.59 ksi
S_t, S_c	19.87 ksi

Table 10.5 : Lamina Material Properties Used in Failure Analysis

Failure Criteria Results							
Ply No.	Tsai-Hill	Tsai-Wu $F_{12}=0$	Max. Strain	Hoffman	Gol'denblat & Kopnov	Norris	Fischer
1	0.71	0.53	0.55	0.21	0.69	0.49	0.52
2	0.65	0.49	0.49	0.15	0.60	0.41	0.43
3	0.59	0.45	0.44	0.11	0.52	0.34	0.36
4	0.54	0.41	0.39	-0.14	0.23	0.06	0.08
5	1.00	1.18	0.96	1.17	1.08	1.13	0.98
6	0.93	1.10	0.87	1.05	1.00	0.97	0.84
7	0.41	0.33	0.26	0.02	0.30	0.17	0.18
8	0.38	0.31	0.23	0.02	0.26	0.14	0.15
9	0.32	0.35	0.17	-0.47	0.22	0.12	0.10
10	0.31	0.33	0.16	-0.45	0.20	0.16	0.09
>10	<1	<1	<1	<1	<1	<1	<1

Table 10.6 : Ply Failure Predictions for Tension/Compression, 0% Bypass at 1,550 lbf Bearing

10.1.4 Correction of Initial Predictions Against Experimental Results

Onset of bearing failure for a Tension/Compression, 0% bypass specimen was predicted to be 1,550 lbf. This resulted in an difference of 51% compared to the average experimental results of 2,337 lbf for bearing failure onset.

Predicted Onset Load	Average Experimental Onset Load	Percent Difference
1,550 lbf	2,337 lbf	51%

Table 10.7 : Predicted versus Experimental Bearing Failure Onset Loads for Tension/Compression, 0% Bypass, with a 0.260" Diameter Fastener

To compensate for this difference, the predicted bearing failure onset load was multiplied by a correction factor, ϕ , of 1.5. Although ϕ is an arbitrary value applied to obtain a better fit with experimental results, it is assumed to be associated with preload and faying surface friction effects. Studies have shown that fastener preload and friction significantly improves bearing strength, with increases of 50% to 100% over pin-loaded cases having been reported[8,19].

Elastic superposition indicates that for a 0% bypass case, the sign of the 0° load has no effect on the stress multipliers (see Table 10.3). This implies that the onset of bearing failure occurs at the same load regardless of the sign of the applied load. However, experimental results did not demonstrate this. Using the same approach as that of the Tension/Compression case, a specimen subjected to biaxial compression, 50% bypass, was examined. Similar to Tension/Compression, a fourth order polynomial resulted in the best fit, but produced a more gradual severity factor profile (see Table 10.8).

Z Position	Bearing Stress	SF
0"	-6,079 psi	1.00
0.0624"	-5,772 psi	0.95
0.1248"	-6,662 psi	1.1
0.1872"	-9,452 psi	1.55
0.2498"	-18,540 psi	3.05

Table 10.8 : Through-Thickness Bearing Stresses and Severity Factors for Compression/Compression, 50% Bypass, with a 0.260" Diameter Fastener

The bearing failure onset load was calculated using equation 10.6 and the Tsai-Hill failure criterion. The first instance of ply failure occurred in ply 5 at a load of 1,750 lbf. Multiplying 1,750 lbf by ϕ resulted in a predicted bearing failure onset load of 2,625 lbf. Experimentally, failure occurred around 2,164 lbf, which was 18% less than predicted.

Predicted Onset Load	Average Experimental Onset Load	Percent Difference
2,625 lbf	2,164 lbf	-18%

Table 10.9 : Predicted versus Experimental Bearing Failure Onset Loads for Compression/Compression, 50% Bypass, with a 0.260" Diameter Fastener

To improve the predicted failure results, compressive loads in the specimen 0° direction were reduced by 18%. Multiplying the failure onset load by a compression reduction factor, Δ , of 0.82 provided agreement with experimental data for compressive loads. For tensile loads, Δ was assigned a value of 1.00.

So, the expected failure onset load is calculated using

$$P_{Onset} = \phi \cdot \Delta \cdot P_{Predicted} \quad \text{eqn. 10.7}$$

where the P_{Onset} is the load at which bearing failure occurs, and $P_{Predicted}$ is the value of P_{brg} in equation 10.6 which causes the Tsai-Hill failure criterion to exceed unity.

10.1.5 Comparison of Bearing Failure Onset Loads for Other Load Cases

Several other load cases were examined to validate this approach for predicting bearing failure onset. Using the same approach as stated in the previous section and the Tsai-Hill criterion, the predicted results for other load cases are shown in Table 10.10.

0° Load	90° Load	Bypass Ratio	Bearing Failure Onset Load		Percent Error
			Predicted	Experimental	
Tension	---	0%	2,325 lbf	2,592 lbf	10.3%
Tension	Tension	0%	2,100 lbf	2,356 lbf	10.9%
Tension	Tension	50%	2,025 lbf	1,778 lbf	-13.9%
Tension	Compression	0%	2,325 lbf	2,337 lbf	0.5%
Compression	---	0%	-1,900 lbf	-1,960 lbf	3.1%
Compression	Compression	50%	-2,152 lbf	-2,164 lbf	0.6%

Table 10.10 : Predictions versus Experimental Results for Various Stress Fields for the 0.260" Diameter Fastener

10.2 Discussion

The non-uniform through-thickness bearing stress distribution in single lap joints was accounted for using a non-dimensional severity factor developed in this work. Using finite element analysis, this factor was calculated for each ply by dividing the stress associated with that ply by the stress at the outer surface. However, comparison of different stress states at the same bearing load revealed an anomaly between bearing stresses and severity factors. This is evident when comparing the uniaxial tension, 0% bypass and the uniaxial compression 0% bypass load cases (see Table 10.11). For example, the bearing stress for the compressive load case at 0.2498" is higher than that for tension, yet the severity factor is lower (see Figures 10.2 and 10.3). This results from the SF being calculated using the respective outer surface stress. In hindsight, a common reference value, such as the theoretical peak bearing stress, should have been used.

