Impact Resistance of Composite Scarf Joints under Load

A thesis submitted in fulfilment of the requirements for the degree of Master of Engineering

by

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The work described in this thesis was conducted as part of a research program of the Cooperative Research Centre for Advanced Composite Structures (CRC-ACS) Ltd
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Declaration

I certify that except where due acknowledge has been made, the work is that of the author alone; the work has not been submitted previously, in whole or in part, to qualify for any other academic award; the content of the thesis is the results of work which has been carried out since the official commencement date of the approved research program; any editorial work, paid or unpaid, carried out by a third party is acknowledged; and, ethics procedures and guidelines have been followed.

______________________________
Min Ki Kim
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# Abbreviations and Acronyms

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<tr>
<th>Term</th>
<th>Definition</th>
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<tbody>
<tr>
<td>2D, 3D</td>
<td>Two-dimensional, Three-dimensional</td>
</tr>
<tr>
<td>BVID</td>
<td>Barely Visible Impact Damage</td>
</tr>
<tr>
<td>CRC-ACS</td>
<td>Cooperative Research Centre for Advanced Composite Structures</td>
</tr>
<tr>
<td>CZM</td>
<td>Cohesive Zone Model</td>
</tr>
<tr>
<td>DOF</td>
<td>Degree of Freedom</td>
</tr>
<tr>
<td>DSTO</td>
<td>Defence Science and Technology Organisation</td>
</tr>
<tr>
<td>EPZ</td>
<td>Eight Point Zero</td>
</tr>
<tr>
<td>FOD</td>
<td>Foreign Object Damage</td>
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<tr>
<td>FPF</td>
<td>Fourteen Point Five</td>
</tr>
<tr>
<td>FTPZ</td>
<td>FourTeen Point Zero</td>
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<td>HW</td>
<td>Heavy Weight</td>
</tr>
<tr>
<td>HWSD</td>
<td>Heavy Weight Spring Drop</td>
</tr>
<tr>
<td>NTPZ</td>
<td>NineTeen Point Zero</td>
</tr>
<tr>
<td>LW</td>
<td>Light Weight</td>
</tr>
<tr>
<td>LWHD</td>
<td>Low Weight Hand Drop</td>
</tr>
<tr>
<td>LWSD</td>
<td>Low Weight Spring Drop</td>
</tr>
<tr>
<td>LWSD</td>
<td>Low Weight Spring Drop</td>
</tr>
<tr>
<td>OPE</td>
<td>One Point Seven</td>
</tr>
<tr>
<td>PCL</td>
<td>Patran Command Language</td>
</tr>
<tr>
<td>SNEG</td>
<td>Surface NEGative</td>
</tr>
<tr>
<td>SPOS</td>
<td>Surface POSitive</td>
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## Nomenclature

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Unit</th>
<th>Definition</th>
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<tr>
<td>$A_C$</td>
<td>Kg*m/s</td>
<td>Accumulative Area</td>
</tr>
<tr>
<td>$a$</td>
<td>m/s$^2$</td>
<td>Acceleration</td>
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<tr>
<td>$E_{a}, E_{i}$</td>
<td>MPa</td>
<td>Young’s modulus</td>
</tr>
<tr>
<td>$f_o$</td>
<td>Hz</td>
<td>Oscillation frequency</td>
</tr>
<tr>
<td>$F_C, F_I, F_R$</td>
<td>kN</td>
<td>Contact, interface and rigid tub forces</td>
</tr>
<tr>
<td>$G_{12}, G_{13}, G_{23}$</td>
<td>GPa</td>
<td>Shear modulus in fibre, matrix, and though-thickness directions</td>
</tr>
<tr>
<td>$G_{I_C, II_C, III_C}$</td>
<td>N/mm</td>
<td>Critical fracture toughness in normal, in-plane and transverse modes</td>
</tr>
<tr>
<td>$g_{eq}$</td>
<td>m/s$^2$</td>
<td>Equivalent gravity</td>
</tr>
<tr>
<td>$h$</td>
<td>m</td>
<td>Drop height</td>
</tr>
<tr>
<td>$I$</td>
<td>mm$^4$</td>
<td>Area moment of inertia</td>
</tr>
<tr>
<td>$K$</td>
<td>N/m</td>
<td>Stiffness</td>
</tr>
<tr>
<td>$L$</td>
<td>mm</td>
<td>Length of span</td>
</tr>
<tr>
<td>$\Delta L$</td>
<td>mm</td>
<td>Applied displacement</td>
</tr>
<tr>
<td>$M$</td>
<td>kg</td>
<td>Total mass of impactor</td>
</tr>
<tr>
<td>$M_{comp}$</td>
<td>Kg</td>
<td>Total mass of composite plate</td>
</tr>
<tr>
<td>$m_I, m_R$</td>
<td>kg</td>
<td>Mass of main body and rigid tub</td>
</tr>
<tr>
<td>$P$</td>
<td>kN</td>
<td>Load</td>
</tr>
<tr>
<td>$s, s_t$</td>
<td>mm</td>
<td>Deflection of the coupon</td>
</tr>
<tr>
<td>$t$</td>
<td>mm</td>
<td>Thickness of coupon</td>
</tr>
<tr>
<td>$V$</td>
<td>volt</td>
<td>Voltage</td>
</tr>
<tr>
<td>$v_i, v_r$</td>
<td>m/s</td>
<td>Inbound and rebound velocity</td>
</tr>
<tr>
<td>$\alpha_{adh}, \alpha_{comp}$</td>
<td>-----</td>
<td>Curve-fit parameter for Power Law used for adhesive and composite delamination</td>
</tr>
<tr>
<td>$\beta$</td>
<td>-----</td>
<td>Parameter for cohesive stiffness</td>
</tr>
<tr>
<td>$\delta^o, \delta^f$</td>
<td>mm</td>
<td>Displacement value at initiation and failure in the traction-displacement law</td>
</tr>
<tr>
<td>$\varepsilon_{as}, \varepsilon_{is}, \varepsilon_{rp}$</td>
<td>mm/mm</td>
<td>Absolute, initial and relative peak strain</td>
</tr>
<tr>
<td>$\theta$</td>
<td>degree</td>
<td>Scarf angle</td>
</tr>
<tr>
<td>$\sigma_p, \sigma_{ult}$</td>
<td>MPa</td>
<td>Normal and ultimate stress</td>
</tr>
<tr>
<td>$\tau^o$</td>
<td>MPa</td>
<td>Traction stress at initiation in the traction-displacement law</td>
</tr>
<tr>
<td>$\tau_s$</td>
<td>MPa</td>
<td>Shear stress</td>
</tr>
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<td>$&lt;:&gt;$</td>
<td>-----</td>
<td>MacAuley bracket</td>
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Summary

Composite structures for aircraft service before and after repair are vulnerable to foreign object damage due to their poor through-the-thickness damage resistance. However, many studies are not aware of the importance of considering pre-loading during an impact event, which is a more realistic impact scenario for aerospace structures. Only a few impact studies have been conducted so far for scarf joint repairs in preloaded composite structures. This project completes an extensive experimental program. A numerical methodology using the finite element program Abaqus was developed.

The tested composite material is a quasi-isotropic Cycom T300/970 prepreg lay-up with 16 plies. In order to represent low and medium impact conditions with a light-weight foreign object, an impactor of 410 g was used throughout the entire test series. Impact force-time history and strain-time history graphs were acquired. Composite laminate coupons and scarf joints test series were carried out in wide ranges of impact energy (2 – 19 J) and a large range of tensile pre-strain levels (0 – 5000 µε). Pre-straining the composite increased the size of the damage area. The work also showed that composite laminate coupons can be used to some extent to replicate the impact and damage response of composite scarf joints.

For the scarf joint, the majority of the damage occurred in adherend regions rather than the adhesive region, but the adhesive damage increased as the pre-strain increased, ultimately leading to catastrophic failure. Delamination is the most dominant failure type, although other typical composite failure modes such as fibre fracture and matrix cracking were also observed. Most importantly, delamination propagation along the lower 45° ply toward the bondline was found to introduce bondline failure in the interface of the adhesive and adherend. Detailed numerical validation of the experimental results was carried out. A 3D model was developed to validate delamination in the damaged laminate coupons and delamination and bondline damage in the adhesive layer. As a result of this work, the development of a numerical methodology to capture the dynamic response of the scarf joints under pre-tension and their interacting failure mechanisms is accomplished.

Introducing both delamination and bondline failures in the numerical scarf joint model leads to the important finding that development of delamination reduces the damage area in the adhesive region as compared to the numerical predictions without delaminations at high
impact energy. The development of composite damage is therefore found to delay catastrophic failure of the joint.
1. Introduction

The use of advanced composite structures has significantly increased in the aerospace industry in recent years. This is particularly due to their excellent mechanical properties such as high specific mass, stiffness and corrosion resistance. However, their application in the industry has been limited so far. An aircraft in flight is vulnerable to foreign object impact damage, such as birdstrike or runway debris during landing and take-off. Damage tolerance issues which can include poor impact resistance, low through-thickness load-bearing capabilities and complex failure modes plague composites when compared with traditional metal alloys.

With an increase in use of composite for aircraft components, many methodologies for repairing damaged composite structures have been studied over the years. Repair is beneficial to the aerospace industry and results in significant cost savings. Certification of repair techniques is as important as the manufacturing and assembly of new components. In particular, as the use of composite materials in the industry becomes more frequent and desirable, the importance of sustainable repairing techniques arises.

Repair methodologies include a variety of bonded and bolted patch designs. In particular, bonded scarf joints minimise bending of adherends (Gacion et al. 2008) and are often used due to the benefits of aerodynamics and stealth (Feih et al. 2007; Herszberg et al. 2007), whereas bolted repairs create a protrusion on the surface, resulting in the degradation of aerodynamic characteristics (Baker et al. 1999). In terms of structural efficiency, when it is important to ensure that the repaired structure fully transfers the stress to the parent structure, adhesively bonded scarf joints are ideal as they create less eccentricities in the loading path and a more uniform stress distribution compared to other types of joints (Gunnion and Herszberg 2005; Harman and Wang 2006). However, such repairs are not without disadvantages, as a significant amount of undamaged parent material needs to be removed to bond the replacement component to the parent structure (Harman and Wang 2006) due to the very low scarf angle required to minimise the amount of peel stress in the joint when using this repair methodology (Baker et al. 1999).
In order to replicate more realistic loading and damage types, it is desirable to investigate the behaviour of a composite structure under dynamic impact loading, whilst under load. Baker et al. (2004) stated that the typical pre-strain level of military aircraft in service is around 4000 - 5000 με. Pre-loading conditions can lead to catastrophic failure by changing the stiffness and strength of the originally tested component. In addition to this, as stated by Mikkor et al. (2006), the critical velocity can also decrease with increasing pre-load. As foreign objects travel at considerably high velocities (Herszberg and Weller 2006), it is vital to study the possibility of severe damage to composites loaded at high strain rates due to high impact velocities. Today, while extensive composite impact studies have been performed under a combination of impact and pre-loading (Davies et al. 1995; Nettles et al. 1995), the general body of knowledge on the performance of scarf repairs generally only considers static loading conditions (Wang and Gunnion 2008). However, it has been recognised by the aerospace industry that bonded joints subject to high strain rate may experience different failure modes to that of the static joint. Recent studies by Feih et al. (2007) and Herszberg et al. (2007) have found that scarf joints may suffer catastrophic failure under a combination of impact and pre-loading. It is currently assumed that a superposition effect of both tensile and bending stresses exists for this failure. This theory does not account for a possible interaction of delamination damage in the composite adherend and adhesive damage in the bondline. The combination of static pre-strain and dynamic impact events represents a more extreme damage scenario, which, to date, is not well understood.

It is desirable to conduct experimental research; however, it takes considerable time to set up accurate experiments, and they can be of significant cost. In addition to this, such tests may have technical restrictions involved (Gacion et al. 2008), leading to a lack of parametric studies for parameters such as scarf angle or the thickness of adhesive. Therefore, researchers tend to rely more and more on numerical methodologies (finite element method), which allows engineers to obtain in-depth results for various scenarios. For example, such methods provide detailed results over the length of the scarf joint for both the tensile strain and the shear stress in the adherends and adhesive, respectively (Baker et al. 2004). However, numerical methods have difficulties in modelling the scarf joint as it is difficult to account for the local stiffness which varies along the bond-line, unlike the lap joint (Gunnion and Herszberg 2005).
becomes even more difficult when a composite scarf joint is used as it should be considered that the results are varied by the differing orientations of plies along the longitudinal compliances within the laminate (Baker et al. 2004). Therefore, it is very important to develop an improved design method that can be more widely used in the aerospace industry.

1.1. Scope
This study focuses on experimental testing and subsequent numerical analysis of impacted and pre-loaded composite coupons and bonded composite scarf joints under varying impact conditions. A methodology will be developed for the numerical modelling of preloaded impact tests to enable capture of critical failure modes and ultimate failure.

The main aim is to validate a modelling strategy that will provide accurate failure characterisation of the tested joints using numerical analysis.

The key research questions for this project have been defined as follows:

1) Can composite coupons be used to characterise composite failure modes which occur during scarf joint impact?

2) Do bondline failure and composite failure modes interact in scarf joints under impact?

3) Is the development of composite damage beneficial or detrimental to catastrophic failure of the joint?

4) What is the effect of pre-strain on damage development during impact for preloaded composite coupons and scarf joints?

The objectives of this research can be categorised as follows:

- For composite coupon tests
  - Compare the effect of pre-straining on the laminate coupons for both elastic and damage response cases
  - Characterise though experiment the behaviour to failure of non-scarfed laminate coupons under the same loading conditions as scarf joints
  - Characterise dominant failure modes
- Develop validated procedures for modelling damaged composite coupons using finite element codes. This includes validation of boundary conditions for the preloaded impact test set-up and prediction of critical failure modes such as delamination.

- For composite scarf joint tests
  - Characterise through experiment the behaviour to failure of bonded composite joints under different impact and preloading conditions
  - Establish differences when compared to composite coupons in failure modes and structural response
  - Compare the effect of pre-straining during impact to the laminate coupons for both elastic and damage response cases
  - Develop validated procedures for modelling damage in composite in finite element codes (implicit and/or explicit)
  - Establish a procedure for developing failure envelopes that can be used in designing scarf joints

1.2. Outline of Thesis

- **Literature review**: rationale for methodology and research questions
- **Material characterisation**: characterisation of the laminate and adhesive materials for numerical analysis
- **Experiment at work**: calibration of the test set-up for accurate experimental results
- **Experimental results summary**: summary of tested results for composite laminates and scarf joints and establishing of pre-straining effect
- **Finite element modelling methodology**: overview of numerical methodology and of parametric studies with different element types and of most appropriate set-up
- **Numerical results summary**: validation of numerical results with experimental results
- **Conclusions**: summary of all the findings and outline of the future work
2. Literature Review

Repaired composite structures are susceptible to impact whilst in service. This literature review will focus on studies discussing impact damage modes and the influence of pre-straining on composite damage development for both composite coupons and scarf joints.

2.1. Laminate Composites

A fibre-reinforced composite is composed of three constituents: the fibres, the matrix and the interface responsible for assuring the bond between the matrix and fibre (see Figure 2-1). A fibre composite material usually consists of one or more filamentary phases embedded in a continuous matrix phase. The fibres play an important role as they carry a significant percentage of the applied load, especially in-plane. The polymeric matrix is important as it protects, aligns and stabilises the fibres as well as assures stress transfer from one fibre to another and, in some cases, alleviates brittle failure by providing alternative paths for crack growth. The other important constituent of the composite material is the interphase which is responsible for assuring the bond between the matrix and fibre (Cantwell and Morton 1991).

![Figure 2-1: Composite constituents (Jones 1999)](image)

Composites are still new materials and when compared to metals, information on aspects such as adequate knowledge and capability of life prediction, infrastructure standards, design methodologies for many infrastructure applications and production scale cost-effective methods for interfacing and joining, is still insufficient. In addition, composites are expensive materials to produce and manufacture. Despite these disadvantages, composites provide the industry with better options in the process of designing, manufacturing and servicing, when compared to metals. Their main advantage is their light-weight – with weight savings in the
order of 25% resulting in reduced cost of transportation. Because of their lighter weight, composites offer high specific strength and stiffness. Furthermore, they have high resistance to corrosion and fatigue and from a design point-of-view, composites provide excellent tailorable ability to specific loading cases.

Due to these benefits and potential, an increase in the use of the composites is noticeable in various applications. The production of carbon fibres is approximately 10000 tonnes per annum and these along with other types of fibres such as glass, are extensively employed in leading edge technologies (Hancox 2000), especially in the aerospace industry where about 50 million kilograms of composite are used annually (Sanjay 2002). The latest new aircraft developments such as Boeing 787 and Airbus 380 are comprised of numerous composite materials (see Figure 2-2). For example, the Boeing 787 contains approximately 35 tons of carbon fibre reinforced plastic, made with 23 tons of carbon fibre.

Figure 2-2: Total materials used for B787 (top) and A380 (bottom) (The Japan Carbon Fiber Manufacturers Association website)
2.2. Impact Scenarios

An aircraft is often exposed to the hazards of impact. Hancox (2000) defined impact as “the relatively sudden application of an impulsive force, to a limited volume of material or part of a structure”. Impact on an aircraft may occur during the manufacturing and assembly processes such as by dropping a tool or within the operation environment when cruising, taking-off or landing such as from birdstrikes or hail. Cantwell and Morton (1991) defined that the impact problem is divided into two conditions: a low velocity impact by a large mass like dropped tool and high velocity impact by a small mass like runway debris or small arms fire. The damage induced through such Foreign Object Damage (FOD) reduces the mechanical properties of the composite structure, such as its strength, durability and stability (Hancox 2000). Composites have low transverse and interlaminar shear strength and thus poor resistance to delamination. They also suffer from the lack of plastic deformation. This means that once composites exceed stresses above a certain level, permanent damage occurs in the structure (Hancox 2000).

Chiu et al. (1997) emphasised that during FOD impact processes prestresses frequently arise. It is most likely that an aircraft will experience an impact under prestressed conditions in real life. As Whittingham et al. (2004) exemplified, aircraft fuselage skins typically experience operational strains up to 1500 με during their service life. Similarly wing skins can experience peak strains in the region of 3000 - 4500 με with 1500 με being a typical strain level away from the immediate vicinity of the root. Horizontal stabilators experience similar strain levels (Whittingham et al. 2004), most likely due to bending moments (Chiu et al.1997). Figure 2-3 schematically illustrates the possible impact zones on the aircraft and also respective impactor sizes with impact velocity, and therefore impact energy, as well as whether the part is under load.

Baker et al. (2004) outlined the typical design parameters for carbon/epoxy airframe components of high performance military aircraft with respect to pre-strain conditions. They stated that the airframe needs to withstand ultimate design strains of ± 3000 to ± 4000 με for mechanically fastened structures, and up to ± 5000 με for bonded honeycomb structure.
Figure 2-3: Impact scenarios over a typical aircraft structure showing possible impact locations and magnitudes (Hachenberg 2002)

2.3. Impact Response of Composite Structures

2.3.1. Definition of Impact Response

Both impact energy and velocity are factors that determine the extent of the damage within the structure (Sierkowski 1995). Both the material’s properties and the structure’s response may be influenced by the strain rate resulting from varying the impact velocity (Cantwell and Morton 1991). Abrate (1991) stated that low velocity/energy impacts cause the entire structure to deform during contact, while in high velocity/energy impact a localised deformation in a small impacted (interaction) area on the structure is experienced. Upon such a point of impact, energy is dissipated over a small region. In addition to this, Cantwell and Morton (1991) stated that unlike low velocity impact loading, the size of the specimen or component is less important when determining its dynamic response in case of high velocity loading by a light projectile.

2.3.2. Composite Failure Mode

When studying the failure characteristics of the structure, both the energy generated (or dissipated) during interaction of the impactor and a target (Baker et al. 2004) and the failure process (Cantwell and Morton 1991) should be taken into account. The major failure modes that can occur during loading of composite materials are fibre fracture, inter fibre transverse matrix cracking, and interlaminar fracture or delamination (Sierkowski 1995).
2.3.2.1. Delamination
Delamination can be defined as the separation of two adjacent plies in laminated composites, a failure mode which is significantly dependent on the various geometrical parameters, material properties, loading and boundary conditions. During impact, this failure is mostly initiated and propagated from the regions where holes, cut-outs and existing transverse cracks exist (Sierkowski 1995). The combination of three different types of modes including tensile crack opening, in-plane shear and in-plane tearing or anti-plane shear will form delaminations. In particular a shear delamination mode is expected to be predominant under impact loading (Sierkowski 1995; Cantwell and Morton 1991). Shear delamination propagates quickly and abruptly when the loading energy reaches a critical level (Sierkowski 1995).

2.3.2.2. Matrix Cracking
In general, stress concentrations – which occur near the fibre matrix interface under transverse tensile stress – initiate matrix cracks at low energy levels. These cracks will stop when reaching the interface of an adjacent ply with different fibre orientations as depicted in Figure 2-4, followed by possible delamination initiation from the transverse crack root. As the delamination grows further, additional transverse matrix cracks tend to appear (Sierkowski 1995). A high tensile stress results in a longer and denser crack propagation pattern. External matrix cracking can be used to estimate the internal delamination in low velocity impact (Bayandor et al. 2003).

![Figure 2-4: Transverse matrix cracking (Lee 1990)](image)

2.3.2.3. Fibre Breakage/Fracture
Crack propagation in the direction perpendicular to the fibre direction results in fibre fracture. As shown in Figure 2-5, the crack tip may break the fibres, while the fibres behind the crack
front are pulled out of the resin matrix. Eventually, continuous propagation will cause separation or a fracture across the full width of the laminate.

For the same impact energy, a higher capacity to absorb energy results in less fibre breakage or crack deflection along the fibres and/or splitting, which results in a higher residual tensile strength. Secondary matrix damage, which occurs after initial fibre failure, is also reduced, allowing residual compressive strength to increase consequently (Bayandor et al. 2003). Failure modes that involve fracture of the matrix or interphase region result in lower fracture energies, whereas failures involving fibre fracture result in significantly greater energy dissipation (Cantwell and Morton 1991). Brittle fibres, such as carbon, have a low strain to fracture and hence provide a lower energy absorbing capability, but it is still greater than matrix damage (Bayandor et al. 2003).

2.3.3. Impact Damage

A number of failure modes can occur in composites. The encountered failure modes depend upon the nature of the impact scenario – such as low velocity impact, ballistic impact, or high-strain rate impact (Wiedenman and Dharan 2006) as shown in examples in Figure 2-6. In addition, the dominant failure mode may also be dependent on the preloading type – tension, compression, and shear.

Low energy damage usually causes Barely Visible Impact Damage (BVID), which is defined as internal damage which cannot be observed externally. It consists of, as depicted in Figure 2-6 (a), multiple delamination cracks between the ply layers and matrix cracking within the plies. As a result, a loss of compression strength and structural integrity occurs. As the impact velocity
increases, composites experience delamination between plies and fibre fracture on the back face of the impact zone, as shown in Figure 2-6 (b). Also fibre and resin crushing could arise locally or globally corresponding to the boundary condition of composite structure. Figure 2-6 (c) represents perforation and rupture of the composite at the impact site while high energy damage occurs; there is a hole in the material that passes through-the-thickness which can be clearly seen by visual inspection. In addition fibres are broken during the impact event and there is delamination damage and cracking around the impact site.

![Composite failure modes](image)

Figure 2-6: Composite failure modes for (a) Low velocity, (b) Medium velocity, (c) High velocity (Mouritz 2007)

Sierkowski (1995) stated that the damage through the thickness is also dependent on the interactive effect of impactor and target (hard striker/rigid target or hard striker/flexible target) as illustrated in Figure 2-7. Initial failure in thin, flexible targets occurs in the lowermost ply as a result of the tensile component of the flexural stress field, whereas damage in thicker, stiffer targets initiates at the top surface due to the contact stress field (Cantwell and Morton 1989).
2.4. Scarf Repair on Composite Structures

In the aerospace industry, patch repairs are considered to be most appropriate method of repairing impact damage. Baker (1984) compared mechanical repairs (like using rivets or bolts) and adhesively bonded patches and presents their applications in Australian aircraft structures. The most recent example using scarf repairs was undertaken for the F/A-18 stabilator (Baker et al. 1999). Mechanical tests and numerical analysis show that the design limit load is achieved without failure.

The main function of the repair is to transfer the stress from parent structures to the substrate structures, while minimising any stress concentrations along the joining regions. The following sections will discuss repairing techniques including scarf repair methodology, design considerations, and comparison with other adhesively bonded repairs, and lastly a summary of research studies on scarf joints.

