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**NUMERICAL MODELLING OF TRANSVERSE
IMPACT ON COMPOSITE COUPONS**

by

Omer Majeed

B. Eng. (Aerospace Engineering)

A thesis submitted to
the Faculty of Graduate Studies and Research
in partial fulfilment of the requirements
for the degree of
Master of Engineering
in Aerospace Engineering

**Department of Mechanical and Aerospace Engineering
The Ottawa-Carleton Institute for Mechanical and Aerospace Engineering
Carleton University
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**Numerical Modelling of Transverse Impact
on Composite Coupons**

submitted by

Omer Majeed

B. Eng. (Aerospace Engineering)

in partial fulfilment of the requirements
for the degree of Master of Engineering
in Aerospace Engineering

Thesis Supervisor

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April 17, 1995

ABSTRACT

Advanced fibre-reinforced composites are being increasingly used in aerospace applications because of their superior specific stiffness and strength properties compared to traditional materials such as aluminum. However, composite materials are susceptible to damage arising from transverse impact loading. The modes of damage arising from transverse impact include matrix cracking, delamination and fibre breakage.

The topic of this thesis is the prediction of the damage resistance behaviour of composite materials under low velocity/high mass transverse impact loading using numerical modelling techniques. Experimental impact testing was performed in support of the numerical investigation effort.

The effects of various physical and numerical parameters on the low energy elastic behaviour of the impact model were investigated. The capability to accurately model low energy elastic impact on composite coupons was demonstrated. The high energy damage-inducing impact simulations were found to be mesh dependent. The model response was found to be dependent on the failure threshold and the fibre post-failure behaviour. Reduction in the failure threshold via reduction of the tensile fibre strength was found to significantly increase the amount of damage in the coupon mesh. Modifications to the fibre post-failure rules were also investigated; increasing the amount of undamaged fibre after failure was found to be an effective means of controlling the unchecked propagation of fibre damage in the coupon mesh.

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NOMENCLATURE

d_x, d_y, d_z	Displacements in x, y and z
k_{comp}	Composite contact stiffness
k_{spring}	Numerical model through-thickness spring stiffness
v_i	Initial impact velocity
A_i	Face area of element i
D_u	Fracture toughness empirical constant for Choi criteria
E_1, E_2	Fibre and matrix moduli, respectively
$(E_1)_{post-fail}$	Fibre post-failure modulus
E_{ply}	Plywood through-thickness modulus
E_{spring}	Numerical model through-thickness spring modulus
F_z, F_y	Forces in z, y direction in crack growth model
G, G_{12}	In-plane shear modulus
G_I, G_{II}, G_{III}	Mode I, II and III strain energy release rates
$G_{Ic}, G_{IIc}, G_{IIIc}$	Mode I, II and III fracture toughnesses
K_i	Bulk modulus of element i
S, S_T	In-plane shear strength; in-plane transverse shear strength
S_i	Interlaminar shear strength
S_{13}, S_{23}	Transverse shear strengths
X	Fibre strength
X_T, X_C	Fibre tensile strength; fibre compressive strength
Y	Matrix strength
Y_T, Y_C	Matrix tensile strength; matrix compressive strength
V_c, V_d	Sliding displacements for nodes c and d for crack growth model
V_i	Volume of element i

W_c, W_d	Opening displacements for nodes c and d of crack growth model
Z_{33}, X_1	Interlaminar through-thickness strength
α	Nonlinear shear stress parameter for Chang criteria
α_E	Fibre post-failure modulus factor
α_S	Fibre post-failure stress factor
$\epsilon_{xx}, \epsilon_{yy}$	Global x- and y-strains
$\theta_x, \theta_y, \theta_z$	Angular displacements in x, y and z
λ_i	Interface spring stiffness of element i
ν_1, ν_{12}	Poisson's ratio
ν_2, ν_{21}	Inverse Poisson's ratio
ξ, ξ_i	Contact Stiffness Penalty Factor; of element i
σ_{xx}, σ_{yy}	Global x- and y-planar normal stresses
σ_{xy}	Global planar shear stress
σ_{zz}	Global through-thickness normal stress
σ_{yz}, σ_{xz}	Global transverse shear stresses
σ_{11}, σ_{22}	Fibre and transverse planar normal stresses
$(\sigma_{11})_{\text{post-fail}}$	Fibre post-failure stress
σ_{12}	Local planar shear stress
σ_{33}	Local through-thickness normal stress
σ_{23}, σ_{13}	Local transverse shear stresses
ϕ	Hourglassing control factor
$\omega_{\max}$	Highest natural element frequency
Δt	Critical time step