Since the analysis did not provide for the increase in bearing strength which arises from fastener preload and faying surface friction, a factor, ϕ , was required to obtain good agreement with experimental results. For this laminate and fastener combination, the value of ϕ was calculated as 1.5. The use of this factor with failure predictions for 0°

tensile load cases produced typical errors of 11%. The majority of the predictions were conservative with respect to the experimental values. However, biaxial tension with a 50% bypass was non-conservative, and overpredicted the onset of bearing failure by 14%.

Z Position	Bearing Stress		Severity Factor	
	Tension	Compression	Tension	Compression
0"	-29,411 psi	-42,118 psi	1.00	1.00
0.0624"	-26,183 psi	-39,655 psi	0.89	0.94
0.1248"	-29,265 psi	-44,784 psi	1.00	1.06
0.1872"	-41,038 psi	-62,775 psi	1.40	1.49
0.2498"	-114,170 psi	-121,330 psi	3.88	2.88

Table 10.11 : Comparison of Bearing Stresses and Severity Factors for Different Load Cases at 1,160 lbf Bearing with a 0.260" Diameter Fastener

A second factor, Δ , was required to obtain accurate predictions when the applied 0° load was compressive. This factor likely resulted from the incorrect selection of the reference stress for the SF calculation, and that superposition does not distinguish a strength difference between compressive and tensile applied loads. Therefore, for this laminate, Δ was calculated as 0.82 for 0° compressive loads, and 1.00 for 0° tensile loads. Only one other compressive case was examined. The predicted bearing failure onset load was within 1% of the experimental bearing failure onset load.

11. CONCLUSIONS AND RECOMMENDATIONS FOR FUTURE WORK

11.1 Conclusions

Sixty-eight biaxial bearing tests were conducted in twelve different stress states using an orthotropic CFRP cruciform specimen. A single shear joint was simulated, with the bearing loads provided by a UNIMATIC blind fastener. The following conclusions were drawn from the experimental results.

1. Test results indicated significant variations in bearing strengths for different stress states with similar bypass ratios. This behaviour was attributed to two main factors:
 - the influence of the tangential stress component from the lateral load, and
 - the change in contact angle.
2. The difference in bearing strengths between uniaxial compression and tension load cases is insignificant for this laminate. However, for biaxial load cases, 0° compressive loads demonstrate lower bearing strengths than those for 0° tensile loads.
3. The application of a bypass load along the bearing direction resulted in strength losses for all stress states.
4. Increasing the d/t ratio resulted in a linear increase in the bearing strength. The strength increase resulted from increased joint stiffness which reduced fastener rotation.

Several definitions of bearing failure were examined. The following conclusions were made:

5. The “First Peak” load was easily detectable, but was associated with the onset of gross bearing damage.
6. The “Percent Hole Elongation” was unreliable, as the characteristic load/displacement plot did not show until the onset of gross bearing damage. Furthermore, the this plot was seldom linear, demonstrating frequent changes in slope, and discontinuities.
7. The “Bearing Strain” approach was successful in detecting bearing failure onset through a characteristic step in the bearing strain. To improve the method accuracy, failure onset loads were cross-checked against the non-bearing strain and displacement signals. However, incorrect onset points were identified in 12% of the cases.

Radiographic and fractographic inspections were used to confirm damage. Both of these methods were used primarily for qualitative assessment. Based on the results, the following conclusions were drawn:

8. Radiographic images were successful for assessing the existence of bearing damage. However, because the amount of bearing damage was small, the images had to be magnified, which left some images blurry. Furthermore, damage resulting from fastener installation occasionally obscured bearing damage, making damage confirmation difficult.

9. Fractographic inspection (destructive) was successful in detecting bearing damage. This approach was capable of providing both through-thickness damage information, as well as the magnitude of the bearing damage.

To supplement the experimental test program, finite element analysis of the joint was performed. The following conclusions were drawn from the analysis:

10. As expected, the 2-D model did not accurately predict the deformation behaviour of a single shear lap joint. The 3-D model successfully demonstrated both fastener rotation and plate bending which resulted from joint load eccentricity.
11. Both the 2-D and 3-D models overpredicted the peak bearing stress with respect to conventional analytical results. This was because the models were based on orthotropic materials, while the theory was developed for isotropic materials.
12. The 3-D model overpredicted the severity factor compared to that in literature for isotropic single shear lap joints. This was due to the orthotropic material properties, and the choice of reference point used for calculating the severity factor.

An attempt to predict the failure onset was made using an empirical approach. Several factors were calculated using experimental and numerical data. The following conclusions were drawn about the prediction method:

13. The prediction technique relied upon three correction factors to account for fastener preload and friction, stress state, and through-thickness stress variations.
14. Once the three empirical factors were calculated, the technique successfully predicted bearing failure onset for various stress states to within 14% of experimental results.