Several types of bonded joints are utilised in the aerospace industry as seen in Figure 2-8.
As for lap joints, this is the cheapest of all joints to manufacture. The joint allows the adhesive to carry the stress in its strongest direction. However, the single lap joint is mostly used in applications where lighter loaded structures are required. This is due to the offset load path, which results in secondary bending moments, and thus introduces severe peeling stresses. Double lap joints with collinear loading paths were developed subsequent to the modification of the single lap joint; but it still produces peel stresses due to the mechanical moment produced by the unbalanced shear stresses acting at the ends of the outer adherends. It is suggested that in order to reduce such concentrated peeling stresses, a bevelled lap joint, where the edges of the adherends are tapered, is preferable (Sina 2008; Baker et al. 2004).

A stepped-lap joint is one of the joints to offer minimum peel stress with a good stress distribution along the bondline. It is ideal to regain approximately equivalent strength, flexibility, and thickness, compared to the parent structure. If the sections to be bonded are relatively thick, the step lap joint is acceptable (Sina 2008).

2.4.1. Scarf Repair Method and Application

Scarf repairs are manufactured by removing the damaged volume at a shallow angle (unless they are in situ components) and installing the substrate part, followed by being bonded with the parent components with adhesive materials by co-curing at adequate pressure and temperature. There are several methods to implement a scarf patch, including soft-patch, hard-patch (moulded), and hard-patch (machined). For more details, Whittingham et al. (2009) provides a comprehensive overview.

This joint type is mostly used in patches as a repairing method. As this joint is to be dealt with throughout this study, a more extensive analysis of its advantages and disadvantages is made as follows:

Advantages:

- Scarfing provides for a large adjoining surface.
- Aerodynamic smoothness is maintained by having the same thickness of the patch as the parent structure.
• Strength restoration is maximised because the adhesive stresses along the scarf joint do not suffer from the considerable stress concentrations present in overlap repairs (Harman and Wang 2006; Baker et al. 1999). This applies not only under static but also under dynamic loading (Sato and Ikegami 2000). This also introduces earlier failure in the adherend outside of the joint zone instead of adhesive peel or shear failures (Gunnion and Herszberg 2006).

• Scarfing lowers the stresses in the patch by utilising a path which has an equivalent stiffness to the parent (Harman and Wang 2006). This is the general case to other bonded joints.

• Scarfing also results in low peel stress due to the lack of eccentricity in the load path (Baker et al. 1999).

• Scarfing offers a higher resistance to fatigue. It is found to be 3.5 times greater than that of double lap joints (Vinson 1989).

Disadvantages:

• Scarfing provides less resistance to creep as scarf joints do not display the “elastic well” found in lap joints (Baker et al. 1999).

• Scarfing requires a large amount of intact parent structure when installing a patch, since a low scarf angle is used to reduce the amount of peel stress in the joint (Baker et al. 1999).

• Scarfing requires careful machining at a low angle in order to have a uniform thickness bondline (Vinson 1989).

• Unlike lap or stepped-lap joints, scarf joints result in a more complicated stress analysis because the stiffness of the bonded surface varies along the bondline, resulting in significant variation of the peel and shear stresses (Gunnion and Herszberg 2006; Baker et al. 1999; Vinson 1989). In the numerical analysis, such severe peaks may create difficulties in convergence of the numerical models while loaded statically (Vinson 1989).

2.4.2. Design Consideration for Adhesively Bonded Scarf Repairs

With regards to adhesive materials, the use of elastic or resilient adhesives is recommended under dynamic impact as these types are enhanced to absorb shock (Kubo 1977).
Using a simplified approach (Baker et al. 2004), an analytical relation of stresses in the bondline with respect to scarf angles is derived. It is assumed that the shear stress in the adhesive layer is reasonably uniform in a scarf joint by having equal stiffness and thermal expansion coefficients for the adherends.

The shear stress, $\tau$, along the bondline may be estimated as

$$\tau_s = \frac{P \sin 2\theta}{2t}$$  \hspace{1cm} \text{Equation (2-1)}

and for the normal stress, $\sigma_T$, to the bondline

$$\sigma_T = \frac{P \sin^2 \theta}{t}$$  \hspace{1cm} \text{Equation (2-2)}

For small scarf angles, the conditions for failure in the adherends are given by

$$\theta < \frac{\tau_p}{\sigma_{ult}} \text{ (in rad)}$$  \hspace{1cm} \text{Equation (2-3)}

where $P$ is a load applied, and $\sigma_{ult}$ the ultimate stress for the adherends. Peel stresses and transverse stresses are very low at low scarf angles, $\theta$, as given by Equation (2-2). Wang and Gunnion (2008) proposed a more detailed analytical method using maximum strain theory.

The shear stress distribution along the bondline is a dominant factor in determining the strength of the adhesive scarf joints; the distribution is dependent on the geometry, and mechanical properties (Hart-Smith 1974). It implies that joints will fail mostly in shear loading (Mode II & III), and this should therefore be treated as the most critical parameter in analysing scarf joint failure.

In the process of designing an adhesive joint, there are several important factors which should be taken into account to maximise the effectiveness of the joint in the structure. Some of the significant findings are summarised in the following subsections, which provides important information for finite element modelling.

2.4.2.1. Bondline
It is important to have the bonded area as large as possible. The length of the scarf bondline should be at least four times the thickness (Petrie 2002). Due to the high resistance to shear
stress, it is ideal for adhesive joints to be loaded in shear (Sina 2008; Loctite 2009; Williams and Scardino 1987) and in compression (Babea and da Silva 2008). In addition to this, joint design should ensure that peel and cleavage stress are minimised (Loctite 2009; Williams and Scardino 1987; Babea and da Silva 2008).

2.4.2.2. Ply Lay-up

Unlike homogenous parent and patch adherends which produce smooth stress distributions along the bondline, the adhesive stresses including local peel and shear stresses along the bondline within the composite material adherends exhibit a strong dependence on the local ply orientations. This corresponds to local variations in adherend stiffnesses within the parent and patch adherends (Harman and Wang 2006; Gunnion and Herszberg 2006; Wang and Gunnion 2008). The more plies a composite has, the higher number of peaks for the peel stress. This coincides with the positioning of 0° plies through the laminate, because their stiffness in the loading direction (under tensile loading) is significantly higher than for +45°, -45° and 90° plies (Gunnion and Herszberg 2006). Similar results were found by Johnson (1989). In addition, the lay-up sequence has more influence on the adhesive peel stress than on the shear stress (Matthews et al. 1982). Wang and Gunnion (2008) concluded that, due to non-uniform stress/strain distribution, the stacking sequence of composite adherends influences the scarf joint strength. It is therefore important to model the individual layers by finite element analysis to account for the strength/stress distribution along the bondline accurately.

2.4.2.3. Scarf Angle

The scarf angle has a strong influence on the peak peel and shear stresses along the bondline. As the scarf angle increases, the stresses in the adherend increase; and the shear strength of the adhesive decreases (Wang and Gunnion 2008; Odi and Friend 2004; Johnson 1989). This is explained by a decrease in the joint length (Odi and Friend 2004). All factors remaining constant, shortening of the scarf joint length leads to an increase in shear stress, due to the resulting reduction in the bonding area. However, the sensitivity of the stresses to the scarf angle reduces in the limiting case of very small scarf angles (Wang and Gunnion 2008) as the adhesive shear stress at each ply end is approximately proportional to the ply stiffness (Wang and Gunnion 2008). Odi and Friend (2004) indicated that low tapers (i.e. less than 3°) would be ideal, and practical repair joints tend to have scarf angles between 1.1° and 1.9° to ensure that the
adhesive layer is never the weakest link (Odi and Friend 2004). This technique also reduces the typical stress concentration caused by the effect of dissimilar modulus adherends. The sensitivity can be further minimised by increasing the laminate thickness (Gunnion and Herszberg 2006). To optimise the scarf, it would be ideal to have a complex taper profile whereby the local scarf angle is reduced adjacent to the 0° plies, and then increased in areas adjacent to less stiff plies (Harman and Wang 2006).

2.4.3. Failure of Scarf Joints
Under static loading, joints usually experience five types of stresses: pure compression, shear, tension, peel, cleavage or, most likely, a combination of these stresses, as seen in real life adhesive joint applications (Sina 2008). The occurrence of several different types of failure modes may then be observed as depicted in Figure 2-9.

Adhesive failure (or debonding) is referred to as the bondline failure in-between the adhesive layer and one of the adherends. Failures that can be dependent on the strength of the bond in relation to that of the adherend are classified into two modes. Firstly, a fracture allowing a layer of adhesive to remain on both surfaces (the adherend remains covered with adhesive) is called cohesive failure. Secondly, failure occurring in one of the adherends away from the bondline and earlier than in the adhesive is referred to as substrate failure. Substrate failure happens when joints made with high strength adhesives are more likely to failure prematurely in the composite before failure in the adhesive occurs due to the relatively low through-thickness strength of most composite materials. When a mixture of adhesive and cohesive failures occurs, this is called 50 % adhesive failure.

![Common Failure Modes for Scarf Joints under Static Loading](image)

Figure 2-9: Common Failure Modes for Scarf Joints under Static Loading
It is important to note that joint failure often involves more than one failure mode (Matthews et al. 1982). The interaction of failure modes may be more pronounced under dynamic loading as seen in Figure 2-10 (Takahashi et al. 2007). During impact, shear cracks and delaminations generally occur in the composite adherend, although their extent is dependent on the scarf angle, lay-up and bondline thickness. Debondings of the adhesive layer are often observed simultaneously in the regions of delamination cracking. Bending cracks (fibre fracture) on the tensile side may also occur.

![Diagram](image)

Figure 2-10: The cross-section of the damaged specimens (Takahashi et al. 2007)

### 2.5. Effect of pre-strain on impact response

A number of researchers (Whittingham 2005; Robb et al. 1995; Chiu et al. 1997) have compared the effect of pre-strain on impact parameters such as impact force, impact duration, damage area/shape, or absorbed energy. Although most of these studies focused on laminates with preload, their results may also be applicable to pre-strained scarf joints.

#### 2.5.1. Peak Force

The force-time history curves are typically acquired in impact experiments and compared to finite element analysis. The shape of the curve indicates the onset of damage and its
propagation (Zhou and Davies 1995). Moreover, impact damage by delamination was shown to relate directly to the maximum impact force induced whatever the incident energy and plate size (Zhang et al. 1999). These conclusions were also confirmed by Lagace et al. (1993) and Sankar (1996) even when no-preload was applied. Hence, it is important to study the peak force in relation to preload.

Some studies have been conducted by past researchers to assess the maximum peak force with varying uniaxial loading types. Whittingham et al. (2004) conducted an experiment using carbon fibre-reinforced polymer (CFRP) (HYE 970/STD 12K) at various loading types, including uniaxial tension, biaxial tension, and shear and at various pre-strain levels up to 1500 με. The preload was found to have no effect on the peak force by the specimens. Mitrevski et al. (2006) performed the experiment to find the pre-strain effect on the E-glass woven/polyester resin composite plates with respect to different impactors’ shapes, including conical, ogival, spherical and flat shapes. They concluded that the peak impact force was independent of the pre-strain level and of the impactor shape at 1000 με, except in the case of the conical shaped impactor where the peak force dropped when pre-strain was present.

Experimentally, Kelkar et al. (1997) found that when using carbon-fibre laminate, a larger peak force under uniaxial tensile preload was observed at 2400 με.

Chiu et al. (1997) concluded that, when applying 20 % of the ultimate strength of graphite/epoxy laminate (T-300/976), the peak force was increased the most by tensile loading, whereas compression loading derived the least peak force (see Figure 2-11). This was explained by a proportional relationship between peak forces and the flexural stiffness of the composite panel.

\[ F_{\text{Pretension}} > F_{\text{No_preloading}} > F_{\text{Precompression}} \]
Rob et al. (1995) experimentally studied the various types of pre-straining effects on the E-glass reinforced/polyester laminate, including uniaxial tension or compression, biaxial tension/tension, compression/compression, and tension/compression. The applied pre-strain levels ranged from 2000, 4000, 6000 με. Robb et al. (1995) provided a valuable insight into the influence of prestress by tabularising the damage indices at 6000 με as shown in Table 2-1 as it was found that the pre-straining effect was seen only above 6000 με. In terms of peak impact force, shear pre-strain had the least effect whereas biaxial tension had the most influence. A similar peak force sequence was found by Chiu et al. (1997) when comparing uniaxial pre-strain loading types. The peak load increased by 3 % (for tension), or decreased by 13 % (for compression) at high strain level of 6000 με.

Table 2-1: Damage indices evaluated at 6000 με (Robb et al. 1995)

<table>
<thead>
<tr>
<th></th>
<th>Unstressed</th>
<th>Uniaxial tension</th>
<th>Uniaxial compression</th>
<th>Biaxial tension</th>
<th>Biaxial compression</th>
<th>Biaxial tension/compression</th>
</tr>
</thead>
<tbody>
<tr>
<td>Absorbed energy</td>
<td>1</td>
<td>1·15</td>
<td>1·38</td>
<td>0·93</td>
<td>1·47</td>
<td>1·55</td>
</tr>
<tr>
<td>Peak impact force</td>
<td>1</td>
<td>1·03</td>
<td>0·87</td>
<td>1·1</td>
<td>0·91</td>
<td>0·67</td>
</tr>
<tr>
<td>Damage area</td>
<td>1</td>
<td>1·24</td>
<td>1·17</td>
<td>1·18</td>
<td>1·02</td>
<td>2·64</td>
</tr>
<tr>
<td>Peak indentation*</td>
<td>1</td>
<td>1·42</td>
<td>1·68</td>
<td>0·88</td>
<td>0·98</td>
<td>2·81</td>
</tr>
</tbody>
</table>

* Only one specimen scanned to obtain indentation results

Khalili et al. (2007) analytically studied the effect of pre-strain in graphite/epoxy composite plates using Sveklo’s elastic contact theory (no introduction of damage). Two loading types,
including biaxial tension and uniaxial tension, were applied up to 180 kN/m. It was found that the in-plane pre-strains influenced the impact force as the maximum force increased marginally (approximately 6%) with increasing pre-loads.

2.5.2. Impact Duration
In the majority of experiments, (Mitrevski et al. (2006) at 1000 με, Kelkar et al. (1997) at 2400 με) in analytical studies (Sun and Charropadhyay (1975) using modified Hertz’s contact law; Khalili et al. (2007) (using Sveklo’s elastic contact theory)) and in numerical studies (Choi 2008), it was found that the total contact duration is reduced with an increase in pre-strain. This was also supported by Choi (2008), who found that tensile in-plane load induced a faster response compared to the compressive load. In contrast, Whittingham (2005) stated that neither uniaxial tension nor shear preload reduced the impact duration significantly, but a significant decrease was found under a biaxial tension preload of 1000 με.

It is important to notice the relationship between the impact duration and pre-strain level. This connection may exist because in force-time history curves the area under the curve represents the impulse energy transferred into the plate during the impact event.

2.5.3. Damage Area
The damage area has been found to increase with pre-strain (Wiedenman and Dharan 2006). The authors studied the effect of the plate thickness on the equivalent damage area (i.e. damage area normalised by the sample thickness) in relation to the compression preload. In this study it was shown that the increase in damage area becomes more pronounced for thicker samples, i.e. as preload increases, the laminate thickness has a stronger effect on the damage. Conversely, the results of Zhang et al. (1999) and Zhang et al. (1996) imply that un-preloaded plates, compared to preloaded ones, have larger damage areas if subjected to compression prestress. In other cases, regardless of any possible relationship between preload and other variables such as indentation depths and absorbed energy, the damage areas remain similar between preload and non-preloaded laminates for biaxial tension loading (Mitrevski et al. 2006). The same trend was found by Herszberg and Weller (1997) for tension loading between 49 - 98 kN (equivalent to 3920 - 7840 με), except where the impact velocity approached the critical
velocity, which was simulated in good agreement using finite element analysis (Mikkor et al. 2006).

Choi (2008) concluded that in-plane compressive load induces a slightly larger damage area than in zero or tensile load, while the in-plane load had no effect on contact force. Similarly, Chiu et al. (1997) found that, although the maximum force under precompression loading was lower than that of non-prestressed loading, the damage area was larger in the former case. A similar finding was observed in Sun and Chen (1985) and Hancox (2000) as well. They concluded that the delamination buckling during compression (or according to Sun and Chen (1985) a softening effect on the laminate stiffness), results in a more severe dynamic plate response, and in this case, the damage area was enlarged.

In the case of different biaxial prestress types, Robb et al. (1995) found that while there is little effect from the unstressed value in the tension/tension and compression/compression quadrants, the effect of tension/compression loading causes a drastic increase in the damage area. This was further demonstrated by the catastrophic failure of several of the test specimens impacted at the highest pure shear loading condition. In contrast to this, Whittingham (2005) found that biaxial tension prestress cases at 2000 $\mu$e produced the most influence on the internal damage area with a 20% increase over the unstressed case. Similar effects were noticed for uniaxial tension and biaxial shear prestress cases with 7% and 12% increases, respectively.

Li et al. (2007) experimentally conducted dynamic impact testing of composite scarf joints (T300/C970) under pretension, applying up to approximately 4000 $\mu$e. While the peak impact force was not significantly influenced by the pretension, the damage types varied from “damaged” to “catastrophically failed” when using higher pre-strains. In numerical analysis by Herszberg et al. (2007), with the assumption that the damage occurs mostly in bondline rather than the adherend regions, the damage area is found to increase by approximately 70% when compared with the pre-strain levels from 800 to 3900 $\mu$e, where no significant effect on impact force was found with the varying pre-strain.

### 2.5.4. Damage Shape

With various combinations of both uniaxial and biaxial prestresses (and different in-plane loading orientations), the damage shapes on the impacted specimens varied significantly as...
seen in Figure 2-12. Similarly, Chiu et al. (1997) also demonstrated the damage area shape under uniaxial preloading types. The greatest element of commonality was that the major axes for ellipses of the precompression impact damage were in the longitudinal direction, whereas the major axis of the pretension is in the transverse direction. Despite this apparent relationship, the prediction of these damage shapes with respect to different loading conditions was not numerically validated.

![Damage Shapes with respect to preloading conditions](Robb et al. 1995)

Under ballistic impact test conditions, it was seen that the delamination damage was found to be generally circular when subjected to zero preloading, and became square shaped with the largest dimension being perpendicular to the preload direction, when subjected to initial compression preloading (Wiedenman and Dharan 2006). This finding is significantly different to low impact energy and is attributed to the higher impact velocity.

For composite scarf joints, assuming the adherend behaviour as an elastic material, i.e. no failure, the damage pattern in the adhesive region is non-symmetrical when conducting numerical analyses using a ply-by-ply approach (Feih et al. 2007). The same result was found when modelling the adherend as orthotropic. However, no sectioning was undertaken to verify the extent of delamination versus adhesive failure and it is postulated that the damage shape might be a result of failure mode interaction.
2.5.5. Absorbed Energy
It was found by Whittingham (2005) that the case of non-catastrophic failure of tested laminate coupons under uniaxial and biaxial pre-strains increased the absorbed energy, however no change was observed for the shear prestress case. It was also seen that as the absorbed energy increases, there is a general increase in the damage area. In contrast, Robb et al. (1995) shows that the absorbed impact energy is greatest when there is a combination of tension/compression components present in the pre-strain and at a minimum in the tension/tension quadrant (see Error! Reference source not found.). For the biaxial tension case, the absorbed energy decreased as the damage area increased, unlike the other loading types where the damage areas increased as the absorbed energy increased.

In association with impacted plate size, it was analytically concluded that the amount of energy absorbed by the plate increased with increasing plate size (Sun and Chattopadhyay 1975). The absorbed energy relation can be dependent on the impactor shape as well. Mitrevski et al. (2006) stated that at an initial impact energy of 4 J, the absorbed energy increased with the level of preload, but such result is only observed when using a conical impactor and not for other shapes. Also, at a slightly higher impact energy of 6 J, no such relationship was observed. According to Robb et al. (1995), in attempting to correlate absorbed energy with the damage area, it is hard to confirm any relationship due to the difference in the dominant failure mode at the micromechanical level and the different associated fracture energies.

2.5.6. Residual Strength
Hancox (2000) stated that a combination of impact and superimposed tensile or compressive stress caused more damage than either factor on its own. Stress to failure, after Tensile After Impact (TAI), was compared as a function of impact energy. It was clearly seen that specimens unstrained before/after impact require more stress to cause complete failure in TAI than in the case of prestressed specimens (see Figure 2-13). In contrast, after Compression After Impact (CAI) testing with damaged plates having undergone precompression, it was seen that the preloading effects strengthened the CAI compression response, resulting in a higher strength for compressive preloaded plates (Zhang et al. 1996; Zhang et al. 1999). This is due to the finding that un-loaded plates have larger damage areas under compression. In other words, the
residual strengths after both TAI and CAI tests are dependent upon the damage area and larger damage areas decrease the overall strength.

According to Whittingham (2005), residual tensile strength and residual tensile stiffness were not affected by pre-strain. In addition, the stiffness does not change between damaged and undamaged specimens; this may be attributed to mostly intact fibres despite the presence of delaminations, as the fibre is the most dominant factor for the tensile stiffness. In a similar manner, subsequent to dynamic impact testing under tensile preload, the residual tensile strength is independent of the magnitude of the preload except in the region close to critical velocity (Herszberg and Weller 1997) and (Mikkor et al. 2006).

As for scarf joints, it was numerically found that the adhesive strength, after TAI, was reduced in the case of dynamic impact events as a result of greater damage area at higher pre-strain level (Feih et al. 2007; Herszberg et al. 2007). Figure 2-14 below shows the linear relationship between the damage area and residual strength.

Figure 2-13: Effect of tensile prestress (residual strength) on impact energy for composite coupons (after Hancox 2000)
2.6. Conclusion

In this literature review, both advanced composite laminates and adhesively bonded scarf repair under impact and preload were studied. Despite their outstanding mechanical integrity in aerospace applications, composites are still a relatively new class of materials, with a great deal of research into the nature of scarf in aircraft structures needing to be completed in order to meet stringent safety requirements. The study of these laminate structures and joints becomes complicated when considering the combination of initial stress and impact loading, which is the most realistic loading type of an aircraft experience in service.

It is seen that establishing trends and relationships between preload effects and the maximum force, damage area, absorbed energy, and residual strength is very difficult, and a broad range of findings has been presented. It becomes even more complicated, when taking the relationship of the pre-strain conditions, pre-strain levels, and impact energy into account.

With respect to laminated composites, many studies relate to combined loading and impact. Some of these studies concluded that no specific contribution of the prestress effect to the structure was found with respect to peak force. Other studies concluded that there is a pre-strain effect, especially at high strain levels, like 6000 με. It may be seen that this conclusion could be dependent on the pre-strain conditions and pre-strain levels, and also the extent of impact energy. In addition increasing pre-strain level it may reduce the critical velocity required to achieve catastrophic damage. In general, it was seen in most of the literature that pre-strain
leads to more severe damage to the structure when looking at the impact damage size. In addition, the damage size and shape may vary with pre-strain conditions. Despite of all these important findings from the reviewed papers in relation to the preloading effect, there is a need for further studies in order to further understand pre-strain effect on the severity of damage and the damage tolerance. Validated numerical models may be used to minimise testing efforts and experimental uncertainties.

Unlike laminated composites, adhesively bonded composite scarf joints have been mostly studied under static loading conditions (in-plane stress studies). With such results the shear/peel stress distributions along the bondline as well as the failure modes were thoroughly studied. A few studies were related to dynamic loading (out-of plane stress) (Takahashi et al. 1000; Harman and Wang 2005) or a combination of static and dynamic loading conditions by numerical (Herszberg et al. 2007; Feih et al. 2007; Li et al. 2008) and experimental analysis (Li et al 2008).

Any scarf repair in an aircraft structure is likely to be loaded. The literature review highlights that preloaded scarf joints have not been studied under impact conditions. Furthermore, no general consensus exists regarding the effect of pre-straining composite coupons on impact damage and failure. The current work will therefore focus on studying composite coupons and scarf joints of identical thickness and lay-up under zero and positive pre-strain (up to 5000 µε) under impact. Relationships including the pre-strain effect on peak force, strains, damage areas and residual strengths and also their attributes with regard to failure mechanisms (failure modes) should be established. Failure envelopes need to be generated for composite scarf joint failure. This project seeks to complete a comprehensive program of experimental testing, followed by a thoroughly validated numerical methodology (FEM). Doing so will help to establish the outcomes described above. A low weight impactor will be used for the present work to enable damage characterisation for a large range of impact velocities (up to 9.7 m/s). This methodology will allow validation of both low velocity and medium velocity failure modes as was indicated in Figure 2-6. High velocity impact was not considered suitable for this work as the main experimental focus was placed on collection of both strain and force data during the impact event.
“This page is left blank intentionally for double-sided printing.”
3. Material Characterisation

The material property values are critical for the accuracy of the numerical results. This material used for this project is Cycom 970/300, bonded with FM 300 adhesive for scarf joint repairs. Tensile and three point bending tests were conducted to determine the laminate mechanical properties, such as in-plane stiffness and bending stiffness for the Cycom 970/300 prepreg. All the tests were performed using an Instron 50 kN machine.