Advanced fibre-reinforced composites are being increasingly used in a variety of engineering applications including aerospace structures, automobiles, containers, piping, and sporting goods. Fibrous composites are comprised of two distinct constitutive elements, a fibre and a matrix material. Typical fibres found in advanced engineering applications are carbon/graphite, aramids and glass. Matrix materials often used in composite applications are thermoset resins, thermoplastic polymers, a variety of metals and even carbon.

The increased usage of composites is due to a number of advantages they enjoy over conventional materials. In aerospace applications, composites have demonstrated higher strength-to-weight and higher stiffness-to-weight ratios than traditional aluminum alloys as well as improved corrosion resistance and superior fatigue life. Composites also allow for the tailoring of stiffness and strength properties due to their orthotropic/anisotropic characteristics which enables more efficient design. Despite the numerous advantages of composites over conventional materials, they suffer from a number of shortcomings related to damage resistance and tolerance, hygrothermal effects (the effects of temperature and moisture content on a structure) and higher material and production costs.

Composite aircraft structures are susceptible to damage arising from transverse impact loading resulting from events such as tool drops during maintenance or debris strikes during landing or take off. Impact damage in composites is insidious because the surface damage is often minor and difficult to detect while the internal hidden damage may be extensive. The modes of damage induced by transverse impact include matrix cracking, delamination and fibre breakage. Delamination damage is of particular concern as it can reduce the compressive strength of a damaged structure by as much as 40% due to buckling failure modes (Baker *et al.*, 1985).

Damage tolerance is the current design philosophy adopted for composite aircraft structures. The philosophy can be best summarized by the Federal Airworthiness Regulations (FAR - Advisory Circular 25.571-1) which state that “the damage tolerance of structure is intended to ensure that should serious fatigue, corrosion, or accidental damage occur within the operational life of the airplane, the remaining structure can withstand reasonable loads without failure or excessive structural deformation until the damage is detected.”

The damage tolerant philosophy requires that composite structures be designed based on the assumption that barely visible impact damage (BVID) exists at the most critical locations on the surface of the structure exposed to potential impact (Baker, Jones and Callinan, 1985). Due to the BVID requirements, the design allowable ultimate strain for composites is currently limited to about 0.4%. Improvement in the damage tolerance behaviour of composite materials will enable the use of higher design strains (0.5-0.6%), thus allowing further weight savings to be achieved (Sela and Ishai, 1989).

Cairns and Lagace (1990) concluded that, in light of the susceptibility of composites to impact damage, a better defined design methodology was necessary. They separated the impact damage analysis problem into **damage resistance** and **damage tolerance** portions. The impact **damage resistance** of a composite relates to the size and distribution of damage caused by an impact at a specified energy level. The impact **damage tolerance** is a property of a composite to maintain its residual compressive strength in the presence of damage (Straznicky, Worswick, Majeed and Poon, 1993).

In practice, impact damage resistance assessment of composite structures has been made primarily by means of experimental testing of composite coupons. However, the experimental approach necessitates the use of a considerable amount of material and time for comprehensive evaluations. An accurate technique for predicting composite impact damage resistance has been long sought by designers as a means of quickly evaluating a series of designs using a given material. The predicted damage from the best

performing designs could then be put into a damage tolerance model for further design analysis and optimization.

The topic of this thesis is the prediction of the damage resistance behaviour of composite materials under low velocity/high mass transverse impact loading using numerical modelling techniques. The primary goal of this thesis was to generate accurate simulations of low energy elastic and high energy damage-inducing impact. An important element of this work was a series of numerical studies performed to identify the key numerical and physical parameters which influenced results for low energy elastic impact. Verification of the simulations was obtained through comparison with experimental tests performed as part of this research.

The numerical simulations were performed using a finite element-based model; the software employed for the modelling was the LS-DYNA3D finite element program. The parameters that were used to compare the numerical and experimental results were impact force history, impact energy history, strain history, and resultant damage area.