11.2 Recommendations for Future Work

Based on the findings of this thesis, the following areas are suggested for further investigation:

1. Perform additional testing to obtain a statistically significant sample size from which design allowables can be established. This requires testing different bypass and biaxial ratios such that a design envelope can be created.
2. Testing under different environmental (humidity, and temperature) conditions need to be performed to determine the effect on joint strength.
3. Determine if the trends discussed within this work are universal or laminate specific by testing other material system and stacking sequence combinations.
4. Establish the effect of the different parameters on the accuracy of the Bearing Strain method by performing experimental studies.
5. Validate and refine the 3-D finite element model such that the quantitative results are in agreement with the experimental data. The model can then be used to build design envelopes for other material/fastener combinations.
6. Redefine the severity factor in the prediction technique with respect to the theoretical peak bearing stress. Apply the present and refined prediction methods to data available in literature, and compare.
7. Evaluate the suitability of various failure criteria by using a progressive failure approach. Compare results against fractographic and radiographic evidence to establish suitable failure criteria.

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Figure 2.1 : Strengths of Various Bonded Joint Configurations[1]

a) Shearout Failure

b) Net-Section Failure

c) Bearing Failure

Figure 2.2 : Failure Modes In Mechanically Fastened Joints[1]

a) Single shear lap joint

b) Double shear lap joint

Figure 2.3 : Basic Lap Joint Configurations[104]

Figure 2.4 : Non-Uniform Through-Thickness Stress Distribution in Single Shear Lap Joints[2]

Figure 2.5 : Bearing Contact Angle

Figure 2.6 : Effect of Contact Angle, θ_c , on Bearing Strength as a Function of Fastener Clearance, λ [4]

Figure 2.7 : Relationship Between Bearing Stress and Contact Angle, θ_c [4]

Figure 2.8 : Severity Factor Used to Account for Through-Thickness Stress Variations in Mechanically Fastened Joints[5]

Figure 2.9 : Effect of Laminate Stacking Sequence on the Bearing Strength[7]

Figure 2.10 : Preferred Stacking Sequences to Obtain Maximum Bearing Strength[8]

Figure 2.11 : Effect of Fastener Preload on Bearing Strength[19]

Figure 2.12 : Schematic of Load Transfer Within a Multi-Fastener Joint[4]

Figure 2.13 : Bearing Load as a Function of Bypass Load for Varying W/d [8]

Figure 2.14 : Variation of Bearing Stress with W/d [9]

Figure 2.15 : Variation of Bearing Stress with e/d [9]

Loading Conditions	K_T^∞ at A	K_T^∞ at B
Uniaxial, $\sigma_2 = 0$	3	-1
Biaxial, $\sigma_1 = \sigma_2$	2	2
Pure Shear, $\sigma_2 = -\sigma_1$	4	-4

Figure 2.16 : Loading Configuration Effect on K_T^∞ in an Isotropic Plate

Figure 3.1 : Design Methodology for Mechanical Joints[25]

OPEN HOLE :

PIN-LOADED HOLE :

SUPERPOSITION :

of Open and Pin-Loaded Holes

Figure 3.2 : Superposition Principle Applied to a Bearing/Bypass Problem

Figure 3.3 : Assumed Energy Regions by Waddoups et al.[22]

- a) Unidirectional loading of an Off-Axis Specimen used to obtain both tensile and biaxial data[105]

- b) Resolved lamina principal stresses on a unit element subjected to a unidirectional load offset from the principal fibre direction

Figure 5.1 : Biaxial Stress State Obtained by Loading an Off-Axis Specimen

Figure 5.2 : Effect of Thickness to Diameter Ratio on the Through-Thickness Stress Variation[77]

Figure 5.3 : Cruciform Specimen Designed by Monch and Galster Used Slotted Arms to Reduce the Shear Interaction Caused by the Introduction of the Biaxial Load[80]

A				B				C				D				E			
1	0*	25	0	1	0*	25	0	1	0*	25	a	1	0*	25	0	1	0*	25	0
2	45	26	0	2	a	26	0	2	a	26	a	2	45	26	0	2	45	26	0
3	45	27	0	3	a	27	0	3	a	27	a	3	-45	27	0	3	-45	27	0
4	-45	28	0	4	a	28	0	4	a	28	a	4	45	28	0	4	45	28	0
5	-45	29	90	5	a	29	a	5	a	29	90	5	-45	29	90	5	-45	29	90
6	0	30	-45	6	0	30	a	6	a	30	a	6	0	30	90	6	0	30	90
7	0	31	45	7	0	31	a	7	a	31	a	7	0	31	90	7	0	31	90
8	45	32	0	8	a	32	0	8	a	32	a	8	45	32	0	8	90	32	0
9	-45	33	0	9	a	33	0	9	a	33	a	9	-45	33	0	9	90	33	0
10	90	34	-45	10	a	34	a	10	90	34	a	10	90	34	-45	10	90	34	-45
11	90	35	45	11	a	35	a	11	90	35	a	11	90	35	45	11	90	35	45
12	45	36	90	12	a	36	a	12	a	36	90	12	45	36	90	12	45	36	90
13	-45	37	90	13	a	37	a	13	a	37	90	13	-45	37	90	13	-45	37	90
14	90	38	-45	14	a	38	a	14	90	38	a	14	90	38	-45	14	90	38	-45
15	90	39	45	15	a	39	a	15	90	39	a	15	90	39	45	15	90	39	45
16	45	40	90	16	a	40	a	16	a	40	90	16	45	40	90	16	45	40	90
17	-45	41	90	17	a	41	a	17	a	41	90	17	-45	41	90	17	-45	41	90
18	0	42	-45	18	0	42	a	18	a	42	a	18	0	42	-45	18	0	42	90
19	0	43	45	19	0	43	a	19	a	43	a	19	0	43	45	19	0	43	90
20	45	44	0	20	a	44	0	20	a	44	a	20	90	44	0	20	90	44	0
21	-45	45	0	21	a	45	0	21	a	45	a	21	90	45	0	21	90	45	0
22	90	46	-45	22	a	46	a	22	90	46	a	22	90	46	-45	22	90	46	-45
23	0	47	-45	23	0	47	a	23	a	47	a	23	0	47	45	23	0	47	45
24	0	48	45	24	0	48	a	24	a	48	a	24	0	48	-45	24	0	48	-45
		49	45			49	a			49	a			49	45			49	45