3.1. Preparation

In this section, a brief demonstration of the laminate and scarf composite joints manufacturing procedure is given. Furthermore, the strain gauges and their uses for tensile testing and the actual laminate and scarf joints during impact are detailed.

3.1.1. Scarf Joint Manufacturing

1) Cut Cycom 970/300 prepreg into size at different ply orientations, in total 16 plies (refer to Appendix 1). It is important to note that the required sizes for flat panels and scarf joints are different. The milling cutter size should be included.

2) Debulk – composite was debulked every four plies (i.e. 45/90/-45/0) using the debulking tool (see Figure 3-1). This step helps in minimising any voids inside plies/resin and volatiles and keeping the lay-up in position.

![Figure 3-1: Images of debulking tool](image)

3) For full vacuum bagging, the following sub-steps should be conveyed to complete bagging. (a:bottom, g: top)

   a) Release film
b) Peel Ply – to prevent the resin from sticking to the bag

c) Lay 16 plies

d) Peel ply

e) Release film – to prevent adhesion of the composite part to the bleeder layer

f) Breather cloth – to ensure even pressure distribution and to absorb excess resin

g) Vacuum bagging film

Figure 3-2 shows the final stage of vacuum bagging, following sealing the area with the bagging sealant (yellow sticky tape) along the edges of the cure plate. It is important to ensure that there is no loss of vacuum.

Figure 3-2: Vacuum bagged composite laminate

4) Autoclave cures at 180 °C and 100 psi, which takes 6 hours. It is most important to ensure, during the processing, that the resin is not allowed to gel under vacuum to avoid a porous laminate. Furthermore, in order to prevent the laminate from warping, it is cooled inside the vacuum bag in the autoclave. This is the final step for laminate coupons.

5) Scarfing – 5° scarfing, conducted by 1/2 inch milling

6) Bonding – the two sides of scarfed panels are joined using FM 300 film, followed by co-curing at 177 °C and 15 psi. As the condition of the scarfed surface is an important element for the bondline strength, the surface is cleaned prior to joining. Sand paper and an air gun were used to clean the surface. Acetone, which is a typical solution for the cleaning process, should not be used as it is not a pure solution as well as to avoid
spreading dirt from the cleaning cloth. Figure 3-3 (a) shows the FM film before bonding on the left scarf side; the finished scarf joint with bondline in Figure 3-3 (b).

![Figure 3-3: FM 300 and scarfed panel: (a) before bonding (b) after bonding](image)

3.1.2. Strain Gauge Attachment

350 Ω strain gauges (Kyowa, KFG-5-350-C1-11L3M3R) with a gauge length of 5 mm were used. Appendix 2 outlines the procedure for strain gauge attachment on the surfaces of the panels; and Appendix 3 describes the details of the strain gauge such as gauge length, gauge configuration and other manufacturer’s details.

For a composite laminate testing under impact, selected coupons had three strain gauges mounted; two gauges were placed 17 mm away from the impacting areas on the impacting surface aiming for far field strain and checking of strain distribution symmetry; the third was placed at the centre of impact at the back side as shown in Figure 3-4. Ideally, it is good to have numerous strain gauges mounted to obtain a more precise impacting behaviour at many different locations on the panel; however the sampling frequency decreases when increasing the number of strain gauges as explained further in the next section.
3.2. Adherend Characterisation

3.2.1. Relationship between Strain and Voltage

The specimens, having a length of 80 and width of 24.5 mm, were tested according to ASTM D7205-06. To determine the elastic modulus, the lay-up was the same as for the actual impact testing, resulting in a nominal thickness of 3.2 mm for 16 plies. They were stretched at one end, with the other being clamped. A loading rate of 0.5 mm/min was applied. The strains experienced on the top surfaces of the coupons were measured by extensometer and the strain gauges, simultaneously. This comparison of strains was for the purpose of strain gauge calibration.

The Vishay Micro-Measurement P3 model can collect 1 data point every second; however, this sampling rate is too slow to be adopted for high strain rate impact scenarios. Hence, it was required to use other strain-measurement tools to capture the degree of which the panel is stretched by such impact. A DaqBook and DaqBoard system was used for impact testing instead, capable of collecting data points at 100 kHz. However, this tool supports voltage ($V$) or millivoltage ($mV$) in output unit, and calibrated values were not available. The voltage unit needs to be converted to microstrain ($\mu\varepsilon$).
In order to obtain the relationship between the output in micro-strain using the Vishay Micro-Measurement P3 model and in Voltage using the DaqBook acquisition system, a composite laminate coupon was repeatedly subjected to tensile loading. While applying the load, the strain was simultaneously measured by an extensometer with 50 mm gauge length. The coupon was strained up to 2000 με only to avoid any potential damage.

The strain gauge was firstly connected to the P3 model so that the strain acquired by the P3 model is compared with that measured by the extensometer. It was confirmed that both measuring tools have a good agreement (see Figure 3-5 (a)). In a similar way, the strains were measured by the DaqBook acquisition system in volts, followed by the measured voltage being calibrated against the strain measured simultaneously by the extensometer. The voltage units can then be converted by applying a linear equation, acquired data in DaqBook as seen in Figure 3-5 (b). The calibration slope was 6080.1 με/V.

![Figure 3-5: (a) Extensometer versus strain gauge; (b) Relationship of micro-strain and voltage](image)

### 3.2.2. Tensile Testing

Figure 3-6 shows one of typical tested result for specimen T1. The stress-strain relationship is linear as expected. From this relationship, an average unidirectional Young’s modulus ($E_I$) was derived as given in Table 3-1 of 41.9 GPa. The standard deviation for the modulus results is very low as established based on four tensile tests. This is attributed to the high-quality laminate manufacture using the autoclave.
Table 3-1: Summary of Young’s modulus for composite laminates

<table>
<thead>
<tr>
<th>Specimen Label</th>
<th>Modulus ($E_i$) (GPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>T1</td>
<td>43.0</td>
</tr>
<tr>
<td>T2</td>
<td>41.1</td>
</tr>
<tr>
<td>T3</td>
<td>41.7</td>
</tr>
<tr>
<td>T4</td>
<td>41.7</td>
</tr>
<tr>
<td>Mean</td>
<td>41.9 ± 0.8</td>
</tr>
</tbody>
</table>

3.2.3. Three-Point Bending Test
For this test, bending tests were conducted according to ASTM D790-02. Four specimens were tested, having a span length of 100, width of 30 mm and nominal thickness of 3.2 mm. Both modulus and strength were determined as seen in Figure 3-7. As in the tensile test, the loading rate was 0.5 mm/min.

It can be seen that all tests resulted in similar properties as seen in Table 3-2. The flexural modulus was calculated in the linear region of the load vs. displacement graphs, which is up to around 4 mm (dotted line) in displacement. The calculation was done using Equation (3-1):

$$E = \frac{PL^3}{48\delta l} \quad \text{Equation (3-1)}$$

where $P$ is the load, $L$ the span, and $I$ the bending moment (moment of inertia), and $\delta$ the deflection of the specimen. Again, very repeatable results with low standard deviations were experienced, highlighting the manufacturing quality.
Figure 3-7: Stress versus strain in three point bending test for B3

Table 3-2: Summary of measured results after three-point bending test

<table>
<thead>
<tr>
<th>Test ID</th>
<th>Max Load (kN)</th>
<th>Max Stress (MPa)</th>
<th>Flex Modulus (GPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>B1</td>
<td>1.41</td>
<td>689.70</td>
<td>29.64</td>
</tr>
<tr>
<td>B2</td>
<td>1.35</td>
<td>688.03</td>
<td>26.70</td>
</tr>
<tr>
<td>B3</td>
<td>1.41</td>
<td>687.33</td>
<td>28.30</td>
</tr>
<tr>
<td>B4</td>
<td>1.31</td>
<td>667.40</td>
<td>27.17</td>
</tr>
<tr>
<td>Mean</td>
<td>1.37 ± 0.048</td>
<td>683.11 ± 10.5</td>
<td>27.95 ± 1.31</td>
</tr>
</tbody>
</table>

3.3. Adhesive

3.3.1. Scarf Joint Tensile Test

Tensile tests on scarf joints were conducted to validate the FM 300 material property used for joining the adherends. The coupons had an unclamped, free length of 100 mm and a gripped length of 60 mm at each end; they were stretched at a loading rate of 0.5 mm/min until failure occurred.

The extensometer (50 mm of gauge length) was mounted at the centre of the free length, with the strain gauge being mounted in the centre (see Figure 3-8). Data readings were collected for every second. As previously, it was found that both methods resulted in very similar strain readings, which gave confidence to use the centre strain gauge in the dynamic scarf joint tests (refer to Figure 3-9 and Table 3-3 ). It is important to note that the stiffness was calculated in a region of 500 – 1500 µε, which is the same region as for the laminate. It is interesting to note that the stiffness of the scarf joint was measured as 39.85 GPa, which is approximately 5 %
lower than that of the laminate after tensile testing. It may be due to that the free length for the scarf joint was longer by 20 mm than laminate coupon. Nevertheless, they are in a good agreement as expected.

As for the static failure mode, it was seen that the predominant failure occurred in the cohesive region due to cohesive shear failure with little or no fibre fracture and pull-out as seen in Figure 3-10. It was reported by Kumar et al. (2006) that such failure mode is expected for scarf angles more than 2°.

Figure 3-8: Location of the strain gauge and the extensometer

![Figure 3-9: Stress versus strain after tensile testing](image)

$\text{Stiffness} = \frac{Y}{X}$
### Table 3-3: Summary of tensile tests

<table>
<thead>
<tr>
<th>Joint ID</th>
<th>Failure Load (kN)</th>
<th>Joint Stiffness (GPa)</th>
<th>Max Stress (MPa)</th>
<th>Shear Strength (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Test 1</td>
<td>27.43</td>
<td>40.7</td>
<td>346.94</td>
<td>30.12</td>
</tr>
<tr>
<td>Test 2</td>
<td>24.27</td>
<td>39.0</td>
<td>296.54</td>
<td>25.74</td>
</tr>
<tr>
<td>Mean</td>
<td>25.85±2.23</td>
<td>39.85±1.20</td>
<td>321.74±35.64</td>
<td>27.93±3.09</td>
</tr>
</tbody>
</table>

Figure 3-10: Scarf joint after failure along the adhesive area

### 3.4. Numerical Input Parameters

#### 3.4.1. Adherend Material Properties

According to tensile tests, the experimental laminate stiffness is 42.36 GPa which is 90% of the manufacturer’s stiffness based on unidirectional properties. The laminate theory calculations are detailed in Appendix 4. This agreement is considered good. However, a larger difference is found in the three-point bending case.

The three-point bending testing was simulated numerically to identify adequate material properties that ensure the numerical analysis to capture the composite bending behaviour accurately. Since the analysis aimed to validate elastic material behaviour, the radius of the supports and the loading nose was ignored; nodal forces were used instead. The load ($P$) was distributed along the y-direction; note that the edge points had applied only half the load as seen in Figure 3-11.

Initially, with the original ply properties provided by manufacturer, it was found that the numerical flexural modulus was higher than that of the experiment by 26% (see Table 3-2). Best agreement was achieved by reducing the unidirectional properties by 20%, resulting in good agreement.
Based on the numerical analysis, the matrix is most dominant to the bending stiffness; whereas in the tensile test, the fibre is still most dominant to the in-plane stress. Since for impact tests, the testing coupon is mostly deformed in bending (rather than in-plane), the 20% reduced material property (see Table 3-5) is used for all subsequent numerical analyses. This choice of material properties is also more conservative. The resulting in-plane orthotropic properties now slightly under-predicted the tensile test values by (37.5 GPa compared to 40-42 GPa).

Table 3-5: Summary of material properties

<table>
<thead>
<tr>
<th></th>
<th>Manufacturer unidirectional</th>
<th>Manufacturer orthotropic</th>
<th>20% off unidirectional</th>
</tr>
</thead>
<tbody>
<tr>
<td>$E_1$[GPa]</td>
<td>120</td>
<td>47.1</td>
<td>96.0</td>
</tr>
<tr>
<td>$E_2$[GPa]</td>
<td>8</td>
<td>47.1</td>
<td>6.4</td>
</tr>
<tr>
<td>$E_3$[GPa]</td>
<td>8</td>
<td>8.30</td>
<td>6.4</td>
</tr>
<tr>
<td>$G_{12}$[GPa]</td>
<td>5</td>
<td>17.9</td>
<td>4</td>
</tr>
<tr>
<td>$G_{13}$[GPa]</td>
<td>5</td>
<td>3.85</td>
<td>4</td>
</tr>
<tr>
<td>$G_{23}$[GPa]</td>
<td>2.7</td>
<td>3.85</td>
<td>2.1</td>
</tr>
<tr>
<td>$\nu_{12}$</td>
<td>0.45</td>
<td>0.313</td>
<td>0.45</td>
</tr>
<tr>
<td>$\nu_{13}$</td>
<td>0.45</td>
<td>0.262</td>
<td>0.45</td>
</tr>
<tr>
<td>$\nu_{23}$</td>
<td>0.2</td>
<td>0.262</td>
<td>0.2</td>
</tr>
</tbody>
</table>

3.4.2. Adhesive Material Properties

The shear modulus ($E_{II} = E_{III}$) can be calculated by the gradient of shear stress-strain curve, which is given by the manufacturer to be around 907.5 MPa (refer to Figure 3-12). It follows
that the Young’s modulus \((E_I)\), can be estimated by the simple isotropic relationship with the
Poison’s ratio \((\nu)\) of 0.3;

\[
E_I = 2(1 + \nu)E_{II}
\]

\[
\therefore E_I = 2359.5 \text{ MPa}
\]  

Equation (3-2)

The fracture toughness \((G_{IC})\) was found to be 1.3 N/mm in Baker et al. (2004). As for \(G_{IIIC}\) and \(G_{IIIIC}\), these value may be estimated by calculating the area under shear stress and strain curve (see Figure 3-12) and multiplying this value with the adhesive thickness of 0.38 mm (Baker et al. (2004). Hence, \(G_{IIIC} = (G_{IIIIC})\) is 3.33 N/mm for Mode II & III.

![Figure 3-12: Shear stress and strain curve (After Gorden, 2002)](image)

This is higher than Mode I as has been found in many research studies as adhesives show better resistance to shear compared to peel stresses. It is important to note that the fracture energies were measured using static loading, and they may be not the same in case of the dynamic loading case. According to Simon et al. (2005), it was found that the energy release rate in mode I under static loading is larger than that under dynamic loading. However, there was no comparison for shear modes II & III. As reported in literature review, the shear stress distribution along the bondline is a dominant factor in determining the strength of the adhesive scarf joints; the distribution is dependent on the geometry and mechanical properties (Hart-Smith 1974). It implies that joints will fail mostly in shear loading (Mode II & III), so \(G_{IIIC}\) should therefore be treated as the most critical parameter in analysing scarf joint failure. It is therefore of interest to validate a dynamic value of \(G_{IIIC}\) for FM 300.
For the scarf joint tensile test, the maximum stress was evaluated as $\sigma_{adh} = 321.74 \pm 35.64$ MPa (see Table 3-3). The shear strength ($\tau_{ult,2 \& 3}$) for the joint can then be calculated using Equation (3-3) as follows:

$$\tau_{ult,2 \& 3} = \sigma_{adh} \sin(\theta) \cos(\theta)$$

where $\theta$ is the scarf angle, where $\theta = 5^\circ$.

Hence,

$$\therefore \quad \tau_{ult,2 \& 3} = 27.9 \pm 2.2 \text{ MPa}$$

This indicated that the shear strength for scarf joints is 20% less than the manufacturer’s data, which is 42.1 MPa at knee (see Figure 3-12). This is most likely due to a different test method and the simplified calculation assuming uniform shear in Equation (3-3). In fact, the peak stresses with respect to $0^\circ$ plies should be explored as found by Wang and Gunnion (2008).

The tensile yield strength should be a factor of $\sqrt{3}$ higher to account for isotropic yielding. As a result, $\sigma_{ult,1}$ is determined as $48.3 \pm 3.8$ MPa. However, these properties were derived based on static loading and are subject to validation for the dynamic loading case.

The Table 3-6 summarises the material properties as derived from static tests for the cohesive element formulation in Abaqus. However, it is important to note that these properties need to be validated for dynamic impact. It is expected that especially the fracture toughness in mode II will be sensitive to the high strain rates experienced during impact (Feih et al. 2007).

<table>
<thead>
<tr>
<th>Material Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$E_{I}$ [MPa]</td>
<td>2359.5</td>
</tr>
<tr>
<td>$E_{II}$ [MPa]</td>
<td>907.5</td>
</tr>
<tr>
<td>$E_{III}$ [MPa]</td>
<td>907.5</td>
</tr>
<tr>
<td>$G_{I}$ [N/mm]</td>
<td>1.3</td>
</tr>
<tr>
<td>$G_{II}$ [N/mm]</td>
<td>3.33</td>
</tr>
<tr>
<td>$G_{III}$ [N/mm]</td>
<td>3.33</td>
</tr>
<tr>
<td>$\sigma_{ult,1}$ [MPa]</td>
<td>52.1</td>
</tr>
<tr>
<td>$\tau_{ult,2}$ [MPa]</td>
<td>30.1</td>
</tr>
<tr>
<td>$\tau_{ult,3}$ [MPa]</td>
<td>30.1</td>
</tr>
<tr>
<td>Density [Mg/mm$^3$]</td>
<td>1.28E-9</td>
</tr>
</tbody>
</table>

* The subscripts of I, II, and III for both ‘$E$’ and ‘G (fracture toughness)’ correspond to peeling mode (or tensile opening), sliding mode (or in-plane shear), and tearing mode (or anti plane shear), respectively.
4. Experimental Impact Testing

4.1. Impactors and Impact Test Rig Structure

4.1.1. Impactor Design

The main aim of this study is to generate high impact energy by using light projectiles at maximum speeds. This will represent the runway debris impact (Cantwell and Morton 1991). A light weight (LW) impactor was designed to have a weight of 410 g (see Figure 4-1). The LW impactor consists of three main components; the rail guards made of Teflon tubing, a hemispherical shaped impacting tup (weighing 67 g) and the main body made of carbon fibre composite, maximising the impactor stiffness during impact.

As a part of the tests, a 4.32 kg heavy weight (HW) impactor shown in Figure 4-2 (originally designed with the impact test rig) was also used. Although this HW impactor was not suitable for the high velocity impact scenario (according to Cantwell and Morton 1991), the HW impactor was used for numerical validation purposes, prior to use of the LW impactor. In this case, the same impact energy was applied resulting in significantly lower impact velocities.

Figure 4-3 shows the test rig used for the impact test series. It has the capability of applying unidirectional and bidirectional tension and compression pre-strain. Appendix 5 details the test rig components and their functions. Also, the instructions for operating the test rig are provided.
in Appendix 6. Appendix 7 demonstrates the steps to collect data for the impact force using the VEE Onelab program.

![Monash impactor](image1)

**Figure 4-2: Monash impactor (Whittingham 2005)**

![Schematic of drop weight tower](image2)

**Figure 4-3: Schematic of drop weight tower**
4.1.2. Maximum Impact Velocity and Friction

Several series of tests were conducted for defining friction through equivalent gravity \((g_{eq})\) which is based on an energy conservative law using following Equation (4-1) and Equation (4-2).

\[ M g_{eq} h = \frac{1}{2} M v^2 \]  
\[ \therefore g_{eq} = \frac{v^2}{2h} \]

where \(h\) is the drop height; and \(v\) the impact velocity.

Two initial test series were conducted; hand-drop and spring drop. Initially, the hand drop tests were done and gravity was calculated. The maximum drop height was 2.8 m. As tabularised in Table 4-1, the measured overall velocity was 7.17 m/s, and thus a gravity of 9.19 m/s\(^2\) was achieved. Following this, accelerated inbound velocities were measured using springs on as depicted in Figure 4-3, as the springs help to increase the inbound velocities. This procedure is equivalent to a further increase of drop height. In this test, it was found that the measured overall inbound velocity was around 9.48 m/s which is 32 \% higher than that from hand drop tests. This is the upper velocity limit achievable with the test set-up.

<table>
<thead>
<tr>
<th></th>
<th>Height (m)</th>
<th>Velocity (m/s)</th>
<th>Equivalent Gravity (m/s(^2))</th>
<th>Equivalent Height (m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Hand Drop</td>
<td>2.8</td>
<td>7.17 ±0.04</td>
<td>9.19 ± 0.04</td>
<td>2.8</td>
</tr>
<tr>
<td>Spring Drop</td>
<td>2.8</td>
<td>9.48 ± 0.08</td>
<td>-</td>
<td>4.58</td>
</tr>
</tbody>
</table>

* If maximum gravity is equal to 9.81 m/s\(^2\), the equivalent height based on measured velocity of 9.48 m/s can be calculated.

4.1.3. Calculation of Test Parameters

Absorbed energy \((E_a)\) is simply calculated by the kinetic energy equation when inserting the difference of inbound \((v_i)\) and rebound \((v_r)\) velocities as follows:

\[ E_a = \frac{1}{2} M (v_i^2 - v_r^2) \]  
Equation (4-3)

where \(M\) = the mass of impactor.

The impact energy \((E_i)\) is calculated as follows:

\[ E_i = 0.5 \times M \times v_i^2 \]  
Equation (4-4)
The deflection \( s_t \) can be calculated by double integration of the contact force \( F_C \) using the following equations:

\[
A_C = \int_{t_1}^{t_2} F_C \, dt = \int_{t_1}^{t_2} Ma \, dt \quad \text{Equation (4-5)}
\]

\[
=> Mv_{t_2} - Mv_{t_1} = \int_{t_1}^{t_2} F_C \, dt
\]

\[
v_{t_2} = \frac{ds}{dt} = v_{t_1} + \frac{1}{M} \int_{t_1}^{t_2} F_C \, dt \quad \text{Equation (4-6)}
\]

\[
\int_{t_1}^{t} \frac{ds}{dt} \, dt = s_t - s_{t_1} = \int_{t_1}^{t} v_{t_1} \, dt + \frac{1}{M} \int_{t_1}^{t} \left[ \int_{t_1}^{t_2} F_C \, dt \right] \, dt \quad \text{Equation (4-7)}
\]

\[
s_t = s_{t_1} + v_{t_1}(t - t_1) + \frac{1}{M} \int_{t_1}^{t} \left[ \int_{t_1}^{t_2} F_C \, dt \right] \, dt \quad \text{Equation (4-8)}
\]

where \( A_C \) is the accumulative area, representing the area under the force-time history graph. \( s_{t_1} \) and \( v_{t_1} (= v_i) \) are the initial deflection of the plate and inbound velocity prior to impact, respectively. \( a \) denotes the acceleration.

### 4.2. Calibration

Several different calibration tests were conducted; this includes calibration of the optical array distance and the force transducer.

#### 4.2.1. Optical Array Distance

The separation distances for each sensor pair need to be exact to determine the inbound and rebound velocities using VEEOne Lab. In the past, it had been observed that the optical sensors (see Figure 4-3) were moved by the impactor falling down; this may change the separation distances following re-attachment of the sensors with glue – even a minor distance change affects the calculation of the inbound and rebound velocity. The initially given distances from the impact rig manual were updated with the newly calibrated values in the data acquisition software (see Table 4-2). The sensor pair of 3 and 4 was chosen to obtain the velocities as these sensors are located closest to the target and as such resulted in the most accurate velocities.
Table 4-2: Optical sensor distances

<table>
<thead>
<tr>
<th>Sensor Number</th>
<th>Separation Distance from Sensor 4 (mm)</th>
<th>Before Calibration</th>
<th>After Calibration</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>60</td>
<td>59.45 ± 0.01</td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>30</td>
<td>29.75 ± 0.055</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>6</td>
<td>6.2 ± 0.025</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>0</td>
<td>0</td>
<td></td>
</tr>
</tbody>
</table>

4.2.2. Force Transducer

A PCB Piezotronics model 201B04 piezo-electric force transducer is attached to the impactor (see Figure 4-4). The force transducer was calibrated prior to testing by PCB Piezotronics. The sensitivity of the transducer was 1.14 mL/Newton (mV/N). The conversion factor needed to be updated in the data acquisition software.

Both the LW (see Figure 4-5 (a)) and HW impactor consists of three main components: the main body (containing most of weight), the force transducer, and the rigid tub. Table 4-3 details the properties of both impactors. It is commonly assumed that the contact force \( F_C \) during interaction of the rigid tub and the composite panel corresponds to the actual impact force measured with the force transducer \( F_I \).