The contents of this thesis are broken down by chapter as follows:

- *Chapter 2* is a review of literature of experimental and predictive composite impact damage resistance research;
- *Chapter 3* describes the experimental procedure for impact testing and presents the results of those tests;
- *Chapter 4* briefly addresses the theory behind the dynamic finite element software used in this thesis. The chapter contains the results of investigative numerical studies and an improved impact model is defined based on those studies;
- *Chapter 5* contains the comparison of the experimental results with the results of the low and high energy impact simulations using the improved impact model. Results from simulations containing modified fibre

strengths and modified fibre post-failure rules are also presented;

- *Chapter 6* is a discussion of the results from the preceding two chapters;
- *Chapter 7* contains the conclusions of the work performed in this thesis as well as recommendations for future research and a summary of contributions.

2.0 Introduction

The scope of this chapter is to introduce the reader to the topic of impact damage resistance in composites and to review the research that has been conducted in this area. The description of the stress state within an undamaged composite coupon under transverse impact loading is described and the experimentally observed failure behaviours that researchers have identified in laminated composites are summarized. A review of current failure and post-failure theories and a discussion of results from earlier analytical and numerical work is then conducted. As the work performed for this thesis involves the numerical simulation of high mass/low velocity impact on composites, this chapter is focussed on reviewing research of similar types of impacts.

There are two principal techniques to determine the damage resistance of composites under transverse impact loading: experiment and prediction. Traditionally, experimental impact tests have been used to assess composite impact behaviour; however, these tests are expensive in terms of both material and time. Recently, efforts have been made in trying to develop an accurate predictive approach to model the impact event and the resulting damage.

The two predictive approaches which have been used by researchers to model the impact response of composites are analytical methods and numerical modelling. Analytical methods involve determining the closed-form solution of the governing differential equations of the defined problem. The advantage of analytical solutions is that they are more computationally efficient to solve; however, the disadvantage of the

analytical approach is that only problems with relatively simple geometries and boundary conditions may be solved. Numerical modelling methods involve the discretization of the problem geometry such that the governing differential equations may be more easily solved. Numerical methods allow for the solution of more geometrically complex problems, but these methods necessitate more computational effort.

Impact can be broken down into the categories of low energy (elastic) and high energy (inelastic, damage-inducing). Both predictive approaches have had success in modelling elastic impact; however, the same cannot be said of the modelling of impacts where the damage is significant. Accurate modelling of damage-inducing impact has proved elusive due to the complexities of the time-dependent stress state and the failure and post-failure behaviour of impacted composites. Researchers have concluded that the response of a composite and the propagation of damage are coupled in that the dynamic response affects the stresses, the stresses affect the failure modes and locations, which in turn affect the dynamic response (Humphreys, 1981; Edlund, 1993). Therefore, accurate damage prediction cannot be made without including appropriate failure and post-failure behaviour in predictive composite damage models.

High energy impact events can be further divided into the categories of low mass/high velocity and high mass/low velocity. The division of impact events into two separate categories is necessary as both categories produce different types of damage states in composites for a given impact energy level. Ballistic projectiles or kicked-up runway stones typify low mass/high velocity objects and they tend to produce very localized damage response in composites. This is because, typically, the composite structure is too large to deflect and absorb energy at the same rate at which energy is being transferred from the high velocity projectile. Consequently, the impact energy is transferred close to the point of impact resulting in visible localized damage. Dropped tools are examples of high mass/low velocity objects and they tend to produce larger damage areas in composites. Under high mass/low velocity impact, the composite structure has more time to deform, thus absorbing more impact energy with bending or

membrane modes of deflection. As a result, damage is distributed over a larger area. These types of impact are difficult to detect as there is often little associated surface damage.

2.1 Description of the Impact Stress State

The purpose of this section is to describe the development of the stress state within a laminated composite subjected to impact and to introduce the reader to the stress terms and conventions that will be used repeatedly throughout this thesis. For the sake of simplicity, the influence of damage on stress state is neglected in this description.

As previously discussed, typical high mass/low velocity impacts on composites arise due to events such as tool drops on aircraft structure. In order to experimentally simulate such an event, researchers have developed test apparatus utilizing an impactor, a supporting base and a composite coupon, as shown in Figure 2.1(a). While the aforementioned basic elements are common to all impact test apparatus, there is no set standard.