* - scrim cloth ply

a - FM300K film adhesive

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Figure 5.5 : Thickness Variation of Manufactured Cruciform Specimens

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b) Radiographic Image of Specimen #957-200 (2,000 lbf)

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a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #953-200

b) Radiographic Image of Specimen #953-200 (2,000 lbf)

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b) Radiographic Image of Specimen #943-312 (-5,200 lbf)

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b) Radiographic Image of Specimen #968-200 (-1,160 lbf)

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a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #956-200

b) Radiographic Image of Specimen #956-200 (-1,300 lbf)

Figure 8.34 : Test Results of Specimen #956-200 Subjected to Compression/Compression, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #955-200

b) Radiographic Image of Specimen #955-200 (-1,000 lbf)

Figure 8.35 : Test Results of Specimen #955-200 Subjected to Compression/Compression, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #952-200

b) Radiographic Image of Specimen #952-200 (-1,300 lbf)

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b) Radiographic Image of Specimen #964-260 (-2,100 lbf)

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b) Radiographic Image of Specimen #956-260 (-3,000 lbf)

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a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #952-260

b) Radiographic Image of Specimen #952-260 (-3,000 lbf)

Figure 8.39 : Test Results of Specimen #952-260 Subjected to Compression/Compression, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #958-312

b) Radiographic Image of Specimen #958-312 (-5,400 lbf)

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b) Radiographic Image of Specimen #949-312 (-4,500 lbf)

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b) Radiographic Image of Specimen #960-200 (-1,060 lbf)

Figure 8.47 : Test Results of Specimen #960-200 Subjected to Compression/Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #962-200

b) Radiographic Image of Specimen #962-200 (-1,225 lbf)

Figure 8.48 : Test Results of Specimen #962-200 Subjected to Compression/Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #966-200

b) Radiographic Image of Specimen #966-200 (-1,900 lbf)

Figure 8.49 : Test Results of Specimen #966-200 Subjected to Compression/Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #959-260

b) Radiographic Image of Specimen #959-260 (-2,000 lbf)

Figure 8.50 : Test Results of Specimen #959-260 Subjected to Compression/Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #953-260

b) Radiographic Image of Specimen #953-260 (-2,000 lbf)

Figure 8.51 : Test Results of Specimen #953-260 Subjected to Compression/Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #952-312

b) Radiographic Image of Specimen #952-312 (-4,500 lbf)

Figure 8.52 : Test Results of Specimen #952-312 Subjected to Compression/Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #947-312

b) Radiographic Image of Specimen #947-312 (-4,500 lbf)

Figure 8.53 : Test Results of Specimen #947-312 Subjected to Compression/Tension, 0% Bypass

Figure 8.54 : Fractography of Specimen #952-312

Figure 8.55 : Fractography of Bearing Face of Specimen #952-312 (Inset of Above)

Figure 8.56 : Fractography of Non-Bearing Face of Specimen #952-312

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #961-200

b) Radiographic Image of Specimen #961-200 (-1,300 lbf)

Figure 8.57 : Test Results of Specimen #961-200 Subjected to Compression/Tension, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #963-200

b) Radiographic Image of Specimen #963-200 (-2,500 lbf)

Figure 8.58 : Test Results of Specimen #963-200 Subjected to Compression/Tension, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #965-200

b) Radiographic Image of Specimen #965-200 (-1,250 lbf)

Figure 8.59 : Test Results of Specimen #965-200 Subjected to Compression/Tension, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #963-260

b) Radiographic Image of Specimen #963-260 (-2,100 lbf)

Figure 8.60 : Test Results of Specimen #963-260 Subjected to Compression/Tension, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #947-260

b) Radiographic Image of Specimen #947-260 (-2,000 lbf)

Figure 8.61 : Test Results of Specimen #947-260 Subjected to Compression/Tension, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #951-312

b) Radiographic Image of Specimen #951-312 (-4,700 lbf)

Figure 8.62 : Test Results of Specimen #951-312 Subjected to Compression/Tension, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #950-312

b) Radiographic Image of Specimen #950-312 (-4,400 lbf)

Figure 8.63 : Test Results of Specimen #950-312 Subjected to Compression/Tension, 50% Bypass

Figure 8.64 : Fractography of Specimen #951-312

Figure 8.65 : Fractography of Bearing Face of Specimen #951-312 (Inset of Above)

Figure 8.66 : Fractography of Non-Bearing Face of Specimen #951-312

Figure 8.67 : Fractography of Specimen #963-200

Figure 8.68 : Fractography of Bearing Face of Specimen #963-200

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #966-260

b) Radiographic Image of Specimen #966-260 (2,400 lbf)

Figure 8.69 : Test Results of Specimen #966-260 Subjected to Tension/Compression, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #961-260

b) Radiographic Image of Specimen #961-260 (-2,200 lbf)

Figure 8.70 : Test Results of Specimen #961-260 Subjected to Tension/Compression, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #948-312

b) Radiographic Image of Specimen #948-312 (4,800 lbf)