Table 4-3: Impactor properties

<table>
<thead>
<tr>
<th>Property</th>
<th>LW</th>
<th>HW</th>
</tr>
</thead>
<tbody>
<tr>
<td>Total Mass</td>
<td>0.410 kg</td>
<td>4.325 kg</td>
</tr>
<tr>
<td>Impact Tub</td>
<td>0.067 kg</td>
<td></td>
</tr>
<tr>
<td>Diameter (outer)</td>
<td>12 mm</td>
<td></td>
</tr>
<tr>
<td>Diameter (inner)</td>
<td>6 mm</td>
<td></td>
</tr>
<tr>
<td>Impactor deformation</td>
<td>None (Rigid)</td>
<td></td>
</tr>
<tr>
<td>Tub/Main body mass ratio</td>
<td>0.195</td>
<td>0.0157</td>
</tr>
</tbody>
</table>

Figure 4-4: Rigid tub and force transducer (After Whittingham 2005)

Figure 4-5 (a): LW impactor (After Whittingham 2005)
For this study, it is necessary to consider the distribution of the mass on both sides of the force transducer, as the transducer was mounted in between the main body and the rigid tub as seen in Figure 4-5. The importance of such consideration is stressed and demonstrated with the diagram in Figure 4-5 (b) and the equations as follows:

\[
F_C = F_I + F_R \quad F_R = m_R \times g \quad F_I = m_I \times g
\]

\[
F_C = F_I \left( 1 + \frac{m_R}{m_I} \right)
\]

Equation (4-9)

where \( F_I \) indicates the interface force as measured by the force transducer and \( F_R \) is the rigid tub force. \( F_C \) is the contact force. Also, \( m_I \) and \( m_R \) indicate the mass of the rigid tub and main body, respectively, and \( g \), the gravity (= 9.81 m/s\(^2\)).

The LW impactor has masses \( m_W \) and \( m_I \) of 343 g and 67 g, respectively. Equation (4-9) shows in this case that the contact force \( (F_C) \) is significantly higher than the interface force \( (F_I) \) as measured by the force transducer – the difference is 19 %. When considering the HW impactor, which has masses \( m_I \) and \( m_R \) of 4258 g and 67 g, correspondingly, Equation (4-9) shows that the mass distribution has an insignificant effect on the interface force, resulting in an only 1.5 % higher contact force compared to the interface force. It is important to note that all forces plotted in this study are the tip forces, which means the interface forces from experimental tests were converted into contact forces using Equation (4-9). This conversion is validated numerically in Section 6.2.2.
5. Experimental Results

5.1. Composite Coupon Tests

Composite coupon tests were used for several test series as listed in Table 5-1. For the first test series, coupons were impacted with the heavy weight (HW) impactor at very low impact energy, dropped at a height of 0.1 m to obtain the elastic impact response. It was confirmed that there was no damage to the specimens by C-scanning. This limited number of tests was used for numerical validation purposes only. As for second to fifth series, the light weight (LW) impactor was adopted for light impact scenarios at low to medium impact velocities resulting in different impact energies. Similar to the first series, the second and third series were conducted for elastic response of the laminates with low impact energy. For example, as for 2 J the LW impactor was dropped at a height of 0.5 m. These results are used for the validation of FE model for elastic response prior to damage response modelling. As for the fourth and fifth series, the coupons were all damaged due to the combination of the different pre-strain levels and high impact energy. The results from the fourth and fifth series were used for validation of the numerical delamination prediction and sectioning was conducted for a detailed damage profile through the thickness. A comparison was undertaken with the results from scarf-joint impact at a similar impact energy. All laminate composite coupon test results are tabularised in Appendix 8.

<table>
<thead>
<tr>
<th>Test Series</th>
<th>Impactor Type</th>
<th>Specimen</th>
<th>Impact Energy (J)</th>
<th>Pre-strain Level (με)</th>
<th>Purpose</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>HW</td>
<td>2</td>
<td>3.5±0.15</td>
<td>1000, 2000</td>
<td>FE validation (Elastic response)</td>
</tr>
<tr>
<td>2</td>
<td>LW</td>
<td>1</td>
<td>1.8</td>
<td>1000</td>
<td>FE validation (Elastic response)</td>
</tr>
<tr>
<td>3</td>
<td>LW</td>
<td>5</td>
<td>2±0.05</td>
<td>0 ~ 4000</td>
<td>FE validation (Elastic response)</td>
</tr>
<tr>
<td>4</td>
<td>LW</td>
<td>5</td>
<td>7.5±0.3</td>
<td>0 ~ 4000</td>
<td>FE validation for composite damage modelling (Delamination)</td>
</tr>
<tr>
<td>5</td>
<td>LW</td>
<td>10</td>
<td>10±0.6</td>
<td>0 ~ 4000</td>
<td>Damage Extent with respect to pre-strain levels</td>
</tr>
</tbody>
</table>
5.1.1. HW impactor

These tests were conducted mainly for numerical validation purposes. The HW impactor was dropped from a very low height (0.1 m) resulting in an impact energy of 3.5 J. Two tests were conducted at different pre-strain levels.

It is shown in Figure 5-1 that the force increases by 10 % as the pre-strain level is increased. In contrast, the pre-strain reduced the impact duration by 9 % as highlighted by the two dotted lines. Consequently, it is also seen that the peak force at 2000 µε pre-strain (HW2) is reached earlier than that under 1000 µε pre-strain (HW1), which was also found in literature studies (Whittingham 2005; Choi 2008). The force distribution is similar regardless of the pre-strain level, forming a bell shape. In both cases, the force transducer picked up vibrations within the impactor during and following the impact event as clearly seen in region A, but the same periodic peaks are also observed during the impact event. This is explained by the force transducer sitting in between two masses connected by a thread. This vibration was considered unimportant to model numerically but may introduce some inconsistency when trying to determine the impact duration.

![Figure 5-1: Force-time history for HW impactor](image-url)

5.1.2. LW impactor

For impact testing, the pre-strained laminate panel was subjected to high pre-strain levels up to 4000 µε. The strains at three different locations, and in-bound and rebound velocities as well as
the contact force were measured. Important relationships such as impact/absorbed energies associated with damage areas by C-scanning, and pre-strains against the peak force, and impact duration were established.

5.1.2.1. Force – Time History

Figure 5-2 (a) shows the influence of the pre-strain on the impact force-time history for an impact energy of 2 J (elastic response). In the initial portion of the force-time history (A), the force increases with higher pre-strain, because of the increased stiffness of the pre-strained panel. However, in the later portion of the force-time history (B), the force decreases with increases in pre-strain. The same phenomenon was found by Herszberg et al. (2007). In case of an impact energy of 10 J with damage development, similar force-time history patterns were observed as seen in Figure 5-2 (b). It is interesting to note that the force-time history graphs with damage developing more noise compared to the elastic 2 J case. This may be attributed to damage development during impact event. It seems that the extent of noise increases as the damage area increases with higher pre-strains.

Figure 5-2: Force at various pre-strain; (a) 0 µε (LWHD4), 2000 µε (LWHD6) and 4000 µε (LWHD8) for 2 J; (b) 0 µε (LWSD1) and 4000 µε (LWSD10) for 10 J

Figure 5-3 plots the impact peak force as a function of the various pre-strain levels for three levels of impact energy. For 2 J, it was clearly seen that the peak force was significantly increased with higher pre-strain level. The peak force increased by 43.3 % at 4000 µε pre-strain, compared to that at 0 pre-strain. Chiu et al (1997) commented on the effect of bending stiffness increase with tensile prestrain (as compared to compressive prestrain) on peak force. This is consistent with the presented elastic results. In case of 7.5 J, the peak force increased by 23 %
when compared to the response with zero pre-strain. With regards to 10 J cases, it seemed that the peak force remained nearly constant with increasing pre-strain level from 0 to 4000 με pre-strain. The increase from zero pre-strain to 4000 με pre-strain was around 5.8 % after averaging last two data at 4000 με pre-strain, which is not significant. The peak force for 10 J did not show a similar increase with pre-strain when compared to 2 J as 10 J resulted in damage on the impacted specimens. This leads to energy being transferred to the specimens for damage initiation and propagation. Also, damage can reduce the flexural stiffness, resulting in lower peak force with respect to the damage size. Similar results from Nettles et al. (1995) found that pre-strain effects on peak force become negligible if the damage area increases in the test coupons. Therefore, it can be concluded that the peak force relationship with pre-strain depends on the amount of damage development.

![Figure 5-3: Peak force versus pre-strain for laminates (2, 7.5 and 10 J)](image)

### 5.1.2.2. Impact Energy versus Force

The effect of the impact energy on the peak force was studied. Figure 5-4 summarises test results for all tests conducted on the flat panels. A wide range of impact energies were studied, ranging from 2, 4, 7.5 and 10 J under various pre-strains up to 4000 με (see Figure 5-4). It is clearly seen that the higher the impact energy, the higher the peak force introduced. The outcome seems true for all pre-strain levels investigated. The result is similar to some research studies summarised by Abrate (1991) in which the peak force increased with increasing impact
energy under no pre-strain. The peak force does not increase linearly with impact energy due to damage development.

![Graph showing force versus impact energy for laminates](image)

**Figure 5-4: Force versus impact energy for laminates**

### 5.1.2.3. Strain

Selected specimens were strain-gauged as discussed in Section 3.1.2. For 2 J results, it was seen that the specimens on the back of the impacted side experienced tensile loading (see Figure 5-5 (a)). It is important to note that the initial pre-strains for LWHD 6 and LWHD 10 were subtracted from the total pre-strain for easier comparison. The relative peak strain ($\varepsilon_{rp}$) was calculated by subtracting the initial strain ($\varepsilon_{is}$) from the acquired total strain (or absolute strain) ($\varepsilon_{as}$) as seen in Equation (5-1):

$$\varepsilon_{rp} = \varepsilon_{as} - \varepsilon_{is}$$

Equation (5-1)

It is clearly seen that an increase in pre-strain decreases the relative peak strain during impact; the relationship is approximately of linear form for elastic responses (see Figure 5-5 (b)). This proves that initial high in-plane strain/stress and thus high flexural stiffness result in higher resistance to bending. In addition, the shape of the strain curve becomes smoother as the pre-strain increases, and also the impact duration reduces significantly.
In contrast to the back side of the impacted surface, the top surface experiences compressive stresses (see Figure 5-6). Comparing SG 1 and SG 2, it is seen that the impact duration was almost identical. More importantly, it is also seen that SG 1 & 2 experience symmetrical impact behaviour about the impact centre as the graph patterns were matched.

Following impact, the initial pre-strain value is again obtained. This highlights the condition of fixed-displacement. Furthermore, as expected, it can also be seen that the plate shows oscillating behaviour following impact. Both findings are discussed further in the following.
The strain difference following impact (compared to initial pre-strain) \( \epsilon_{\text{dif}} \) is calculated by subtracting the pre-strain value \( \epsilon_{\text{is}} \), which is denoted in solid lines in Figure 5-6, from the after-impact average oscillation strain level \( \epsilon_{\text{aos}} \), which is denoted in dashed lines.

\[
\epsilon_{\text{dif}} = |\epsilon_{\text{is}} - \epsilon_{\text{aos}}| \quad \text{Equation (5-2)}
\]

For all coupons investigated, the strain difference \( \epsilon_{\text{dif}} \) resulted in values close to zero. This implies that there is no slip in the clamps throughout impact and fixed displacement may be considered for the numerical analysis.

The oscillation frequency was averaged over five periods of oscillations. The far field strain gauges (SG 1 and SG 2) were used to calculate the oscillation frequencies following impact.

It is clearly seen that pre-strain dominates the oscillation frequency. It is shown in Figure 5-7 that the oscillation frequency increases linearly with an increase in pre-strain level.

Based on the oscillation frequency \( f_o \) and mass \( M_{\text{comp}} \) of the composite plate, the stiffness \( K \) can be derived using following Equation (5-3):
\[ f_o = \frac{1}{2\pi} \sqrt{\frac{K}{M_{\text{comp}}}} \]  

Equation (5-3)

where \( K \) is in N/m; and \( M_{\text{comp}} \) in kg.

It is clearly seen that a higher stiffness was experienced with higher oscillation frequency which is due to the initially applied strains. The bending stiffness increased by a factor of four between zero and 4000 \( \mu \varepsilon \) pre-strain.

![Graph showing oscillation frequency and stiffness versus pre-strain for 10 J](image)

**Figure 5-7: Oscillation frequency and stiffness versus pre-strain for 10 J**

5.1.2.4. Impact Duration

Comparing force and strain histories as functions of time (see Figure 5-8), it is seen that both curves exhibit very similar patterns and that peaks occur at the same point of time. On the other hand, it is clearly seen that the strain resulted in shorter impact durations by comparison. This is attributed to the fact that the load cell mounted between the main impactor’s body and the impactor tub picked up impactor vibrations during impact. This is the same characteristic as observed for the HW impactor discussed earlier. Therefore, if possible, the impact duration was determined from the strain response to achieve more accurate results.

In Figure 5-9, a comparison of the impact duration is given. A significant decrease is obtained for the elastic case of 2 J. For damaged laminates the same trendline as for the elastically deformed
specimens was not observed. This is consistent with the results for the peak force shown previously in Figure 5-3 and again attributed to the development of damage.

![Graph showing force versus strain for LWHD6 (2 J, 2000 µε pre-strain)](image)

**Figure 5-8: Force versus strain for LWHD6 (2 J, 2000 µε pre-strain)**

![Graph showing impact duration versus pre-strain](image)

**Figure 5-9: Impact duration versus pre-strain**

5.1.2.5. Deflection

Figure 5-10 shows the relationship of the deflection of the plate against its respective pre-strain levels using Equation (4-8). It is clearly seen that the panel deforms less as the panel is pre-
strained more. This is due to the fact that the pre-strain stiffens the panel. This finding is consistent with the findings of Sun and Chattopadhyah (1975), Sun and Chen (1985), and Whittingham (2005). An increase in impact energy increases the deflection of the panel.

\begin{figure}[h]
\centering
\includegraphics[width=\textwidth]{deflection.png}
\caption{Deflection for 2, 7.5 and 10 J}
\end{figure}

\subsection*{5.1.2.6. Damage Area}

Barely Visible Impact Damage (BVID) comprising extensive internal delamination is a typical failure pattern for composites following impact. It is detected by sending a pulse through the laminate and receiving the reflected pulse from the discontinuity or interface inside laminate. Consequently, C-scanning was used to map the area of damage around the impact site prior to sectioning and compression tests. C-scanning was conducted at DSTO (UT Win UltraPac).

With C-scanning, both sides of the panel (impacted, non-impacted) were scanned. It was seen that the images for both sides were almost identical. Following the completion of the C-scanning all the specimens, it was found that there was no damage for 2 J cases due to the lower impact velocities; whereas all impacted specimens at 7.5 and 10 J had formed significant damage areas. Figure 5-11 below shows the size of damage area on the impacted specimen after C-scanning for 4000 \( \mu \varepsilon \) case of 10 J (LWSD9).

The C-scans displays an elongated shape at 45° due to fibre lay-up direction. The crack propagation can be seen by visual observation at the back face of the specimens. Some damage
is confined to the outer ply of the back face and is due to the bending strains which cause additional splitting of the ply along the fibres. The outer lamina fibres are at 45° in these specimens and offer no resistance to such failure mode (Zhang et al. 1999). All damaged specimens have a similar elongated shape for 10 J; whereas such elongation along 45° is very small for 7.5 J except for LWHD 17. The remaining C-scan images are displayed in Appendix 9.

Figure 5-11: C-scanning results of LWSD 9 (4000 με) for 10 J: (a) C-scanning image (b) only damaged area using ‘Image J’ (c) the back side of damaged specimen with strain gauge attached

In relation to the pre-strain effect, it is easily observed that the pre-strain increases the damage area (see Figure 5-12 (a)), especially at high pre-strain level. This was expected based on the previous results for peak forces and impact duration. It is also seen that the damage area increased with an increase in absorbed energy (see Figure 5-12 (b)). These results give confidence to conclude that the relationship amongst the three, main parameters (impact energy, absorbed energy and pre-strain level) are consistent with each other.
It is of interest to evaluate the peak force as a function of damage area. Lagace et al. (1993) and Sankar et al. (1996) stated that the peak force is shown to be proportional to the size of the delamination. These conclusions are based on non-preloaded impact case. For the current study, no distinct relationship between the damage area and the peak force for 7.5 and 10 J was found as seen in Figure 5-13. It seems that the peak force reaches a maximum value at about 4.5 kN.

**5.1.2.7. Sectioning**

The coupons tested for 7.5 J impact energy from 0 to 4000 pre-strain levels (LWHD12 to LWHD17) were sectioned. Polished cross-sections were used to characterise the damage profile
through-the-thickness of impacted composites. Locations of delaminations, matrix cracking, and fibre fractures were recorded.

Figure 5-14 illustrates one of the sectioning results, LWHD17 (7.5 J, 4000 με pre-strain). Most of the delamination is detected within the interface of different ply angles. In addition, matrix cracking and fibre breakage across plies are detected. The damage is symmetrical about the impact centre. In addition, the damage is more severe within the bottom plies. As discussed in the literature review (Section 2.3.3), it would be expected to have most damage in the lower plies as the thickness is small compared to the length of the tested specimens. However, applying pre-strain increases the bending stiffness and results in a different damage profile.

![Figure 5-14: Sectioning view for LWHD 17 (7.5 J, 4000 με pre-strain)](image)

*It is noted that the lines emphasize detected delamination and cracks.

5.1.2.8. Compression After Impact (CAI) Test

Compression tests were conducted for the impacted specimens to further characterise the extent of impact damage.

Specimens had to be cut into smaller size, 115 mm long and 95 mm wide, from the original size (200 × 100 mm) to fit the anti-buckling compression rig. The acquisition system was able to capture the load and displacement of the loading for a loading rate of 1 mm/min. Special care was taken to grind the specimens parallel on the loaded ends.

For CAI testing, only 10 J specimens were tested. Failure occurred suddenly. The locations of failure on the specimens were divided into two categories: 1) near fixed grip for undamaged
specimens or specimens with insignificant amount of damage, 2) at the centre initiating from the impact damage site. The residual shape of the plate after failure is illustrated in Figure 5-15. The photograph indicates a classical compression failure following impact: continuous blister propagation on impacted side and unstable blister propagation on back side to the edges of the plate (Zhang et al. 1999).

Specimens with larger damage area failed at lower stresses as expected, irrespective of failure location. Figure 5-16 shows the established relationship between damage area and residual strength. With an increase in damage area, the residual strength dropped almost linearly. According to the previously defined relationships between damage area and pre-strain, it can be said that with higher pre-strain, the residual strength reduces linearly, indicating that the pre-strain plays an important role with regards to the residual strength. It can therefore be concluded that pre-straining increases the damage area, but does not seem to affect the damage mode. Nevertheless, the presence of two failure types leads to uncertainties in the result interpretation. For scarf joints, TAI (tension-after-impact) tests rather than CAI (compression-after-impact) tests were conducted instead.

![Figure 5-15: Residual shape of the specimen LWSD 7](image)

60
5.2. Scarf Joint Tests

Composite scarf joints were pre-strained up to 5000 με and subjected to different levels of impact energy ranging from 1.8 J to 19 J (see Table 5-2) in the same manners as the composite laminates. All test results are summarised in Appendix 10. Similar to the testing of the composite coupons, several parameters were compared against pre-strain, including peak force, absorbed energy, impact durations based on force and strain-history, damage area, and residual strength from tensile testing.

<table>
<thead>
<tr>
<th>Impact Energy (J)</th>
<th>Pre-strain Level (με)</th>
<th>No. of Specimens</th>
<th>Purpose</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.8</td>
<td>1000</td>
<td>1</td>
<td>Elastic Response</td>
</tr>
<tr>
<td>4.5±0.09</td>
<td>0, 1000, 2000, 3000, 4000, 5000</td>
<td>6</td>
<td></td>
</tr>
<tr>
<td>8±0.15</td>
<td>0, 1000, 2000, 3000, 4000</td>
<td>5</td>
<td>Damage Response</td>
</tr>
<tr>
<td>16</td>
<td>4500</td>
<td>1</td>
<td></td>
</tr>
<tr>
<td>19±0.21</td>
<td>0, 1000, 2000, 3000, 4000</td>
<td>5</td>
<td></td>
</tr>
</tbody>
</table>

5.2.1. Force – Time History

It is of interest to study the impact force patterns for scarf joints in relation to the impact energy and pre-strain levels.

Figure 5-17 (a) shows a comparison of the elastic response of scarf joints under three different pre-strain levels of 0, 2000, and 5000 με subjected to an impact energy of 4.5 J. The initial force
gradient, which is indicated by region ‘A’ shows the stiffening effect of the tensile pre-strain (i.e., increase in force). The time to reach the maximum force during impact shortens with higher level of pre-strain. This leads to an earlier occurrence of force gradient in region ‘B’, and consequently shorter impact duration. For the damage response, to some extent, similar patterns were observed for 8 J (see Figure 5-17 (b)) and 19 J. However, due to forming of damage in the scarf joints during dynamic impact, the greater amount of noise was captured in force time graphs, unlike for the elastic response. Moreover, with a great amount of damage or sudden failure of the specimens, second peaks either disappear or drop drastically and the impact duration may be significantly increased.

Figure 5-17: Force-time history for scarf joint: (a) for 4.5 J, (b) for 8J

The relationship of pre-strain level and impact force can be established as seen in Figure 5-18. In the elastic response region (4.5 J), it is seen that the peak force increases linearly with an increase in pre-strain. For example, at 4000 με pre-strain, the peak force increased by 33 % when compared to that at zero pre-strain level. The peak force was increased by 9 % for an impact energy of 8 J. It is clearly seen in Fig. 5-18, unlike for the 4.5 J and 8 J cases, that the peak force induced by 19 J case is reduced as pre-strain levels increase. For example, the peak force at 4000 was dropped by 16 %, compared to 0 με. As the finding for scarf joints is similar to that
for the composite laminate testing (see Fig. 5-3), it can again be concluded that the extent of the damage formed in the specimens significantly affects the peak force.

![Graph showing impact force with respect to pre-strain levels](image)

Figure 5-18: Impact force with respect to pre-strain levels

The impact peak force is plotted as a function of impact energy, ranging from 1.8 to 19 J as seen in Figure 5-19. Results are categorised by pre-strain levels from 0 to 4000 µε pre-strain. It is seen that the peak force increases in a similar manner to the laminate results with impact energy (see Figure 5-3). The deviation from a linear relationship is again attributed to the extent of the damage occurring in either/both adherends or/and the adhesive regions. In other words, it can be postulated that the pre-strain levels influence the size of the damage area, especially beyond 3000 and 4000 pre-strain with higher impact energy levels such as 19 J.
5.2.2. Strain – Time History

Strains from SG1 and SG2 are discussed mainly in this section as SG 3 was unable to capture the complete strain-time history due to damage on the surface under the strain gauge during the impact event.

For the lowest impact energy case (OPS) as seen in Figure 5-20, the strain remains at its pre-strain value following impact. However, with the presence of damage during impact, the strain level after impact may drop. The result is similar to the one observed for laminate composite panels and is generally attributed to permanent deformation of the specimen following impact, rather than slipping of the specimen in the grips.
5.2.3. Impact Duration

Similar to the laminate composite results, the impact duration based on the force-time history was longer than that observed from strain values because the force-time history captures vibrations created by the impactor components (see Figure 5-21). Hence, the actual impact duration is determined based on the strain-time history graph if possible.

It is clearly evident from Figure 5-22 that pre-strain shortens the impact duration if no damage is present. For 4.5 J, the impact duration was shortened by around 50% when comparing zero pre-strain and 3000 pre-strain. The trend for 8J is less obvious due to damage development. This is the same behaviour as observed for the composite laminates.
5.2.4. Deflection

With Equation (4-8) the deflection can be calculated based on the force-time history. It is seen in Figure 5-23 that higher impact energy induces more deflection to the panel during impact. With higher pre-strain, the deflection is smaller due to higher stiffness added by applying tension load. However, the relationship becomes less obvious when damage is formed in the panel as seen in the case of 19 J. This is the same behaviour as observed for the composite laminates.

Figure 5-22: Impact duration versus pre-strain

Figure 5-23: Deflection versus pre-strain for 4.5, 8 and 19 J
5.2.5. Damage & Failure Inspection

Two methods were adopted to evaluate the impact damage for composite scarf joints: 1) evaluation of the damage area, which is accomplished by NDE technique using the C-scanning method; 2) characterisation of the failure types through-the-thickness, which is completed by inspection of polished cross-sections.

5.2.5.1. Damage Area

Following impact, all specimens were c-scanned so that the extent of the damage could be evaluated. Due to the adhesive bondline, it was again considered necessary to scan the specimens both from top and bottom surfaces.