Figure 8.71 : Test Results of Specimen #948-312 Subjected to Tension/Compression, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #956-312

b) Radiographic Image of Specimen #956-312 (4,800 lbf)

Figure 8.72 : Test Results of Specimen #956-312 Subjected to Tension/Compression, 0% Bypass

Figure 8.73 : Photograph of Faying Surface of Specimen #961-260

Figure 8.74 : Fractography of Bearing Face of Specimen #961-260

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #962-260

b) Radiographic Image of Specimen #962-260 (2,200 lbf)

Figure 8.75 : Test Results of Specimen #962-260 Subjected to Tension/Compression, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #965-260

b) Radiographic Image of Specimen #965-260 (1,600 lbf)

Figure 8.76 : Test Results of Specimen #965-260 Subjected to Tension/Compression, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #955-312

b) Radiographic Image of Specimen #955-312 (5,000 lbf)

Figure 8.77 : Test Results of Specimen #955-312 Subjected to Tension/Compression, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #946-312

b) Radiographic Image of Specimen #946-312 (4,400 lbf)

Figure 8.78 : Test Results of Specimen #946-312 Subjected to Tension/Compression, 50% Bypass

Figure 8.79 : Fractography of Bearing Face of Specimen #946-312

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #971-200

b) Radiographic Image of Specimen #971-200 (1,400 lbf)

Figure 8.80 : Test Results of Specimen #971-200 Subjected to Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #972-200

b) Radiographic Image of Specimen #972-200 (2,000 lbf)

Figure 8.81 : Test Results of Specimen #972-200 Subjected to Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #969-260

b) Radiographic Image of Specimen #969-260 (2,900 lbf)

Figure 8.82 : Test Results of Specimen #969-260 Subjected to Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #970-260

b) Radiographic Image of Specimen #970-260 (2,500 lbf)

Figure 8.83 : Test Results of Specimen #970-260 Subjected to Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #974-260

b) Radiographic Image of Specimen #974-260 (2,500 lbf)

Figure 8.84 : Test Results of Specimen #974-260 Subjected to Tension, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #970-200

b) Radiographic Image of Specimen #970-200 (1,800 lbf)

Figure 8.85 : Test Results of Specimen #970-200 Subjected to Tension, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #972-260

b) Radiographic Image of Specimen #972-260 (2,000 lbf)

Figure 8.86 : Test Results of Specimen #972-260 Subjected to Tension, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #973-260

b) Radiographic Image of Specimen #973-260 (2,200 lbf)

Figure 8.87 : Test Results of Specimen #973-260 Subjected to Tension, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #971-260

b) Radiographic Image of Specimen #971-260 (3,400 lbf)

Figure 8.88 : Test Results of Specimen #971-260 Subjected to Tension, 50% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #974-200

b) Radiographic Image of Specimen #974-200 (-2,300 lbf)

Figure 8.89 : Test Results of Specimen #974-200 Subjected to Compression, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #973-200

b) Radiographic Image of Specimen #973-200 (-2,000 lbf)

Figure 8.90 : Test Results of Specimen #973-200 Subjected to Compression, 0% Bypass

a) Experimental Results of Strain and Displacement versus Bearing Load for Specimen #969-200

b) Radiographic Image of Specimen #969-200 (-2,200 lbf)

Figure 8.91 : Test Results of Specimen #969-200 Subjected to Compression, 50% Bypass

Figure 8.92 : Effect of Bypass Load on Bearing Strength of Tension/Tension Specimens

Figure 8.93 : Effect of Bypass Load on Bearing Strength of Compression/Compression Specimens

Figure 8.94 : Effect of Bypass Load on Bearing Strength of Tension/Compression Specimens

Figure 8.95 : Effect of Bypass Load on Bearing Strength of Compression/Tension Specimens

Figure 8.96 : Bearing Strength for Various Stress States, 0% Bypass

Figure 8.97 : Bearing Strength for Various Stress States, 50% Bypass

Figure 8.98 : Typical Effect of d/t on the Bearing Strength

Figure 9.1 : Discretization of Structural Component Into Finite Elements[90]

Figure 9.2 : 2-D Finite Element Model of Pin-Loaded Cruciform

Figure 9.3 : Boundary Conditions for 2-D Model

Figure 9.4 : Load Conditions Applied to the 2-D Model

Overall 3-D Model

a) Load Conditions Applied to the Specimen

Figure 9.5 : 3-D Finite Element Model

b) View Showing Lower Left Corner of Hole in Composite Specimen

c) View Showing Lower Left Corner of Titanium Pin

Figure 9.5 : 3-D Finite Element Model (con't)

d) Showing Top Left Corner of Hole in Titanium Plate

e) Showing Lower Right Corner of Titanium Plate

Figure 9.5 : 3-D Finite Element Model (con't)

Figure 9.6 : Hole and Pin Deformation Under Loading

Figure 9.7 : Bearing Stress for a Uniaxial Loaded Specimen with 0% Bypass, 2-D Model

Figure 9.8 : Through-Thickness Bearing Stress for a Uniaxial Loaded Specimen, with 0% Bypass

OPEN HOLE :

PIN-LOADED HOLE :

SUPERPOSITION :

of Open and Pin-Loaded
Holes

Figure 10.1 : Superposition Principle Applied to Tension/Tension, 0% Bypass Loading

Figure 10.2 : 3-D FEA Through-Thickness Bearing Stress for Uniaxial Pure Bearing

Figure 10.3 : 3-D FEA Through-Thickness Severity Factors for Uniaxial Pure Bearing

APPENDICES

SPECIMEN MANUFACTURING

Fabrication

Fabrication of these biaxial specimens is complex due to their configuration and stacking sequence. In a collaborative effort between SMPL-IAR, Carleton University, and CAE Aviation in Edmonton, an economic and repeatable procedure was developed for the manufacturing of the cruciform specimens.

Materials

The material of interest was graphite/epoxy AS4/3501-6, Type III, manufactured by Hercules Inc. in October of 1995, batch number 7941-4, and supplied in 12" wide by 500' long prepreg rolls.

The filler material was FM300K film adhesive, batch number 5861-0001, manufactured by CYTEC Engineered Materials Inc. in October of 1996, and supplied in roll form. The adhesive had an average weight of 0.05 psf, and was selected to match the cured AS4/3501-6 ply thickness. Any filler material would suffice subject to it having a final cured thickness equivalent to, and a cure cycle compatible with, that of AS4/3501-6. Originally FM73 was selected due to its availability, but was later rejected since it was not cure compatible.

To maintain the shelf lives, both materials were stored in sealed bags below 0°F when not in use.

Cutting the Material

All required pieces were cut to size prior to beginning the layup. Rolls were removed from the freezer and while still sealed, left to thaw to room temperature under circulating air. This prevented both fibre breakage during unrolling and condensation on the material surface. Once at room temperature, templates were used to cut the material to the required patterns. Both the graphite/epoxy prepreg and FM300K film adhesive were cut using a sharp utility knife. To minimize waste and improve the efficiency of the cutting

process, large quantities of a specific pattern were cut at a time. Cut material was stacked in the quantities required for each specimen, then sealed in bags and returned to the freezer.

Preparation

Specimen layup is quite detailed, so documentation was maintained on each ply to ensure proper sequencing and orientation. This documentation consisted of separate layup sheets for each of the 48 lamina. In addition, a check sheet to record which plies were laid down was kept. Another sheet documented the quantity, size, and orientation of the required pieces to complete one specimen.

Layup and Curing Plates

To facilitate layup, a multipurpose layup and curing jig was designed and manufactured. The purpose of the jig was two fold: to ensure that the specimen was square and conformed to the required dimensions; and to serve as a resin dam during the curing process. Two jigs were eventually made, each slightly different to improve on previous deficiencies. Jigs were made from aluminum 2024-T4 stock $\frac{3}{4}$ " and $\frac{1}{2}$ " thick.

Layup

Precut material was removed from the freezer and allowed to defrost for two hours. Bags remained sealed, and air was circulated over them to reduce condensation. While defrosting, the corner plates were secured in place to ensure proper sizing of the specimen. Once the pieces were fully defrosted, they were positioned around the layup jig to minimize handling and time (see Figure A.1). Layup started at ply 2 as the first ply was the peel ply. Ensuring that all exposed surfaces were free from debris, the centre region 'A' was first laid down (see Figure A.2a). Plies were laid such that they looked like their respective layup sheet when the backing paper was removed. Next regions 'B' and 'C' were positioned such that their edge fitted flush against the edge of region 'A' (see Figure A.2b). Finally regions 'D' and 'E' were fitted in place with their edges against those of regions 'B' and 'C' (see Figure A.2c). When a ply was completed, the ply number was crossed out on the check sheet and the number written on the carrier paper of region 'A'. This coding enabled each ply to be traced should it be so required.

The carrier paper was then removed using the reverse order used for layup, beginning with the pieces furthest away to avoid contact with the prepreg (see Figures A.2d, e, and f). This process was repeated until all 48 plies were completed. Finally, another sheet of peel ply was placed on top of the final ply to protect it from contamination. Completed layups were removed from the jig, bagged, and placed flat in the freezer until ready for curing.

During layup, oversized pieces were trimmed before final positioning. Any pieces which could not be trimmed were discarded - accounting for approximately 3% of all material cut. Material which was contaminated beyond cleaning was also discarded - accounting for another 3% of waste.

After the first eight specimens (#941 to #948), it was discovered that 0.05 psf FM300K cured slightly thicker than AS4/3501-6. As no smaller size was available in that style, it was decided that the occasional ply of 0.05 psf adhesive would be omitted to maintain a constant laminate thickness. Therefore, all FM300K pieces were omitted from plies 8, 20, 30, and 43 for specimens #949 to #957.

Specimens were not debulked at regular intervals during the layup as this process was found to considerably increase the labour, while adding no significant improvement to the final specimen quality. Both hot and cold debulking methods were tried on the first few specimens, and from ultrasound data no discernible differences were found between specimens. Therefore, a single one hour debulk was performed at the end of the layup. To assist in removing trapped air during the debulk, the laminate was lightly perforated using the tip of a utility knife every four to six plies during layup.

Bagging Sequence

Curing composite laminates requires that a certain amount of resin be extracted from the system in a controlled manner to obtain the desired mechanical properties. This task is usually performed during the cure, and the amount of resin removed is determined by the

bagging sequence. Figure A.3 shows a schematic of the bagging sequence used for curing the biaxial specimens.

Initially, layup plates were cleaned with Methyl Ethyl Ketone (MEK) and coated with a release agent (eg: Frekote 700™) to protect against resin damage. Pressure sensitive tape (eg: Tacky Tape™) was placed in the recesses of the Baseplate (see Figure A.4a). A clean sheet of non-porous release film was stretched over the Baseplate and fixed to the pressure sensitive tape. The release film was trimmed along the centreline of the tape, leaving about a 1/4" of exposed tape to act as a seal (see Figure A.4b). Next, using the completed specimen as a guide, the Corner Plates were positioned as required and bolted in place (see Figure A.4c). Once the Corner Plates were fastened, the specimen was removed, and a sheet of thin breather cloth laid down. The thin breather cloth promotes air and resin draw through the bleeders which assists in the debulking and consolidation of the laminate. Next, four sheets of 7781 style fibreglass cloth and a sheet of porous release film were laid down (see Figure A.4d). The porous release film permitted resin flow into the fibreglass, while still allowing the fibreglass to be separated from the specimen after curing. The specimen was placed in the jig, then another sheet of porous release film, followed by four fibreglass bleeders and a thin air breather, were placed on top (see Figure A.4e and f).