It was found that the specimens subjected to 4.5 J or less showed no damage in either adherend or adhesive region, irrespective of the pre-strain levels. However, damage was initiated at 8 J and 1000 µε pre-strain level. The damage became more severe as the impact energy and pre-strain levels increased. Figure 5-24 below shows a typical c-scanning result. The damage area is indicated by the difference in colour compared to the surrounding area, which represents the undamaged part. It is shown by c-scan that most of delamination damage was formed at the vicinity of the impact point. It is speculated that the blue colour region indicates the location of the delamination inside the laminate that is located above the bondline; whereas, the other colours indicate occurring the damage within the adhesive region or interfacial failures or any delamination underneath bondline. This indicates that the damage is more extensive on the tensile side during impact, which is the same as for the composite laminate.

![C-scanning damage area for 8 J and 3000 µε pre-strain (EPZ4)](image-url)

Figure 5-24: C-scanning damage area for 8 J and 3000 µε pre-strain (EPZ4)
To some extent, the damage shape is now dependent on the impact energy and pre-strain level. With low impact energy, the damage shape is close to circular shape as shown in Figure 5-25 for EPZ2 and EPZ3. With higher pre-strain levels and impact energy, the damage propagates along the width (y-direction) and along the left bondline (tension side) for EPZ4 and EPZ6. However, with the combination of higher impact energy and higher pre-strain levels, the damage shape becomes also more elongated along the 45° ply direction (see Figure 5-26), particularly near the back face, which was also found for the tested composite laminates with 10 J impact energy. The semi-circular shape becomes less obvious. The presence of the adhesive bondline varies the damage shape compared to laminate plates for higher energy impact cases.

Upon or during impact, some of specimens failed catastrophically by being separated into two parts along the scarf bondline. The most noticeable point is that with sufficient impact energy (in this case it is above 16 J), the catastrophic failure was induced above a pre-strain of 4000 με, indicating that the pre-strain contributes significantly to sudden failure of the specimens.

![Figure 5-25: Damage shape (Not to Scale)](image1.png)
* X- direction indicates the loading direction (0° ply)

![Figure 5-26: Rear face of impact point for NTPZ4 (19J, 3000 με)](image2.png)
Figure 5-27 (a) shows the damage area as a function of pre-strain level for 8 J and 19 J. It is evident that as the pre-strain increases the damage area increases. With respect to absorbed energy, the damage area increases with larger absorbed energy, especially from a region of 6-10 J to a region of 14-16 J of absorbed energy (see Figure 5-27 (b)). However, the relationship is very less obvious within the region of 6-10 J. This may be attributed to the fact that to some extent the adhesive region contributes to the energy absorption as the adhesive is more ductile than the laminate.

![Graph showing damage area versus pre-strain and absorbed energy](image)

**Figure 5-27:** (a) Damage area versus pre-strain; (b) Damage area versus absorbed energy for scarf joint

### 5.2.5.2. Failure Modes

Following C-scanning, some of the damaged specimens were selected and cut at the impact point for sectioning.

One of sectioning results is illustrated in Figure 5-28, showing a longitudinal cut through the scarf specimen for EPZ 3 (8J, 2000 µε). The section highlighted in red circles shows the interfacial failure between adhesive and adherend regions on the lower interface (tension side). The occurrence of such failure is linked to the presence of delamination which occurred in between the lowest 0 and 45 ply interface. As highlighted in the right figure, cohesive failure also occurred as cracks propagated cut through the adhesive around the impact location. It was also observed that no adhesive-related damage was seen along the bondline towards the top surface. This is consistent with the interpretation of the C-scanning results. The majority of
damage is found to occur in the adherends as typical laminate plate impact failure modes, including delamination, matrix cracking, fibre crack, and bending fractures. These failure modes are very similar to failure modes in scarf joints tested with zero pre-strain, which were described by Harman and Wang (2005) and Takahashi et al. (2007). In addition, the through-the-thickness damage profile is of pyramid shape, which is normally seen in flexible laminate failures, showing that a great amount of damage is formed within the lower plies (on the tension side). However, as expected from the result of the composite laminate, the top plies may be damaged due to the pre-straining effect which makes the panel rigid.

* The adhesive bondline shows a tight-knit tricot carrier for ease of controlling bondline thickness and for its good blend of structural and handling properties during lay-up (Peraro, 2000).

With higher impact energy and the same pre-strain level, the failure modes are similar as seen in Figure 5-29 (a). Interfacial failure (or adhesive failure) occurs around regions of delamination in the interface of the lowest 0 and 45 degree plies. It is observed that this type of failure was found in all scarf joint that failed during impact. However, the adhesive failure that cuts through the adhesive region was more pronounced as depicted in Figure 5-29 (b) with a larger number of interfacial failures. In addition, cracks were seen in the upper cohesive region (see Figure 5-29

Figure 5-28: Microscopy image (5 X zoom) for EPZ 3 (8J, 2000 με)
(c)). It can be said that although the failure modes are the same, adhesive failure or interfacial failures are extended by impact at higher impact energies.

As stated, a common observation is the interfacial failure around the 0° plies for all investigated scarf joints with sectioning. It is desirable to investigate the development of failure. Two possible failure scenarios may occur: 1) the interfacial (or adhesive) failure triggers the delamination along the interface between 0° and 45° plies, which then propagates towards the centre; or 2) delamination is triggered first due to impact deformation in the adherend region, followed by interfacial failure due to crack growth from the adherend region into the adhesive region. This sequence of events can be indirectly studied by comparing the failure types (or size) of laminate coupons and scarf joints for the same impact energy and pre-strain level, which is undertaken in Section 5.3.
5.2.6. Tensile After Impact (TAI) Tests

It is of interest to study the load-bearing capability of the damage scarf joints under tensile loading following impact to further characterise the damage.

Two different types of failure were observed. For the first, failure occurs along the bondline. NTPZ1 is exemplified as seen in Figure 5-30 (a). Interfacial failure between the adherend and the adhesive was seen due to cohesive shear failure with little or no fibre fracture and pull-out. For the second failure mode, as seen in Figure 5-30 (b), failure takes place in the adhesive region and adherend. Also, fibres were ruptured, mostly in the lower 45° ply. No failure trend in relation to damage area or pre-strain level was observed.

Figure 5-30: Images of failure after TAI (side view): (a) for NTPZ1 (19J, 0 με); (b) for FTPZ (14 J, 0 με)

Figure 5-31 shows the linear relationship between residual strength and damage area. The two failure modes are identified. It is clearly seen that with larger damage, a smaller residual strength is obtained. This linear relationship seems true regardless of pre-strain levels in the impact test. Based on numerical results from Feih et al. (2007), which assumed that no damage within adherends but only in the adhesive bondline is formed, a similar relationship with a linear trendline was found. It may be speculated that the damage tolerance of the damaged specimens is mostly determined by the amount of adhesive damage, although significant
amounts of damage are shown to occur in the adherend. The damage in the composite adherends might therefore not influence the results significantly. Further work will be undertaken to validate this statement.

5.3. Comparison between Laminate and Scarf Joint

It is of interest to compare the impact (damage) response of both the laminate and scarf joint at a similar impact energy. It is important to identify the failure mechanism in the laminate itself and compare it to the events with a scarf bondline embedded in the laminate.

Force-time history graphs (Figure 5-32 and Appendix 11) clearly identify similar force-time histories for both laminate and composite scarf joints. This implies that the impact response on both is very similar, although the damage type is not identical as observed by sectioning. In fact, the total damage area for the scarf joint is larger than that for the laminate due to additional adhesive failure mode within the bondline (see Figure 5-33).
As is observed in scarf joint sectioning, the majority of damage occurs in the adherend by forming delaminations. The common location of delamination is the interface of 0° and 45° plies. In terms of damage shape, at lower pre-strain level, similar damage shapes were observed as seen in Figure 5-33. However, at 4000 µε pre-strain, a clear shape difference was observed as the damage shape for the laminate remains circular with fibre splitting at the back side of the impacted site as discussed earlier; whereas for the scarf joint the circular damage area is superimposed with the adhesive damage area, resulting in a half-circular shape. It implies that although the force-time history graphs are very similar, the damage area and the damage shape become dependent on the configuration of the targets, especially at higher pre-strain level. This indicates that in scarf joints during impact, delamination in the adherend region is firstly initiated due to high bending stress during impact, which then triggers the adhesive failure as the delamination propagates along the ply towards the bondline. As a result of the delaminations, adhesive failure occurs.

Figure 5-32: Force-time history: (a) at 1000 µε for elastic response; (b) at 2000 µε for damage response (EPZ3: 282.26 mm² for 8 J; LWHD14: 168.117 mm² for 7.5 J)
5.4. Conclusion

The experimental study for composite laminates and scarf joints results in an extensive database for validation of numerical results. Firstly, with the 2 J experimental results, which are considered elastic, it was confirmed that the strains before and after impact remain the same, which means that no slippage occurred throughout impact duration. Therefore, boundary conditions should be set in such a way that the applied pre-strain level remains constant (fixed displacement). When comparing the impact forces from the composite laminates and the scarf joints, the force impact responses are very similar, which implies that the bondline does not affect the elastic response.

10 J experimental results, including impact force and damage area, are required to validate composite damage models. The validated composite damage parameters can then be adopted for scarf joint modelling. The initial development of delamination damage should be similar in both models. The damage shapes after C-scanning were similar for the composite laminate plates and scarf joint (EPZ2 and EPZ3), especially at low impact energy with low pre-strain level. The common shape is typically circular around the impacted centre. With higher impact energy, fibre splitting was formed at the back side of the impacted site, while the circular delamination shape remained for composite laminates. On the other hand, for scarf joints, due to the

Figure 5.33: Damage area versus pre-strain for scarf joint and laminate
combination of failures in the bondline and in the adherend, the damage shapes became different. With higher pre-strain level, the damage propagates along the bondline and width for EPZ4 and EPZ6, resulting in semi-circular shapes.

After sectioning, for both damage area and the typical composite failures were seen including delamination and fibre fracture and matrix cracking, while delamination is the most dominant failure. The typical upside-down pyramid shape of the damage profile through-the-thickness in composites was seen. However, it is important to note that due to the pre-straining effect to the plate, the pyramid shape from the top ply was found as well. This means that delamination is required between all plies to validate the material properties during impact. However, it is very important to emphasise that for scarf joints the damage occurred mostly in adherend region instead of the adhesive region. It is also apparent that adhesive failure is caused by the propagation of delamination between plies. This finding is vital for the numerical methodology for scarf joint modelling as both the adhesive and delamination failure should be introduced to accurately represent the failure mechanism.
6. Finite Element Modelling Methodology

This section explains the finite element methodology including the choice of element types (2D, 3D and cohesive elements). In addition, extensive parametric studies for different parameters are carried out for both composite laminates and scarf joints.

6.1. Element Aspects and Procedural Overview

Many different types of elements are available in Abaqus; 2D shell and 3D solid elements are the commonly used element types as seen in Figure 6-1. Firstly, 2D shell elements are commonly used to model structures when their thickness is significantly smaller than their span length. The geometry is defined by the reference surface which is by default the mid-surface; and the thickness is defined in the section property. The 2D shell elements have displacement and rotational degrees of freedom (DOF) at each node. Because of this, the 2D shell elements are more appropriate for structures undergoing bending deformation. The surface direction (called normal direction) to define the top (SPOS) and bottom (SNEG) surface can be controlled by the node numbering. Secondly, 3D solid elements represent the full 3D stress-state as it physically represents the thickness of the geometry unlike 2D shell elements. However, 3D elements have only displacement DOFs which may result in poor bending performance.

Table 6-1 shows a comparison between the capabilities of the 2D model approach and the 3D solid model. Most importantly, shell elements are better suited for bending, which is the primary deformation during impact. However, a detailed model of the scarf joint requires 3D
solid elements to resolve the interface between the individual plies and the adhesive bondline. It was therefore decided to validate the bending performance of the 3D solid element model against the shell model for the laminate impact tests for the elastic response. The predictions were validated against the elastic impact response of the composite laminates with both the light weight (LW) and heavy weight (HW) impactor. Delamination was included in the 3D solid model only and validated against the impact response of composite laminates with damage. This validated 3D model was then extended to include the adhesive bond layer to compare the impact response of the composite scarf joints.

<table>
<thead>
<tr>
<th>Table 6-1: Overview of 2D shell and 3D solid models</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Element type</strong></td>
</tr>
<tr>
<td>------------------</td>
</tr>
<tr>
<td>Bending performance</td>
</tr>
<tr>
<td>Ply orientations</td>
</tr>
<tr>
<td>Composite failure</td>
</tr>
<tr>
<td>Delamination</td>
</tr>
<tr>
<td>Composite plate model</td>
</tr>
<tr>
<td>Scarf joint model</td>
</tr>
</tbody>
</table>

Numerical simulations are accomplished by using MSC.Patran (version 2010, R1) as pre-processor and by Abaqus version 6.9 as solver. Patran Command Language (PCL) was used in Patran in order to reduce the modelling times, especially when changing the size of model or mesh density. Abaqus/Standard (implicit analysis) was adopted for pre-tensile loading. Subsequently, Abaqus/Explicit was used to represent the dynamic impact loading.

**6.2. FE Model Set-up & Geometry**

The geometry and boundary condition set-up will be briefly explained in the following subsections.
6.2.1. Boundary Conditions Set-up

To account for the pre-tension loading at various pre-strain levels, prescribed displacement on one side of the panel was applied irrespective of the element type (2D and 3D) (see Figure 6-2), following conversion of pre-stain to displacement via the following equation:

\[ \Delta L = \varepsilon \times L \]  

Equation (6-1)

where \( \varepsilon \) is applied pre-strain; \( \Delta L \) and \( L \) are the applied displacement and the length of span, respectively. The pre-strain was evaluated at the centre of the plate over a length of 5 mm corresponding to the strain gauge location.

![Figure 6-2: Schematic of initial numerical setting](image)

The Table 6-2 below shows the required displacement to apply for the numerical models to satisfy initial strain levels.

<table>
<thead>
<tr>
<th>Pre-strain (µε)</th>
<th>Required Displacement, ( \Delta L ) (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1000</td>
<td>0.14</td>
</tr>
<tr>
<td>2000</td>
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<td>3000</td>
<td>0.42</td>
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<tr>
<td>4000</td>
<td>0.56</td>
</tr>
</tbody>
</table>

6.2.2. Impactor Geometry

This section proves the mass distribution theory detailed in Section 4.2.2 using numerical analysis by comparing results from a full size impactor model and the simplified tip impactor model for HW and LW impactors as seen in Figure 6-3. The full impactor is modelled as shown in Figure 6-3 (a) in a simplified manner. The interface representing the force transducer was modelled using contact between the two components. The main reason to implement the full impactor model is to capture the contact force at the interface between the rigid impactor tub
and the main body part. This is the accurate method for acquiring the force in the same manner as for the real force transducer placed in between the two components. Secondly, the impactor was modelled as the tip only (see Figure 6-3 (b)), which is the only part to interact with the target.

The tip represents the rigid tub in hemispherical shape. Its density is adjusted to account for this total mass (see Table 6-3). It is worth noting that instead of using the real volume from the experiment, the numerical impactor volume was adopted to ensure the correct mass. The discretised volume may differ from the real volume.

![Numerical model geometries for impactor; (a) full model impactor, (b) analytical surface impactor](image)

**Figure 6-3: Numerical model geometries for impactor; (a) full model impactor, (b) analytical surface impactor**

<table>
<thead>
<tr>
<th></th>
<th>Full Impactor Model</th>
<th>Simplified Impactor Model</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Volume (mm³)</td>
<td>Density (ton/mm³)</td>
</tr>
<tr>
<td></td>
<td>Main Body</td>
<td>Tub</td>
</tr>
<tr>
<td>LW</td>
<td>1.50 × 10⁶</td>
<td>4503.29</td>
</tr>
<tr>
<td>HW</td>
<td>2.83 × 10⁻³</td>
<td>1.48 × 10⁴</td>
</tr>
</tbody>
</table>

**6.2.2.1. Set-up**

In order to acquire the interface force, the model is created in such a way that the two components of the impactor are separated with the interface nodes for both being placed at the same location. Both component interfaces are constrained by contact (penalty constraint method). The interface force \( F_I \) as well as the contact force \( F_C \) is predicted during the impact event.
As for the selection of material models for the rigid tub impactor and the main body impactor, three combinations were considered: (1) both modelled as rigid materials, (2) both modelled as elastic and (3) rigid for the rigid tub impactor and elastic for the main body impactor. However, it was found that combinations (1) and (2) led to computational convergence problems. Hence, only option (3) is considered in the following.

### 6.2.2.2. HW Impactor

For the full HW impactor model, significant amount of computational noise necessitated filtering the force-time history graph, particularly for the interface force. An averaging method across two adjacent points was used. This averaging method produced the best results as it removed the high frequencies but did not obscure any of the significant peaks in the numerical data.

Firstly, it is evident from Figure 6-4 (a) that the interface and tip forces are close to identical. This was expected as mentioned earlier due to the relatively heavy weight of the main body component compared to the tub. Secondly, for the heavy impactor, an analytical surface impactor geometry was considered, which represents only the hemispherical shape of the tip of the impactor but includes the weight of the entire structure through its adjusted mass. It was found that the simple impactor saves significant computational time and gives close results to the full impactor model as shown in Figure 6-4 (b). Hence, in the heavy impactor case, the analytical impactor was adopted instead.

Figure 6-4: Force-time history for HW impactor at an impact energy of 3.5 J and 1000 μs pre-strain: (a) Interface and contact forces; (b) Full impactor versus analytical surface impactor
6.2.2.3. LW impactor

Unlike the HW impactor simulation, the computational noise is minimal for this analysis; this may be due to the weight of the main body in relation to that of the rigid tub. Hence, for the LW impactor, a filtering process was not needed.

Figure 6-5 (a) shows a comparison of the predicted force at the interface (force transducer, $F_I$) and tip (contact force, $F_C$). As expected from the mass ratios, the contact force is significantly higher by 17.5 % than the interface force, with the impact duration remaining the same. The theoretical deviation of the contact force according to Equation (4-9) is included in Figure 6-5 (a) and shows excellent agreement with the numerical result for the tip force. It is therefore proven that the impactor model is capable of capturing the interface force, which is equivalent to the force transducer. The mass relation equation in Section 4.2.2 is also validated. As discussed, all experimental data presented for LW impactor were transformed to the value for the contact force.

It is seen in Figure 6-5 (b) that the analytical surface impactor and the full impactor model result in excellent agreement. Since the analytical impactor results in increased computational efficiency, it was decided to use the analytical impactor instead of the full impactor model for all LW impactor cases.

![Figure 6-5: Force-time history for LW impactor at 2 J and 1000 με](image)
6.2.3. Composite Laminate

The laminate panel was modelled with Patran using both 2D shell and 3D solid elements. In general, 2D elements are most appropriate to represent the impact response for this flexible plate as by default this 2D element account for the rotational degrees. The 3D elements are capable of accounting for full 3D stress-state, however, the computational time is more expensive and their bending performance is poor and generally too stiff. However, 3D elements will be needed for a full representation of scarf joint details to represent the individual plies interacting with the angled adhesive bondline. Therefore, both 2D and 3D elements were used for the laminate flat panel and their response was compared in terms of mesh density, impact force and strain.

The ply orientations for 3D (ply by ply) and 2D composite shell elements are described differently in Abaqus (see Figure 6-6). For shell elements, the global orientation system was set with x and y- direction describing in-plane directions and z the through-the-thickness direction. The first column in the shell section card indicates the thickness of each ply according to the ply orientation, which is assigned by the last column. The second column indicates the number of integration points, default of 3, in each ply. The third column indicates the name of the ply material property which is assigned in the * Material card. Secondly, 3D elements require a *Solid section for each ply and an orientation coordinate system for each ply direction.

![Figure 6-6: Element set-up: (top) 2D shell element; (bottom) 3D solid element](image-url)
6.3. FE Parameter Studies

It is necessary to undertake a mesh sensitivity study in which the results should remain constant after a certain degree of mesh refinement. Several models with different mesh seed sizes (MSS), ranging from 10 to 0.625 mm, were created to determine the mesh sensitivity. The following parameters were compared: contact force, energy, displacement and frequency.

6.3.1. Shell Mesh Study (2D)

A mesh refinement is undertaken in the area of high stress or strain gradient surrounding the impact location. 2D shell elements, S4R (4 node elements with reduced integration) in ABAQUS are adopted. The model was clamped at each end with zero pre-strain applied. Additionally, it was assumed that there is no failure occurring during impact, i.e. the model behaves elastically.

It is evident that the finer mesh, the longer the running time. For example, MSS 0.625 takes 565 times longer than MSS 10. The results were compared as shown in Figure 6-7. The percentage of difference indicates the comparison of the respective values for each mesh refinement. With a finer mesh, the differences become smaller in terms of maximum deflection of the panel and contact force. In terms of hourglassing, all different mesh densities were deemed stable since all hourglass energies were less than 0.5 % of the respective internal energy. The hourglass energy decreases with smaller mesh size. MSS 1.25 was chosen for the FE modelling as the mesh results start to converge in between MSS 1.25 and MSS 1.0.

![Figure 6-7: Differences of each mesh seed size level](image)

In this study, as the impacting area is suffering from high stress, the finest mesh, having MSS 1.25 mm, is used in the impactor vicinity and the mesh becomes coarser (MSS 2.5 mm to 5 mm)
away from the centre as seen in Figure 6-8. Moreover, as the model is expected to have higher stresses near the grip areas, it is also considered necessary to have a finer mesh (2.5) in this region.

![Figure 6-8: Final mesh for a composite laminate](image)

**6.3.2. Solid Mesh Study (3D)**

Similarly to the shell study, the mesh seed size was initially varied from 10 mm to 1.25 mm. Since these 3D models were modelled ply by ply, they contained a significantly larger number of nodes and elements, and the analysis takes a much longer compared to shell elements.

It was concluded that amongst 4 different uniform mesh seed sizes, the results converged between MSS 2.5 mm and MSS 1.25 mm. Similar to shell elements, it was decided to use a transition mesh to save computational time and to have more precise results by utilizing a finer mesh (1.25 mm) at the high strain gradients around the impactor location.

**6.3.3. Element type for Adherend**

This section validates the accuracy of the 3D sold model to simulate a primary bending problem compared to the 2D shell model. Solid elements do not have rotational degrees-of-freedom (DOF 4, 5 and 6). For the scarf joint tests, only the 3D model is to be used, thus it is important to check its accuracy for capturing the bending deformation for the laminate model.

For the 3D element model, each layer of solid element represents one ply orientation. However, it should be noted that Abaqus/Explicit supports only one integration point through-the-thickness for a single 3D element (reduced integration), so it is not possible to directly extract the strains on the surface of the elements for result comparison. A common practice to measure the strain on the top surface, when using 3D elements is to add “dummy” shell plies of thin and
low stiffness material (see Figure 6-9), which share their nodes with the outer surface nodes of the 3D elements. This practice avoids errors when extrapolating the strain from the integration points to the nodes, since strains are most accurate at integration points. In this study, the dummy shells used have a thickness of 0.01 mm and 1% of the original ply property used for the composite.

3D solid and 2D shell elements derive very similar results for the impact force as shown in Figure 6-10 (a). In addition, both 2D and 3D also have very similar patterns in terms of strains (see Figure 6-10 (b)). The difference on average is within 5%. This implies that the 3D model with its current mesh density is suitable to use in the following impact simulations.
6.3.4. Ramp-up

It should be noted that for the ramping-up phase, Abaqus/Explicit was used instead of Abaqus/Standard for 2D shell elements. With Abaqus/Explicit, it was necessary to determine ‘minimum time to be used for the ramp-up as kinematic energy (oscillation) may become significant. Ideally, longer times are desired to minimise the dynamic effect. During the preloading step (step 1) in Abaqus/Explicit, the rate of pre-straining is controlled by using the smooth step definition method defined through *AMPLITUDE. The ‘smooth step’ method helps to minimise the inertia effect in explicit analyses (see Figure 6-11). In order to minimise inertia effects, a ramp-up over a relatively long time (0.003 s) was used for prescribing displacements in longitudinal direction, followed by the displacement being fixed during impact duration (0.001 s). On the other hand, using Abaqus/implicit in step 1 is free of inertia effect and independent of time, so that the amplitude line can be constant as described by the red line. The region from t=t_2 to t=t_3 of constant amplitude (fixed displacement) is used in step 2 for both implicit and explicit analyses.

![Figure 6-11: Smooth Step Definition](image)

6.3.5. Contact Algorithms

Of the many contact options available in Abaqus, “contact pair” is chosen for impact modelling as this contact algorithm is broadly used in many applications. With this, there are parameters which need to be studied, including mechanical constraints, and penalty stiffness values.