Next, the End Bars were prepared by placing Polyimide Film Tape along one edge, and pressure sensitive tape along a perpendicular edge and the ends (see Figure A.4g). End Bars were placed between the Corner Plates with the pressure sensitive tape down and the Polyimide tape against the specimen. The Polyimide tape prevented resin from adhering to the End Bars, while the pressure sensitive tape ensured a good seal to prevent resin flow outside the jig.

Another sheet of porous release film was placed over the specimen such that it covered the entire jig. The Caul Plate was placed on top of the release film and the specimen. More pressure sensitive tape was used to seal around the outside perimeter of the Caul

Plate, and the porous release film was then secured to the tape (see Figure A.4h). Another layer of non-porous release film was placed over the entire jig, followed by a sheet of breather cloth (see Figure A.4i). Finally, a sheet of bagging material, complete with vacuum coupling, was placed over the jig and sealed to the perimeter of the Baseplate using pressure sensitive tape (see Figure A.4j). The integrity of the bag and seal were then checked by connecting it to a 25 in. Hg vacuum source.

Originally, 10 plies of 7781 style fibreglass cloth were used to bleed from the top surface of the laminate; however, this method gave a pronounced curvature to the specimens. This problem was resolved by bleeding from both sides of the laminate which has reduced, but not eliminated the curvature.

Curing

Specimens were cured in a, 200 psig, 700°F autoclave with working dimensions of 6' long by 4' in diameter. The cure cycle followed that prescribed AS4/3501-6 cure cycle as specified Figure A.5 shows the temperature, pressure, and vacuum cycles for curing AS4/3502-6 as recommended by Hercules Inc.. Slight variation from the cure cycle can result in degraded material properties and/or physical irregularities. Accidental deviation from the cure cycle resulted in one defective specimen being produced.

Post-Cure

After the specimen had cooled and was removed from the jig, it was subjected to a four-hour post-cure at 350°F and atmospheric pressure. The specimen was then cooled overnight, after which it was checked for voids and delaminations using ultrasound. The purpose of the post-cure is to relieve any mechanical stresses which developed during the curing process.

Machining

Part of the function of the curing plates was to minimize after cure machining. However, some touch up work was required to improve the quality of the specimens. Specimens tended to have sharp corners on the upper surface due to the gap between the Caul Plate and the Corner Plates. A file was used to remove any lips and burrs which formed,

leaving specimens with smooth corners making them easier to handle. The polyimide covered end bars at the ends of the specimen arms provided a good finish; however, a 6" diameter, 60 grit diamond plated cutting wheel was used to cut ¼" off the ends to remove any surface irregularities.

Further machining was required to permit load introduction into the specimen. Each mechanical gripping system was designed to introduce the load into the gripping region through four bolts. Four ½" diameter holes were machined into each of the specimen arms on a CNC mill using solid carbide drill bits. The holes were offset 1" from the arm ends, and on 2" centres about the arm centreline (see FigureA.6).

Figure A.1 : Organization of precut material around the layup jig to minimize handling and time when laying up a biaxial cruciform specimen

Figure A.2a : *Layup Step 1* - Placement of the region 'A' pieces on the peel ply, tacky side down

Figure A.2b : *Layup Step 2* - Placement of region 'B' and 'C' pieces with their edges flush against those of region 'A'

Figure A.2c : *Layup Step 3* - Placement of region 'D' and 'E' pieces against those of regions 'B' and 'C'

Figure A.2d : *Layup Step 4* - Removal of protective carrier paper from regions 'D' and 'E', beginning at the piece furthest away to prevent physical contact

Figure A.2e : *Layup Step 5* - Removal of protective carrier paper from regions 'B' and 'C', exposing the FM300K adhesive

Figure A.2f : *Layup Step 6* - Completion of ply by removal of carrier paper from region 'A', exposing the AS4/3501-6 prepreg oriented in the -45 direction

1. Release Film	Non-porous Teflon coated
2. Air Breather	1 Ply Airweave (or equivalent)
3. Bleeders	4 plies 7781 style Fiberglass cloth
4. Release Film	Porous
5. Peel Ply	Airtech Release ply C (or equivalent)
6. Prepreg Layup	48 Ply AS4/3501-6 laminate
7. Vacuum Bag	Nylon high temperature
8. Vacuum Coupling	
9. Caul Plate	Aluminum, ¼" thick (nominal) with thin uniform coat of release agent
10. Sealant	High temperature, pressure sensitive tape
11. Resin Dam	Aluminum, ¾" thick (nominal) with thin uniform coat of release agent
12. Baseplate	Aluminum, ½" thick (nominal) with thin uniform coat of release agent

Figure A.3 : Bagging Sequence Use for Curing AS4/3501-6 Thick Laminates

Figure A.4a : *Bagging Step 1* - Pressure sensitive tape is placed in the recesses of the Baseplate to provide a adequate seal against resin leakage

Figure A.4b : *Bagging Step 2* - A shaped sheet of non-porous release film is used to prevent resin damage from occurring to the Baseplate

Figure A.4c : *Bagging Step 3* - Using the specimen, Corner Plates are located in their proper positions before being bolted down

Figure A.4d : *Bagging Step 4* - An air breather, followed by four plies of fibreglass bleeders, and then a sheet of porous release file are laid in the jig

Figure A.4e : *Bagging Step 5* - The specimen is placed back in the jig on top of the porous release film

Figure A.4f : *Bagging Step 6* - Another sheet or porous release film, then four fibreglass bleeders and an air breather are laid on top of the specimen

Figure A.4g : *Bagging Step 7* - End Bars are prepared by covering one side with Polymide tape, and a perpendicular side with pressure sensitive tape with it stretched over the ends

Figure A.4h : *Bagging Step 8* - With End Bars in position, a sheet of porous release film is secured using pressure sensitive tape, and the Caul Plate placed on top of the specimen

Figure A.4i : *Bagging Step 9* - A sheet of non-porous release film followed by an air breather are used to cover the entire jig

Figure A.4j : *Bagging Step 10* - A vacuum bag and coupling are sealed to the Baseplate using pressure sensitive tape to provide an air tight unit

AS4/3501-6 Curing Cycle

1. Place vacuum bagged lay-up in autoclave and close autoclave.
2. Apply minimum vacuum of 25 in. of Hg.
3. Apply 85 ± 5 psig.
4. At a rate of 30°F to 50°F per minute, raise the laminate temperature to $240 \pm 10^\circ\text{F}$, while holding 85 ± 5 psig autoclave pressure and 20 to 29 in. of Hg vacuum.
5. Hold at $240 \pm 10^\circ\text{F}$, 85 ± 5 psig, and 20 to 29 in. of Hg for 60 to 70 minutes.
6. Raise pressure to 100 ± 5 psig and vent the vacuum bag to atmospheric pressure.
7. Raise the temperature at a rate of 30°F to 50°F per minute to $350 \pm 10^\circ\text{F}$. Hold for 120 ± 10 minutes under a 100 ± 5 psig autoclave pressure.
8. at a rate of $5 \pm 10^\circ\text{F}$ per minute, lower laminate temperature to 200°F . Release autoclave pressure.
9. Remove lay-up from autoclave and unbag.

Figure A.5 : Manufacturer's recommended cure cycle for AS4/3501-6

Figure A.6 : Hole Locations for Gripping

Procedure for Loading Cruciform Specimens

This section covers the procedures for installation of the specimen into the loadframe in preparation for testing. The mounting of strain gauges, lead wires, and titanium plate installation were completed beforehand (see Figure B.1).

The titanium plate was installed on the adapter ring (see Figure B.2), by aligning the metal dowel pins on the adapter ring with the holes in the titanium plate. The titanium plate was firmly pressed onto the adapter ring until no space between the two existed (see Figure B.3). The snug fit of the dowel pins required that they be secured into place with a couple of light hammer blows. With the adapter ring attached, a Teflon sheets was placed on the back of the specimen, over the adapter ring.

The specimen was placed into the loadframe by inserting the adapter ring in the yoke at the center of the loadframe, (see Figure B.4 & Figure B.5). The 0° specimen orientation was aligned with the horizontal actuators. A second Teflon sheet was placed on the front of the specimen, followed by placing an anti-buckling plate. The plate was then secured to the loadframe using four bolts.

With the hydraulics set to "low pressure", and force feedback selected, actuators were positioned for installation. Actuators control was in terms of a positive or a negative force applied. A positive force applied tension, and a negative, compression. A typical "fast" movement by an actuator has a setting of approximately +/- 500 lb_f. A typical "slow" movement has a setting of approximately +/- 100 lb_f.

Starting with one arm, the four holes in each actuator grip (see Figure B.6), were aligned with the four holes in the specimen arm by using a combination of fast and slow movements. As the holes came into alignment, shoulder bolts were inserted. Once all the bolts were in, each was tightened a few turns. This process was repeated for the remaining three arms.

With all four arms installed in the loadframe, one of the middle cap nuts on the front of each grip, was finger-tightened to align the specimen with the centre of the loadframe. The centre was determined by obtaining an equal gap between the arms and the grip walls. On the back side, the corresponding cap nuts were tightened with a wrench. The other center cap nuts, followed by the outer cap nuts, were then tightened using the procedure described above.

With all the cap nuts in contact with the specimen, they were then tightened an additional 1/8 of a turn using an alternating pattern. The shoulder bolts were then tightened until the bolt heads were approximately 0.25" from the nut.

The bolts supporting the anti-buckling plate were finger-tightened, and the strain gauge leads connected to the data acquisition equipment (see Figure B.7). With the hydraulics set to "hi pressure" and the control limits set, the test then was started.

Procedure for Unloading Specimens

Once testing was completed, specimen removal began by returning the hydraulics to the "low pressure" setting, and turning off the limits off.

On the back side, all the cap nuts were loosened. The shoulder bolts were then removed from one grip at a time. Once the four bolts were removed, the grip were retracted from the specimen until clear. This was repeated for the remaining three grips. Once all grips were clear, the hydraulics were shut off.

The anti-buckling plate was removed, followed by the specimen and adapter ring. With the aid of a screwdriver, the adapter ring was pried from the titanium plate. Another specimen can now be loaded.

Figure B.1 : Specimen and Titanium Plate Prior to Installation in the Loadframe

Figure B.2 : Adapter Ring

Figure B.3 : Adapter Ring Attached to Specimen

Figure B.4 : Placing Specimen in Yoke on Loadframe

Figure B.5 : Specimen in Loadframe without Cover Plate

Figure B.6 : Grip on Loadframe with Specimen Arm Installed

Figure B.7 : Installed Specimen in Loadframe Ready for Test