6.3.5.1. Kinematic or Penalty Methods with Contact pair

Kinematic constraints result in a higher contact force (stiffer result) since this type does not allow any penetration as compared to penalty constraints (see Figure 6-12). Both methods
therefore result in small differences as seen in Table 6-4. However, in terms of the numerical stability, both proved reliable. As the penalty method is commonly used, it was decided to use the penalty methods for impact.

![Figure 6-12: Kinematic (left) and Penalty (right) Contact Formulation (Abaqus 6.9 Documentation 2009)](image)

<table>
<thead>
<tr>
<th>Table 6-4: Mechanical constraints summary</th>
</tr>
</thead>
<tbody>
<tr>
<td>Penalty</td>
</tr>
<tr>
<td>Peak force (kN)</td>
</tr>
<tr>
<td>E11 Strain (1st ply)(µε)</td>
</tr>
<tr>
<td>Time (s)</td>
</tr>
<tr>
<td>Penetration (including clearance)</td>
</tr>
<tr>
<td>Deflection (16th ply)</td>
</tr>
</tbody>
</table>

6.3.5.2. **Penalty Stiffness (k) with Penalty Method**

With the penalty method, the maximum contact force depends on the spring stiffness factor and the penetration depth. By default, the penalty stiffness, k, is set to 1. A larger penalty stiffness prevents the impactor from penetrating into the slave model. However, one should be aware that the increased k reduces the required time step, and thus increases the computational time required. The results are tabulated in Table 6-5 as a function of different penalty stiffness values.

As expected, increasing k values converged to kinematic contact (due to an increase in stiffness in spring) as seen by the increasing contact force overall. Especially, when the k value dropped to 0.01 from a default of 1, the contact became softer by 5 %; whereas even with an increased value, especially from 1 to 40 the force increased but by less than 0.5 %. This suggested that the default k value is suitable to use but the decreased value should not be used as the penetration is increased significantly. It is evident that hard contact increases the computational time.
<table>
<thead>
<tr>
<th>Factor (k)</th>
<th>Contact Force (kN)</th>
<th>Strain (E11)</th>
<th>Duration (s)</th>
<th>Penetration (mm)</th>
<th>Time Elapsed</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.01</td>
<td>3024.51</td>
<td>11363.2</td>
<td>0.0066</td>
<td>0.21672</td>
<td>1</td>
</tr>
<tr>
<td>1</td>
<td>3165.07</td>
<td>12379.0</td>
<td>0.0065</td>
<td>0.00408</td>
<td>0.95</td>
</tr>
<tr>
<td>20</td>
<td>3151.72</td>
<td>12341.9</td>
<td>0.0064</td>
<td>-0.00289</td>
<td>2.31</td>
</tr>
<tr>
<td>40</td>
<td>3177.68</td>
<td>12362.4</td>
<td>0.0063</td>
<td>-0.00309</td>
<td>2.98</td>
</tr>
</tbody>
</table>

### 6.4. Delamination

#### 6.4.1. Cohesive Zone Model (CZM)

The cohesive Zone Model (CZM) is a widely used approach to predict delamination and failure of adhesive materials. In a CZM approach, the failure response and crack propagation is simulated using a traction-displacement law. Figure 6-13 gives an example using bilinear cohesive law shape. This law relates the traction stress (τ) to the displacement (δ). This law consists of three degradation processes; damage initiation, softening and lastly failure (degradation propagation). Damage initiation occurs when the traction attains the material strength (τ₀). The phase in which the stiffness is gradually reduced is called softening phase. After meeting a final displacement, the degradation is complete and propagated to the neighbouring regions.

![Bilinear cohesive law shape](image)

The bilinear shape is often used, but has also been modified. For example, for DCB and ENF tests (de Moura et al. 2008, reviewed by Babea and da Silva 2008) adopted a trapezoidal law for the cohesive damage model to account for the ductile behaviour of the adhesive. However, it may be more ideal to use bilinear curves for dynamic impact loadings as it is seen that the adhesive is most likely to behave more brittle of high strain rate. In other words, the amount of fracture...
toughness that is measured from static tests would be reduced for the cohesive element to behave so. According to Elder et al. (2009), it seems that the decrement of the fracture toughness should be reduced according to impact velocity as it was found that the adhesive toughness decreases as the impactor velocity increases when using FM300-2. Hence, in this study the bilinear cohesive law is adopted to represent the bondline, which will experiences deformation at high strain rate.

Based on the bilinear law, the displacement at damage initiation in each mode is simply (Davila et al. 2007) as follows:

\[ \delta^0 = \frac{\tau^0}{K} \]  

Equation (6-2)

where \( \tau^0 \) is the traction stress at initiation, and \( K \) is the stiffness in the elastic phase. \( \delta^0 \) denotes the displacement value at initiation.

Similarly, the final displacement values are proportional to their corresponding toughness \( G_c \)

\[ \delta^f = 2 \frac{G_c}{\tau^0} \]  

Equation (6-3)

where \( G_c \) the total area under the traction-displacement law (critical energy release rate) and \( \delta^f \) is the displacement value at failure.

As it is anticipated that the adhesive material will failure under both normal and shear modes at the time of impact with pre-loading, it is ideal to adopt the mixed-mode adhesive behaviour with power law as implemented by Feih et al. (2007) and Herszberg et al. (2007).

To describe the evolution of damage under a combination of normal and shear deformation across the interface, it is useful to introduce an effective displacement, \( \delta_m \), defined as (David et al. 2007):

\[ \delta_m = \sqrt{\langle \delta_t \rangle^2 + \delta_{II}^2} \]  

Equation (6-4)

where \( \langle \cdot \rangle \) is the MacAuley bracket, which sets any negative values to zero. This means with respect to above equation that no failure of cohesive elements occurs under compression
loading. $\delta_i$ and $\delta_{ii}$ ($= \delta_{iii}$) refer to relative displacement in the normal and the shear (in-plane and the transverse shear) directions, respectively.

The power law fracture criterion states that failure under mixed-mode conditions is governed by a power law interaction of the energies required to cause failure in the individual modes. It is given by

$$\left( \frac{G_I}{G_{IC}} \right)^\alpha + \left( \frac{G_{II}}{G_{IIC}} \right)^\alpha + \left( \frac{G_{III}}{G_{IIIIC}} \right)^\alpha = e_d \leq 1$$

Equation (6-5)

In the expression above the quantities $G_I$, $G_{II}$, and $G_{III}$ refer to the fracture toughness in the normal, the in-plane and the transverse shear mode, respectively. $G_C$ denotes the critical fracture energy in each mode. The constant $\alpha$ is chosen to fit the mixed mode fracture test data.

**6.4.2. Numerical Input Parameter for Delamination**

For delamination failure *Cohesive behaviour is adopted. This function is a new feature introduced in Abaqus 6.9 applying the softening degradation technique between interfaces without specifying a physical thickness. The damage degradation occurs in the same manner as *Cohesive element. As the delamination growth is likely to occur under mixed-mode loading. Hence, this option suffices the damage criteria.

Based on observations from sectioning, the delamination may occur in between all plies. For robust delamination damage propagation, the elastic stiffness (or penalty parameter) to define the element constitutive equation needs to be increased to avoid inaccurate representation of the mechanical behaviour of the interface. It has to be ensured that the elastic behaviour prior to delamination onset is properly captured. In essence, however, the value should not exceed a value that may cause numerical errors related to computer precision. The value of the penalty stiffness, $K$, is $1.6 \times 10^6$ N/mm$^3$ based on Equation (6-6) from Turon et al. (2007) is applied.

$$K = \frac{\beta E_3}{t}$$

Equation (6-6)

where $K$ is stiffness, $E_3$ is Young’s modulus of ply, $t$ is thickness of ply, and $\beta$ is parameter much larger than 1 (in this case $\beta = 50$).
Due to a lack of material data for Cycom T300/970 prepreg for fracture toughness, the fracture toughness for Mode I, II, and III for a carbon-epoxy prepreg (T300/913) was adopted instead with an experimentally evaluated power law parameter of $\alpha_{\text{comp}} = 1.21$ (Pinho 2005) as seen in Figure 6-14 below (see Table 6-7 as well).

Figure 6-14: Total fracture toughness, as a function of mode ratio (Pinho 2005)

Table 6-6 below compares the unidirectional mechanical properties of T300/970 and T300/913. It is seen that the properties are similar, suggesting that T300/913 can be used instead of T300/970.

<table>
<thead>
<tr>
<th>Material property</th>
<th>T300/970 (manufacturer)</th>
<th>T300/913</th>
</tr>
</thead>
<tbody>
<tr>
<td>$E_1$ (GPa)</td>
<td>120</td>
<td>132</td>
</tr>
<tr>
<td>$E_2$ (GPa)</td>
<td>8</td>
<td>8.8</td>
</tr>
<tr>
<td>$E_3$ (GPa)</td>
<td>8</td>
<td>8.8</td>
</tr>
<tr>
<td>$G_{12}$ (GPa)</td>
<td>5</td>
<td>4.6</td>
</tr>
</tbody>
</table>

The values of strengths of both normal and shear loadings are determined by matching the damage area based on LWHD17. Three different maximum strengths were compared, with the same values of the strength for normal and shear directions. As found in C-scanning, the delaminated area for LWHD 17 is around 198 mm$^2$ (fibre splitting is ignored).

It is obvious that an increase in maximum allowable strength reduces the delaminated area (see Figure 6-15). For a given value of 70 MPa, the numerical result is non-conservative, as the
predicted damage area is smaller than the experimental one. On the other hand, with 45 MPa, the result is over-predicted, showing more than 20 % error. Therefore, the value of 60 MPa is deemed to be appropriate since the error is not only less than 8 %, but also the result is conservative. This value was also used in Pinho (2005), and is used for the remainders of numerical analyses for the laminate as well as the scarf joints to represent delaminated failure. The FE input parameters for modelling delamination are tabularised in Table 6-7.

Figure 6-15: Delaminated area with respect to maximum strength in numerical model

Table 6-7: Cycom 970/T300 numerical input parameters for *Cohesive Behaviour for delamination

<table>
<thead>
<tr>
<th>Material Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$K_I$ [N/mm$^2$]</td>
<td>1600000</td>
</tr>
<tr>
<td>$K_{II}$ [N/mm$^2$]</td>
<td>1600000</td>
</tr>
<tr>
<td>$K_{III}$ [N/mm$^2$]</td>
<td>1600000</td>
</tr>
<tr>
<td>$G_I$ [N/mm]</td>
<td>0.258</td>
</tr>
<tr>
<td>$G_{II}$ [N/mm]</td>
<td>1.08</td>
</tr>
<tr>
<td>$G_{III}$ [N/mm]</td>
<td>1.08</td>
</tr>
<tr>
<td>$\sigma_{ult,1}$ [MPa]</td>
<td>60</td>
</tr>
<tr>
<td>$\tau_{ult,2}$ [MPa]</td>
<td>60</td>
</tr>
<tr>
<td>$\tau_{ult,3}$ [MPa]</td>
<td>60</td>
</tr>
<tr>
<td>$\alpha_{comp}$</td>
<td>1.21</td>
</tr>
</tbody>
</table>

* Subscript I, II, and III indicate peeling (or tensile opening), sliding (or in-plane) shear, and tearing (or anti plane) shear modes, respectively. Symbols of $\sigma$, $\tau$ denote normal and shear strength, respectively.

In addition, the force-time histories for different test cases were compared, including no delamination (i.e., elastic response), inclusion of delamination in only one interface between 4$^{th}$
and 5th layer (0 and 45 degree which was seen to be more severely damaged from sectioning), and introduction of delamination between all interfaces. The differences are seen in Figure 6-16. Most importantly, when comparing elastic and damage in all interfaces, it is clearly seen that initial stiffness is very similar, indicating that the values for stiffness (K_I, K_{II}, K_{III}) for *Cohesive Behavior are adequate to be adopted. Delamination in one interface is insignificant compared to elastic model. If the model has delamination introduced in all interfaces, the damage response significantly increases the impact duration while reducing peak forces. This also creates a more noisy impact response, which was experimentally validated.

![Graph showing force-time history for LWHD17 at different damage set-up]

**6.5 Scarf Joint Studies**

**6.5.1 Scarf Joint FE Modelling**

For scarf joints, 3D elements needed to be used to account for the adhesive behaviour through-the-thickness so that adhesive failure can be captured accurately. It is also vital to ensure sufficient mesh density for the cohesive elements in order to avoid any convergence difficulties and to capture the failure regions without any extreme stress discontinuity.

As it is stated in Abaqus Documentation (2009), the normal direction (black arrow) of the cohesive element should be pointed along its thickness direction (Mode I) as seen in Figure 6-17 to account for the normal stress through-the-thickness.

For the convenience of modelling scarf joints, “Tie constraint” option in Abaqus was adopted instead of the interface between the adherend and the adhesive being modelled by sharing
It is necessary to use Tie constraint option because of the generally finer mesh in cohesive layer. This is shown in Figure 6-17.

To avoid instability during complete cohesive region failure, maximum degradation and viscosity options were adopted in the control card, as exemplified below. The viscosity value helps with convergence of simulations.

*Section Controls, Name=control, element deletion= yes, viscosity = 1e-6

The interfaces between the adhesive and the adherend are assigned to the contact algorithm (General Contact) in order to avoid any penetration while deformed.

6.5.2. Scarf Joint Solid Mesh Study (3D)
The mesh sensitivity of the adhesive region was studied by varying the mesh density along the bondline. It is important for the numerical model to capture the failure behaviour and area accurately. The impacted region and the clamped areas are meshed finely compared to the other regions in which high stress gradients are not experienced, similar to the composite laminate model. Comparisons were undertaken with regards to the following parameters: impact force, failure behaviour as well as computational time. As for the impact event, the test case was simulated with an impactor velocity of 9 m/s and a pre-strain of 1000 με so that failure in the adhesive region was predicted.

The mesh density was changed by varying the number of elements for the adhesive layer, ranging from 800 to 8000 elements. A single layer of cohesive elements through-the-thickness was adopted. It is also stated that more accurate local results are typically obtained with the cohesive zone more refined than the elements of the surrounding components (in this case,
Mismatched nodes along the bondline between adhesive and adherend were tied together.

As seen in Table 6-8, an increase in elements in the adhesive region also increases the computational time moderately. The impact force converges for 3200 elements or more in terms of peak impact force, although the difference in each test case is less than 1 %. This increase in computational time is acceptable, thereby highlighting the usefulness of the localised mesh refinement and tie constraints in Abaqus.

<table>
<thead>
<tr>
<th>No. of Element in Bondline</th>
<th>800</th>
<th>1600</th>
<th>3200</th>
<th>4800</th>
<th>8000</th>
</tr>
</thead>
<tbody>
<tr>
<td>Peak Impact Force (N)</td>
<td>6825.27</td>
<td>6746.04</td>
<td>6734.33</td>
<td>6730.44</td>
<td>6733.08</td>
</tr>
<tr>
<td>Computational Time (s)</td>
<td>1</td>
<td>1.07</td>
<td>1.08</td>
<td>1.16</td>
<td>1.27</td>
</tr>
</tbody>
</table>

The scalar stiffness degradation contours at integration points (SDEG) in Figure 6-18 clearly show that the damage zone using 800 and 1600 elements is not resolved sufficiently. As a rule of thumb, at least three elements should capture the damage degradation zone behaviour values of SDEG = 1 (completely damaged, red) to SDEG = 0 (no damage, blue). Test cases with more than 3200 elements captured very smooth damage contours with similar damage area and shape. Based on these results, 4800 elements were chosen for the scarf joint analyses.

6.5.3. Adhesive Studies

Some important parameters in using cohesive elements were studied. As mentioned earlier, by adopting the traction-separation law, cohesive elements capture the complete failure event from elastic response to damage initiation through damage evolution and complete failure with removal of failed elements.
6.5.3.1. Elastic Stress Distribution

It is important to check whether the cohesive element can accurately predict the shear stress distribution along the bondline under tensile static loading. As seen from the sectioned profile, most of the adhesive and interfacial damage occurred around the location of the 0° plies. Wang and Gunnion (2008) stated that the stress distribution in bondline varies with respect to ply orientation through-the-thickness and that, if the loading is applied along the 0° plies, high stress concentrations should be seen at the intersection of 0° plies and the adhesive region. As it is seen in Figure 6-19, the plies at the intersection experience high stress, indicating that this numerical model is able to capture the shear stress distribution correctly.

Figure 6-19: Shear stress distribution; (a) side views with adhesive, 0° plies, (b) adhesive region
* Red circles indicate intersection between 0° plies and the bondline.

6.5.3.2. Maximum Strength Evaluation (Tensile Test)

The static tensile test with scarf joints was simulated numerically. This numerical validation aims at determining an adequate adhesive failure strength to be adopted for the scarf joint, as the static analysis under displacement control is insensitive to parameters for damage evolution and failure. Good agreement with experimental test results was achieved for yield strengths of 69.2 ± 3.81 as seen in Figure 6-20. This higher numerical strength compared to Table 3-6 is attributed to the fact that the analytical equations did not consider the influence of ply orientation and stress concentrations around 0° ply location under static loading and the strength of the adhesive is therefore higher than expected.
6.5.3.3. Damage Initiation

Damage initiation refers to the beginning of degradation of a material point. The process of degradation begins when the stresses and/or strains satisfy a specified damage initiation criterion. Currently, Abaqus offers strain and stress criteria in the form of maximum or quadratic interaction functions. As for output, a value of $S_{DEG} > 0$ indicates that the initiation criterion has been met, resulting in degradation of the stiffness of a cohesive element. Appropriate strength values for the adhesive layer were based on the predictions of the static tensile tests in the previous section, and are assumed to be valid for the dynamic analysis.

For FE input, the following inputs are examples for *Damage Initiation;*

*Damage Initiation, criterion = maxe $\rightarrow$ based on strain criterion

0.0293, 0.044, 0.044

*Damage Initiation, criterion = maxs $\rightarrow$ based on stress criterion

69.2, 40.0, 40.0

It is found that the peak impact force is mostly independent of specific stress and strain criteria (see Table 6-9). Using a quadratic interaction function (QUADE, QUADS) is more conservative when compared to the maximum interaction function, as the failed area is larger as depicted in Figure 6-21.

<table>
<thead>
<tr>
<th>Damage Initiation</th>
<th>MAXE</th>
<th>MAXS</th>
<th>QUADE</th>
<th>QUADS</th>
</tr>
</thead>
<tbody>
<tr>
<td>Peak Impact Force (N)</td>
<td>6781.2</td>
<td>6780.7</td>
<td>6730.0</td>
<td>6730.4</td>
</tr>
</tbody>
</table>
As there is no significant difference in damage initiation, it was decided to use quadratic stress interaction, which has previously been used for scarf joint impact analysis (Herszberg et al. 2007, Feih et al. 2007; Li et al. 2008).

6.5.3.4. Damage Evolution

The damage evolution law describes the rate at which the material stiffness is degraded once the corresponding initiation criterion is reached. It is important to choose the most appropriate power law parameter. Abaqus offers “Power Law” and “B-K” based on mixed mode behaviour. For example, for the “Power Law” criterion, the interaction graph can be drawn as seen Figure 6-22. With various power factors, $\alpha_{adh}$, a wide range of material responses can be modelled. The lines in the graph represent the boundary between failure or no failure during the damage progression stage. Any points falling outside the curve indicate a failed material state. It can be said that results obtained with lower parameters of $\alpha_{adh}$ are more conservative. Most of the time, it is recommended to use a power parameter (or B-K parameter) in between 1 and 2 (LSTC 2007).
A general comparison was made using the two different mixed mode laws and also using different power parameters $\alpha_{adh}$ with the power law. As expected, results show that the different power law parameter varies the response of cohesive element degradation. When varying $\alpha_{adh} = 1$ to $\alpha_{adh} = 2$, the results became less conservative, showing that the failed areas using $\alpha_{adh} = 2$ were smaller (see Figure 6-23) and thus had a higher impact force (see Table 6-10). In comparison of the B-K and Power law, it was seen that B-K results in more conservative predictions with lower impact force and larger damage areas. In this study, the power law with a power parameter of 1 is adopted as this value is considered conservative. The power parameter, $\alpha_{adh}$, will be validated by comparing the results with experimental results.

<table>
<thead>
<tr>
<th>Damage Evolution</th>
<th>Power Law =1</th>
<th>Power Law =2</th>
<th>BK = 2</th>
</tr>
</thead>
<tbody>
<tr>
<td>Peak Impact Force (N)</td>
<td>6730.44</td>
<td>6822.07</td>
<td>6770.34</td>
</tr>
</tbody>
</table>

**Figure 6-23:** SDEG contours for different laws and parameter

### 6.5.3.5. Element deletion

Elements can either be set to remain or to be deleted in the structure upon failure, which affects the damage propagation to the remaining elements. In fact, with element deletion = no (ED=No), the failed element can still carry a small stress, depending on the set level of maximum...
degradation. Abaqus ensures that elements will remain active in the simulation with a residual stiffness of at least 1% of the original stiffness, when setting Maximum Degradation = 0.99 (default). The element deletion study compares the damage area and impact force using the case of NTPZ1 (19 J, 0 με pre-strain).

Figure 6-24 shows damage propagation along the bondline when DE=no and DE=yes are set. In the initial stage at t= 0.00066 s, the failed areas and shapes from both sets of ED=Yes and ED=No were the same, but after a certain point, the damage evolution wave for a set of ED=Yes was propagated faster in both longitudinal and transverse directions, resulting in a larger damage area. This may be attributed to the remaining ability to carry load, which is seen to be significant when comparing damage area predictions against experiments.

![Figure 6-24: Damage progression in cohesive elements, (a) ED = No & MD = 0.99, (b) ED = Yes](image)

For the case of impact energy of 19 J, the force-time histories were compared (see Figure 6-25). With a setting of ED=No and of MD=0.99, the curve pattern is significantly different with a second peak (region ‘A’) being higher than the first peak, which was not seen in the test. In contrast, the second peak was smaller when the completely failed elements were allowed to be removed. This may be attributed to the fact that allowing 1% of stiffness in the failed element
can still introduce significant bending stiffness in the panel during impact. However, the results converge with further reduction of the remaining stiffness (0.001 %) as seen in Figure 6-25. It is also confirmed that the damage area and shape are very similar. Nevertheless, a setting of ED=Yes (9011 s) is chosen for the remaining numerical analyses to ensure conservative damage results.

![Figure 6-25: Force-time history for NTPZ1 (19 J, 0 με)](image)

6.5.3.6. Fracture Toughness for FE input

According to Jacob et al. (2004), Babea and da Silva (2008) and Elder et al. (2009), the fracture energy/toughness may vary by different loading conditions. It would therefore be desirable to study the variation of the fracture toughness in dynamic loading. For scarf joints, Feih et al. (2007) stated that the fracture toughness in normal direction is less significant due to failure occurring mainly in shear (Hart-Smith 1974). For this reason, the fracture toughness in Mode II ($G_{IC}$) and III ($G_{IIC}$) was mainly studied. It is important to note that it is assumed that Mode II and Mode III have the same fracture energy values, i.e. $G_{IIC} = G_{IIC}$.

A validation of most adequate fracture toughness was determined based on test results (STPT and NTPZ6) of failed specimens during impact. Both specimens failed predominantly along the bondline, therefore delamination failure in the laminates was ignored. It has to be stated that ignoring the possible interaction effect of delamination may lead to non-conservative results for the adhesive fracture energy, as energy absorption by other failure modes prior or during
adhesive failure is not considered. This will be investigated further in the numerical analysis chapter.

Due to lack of experimental data to determine the power law parameter $\alpha_{\text{comp}}$, the fracture toughness and damage area is evaluated with different parameters of $\alpha_{\text{comp}} = 1$ and $\alpha_{\text{comp}} = 2$, as the values for most materials are expected to be in this range. As expected from Section 6.5.3, using $\alpha_{\text{comp}} = 2$ derives a smaller impact damage area when comparing results at the same $G_{\text{IIc}}$. By varying $G_{\text{IIc}}$ values, it was found that for power law factor $\alpha_{\text{comp}} = 1$, $G_{\text{IIc}} = 8.75$ N/mm gives the controls of boundary between sudden failure and damages. As for $\alpha_{\text{comp}} = 2$, $G_{\text{IIc}} = 6$ N/mm was the control. These parameters were also confirmed to result in failure for conditions of NTPZ6. For this study, a power law of $\alpha_{\text{comp}} = 1$ with $G_{\text{IIc}} = 8.75$ N/mm was chosen as this value is in better agreement with experimental data listed in Table 3-6. For this study, a power law of $\alpha_{\text{comp}} = 1.0$ with $G_{\text{IIc}} = 8.75$N/m was chosen. It should be noted that this value is significantly higher than the static value given in Table 3-6. The starting point is therefore considered an upper boundary value. Further work for a conservative value of $G_{\text{IIc}}$ requires complete characterization of a failure envelope for both pre-strain and energy, including prediction of all damage modes. This is further investigated in Chapter 7.

<table>
<thead>
<tr>
<th>Power Law Parameter</th>
<th>$G_{\text{IIc}}$ (N/mm)</th>
<th>Sudden Failure Or Damage Area</th>
<th>$G_{\text{IIc}}$ (N/mm)</th>
<th>Sudden Failure Or Damage Area</th>
</tr>
</thead>
<tbody>
<tr>
<td>STPT</td>
<td>Failed</td>
<td>NTPZ6</td>
<td>Failed</td>
<td></td>
</tr>
<tr>
<td>1</td>
<td>10</td>
<td>Damaged</td>
<td>8.75</td>
<td>Failed</td>
</tr>
<tr>
<td>8.75</td>
<td>Failed</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>6.5</td>
<td>Damaged</td>
<td>6.0</td>
<td>Failed</td>
</tr>
<tr>
<td>6.0</td>
<td>Failed</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

6.5.4. Conclusion

Based on parametric studies, it was decided to use the analytical shell impactor in place of the full impactor as the analytical impactor requires much less computational time. In addition, the approach for applying the mass distribution equation to determine the tip force from the interface force was validated. The interface forces from the full model and the equation had a good agreement. This gave confidence to correct the experimental results. The contact pair algorithm was chosen to operate with penalty contact formulation; its penalty stiffness value
$k = 1$ was found to be most suitable for computational efficiency and minimum penetration. The comparison of the composite laminate using 2D shell and 3D solid element gave confidence to use a detailed 3D model for the pre-strain impact loading cases as the results from 2D and 3D models were very similar.

For the elastic response of composite laminates, a 2D shell element model was chosen. A 3D element model was created to introduce delaminations in between plies. After validation against experimental tests, the surface-based cohesive behavior was adopted with a power law parameter ($\alpha_{\text{comp}} = 1.21$). The strength of 60 MPa was found to be most appropriate following parametric studies; the stiffness, $K = 1.6 \times 10^6$ N/mm$^3$, is proven to be sufficiently large, while the fracture toughness was kept the same as found in Pinho (2005). These values are also adopted for scarf joint modelling to capture the delamination as failure mode interaction was seen to be important.

For scarf joints, 3D elements are selected to account for the interaction of individual plies with the angled adhesive bondline. The shear stress distribution along the bondline showed stress concentration with respect to the $0^\circ$ ply orientation. Taking into account the stress concentrations, the maximum allowable strengths for cohesive elements to represent adhesive failure are 69.2, 40, 40 MPa when matching the maximum load from static tensile tests. As for the cohesive element deletion condition, it is decided to set Element Deletion (ED) = yes, resulting in better agreement with experimental force-time-history graphs. In regards to fracture toughness in shear directions ($G_{\text{IIc}}$), 8.75 N/mm with power law parameter $\alpha = 1$ is found to be appropriate for the scarf joint bondline. Mode I fracture toughness was set to $G_{\text{IC}} = 1.35$ N/mm, but was found to be insignificant to the failure prediction.
7. Numerical Results Summary

7.1. Laminate Coupon Predictions
The LW impact test matrix was numerically simulated using Abaqus. The effect of the pre-strain effect on peak force, impulse, impact duration, and relative strain was studied. The delamination area was also predicted.

7.1.1. Elastic Response (2 J)
As mentioned earlier, composite laminates were impacted at low energy (elastic response) to validate the boundary conditions and to validate numerical models prior to damage modelling. In order to carry out the validation, 2D shell element models were used to simulate the elastic response as this element type is considered most appropriate for dynamic impact scenarios. It is noted that it was shown in parametric studies that 3D elements are able to capture a similar impact response. 3D elements were adopted for the damaged response, where delaminations needed to be introduced.

7.1.1.1. Force versus Pre-strain
Similar to the experimental results, it is clearly seen in Figure 7-1 that at region ‘A’, a higher pre-strain creates the stiffer gradient. On the other hand, the force drops earlier with higher pre-strain, shortening the interaction between the impactor and the target. In addition, as the pre-strain level increases, the peak force is reached earlier. This is consistent with experimental findings.

Figure 7-1: Force-time history for elastic response
The force was compared as a function of time for LWHD 10 (see Figure 7-2 (a)). In comparison, both experimental and numerical results are in a good agreement, although the numerical results over-predicted the peak force by 10%. In addition, the initial stiffness (force gradient) and the peak forces were well predicted by the numerical result.

Figure 7-2: (a) Force-time history for LWHD 10 (2J, 4000 με); (b) Force versus pre-strain for laminate

Figure 7-2 (b) compares the experimental and numerical peak forces as a function of pre-strain. Error bars represent differences in numerical predictions when experimental impact energies are matched. Overall, the numerical peak forces were well matched with the experimental ones, giving less than 15% error; whereas the peak force at zero pre-strain was significantly higher than the experimental one by 24% after the experimental test conditions were repeated to confirm the results. The results fit in the trendline validating that the pre-strain increases the elastic peak force as found in tests. Moreover, as the pre-strain increases, a better agreement is achieved, which proves that numerical model is capable of capturing pre-straining effects in dynamic impact scenarios.

7.1.1.2. Impact Duration and Deflection

The numerical results follow the trend of the experiments. Pre-strain shortens the impact duration. The overall discrepancy is within 14%. The impact predictions agree better at higher pre-strain level while the largest error (36%) was found at zero pre-strain level as was also seen for the peak forces in Figure 7-3.
Using Equation (4-8), the maximum deflection experienced by the panel during impact was compared. The maximum deflections were matched well as shown in Figure 7-4. It is clearly seen that the pre-strained panels experiences less deflection. The numerical analysis was able to capture a similar trendline with less than 10% error.

7.1.1.3. Strain versus Pre-strain

The strain values were averaged over 8 elements at the strain gauge location, which cover exactly the size of the strain gauge.
LWHD 8 was compared for peak strain as shown in Figure 7-5. The strain pattern is captured accurately, but the numerical prediction for the absolute strain value is significantly higher with 40% difference.

![Figure 7-5: Absolute strain-time history for LWHD 8 (2J, 3000με)](image)

In terms of relative peak strain, numerical predictions resulted in a similar trendline as the experimental results (see Figure 7-6), indicating that the relative peak strains reduced as the pre-strains increased. However, it is clearly seen that unlike the comparison based on far field strain, the error of numerical model compared to tests increased as the pre-strain level increases.

The numerical model currently does not seem capable of predicting accurate near-field strains. This further investigated, may be due to the following:

1) Predicted strains are considered sensitive to the material stiffness, which was initially reduced by 20% to match the experimental bending stiffness (see Table 3-4). A sensitivity study was undertaken and the results for original stiffness are included in Figure 7-5 (dashed lines). The difference is still 28% and this does not explain the discrepancy.

2) In experimental testing, a small misalignment of strain gauges might affect measured strains.

3) Bonding and transferring of strains between strain gauge and composite during dynamic impact event give an influence to the measure strains. The strains were calibrated during static tests only.
Overall, as peak forces, impact duration and deflection are captured accurately for the entire test series, it was decided not to investigate this issue any further for this thesis.

7.1.2. Damage Response (7.5 J and 10 J)
The 3D model was used for the analysis of delamination failure at 7.5 and 10 J impact energies. It is necessary to use 3D elements to allow the same damage parameters to be used for the scarf joint model (only 3D element can represent the scarf angle through-the-thickness).

7.1.2.1. Impact Force and Delamination Damage for 7.5 J
Figure 7-7 (a) shows the force-time history graph for LWHD 16 (7.5 J and 1000 µε), and its corresponding numerical prediction. The initial stiffness is very similar and the times for the peak forces are very similar. All in all, the curves are in a good agreement. The trendline for impact force with respect to pre-strain levels is also very similar (see Figure 7-7 (b)), indicating that the numerical model can capture the pre-straining effect in terms of peak force as only 19 % error was observed.

It can be seen that delamination is initiated early in the impact event and stopped once the last peak force value is recorded.
With the validated input parameters, numerical predictions were run for all 7.5 J and 10 J cases. LWHD 16 was exemplified and compared with test result as seen in Figure 7-8. The damage size and its shape are in a good agreement.

The delaminations in a through-thickness view for both test and numerical analysis are illustrated in Figure 7-9. Both showed a similar number of delaminated interfaces. In terms of the damage profile through-the-thickness, the damage area in lower plies tends to be larger, which is a typical damage profile for composite laminates. Observed discrepancies may be attributed to the missing failure mode of matrix cracking within the plies. As for future work, it would be desirable to introduce matrix cracking in the numerical model. This is currently not possible as Abaqus 6.9 does not include 3D composite ply failure.
Figure 7-9: Sectioning view: (a) LWHD 16 (test), (b) numerical prediction for LWHD16

Figure 7-10 shows a map of delaminations in each interface. As expected, the damage shape is consistently varied according to ply orientations.

Figure 7-10: Damage shapes in each interface for LWHD 16
Figure 7-11 shows a comparison of the delaminated areas for experimental and numerical results. For 7.5 J, they are in a good correlation with less than 7 % error except for the zero pre-strain level (32 % error). The numerical analysis consistently over-predicts as expected based on the conservative strength allowable of 60 MPa. The numerical model is also capable of predicting a similar trendline for damage area against pre-strain, showing that the damage area varies with different pre-strain level.

![Figure 7-11: Damage area versus pre-strain (test versus numerical prediction) for 7.5 J](image)

7.1.2.2. Impact Force and Delamination Damage for 10 J

Figure 7-12 (a) shows an impact force-time history graph for LWSD 10 (10 J, 1000 µε). It is clearly seen that the peak force from numerical model is over-predicted by 10 %. For the overall results for 10 J case, the numerical analysis over-predicts peak forces, by 20 % (see Figure 7-12 (b)). Further numerical investigation should be undertaken to investigate whether the introduction of other failure modes, such as matrix cracking or fibre fracture, may explain the higher discrepancy. The influence of introducing other failure modes on the predicted area of delamination also needs to be investigated.

With respect to delamination development, the delaminations started formed at $t=0.0003$ s and their propagation is stopped at $t=0.00135$ s. Similar to lower impact energy case at the same pre-strain level, the delamination propagation is terminated after the last peak force.
Figure 7-12: (a) Force-time history for LWSD 3 (10J, 1000 µε); (b) Peak force versus pre-strain for 10 J

Figure 7-13 shows a comparison of the delaminated areas for experimental and numerical results. Similar to 7.5 J, the numerical models for 10 J accurately modelled the delaminated area with less than 10 % error except for the zero pre-strain level (50 % error). The numerical analysis consistently over-predicts as expected based on the conservative strength allowable of 60 MPa. The numerical model is also capable of predicting a similar trendline for damage area against pre-strain. The good agreement gives confidence to apply the validated parameters to scarf joint modelling.

*Error bars indicate experimental and numerical variations due to validations in impact energy – standard deviation of ± 0.6 J

By introducing delamination in all interfaces using *Cohesive Behavior, the numerical predictions models were capable of capturing a similar trendline as compared to the tested results. Such a good agreement is promising in that the numerical model with delamination embedded in interfaces of plies can be used confidently for the analysis of the scarf joint.
7.2. Scarf Joint Predictions
In this section the scarf joint impact tests and analyses are compared.

7.2.1. Elastic Response (4.5 J)
For the 4.5 J impact energy case, no damage was detected in both adhesive and adherend (and no interfacial failure) based on C-scanning and sectioning. No cohesive behavior is included in between plies, i.e. a purely elastic response is modelled. The results are discussed in the following sub-sections.

7.2.1.1. Force – Time History and Impact Peak Force
Figure 7-14 shows the comparison of a force-time history pattern. When comparing the impact force response for the numerically predicted scarf joint and laminate coupon for elastic response, their responses are very similar although the peak forces are lower by 10 % and impact duration is longer by 5 % for the scarf joint due to the presence of the adhesive region that deformed more plastically.

In comparison of the experimental (FPF6, 4.5 J and 5000 µε) and numerical scarf joint results, although the second and third peak forces are over-predicted by numerical prediction, the curve pattern is very similar. Especially, the force-time stiffness up to the first peak force is in good agreement. However, due to zero-dissipated energy in the numerical prediction (unlike a real test, although no damage is experienced), the remaining stiffness for the rebounding stage for the numerical analysis is stiffer than that for the experiment, introducing higher second and 3rd peak forces in the numerical model.

![Figure 7-14: Force-time history for FPF6 (4.5 J, 5000 µε)](image-url)
The overall comparison for peak forces at different pre-strain levels is given in Figure 7-15. The highest discrepancy, about 30%, was found at zero pre-strain, which is consistent with the findings for the laminate coupons. Otherwise, the overall error is within 15%. The numerical predictions are capable of capturing the impact response at higher pre-strain levels.

![Figure 7-15: Force versus pre-strain for 4 J for scarf joint](image)

**7.2.1.2. Impact Duration and Deflection**

The impact durations are compared based on force-time histories as the strains were not measured. As expected from laminate and scarf joint testing results, the numerical results resulted in a shorter impact duration than experimental tests with an overall discrepancy of 38%). The impact duration is again shortened with an increase in pre-strain as seen in Figure 7-16. Interestingly, Figure 7-16 also illustrates that numerical prediction at higher pre-strain result in better accuracy.

![Figure 7-16: Duration versus pre-strain for 4 J for scarf joint](image)
The deflection experienced during impact testing was calculated and compared with that from the numerical model as seen in Figure 7-17, showing that they are in a good agreement (5% error). Numerical results represent a similar trendline and the error was negligible especially at higher pre-strain level.

In conclusion, the numerical analysis is capable of accurately capturing the elastic response of scarf joints under various pre-strain levels. This implies that the introduced adhesive region which is represented by inserting cohesive elements with *Tied function accurately simulates the impact response. The comparison of elastic laminate and scarf joint response (Figure 7-14) also confirms this. Hence, such a good correlation gives confidence to use the evaluated scarf joints for damage response cases (8 and 19 J).

7.2.2. Damage Response (8 J)

Accurate damage prediction requires interaction of delamination and adhesive failure, which makes this analysis very challenging. As a typical analysis takes in the order of 2 days to run (with 2 x quad core AMD operation 2356 2.3 GHz and 16 multiple cpus), only selected test cases were investigated.

For 8 J, EPZ7 (4000 µε) was investigated. These results are compared with the results from the numerical analysis with respect to damage area and impact force. In addition, the results were compared with and without delamination while failure in the adhesive regions is introduced for both cases. It is anticipated to observe the difference in adhesive damage by introducing the delaminations in between plies.
7.2.2.1. Peak Force

The numerical model with introduction of delamination and adhesive failure generated a lower impact force response especially in the peak region, compared to the model without the delamination but with adhesive failure (see Figure 7-18). In addition, the former model has a longer impact duration. These results could be anticipated as the dissipation energy from the initiation and propagation of delamination causes a lower flexural bending stiffness, resulting in lower impact force and longer impact duration.

When comparing the numerical result (with delamination and adhesive failure) and experimental results, the numerical model derived some additional peak forces, which are not seen in the test as depicted in Figure 7-18. This is most likely either due to: (1) the under-prediction of failure area or (2) neglecting several composite failure modes as also reported for the composite laminate under impact. However, the numerical prediction was able to capture the accurate impact response as the initial stiffness gradient was similar and the first peak force is well matched. Overall, the error is less than 10 %.

![Figure 7-18: Force time history for EPZ7 (8 J, 4000 µε)](image)

As for the failure development of the scarf joint (see Figure 7-19), delamination damage is initiated at $t = 0.00024$ s and is stopped at $t = 0.0012$ s. The scarf joint experiences longer duration of delamination development, $t = 0.00096$ s, compared to laminate result for an impact energy of 7.5 J, $t = 0.00051$ s, while the laminate coupon experienced an earlier onset of delamination. This may be attributed to material degradation from the adhesive region. In terms of the impact force-time history, both scarf joint and laminate coupon exhibit a similar impact response, although the scarf joint experienced slightly lower peak force and longer...
impact duration, which was also found in the experimental comparison. This is due to the additional development of adhesive damage, although no complete adhesive failure occurs (SDEG < 1).

![Graph showing force-time history comparison for laminate and scarf joint](image)

**Figure 7-19:** Force-time history comparison for laminate and scarf joint

### 7.2.2.2. Damage Area

From C-scanning, the damage area for EPZ7 had a size of 431 mm². Figure 7-20 below illustrates the damage areas in individual parts and also altogether. The cohesive elements were not failed when saying SDEG = 1 represents the complete failure. On the other hand, the delaminations occurred in almost every ply interface. The bottom plies are inclined to more severely damage; it may be due to high bending stress during impact. However, the numerical model, was unable to capture the delamination that propagates toward the bondline along the interface between 45° and 0° ply as indicated in red arrow.

It is apparent that the damage occurred along the bondline is larger for the numerical model with adhesive failure only (see Figure 7-20 (a)), compared to for that with delamination and adhesive failures (see Figure 7-20 (b)). It is also seen that due to delamination which interacted with the bondline as seen in Figure 7-20 (d), the damage distribution along the bondline is different to the numerical model without delamination. It may be postulated that delamination delays the catastrophic failure of scarf joints.
In terms of total damage area, the damage areas were evaluated at different SDEG parameter values for the cohesive element, ranging from 0.5 to 1. The damage areas then vary as seen in Figure 7-21; at SDEG = 0.5, the closest damage area is found. Overall the damage areas in regard to the SDEG parameter are unconservative, i.e. too small. This is attributed to the value of GIIC=8.75N/mm. This value is significantly higher than the static fracture toughness of the adhesive based on its stress-strain curve and adhesive thickness of 0.38mm. This value was derived assuming that no composite failure occurs in the adherend. It has now been shown that this is not the case. The result is therefore not unexpected. The value of $G_{IIc}$ needs to be refitted with delamination damage present. This was unfortunately out-of-scope for the current project due to time constraint.
7.2.3. Damage Response (19 J)

7.2.3.1. Peak Force

For 19 J, NTPZ3 (2000 µs) was investigated. Figure 7-22 illustrates the impact force-time history. By setting elastic and damage parameters for the adherend while the adhesive is allowed to fail, the initial peak force is lower and the impact duration is longer for damage case. However, the difference is not significant. In addition, compared with the experimental force pattern, the numerical model excessively over-predicted by 65 %. Further studies are required to investigate possible improvements. Other failure modes, including fibre fracture and matrix cracking, were excluded at the current numerical models.

With respect to damage development, the delamination is induced first in the adherend, followed by the adhesive failure being initiated upon reaching the first peak force. It is noticeable that adhesive failure takes longer to form the delamination, although the delaminated area is bigger. The adhesive failure is confined in a small adhesive region. This is due to the differences in fracture energies. The adhesive is more ductile than the adherend. It is also important to note that for the numerical result with adhesive failure only, the onset of adhesive failure is actually the same as for that with adhesive and delamination failures.

![Figure 7-22: Force-time history for NTPZ3](image-url)
7.2.3.2. **Damage Area**

The compared damage area from specimen NTPZ3 excluded the fibre splitting as seen in Figure 7-23. As a result, the damage area for NTPZ3 is 625 mm².

![Image of NTPZ 3 c-scanned maps scanned from bottom (left) and top (right) surface](image)

Figure 7-23: NTPZ 3 c-scanned maps scanned from bottom (left) and top (right) surface

Figure 7-24 illustrates the damage areas in individual parts and also together. The cohesive elements did not fail when setting SDEG = 1, which represents complete failure. On the other hand, delaminations occurred in almost every plie interfaces. The bottom plies are prone to more severe damage; this may be due to high bending stress during impact. However, this numerical model was again unable to capture the delamination propagating toward the bondline along the interface between the 45° and 0° plie as indicated by the red arrow.

Figure 7-24 (a) shows the result when the laminate elastically deforms (no delaminations), but the adhesive regions fails. By comparison, it is obvious that the size of damage area in the bondline is bigger without introducing the delamination than that of damage area with the delamination. Similar to finding from EPZ 7, this indicates that introducing delamination can enhance the joint strength and delay catastrophic failure, which is a very important finding. Therefore, in essence, it is important to model both damage types for accurate predictions of failure in composite scarf joints.
Figure 7-24: Damage areas for NTPZ3 at different zoom-in views; (a) elastic response damage area (no delamination), (b) cohesive failure with delaminated area (coloured in gray), (c) cohesive failure for adhesive region, (d) side view of bondline and delaminated area in different plies (bottommost ply represents 2nd ply, 90°)

* Note that the complete failed element represented by the holes are set at SDEG =1

In terms of total damage area, the damage areas were again evaluated for different SDEG parameter values for the cohesive element, ranging from 0.5 to 1. The damage areas vary when using different values as seen Figure 7-25. The damage area and its shape are dependent on the SDEG parameter. Amongst them, at SDEG = 0.9, the damage area is matched best with the experimental damage area. However, major damage failure modes (fibre splitting) were excluded, which would most likely result in further energy uptake by the composite adherend reduction in peak force and therefore reduction of the adhesive failure areas.

Figure 7-25: Damage areas and shapes at different SDEG parameters for NTPZ 3
7.2.4 Conclusions

Various numerical predictions were compared to experimental results. These include composite laminate coupons using 2D shell element for elastic response and 3D ply-by-ply solid elements with delamination embedded for damage response. Likewise scarf joint models are validated for elastic response (introducing adhesive damage but not delamination) and for damage response (introducing both adhesive and delamination). The main comparisons are based on impact force-time history, strain-time history, impact duration, deflection, and damage area.

With respect to impact force, firstly, it is generally seen that the numerical models accurately captured the forces at lower impact energy and interestingly at higher pre-strain levels. For the elastic response, the overall discrepancy is around 15 % for both laminates and scarf joints. For both, the highest discrepancy was found at zero pre-strain level. Nevertheless, similar to findings from experimental testing, the numerical prediction captured the pre-straining effect to the peak force accurately – pre-straining increases the peak force. For the damage response, it is clearly seen that as the impact energy increases, the discrepancy is increased. For an impact energy of 7.5 J case for laminate composite, the overall error for the peak force is around 13 % while for 10 J case, the overall error increased to 20 % but at worst 30 % error. This is attributed to the fact that only delamination failure is modelled, but other composite failure modes become more dominant at higher impact energy levels. For the scarf joint analyses, this discrepancy becomes more evident as the impact energy increases from 8 J to 19 J. Delamination is generally considered to initiate from matrix cracking (Sierkowski 1995). In addition, fibre fracture results in significantly greater energy dissipation (Cantwell and Morton 1991). With the introduction of other failure modes, it is postulated that the differences will be reduced.

For the laminate composites, damage shape and size for delamination were captured accurately at different pre-strain levels and impact energy – on average 7 % and 10 % differences only are observed for 7.5 J and 10 J. The highest discrepancy was again found at zero pre-strain, having 32 and 50 % error for 7.5 J and 10 J. For scarf joints, the numerical models were able to capture the delamination effect to the scarf joint strength. In other words, as the majority of damage occurred in adherend region based on experimental results, the similar trends were found in numerical analyses at different impact energy. However, the numerical method was unable to capture the delamination propagation
toward the bondline at bottom 45° ply, which triggers the adhesive failure. It is interesting to note that as the impact energy increases, the onset of the delamination initiation is increased. In addition, a higher impact energy induced longer delamination durations, and resulted in larger delaminated area. It was seen that delamination failure is initiated earlier than adhesive failure initiation. Adhesive failure occurs over a much longer time period than delamination, which is attributed to the fracture energies. The adhesive is more ductile than the adherend.

In terms of impact duration, although the numeral results tend to simulate shorter impact duration, a good agreement (14 % error) was observed, compared to the experimental results, especially at 2 J impact energy for the laminated composites. However, the discrepancy increased for 4.5 J impact energy for scarf joint as the compared impact durations were based on the force not strain. Nevertheless, in all cases, it was found that the impact duration is shorter with higher pre-strain. The deflection of the coupons during impact was also compared. The numerical models for all cases very accurately captured the deflection; the discrepancy was less than 10 % overall.

When comparing the numerical results of 7.5 J of laminate composite and of 8 J scarf joints, a very similar impact response was seen based on the force-time history graph. Similar to experimental comparison, the scarf joints experienced longer impact duration and a lower peak force. With respect to the damage development, while the laminate composite had an earlier initiation of the delamination, the scarf joint had a longer damage degradation due to the adhesive bondline failure.
8. Conclusion

8.1. Summary of Findings

Four main research questions were postulated and answered in the course of the presented research work. In this section, the key findings relating to these research questions are summarised and discussed.

1) Can composite coupons be used to characterise composite failure modes which occur during scarf joint impact?

The experimental results show that the impact response for laminate coupons was very similar to the response obtained for the scarf joints. The adhesive bondline therefore does not have a significant influence on the elastic response, which is attributed to its minimal thickness and a good interface bond between adhesive and adherends. Important similarities are observed for the damage response for both damage area and force-time history patterns. The damage area due to delamination is similar, especially for moderate pre-strain levels without significant adhesive bondline damage. For higher impact energy levels, adhesive bondline damage and delamination start to interact, leading to larger damage areas for the scarf joint under impact. However, to reduce manufacturing related costs, it may be recommended to use composite coupons to investigate the extent of delamination damage.

2) Do bondline failure and composite failure modes interact in scarf joints under impact?

Following sectioning of the damaged scarf joint and identification of the damage area by C-scanning, damage profiles through-the-thickness showed that damage was introduced in both the adherends and adhesive bondline region. For scarf joints under impact, two energy absorbing damage mechanisms are therefore introduced. Most of damage occurred in the adherend region (including delamination, fibre fracture and matrix cracking) rather than in the adhesive (i.e. adhesive failure, adhesive cracking). This damage pattern is independent of the pre-strain level. Most importantly, the sectioned scarf joints exhibited cohesive failure along the bondline, which interacted with the delamination propagation along the lower 45° and 0° ply interface. Comparisons with the laminate coupons and numerical model predictions indicate that damage development occurred first by delamination and later propagated into the adhesive bondline.
3) Is the development of composite damage beneficial or detrimental to catastrophic failure of the joint?

Numerical prediction showed that the failure area within the adhesive region was smaller when delamination was included in the model as compared to numerical predictions without delamination failure between the plies. Delamination failure was found to be present in between most ply interfaces, with the largest damage area occurring in the lower 0°/45° ply interface. This research proves that propagation of ply delamination absorbs a significant amount of impact energy during the impact of scarf joints. This secondary failure mechanism of delamination (as well as other composite failure modes not considered in this work) is found to delay the catastrophic failure of the joint and therefore beneficial in preventing the event of catastrophic failure.

4) What is the effect of pre-strain on damage development during impact for preloaded composite coupons and scarf joints?

The measured and predicted damage area (delamination and bondline failure) generally increased for high pre-strain levels for both laminate coupons and scarf joints. It is important to note that above a high impact energy level (in this case it is above 16 J) for scarf joints, catastrophic joint failure was induced by a high pre-strain of 4000 µε. This indicates that the pre-strain can contribute significantly to sudden failure of the joint. The damage shapes in laminate coupons were not dependent on the pre-strain values. However, the delamination shape was changed by the pre-strain for the scarf joints. With higher pre-strain levels, the damage propagated along the width and along the bondline (tension side), resulting in a semi-circular shape. This is due to the interaction of the adherend failure and the bondline failure. It can be concluded that the pre-straining effect can be seen in both the laminate coupons and scarf joints, and it may lead to catastrophic failure for scarf joints.

8.2. Future Work

Throughout the previous chapters, suggestions for improvement of the numerical predictions were undertaken. These suggestions are based on observed discrepancies when numerically validating experimental results. Two main aspects for follow-up are suggested as follows:
1. Obvious discrepancies were identified at the highest impact energy levels for both laminate and scarf joints based on the force-time history and peak forces. Over-prediction by the numerical model is most likely due to neglecting of important composite failure modes in the numerical model, such as fibre fracture and matrix cracking. Both mechanisms can absorb significant amounts of energy during impact. As Abaqus currently does not support any in-plane damage failure in 3D solid elements, a methodology needs to be developed to represent all composite failure observed in the experimental test series.

2. Failure predictions for cohesive bondline failure are very sensitive to the value of the fracture toughness for FM 300 - especially in shear modes ($G_{IIC}$ and $G_{IIIc}$). More accurate mechanical properties need to be defined under dynamic loading condition rather than static loading. The value for the critical fracture toughness may be calibrated based on numerical simulations including both adhesive and composite failure.

Upon achieving of the above suggestions, the numerical model is anticipated to accurately simulate the damage development and the impact response of the scarf joint under the investigated ranges of impact energy and pre-strain conditions. Following this, it is anticipated that a failure envelope of scarf joints with respect to impact energy and pre-strain level can be numerically developed. Further experimental testing should be conducted to validate the numerical results for higher impact energy levels (> 20J). This is currently not possible due to height (velocity) limitations of the impact test rig.
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References


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Appendix 1

A Roll of Prepreg

Fibre Direction

200 mm 200 mm 200 mm 200 mm

600 mm

200 mm 200 mm 200 mm

600 mm

200 mm

200 mm

45°

0.2 mm

Side view

A Roll of Prepreg

○ 1

○ 2
Connected by sticky tape
Appendix 2

Apparatus

- M-Prep Conditioner A
- M-Prep Neutralizer 5A
- CSP-1 Cotton-tipped Applicators
- M-Bond 200 Adhesive
- M-Bond 200 Catalyst

Procedure

1. Marking the positions (alignment marks) where the strain gages need installing on the specimens with a ballpoint pen.
2. Cleaning the specimens by applying M-Prep Conditioner A and scrubbing with cotton-tipped applicators, followed by slowly wiping through with a gauze sponge to remove all residue and conditioner. Repeating these steps to apply a liberal amount of M-Prep Neutralizer 5A with care mentioned.
3. Positioning the gauge, whose gauging surface is stuck to a sticky tape, at the marked layout line on the specimen.
4. After tucking the loose end of the tape under and pressing to the specimen surface so that the gage and terminal lie flat, with the bonding surface exposed, applying M-bond 200 catalyst to the bonding surface of the gauge and terminal. Then one or two drops of M-bond 200 are to be applied at the fold formed by the junction of the taped and specimens surface.
5. After re-positioning the gauges on the marked lay-out line, applying firming thumb area pressure to the gauge and terminal are for at least 1 min, followed by removing the tape slowly and carefully.
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Appendix 3

**Boarder**

- 5B38-02 Amplifier
  - Full bridge input
  - Provide an insolated bridge excitation of +10V and a protected, isolated precision output of -5V to +5V.
  - Sensitivity: 3mV/V
  - Bridge range: 300Ω to 10KΩ
- Wide-bandwidth single-channel signal conditioning module

**Wire of Strain gage**

![Diagram of Wire of Strain gage]

**Strain Data Acquisition System**

It was needed to find the right combination of input voltage (+ or -) to the channels on the board to avoid the odd values. On the board, it has three plugs (+,-,-) but it was not really detailed which wires of strain gauges go to which plugs.
Six possible combinations were attempted to find the adequate connection. It was found that the shown combinations gave appropriate output voltages. Eventually, 1\textsuperscript{st} and 2\textsuperscript{nd} white wires were independent on the inner or outer minus plugs as they did not make much difference.
Appendix 4

Lamination Theory

\[ [\tilde{Q}] = [T^{-1}][Q][T] \]

Where

\[
[\tilde{Q}] = \begin{bmatrix}
\tilde{Q}_{11} & \tilde{Q}_{12} & 0 \\
\tilde{Q}_{21} & \tilde{Q}_{22} & 0 \\
0 & 0 & \tilde{Q}_{66}
\end{bmatrix}
\]

\[
[Q] = \begin{bmatrix}
Q_{11} & Q_{12} & 0 \\
Q_{21} & Q_{22} & 0 \\
0 & 0 & Q_{66}
\end{bmatrix}
\]

\[ Q_{11} = \frac{E_1}{1 - v_{12}^2 E_2/E_1} \]

\[ Q_{22} = \frac{E_2}{1 - v_{12}^2 E_2/E_1} \]

\[ Q_{12} = \frac{v_{12} E_1}{1 - v_{12}^2 E_2/E_1} \]

\[ Q_{66} = G_{12} \]

\[
[T] = \begin{bmatrix}
m^2 & n^2 & 2mn \\
n^2 & m^2 & -2mn \\
-mn & mn & m^2-n^2
\end{bmatrix}
\]

\[
[T^{-1}] = \begin{bmatrix}
m^2 & n^2 & -2mn \\
n^2 & m^2 & 2mn \\
-mn & mn & m^2-n^2
\end{bmatrix}
\]
Where \( m = \cos \theta \), \( n = \sin \theta \)

\[
\begin{align*}
\bar{Q}_{11} &= Q_{11}m^4 + Q_{22}n^4 + 2m^2n^2(Q_{12} + 2Q_{66}) \\
\bar{Q}_{12} &= m^2n^2(Q_{11} + Q_{22} - 4Q_{66}) + (m^4 + n^4)Q_{12} \\
\bar{Q}_{16} &= [Q_{11}m^2 - Q_{22}n^2 - (Q_{12} + 2Q_{66})(m^2 - n^2)]mn \\
\bar{Q}_{22} &= Q_{11}m^4 + Q_{22}n^4 + 2m^2n^2(Q_{12} + 2Q_{66}) \\
\bar{Q}_{26} &= [Q_{11}n^2 - Q_{22}m^2 + (Q_{12} + 2Q_{66})(m^2 - n^2)]mn \\
\bar{Q}_{66} &= (Q_{11} + Q_{22} - 2Q_{12})m^2n^2 + Q_{66}(m^2 - n^2)^2
\end{align*}
\]

\[
\begin{align*}
\bar{Q}_{21} &= \bar{Q}_{12} \\
\bar{Q}_{61} &= \bar{Q}_{16} \\
\bar{Q}_{62} &= \bar{Q}_{26} \\
[A] &= \sum_{i=1}^{N} [\bar{Q}]^i (z_i - z_{i-1}) \\
&= \sum_{i=1}^{N} [\bar{Q}]^i (t_{\text{ply}})
\end{align*}
\]

where \( t_{\text{ply}} \) is a thickness of each ply.

Since each ply in this laminate is the same material, the \((Q)\) matrix for each layer is the same. The lamina stiffness matrix in the principal material directions is

\[
[Q] = \begin{bmatrix}
Q_{11} & Q_{12} & 0 \\
Q_{21} & Q_{22} & 0 \\
0 & 0 & Q_{66}
\end{bmatrix}
\]

For example, based on manufacturer’s data,

\[
= \begin{bmatrix}
123.7 & 3.711 & 0 \\
3.711 & 8.247 & 0 \\
0 & 0 & 5
\end{bmatrix} \text{ GPa}
\]

The various plies within the laminate are oriented in different directions, and therefore, the lamina stiffness matrices must be transformed into the laminate or reference coordinate system. The transformed lamina stiffness matrices are found through the use of equation above. Thus, for the two plies oriented at 0°

\[
\begin{align*}
m &= \cos(0) = 1 \\
n &= \sin(0) = 0
\end{align*}
\]

\[
\bar{Q}_{11} = Q_{11}(1)^4 + Q_{22}(0)^4 + 2(1)^2(0)^2(Q_{12} + 2Q_{66}) \\
&= Q_{11}
\]

144
\[ \bar{Q}_{12} = Q_{12} \]
\[ \bar{Q}_{16} = Q_{16} \]
\[ \bar{Q}_{22} = Q_{22} \]
\[ \bar{Q}_{26} = Q_{26} \]
\[ \bar{Q}_{66} = Q_{66} \]

\[
[\bar{Q}] = \begin{bmatrix}
123.7 & 3.711 & 0 \\
3.711 & 8.247 & 0 \\
0 & 0 & 5
\end{bmatrix} \text{ GPa}
\]

It is obvious that the transformation through 0\(^0\) leaves \((\bar{Q})= (Q)\). For the two plies oriented at 45\(^0\), the transformed stiffnesses are found as

\[
m = \cos(45^0) = \frac{\sqrt{2}}{2}, \quad n = \sin(45^0) = \frac{\sqrt{2}}{2}
\]

\[
\bar{Q}_{11} = Q_{11} \left( \frac{\sqrt{2}}{2} \right)^4 + Q_{22} \left( \frac{\sqrt{2}}{2} \right)^4 + 2 \left( \frac{\sqrt{2}}{2} \right)^2 \left( \frac{\sqrt{2}}{2} \right)^2 (Q_{12} + 2Q_{66})
\]

\[
= 39.84 \text{ GPa}
\]

Similarly, \(\bar{Q}_{12} = 29.84225\)

\[
\bar{Q}_{16} = 28.86 \\
\bar{Q}_{22} = 39.84 \\
\bar{Q}_{26} = 28.86 \\
\bar{Q}_{66} = 31.13125
\]

\[
[\bar{Q}] = \begin{bmatrix}
39.84 & 29.84 & 28.86 \\
29.84 & 39.84 & 28.86 \\
28.86 & 28.86 & 31.13
\end{bmatrix} \text{ GPa}
\]

In the plies oriented at -45\(^0\),

\[
m = \cos(-45^0) = \frac{\sqrt{2}}{2}, \quad n = \sin(-45^0) = -\frac{\sqrt{2}}{2}
\]

\[
[\bar{Q}] = \begin{bmatrix}
39.84 & 29.84 & -28.86 \\
29.84 & 39.84 & -28.86 \\
\end{bmatrix} \text{ GPa}
\]

Note that the only difference between the +45\(^0\) and -45\(^0\) transformed stiffness matrices is the sign of the shear-extensional coupling terms \(\bar{Q}_{16}, \bar{Q}_{26}, \bar{Q}_{61}, \bar{Q}_{62}\). Finally, the transformations for the 90\(^0\) plies yield
\[ m = \cos(90) = 0 \quad n = \sin(90) = 1 \]

\[
[\tilde{Q}] = \begin{bmatrix}
8.247 & 3.711 & 0 \\
3.711 & 123.7 & 0 \\
0 & 0 & 5
\end{bmatrix} \text{ GPa}
\]

Which is the same as \((\tilde{Q})\) for the 0° plies with the \(\tilde{Q}_{11}\) and \(\tilde{Q}_{22}\) terms interchanged.

Now that the transformed lamina stiffness matrices have been computed, the laminate stiffness can be determined with the following equation;

\[ A_{11} = \sum_{i=1}^{N} \tilde{Q}_{i11}(t_{i\text{ply}}) \]

\[ = 39.84 \times 0.2 + 8.24 \times 0.2 + 39.84 \times 0.2 + 123.7 \times 0.2 + 39.84 \times 0.2 + 8.24 \times 0.2 + 39.84 \times 0.2 + 123.7 \times 0.2 + 39.84 \times 0.2 + 8.24 \times 0.2 + 39.84 \times 0.2 + 123.7 \times 0.2 = \]

\[ = 169.30 \text{ GPa} \times \text{mm} \]

\[ A_{12} = 4 \times 0.2 \times (3.71 + 29.84 + 29.84 + 3.71) \]

\[ = 53.68 \text{ GPa} \times \text{mm} \]

\[ A_{16} = 4 \times 0.2 \times (0 + 28.86 - 28.86 + 0) \]

\[ = 0 \text{ GPa} \times \text{mm} \]

\[ A_{22} = 169.30 \text{ GPa} \times \text{mm} \]

\[ A_{26} = 0 \text{ GPa} \times \text{mm} \]

\[ A_{66} = 57.81 \text{ GPa} \times \text{mm} \]

Note that the extensional-shear coupling terms in the 45° and -45° plies have cancelled each other in the laminate (A) matrix. This explains why balanced laminates do not exhibit extensional-shear coupling.

In order to determine effective elastic constants for this laminate, it is necessary to invert the (A) matrix. Though it has not been shown explicitly, the (B) matrix for this laminate is zero and thus the extensional and bending moduli are uncoupled.

\[
[a] = [A]^{-1} = \begin{bmatrix}
169.30 & 53.68 & 0 \\
53.68 & 169.3 & 0 \\
0 & 0 & 57.81
\end{bmatrix}^{-1} \text{ GPa}^{-1} \times \text{mm}^{-1}
\]

\[
[a] = \begin{bmatrix}
0.0066 & -0.0021 & 0 \\
-0.0021 & 0.0066 & 0 \\
0 & 0 & 0.0173
\end{bmatrix}^{-1} \text{ GPa}^{-1} \times \text{mm}^{-1}
\]
\[ E_x = \frac{1}{2h}a_{11} = \frac{1}{2(1.6)(0.0066)} = 47.3 \text{ GPa} \]

\[ E_y = \frac{1}{2h}a_{22} = \frac{1}{2(1.6)(0.0066)} = 47.3 \text{ GPa} \]

\[ G_{xy} = \frac{1}{2h}a_{66} = \frac{1}{2(1.6)(0.0173)} = 18.0 \text{ GPa} \]

\[ \nu_{xy} = -\frac{a_{12}}{a_{11}} = -\frac{-0.0021}{0.0066} = 0.31 \]
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Appendix 5

**Crosshead**

The main purpose of the crosshead is to raise/lower the impactor and to release the impactor by the retracting pneumatic ram (or pin) that is mounted onto the crosshead.

**Impactor Brake**

To prevent multiple impacts on the sample, a pneumatic ram brake located on the base of the drop tower catches the impactor immediately after the initial strike. A dummy wooden brake was placed in the original brake as seen in Figure 4-3 to account for the smaller size of the LW impactor.

**Caution:** Ensure placement of support underneath the brake in order to avoid the break being bent severely due to impactor and force in rebounding. Additionally, prior to tests, make sure to check the alignment of the wooden brake — misalignment may cause severe damage to the impactor as well as the test rigs (guide rails and optical sensors).

**Hydraulic Rams and Pressure Transducers**

ENERPAC P-80 hand pumps were used to apply controlled pressure load increase to the hydraulic cylinders. Ram pressure was monitored by dial gauges and pressure transducers converting to force in Newton on each hydraulic line.

**Procedure**

1) In the protect shields being uninstalled, place the specimen in between the grips.
2) Following the fine adjustment (alignment) at the right centre, screw the bolts on the jaws tight.
3) Attach the impactor to the release pin mounted in crosshead/bar
4) Lift the impactor up to the desired height corresponding to impact energy by initially setting lower/higher button, followed by rolling the handle (wrench).
5) Set the hand pump to apply tension
6) Stroke the hand pump while reading the pressure gauge, force as well as strain gauges from three spot up to certain reasonable strains are attained to based on the SG 3. In order to avoid any slips in grips after each stroke, the bolts should be kept re-tightening.
7) Upon the strain being reached to the values needed, release the pin to drop the impactor. It should be set the brake to be activated after the impactor being at first rebound. Make sure to set the impactor brake on to avoid the rebounding impact.

8) Following impact remove the tension loading by releasing the hand pump to avoid further damage due to continued tensile loading.

---

Test rig schematic showing loading arm operation from Ref.1; initial set at $t=0$ (upper) and the loading arm movement after stroking hand pump at $t_1=t$ (lower) (After Whittingham 2005)

---

**Caution**

Further to the minor cautions mentioned earlier, the following bullet points are listed for extra cautions that the user should be aware of during testing. Otherwise it causes to damage to any of the components of the test rig or to less accuracy of the test results.

- The vane on the side of the impactor should be aligned properly so that it passes the optical sensors without any collisions.
• The force transducer should be installed in the reference line, parallel to the long side of impactor to avoid any possibility of breakage, especially after collision with bracket.

• The hand pump should be not sit at an angle; it should sit horizontally, otherwise the load cell does not measure the applied pressure accurately. the measured pressure becomes significantly unstable unreasonably.

• The hand pump is not supported by the rubber-like material, which is deformable. (⇒ no rubber in between the pump and the fixed end when applying either tension or compression)

• Each gripping size on the machine is approximately 25 mm long. Make sure the specimens are long enough to be held firmly by the grips

**Data Acquisition System**

Data acquisition systems were used to collect force, strain and velocity. The next sections will describe the respective operating steps and required details.

**VEE OneLab impact test acquire software**

It consists of a personal computer with a DT301 PCI card and the VEE OneLab visual block programming language. The card was capable of scanning at 225 kHz across 16 channels. The maximum scan rate possible on each channel is equal to 225 kHz divided by the number of channels. However, to capture the data as frequently quick as possible at the required maximum scan rate, only 4 channels were used corresponding to 56 kHz for each channel.

CH1: Force Transducer
CH2: Optical Sensor 2
CH3: Optical Sensor 3
CH4: Optical Sensor 4 [Measuring times when the impactor passes through the sensor array for inbound and rebound velocities]

The inbound and rebound velocities are determined using an optical sensor array, which is located on the side.
The velocities were able to be captured by the reader or, simultaneously, by data acquisition system, capable of scanning a sensor at a rate of 225 kHz as mentioned earlier. Figure below exemplifies the results from data acquisition system, viewed by VEEOne lab, then the velocities can be manually calculated since the time the vane travels is measured.

Output from optical sensors (Whittingham, 2005)

**Daqview for Daqboard 112**

In this test, with three channels in total, each channel was capable of collecting data at 33.3 kHz according to the data acquisition board’s maximum capability, 100 kHz and divided by the number of channels in use. The detailed operating steps are stated in as below

- Channel Setup Window
a) Channel numbers - these should be “on” if stain gauges are connected with the corresponding channel numbers
b) Polarity - bipolar should be displayed in relation to amplifier model
c) Units - volt or millivolt can be selected for output units
d) Readings - the values associated with units set are shown simultaneously during testing; if maximum voltage is of ±4.99V is displayed, the connections between the strain gage and the daqbox are somewhat wrong

- Acquisition Setup
  a) Pre-trigger – the number of scans to acquire before the trigger event
  b) Trigger event – “rising edge” or “falling edge” are only available. These monitor value with hysteresis on selected channel; triggers when parameter is satisfied. In this test, rising edge option is used for Ch5 (tension occurred) and falling edge for Ch4&6 (compression)
  c) Stop event – “Number of Scans” is selected, followed by the number of scans. In this test, 50,000 scans were asked to be recorded.
  d) Clock source – Internal is only available; 100 kHz is only selective for internal clock speed.
  e) Scan rate – The scan frequency can be set in units of seconds, milliseconds, minutes, or hours. The maximum frequency is dependent on the number of channels that are enabled.

- Data Destination
  a) It enables to set the directory to store the acquired data with file names; Txt or Bin files can be obtained as preferred.
  b) If the above setups are set and press acquire button to collect the data.

The data acquisition outputs only voltage. It is necessary to convert the voltage to strain for validation. For the conversion factor, the simple tension testing carried out with the extensometer being mounted. The details are demonstrated in Calibration section.

**Caution:** All the connections between daqbook/boarder. Daqbook/laptop (for daqview), boarder/strain gauges should be accomplished prior to executing the daqview software. Otherwise, the daqview will not read/collect the data.
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Appendix 6

Due to the use of CRC-ACS impactor to the Monash impactor, the procedure of for the test with minor changes in operation of the test rig is as follows:

To reattach the crosshead/bar to the release pin

1) Make sure the airline is on
2) Mode should be set to ‘Lift/Lower’
3) Crosshead should be set to ‘Enable’
4) By using wrench with care, lower the crosshead/bar assembly to below the height determined by the rocker switch – Manually press the rocker switch downwards.
5) The crosshead brake ram should be retracted by pushing the crosshead brake on/off buttons (located on the side of the control box)
6) Mode should be set to ‘Setup’
7) Press yellow release trigger to retract the release pin momentarily
**To lift/lower**

1) Mode should be set to ‘Lift/Lower’
2) By suing wrench with care, lift the crosshead/bar assembly retracted to the release pin to the desired height, which should be above rocker switch

**To get ready for the drop**

1) Place all safety shields in place, otherwise the system is deactivated
2) Brake should be on by setting crosshead to ‘Enable’
3) Mode should be set to ‘Drop’ and then the rocker switch should be turned on upwards.
4) Upon the drop-ready light starting blinks, press yellow release trigger
Appendix 7

This instruction was introduced by Dr. Tom Mitrevski.

It needs two main steps to obtain force time history data using two separate *.vee (VEE OneLab format); Impact Testing.vee (so as to acquire the data during testing) & Write default & coord.vee (so as to extract the force dat from the data, Testing.vee)

**Step1. Impact Testing.vee**

1. Open this program in VEE OneLab.
2. Click on ‘configure’ in the ‘A/D Config’ box
3. Specify the file name (e.g., filename.dtv) and directory that impact test will be saved to; it needs to be applied to each channel
4. Click on ‘start’ when the impact test rig is ready for testing
5. Drop the impactor, once clicking on ‘start’

**Caution:** since the program runs only for a few seconds, the impactor has to be dropped as soon as possible subsequent to pressing ‘start’. Otherwise, the data is not acquired from the impact.

6. Once this is done, the file should be saved in a filename.dtv format.

**Step2. Write default & coord.vee**

1. Open this program in VEE Onelab
2. Click on ‘default’ box and specify filename and directory to save the force data.
3. Click on ‘coord format’ box and specify filename and directory.
4. Click ‘start’
5. Click ‘load file’
6. Open filename.dtv file from Step 1.
7. Go to directory where files are and open using notepad or excel to obtain the time and force data

**Caution:** Cable wire connecting the transducer and the data collector should be laid in the path of the impactor travelling.
## Appendix 8

### Summary of Laminate Test

<table>
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<th>Absorbed Energy (J)</th>
<th>Peak Force (kN)</th>
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Summary of maps of c-scan for laminate (Not to scale)
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## Appendix 10

### Summary of Scarf Joint Test

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“This page is left blank intentionally for double-sided printing.”
Appendix 11

For 0 µε pre-strain

For 1000 µε pre-strain

For 3000 µε pre-strain

For 4000 µε pre-strain