The impactor (also known as a tup) has a mass, m , and typically has a hemispherical head of radius, r . A given impact energy is imparted to the impactor by raising its height to the equivalent potential energy, h , above the coupon, giving the tup a velocity, v , at impact. Often the tup is instrumented to measure data such as impact force and impact velocity. The supporting base upon which the coupon rests can either be of rigid (steel) or compliant (aluminum/plywood sandwich) construction. The basis for having either a rigid or compliant support is to enable the modelling of different substructures to which a composite structure may find itself fixed. A rigid support would simulate the support a composite structure might see if it were part of some primary structure, for example, a wing skin panel near the root of a wing. A compliant support would simulate the support boundary conditions of a composite structure if it were part

of some secondary structure such as a fuselage panel or a control surface. The composite specimen is typically a thin rectangular plate of a specified lay-up.

The dynamics of the impact event can best be described as the transfer of kinetic energy from the impactor to strain energy in the deflecting composite specimen and supports. If the stresses are sufficiently high, failure occurs and some of the stored strain energy is converted into free surface energy and heat energy due to broken atomic bonds. The balance of the stored strain energy is transferred back to the impactor and the impactor rebounds.

Figure 2.1 - Impact Stress State

Under typical experimental transverse impact, the simply-supported composite plate is subjected to a time-varying transverse impact force from the impactor. This force

gives rise to a time-varying stress state within the plate. The resulting stresses induce bending, membrane stretching, transverse shear and local contact displacements within the target (Shivakumar, Elber and Illg, 1985). If the stresses are high enough, matrix and fibre strengths are exceeded and failure ensues.

The stress state within the specimen can be described by looking at an arbitrary location within a given lamina of the specimen shown in Figure 2.1(b)¹. The global stresses at a discrete location within a lamina of the coupon are shown in Figure 2.1(c). They are defined as follows:

- σ_{xx} , σ_{yy} are the global planar normal stresses;
- σ_{xy} is the global planar shear stress;
- σ_{zz} is the global through-thickness normal stress;
- σ_{yz} , σ_{xz} are the global transverse shear stresses.

However, for the purposes of analysis, it is necessary to transform the global stresses within a lamina to a local longitudinal (fibre) - transverse coordinate system. For example, to determine if fibre or matrix failure has occurred in an analysis, the global stresses must be resolved into local stresses. The resolution of local stresses requires that the global stresses be transformed using so-called *rotation matrices* which transform the stresses by an angle of θ , which is the orientation of the lamina with respect to the global coordinate system. The transformed local stresses are depicted in Figure 2.1(d) and, for the purposes of this thesis, are defined as follows:

- σ_{11} , σ_{22} are the fibre and transverse planar normal stresses, respectively;
- σ_{12} is the local planar shear stress;
- σ_{33} is the local through-thickness normal stress;
- σ_{23} , σ_{13} are the local transverse shear stresses.

¹ The global axes are defined using the right-hand rule with the x-direction along the length of the specimen. This is the convention that has been adopted by Boeing and is used in this thesis.

2.2 Experimental Research

The development of the Characteristic Damage State, CDS, for so-called thin laminates is described herein based on what has been observed in previous experimental work². The effects of material factors, loading rate, specimen geometry and support conditions on the response of composite laminates are also discussed.

2.2.1 Development of the Characteristic Damage State (CDS)

In this thesis, the Characteristic Damage State refers to the three major mutually orthogonal modes of damage typically found present in thin composite laminates subjected to impact damage. The three modes are matrix cracking, delamination and fibre breakage and they generally occur in the order listed with increasing impact energy (Srinivasan, Jackson, Smith and Hinkley, 1992). Both matrix and fibre failure are generally thought to occur due to the strength of materials being exceeded (Agarwal and Broutman, 1990), while delamination is thought to propagate due to fracture mechanisms.

Impact surface damage in the form of a permanent, plastic indentation is the first minor type of damage observed in impacted composites. This plastic indentation has been reported by authors Yang and Sun (1982) and Vietinghoff (1993). It has been widely reported that the initial major mode of failure in laminated composites under low velocity transverse impact loading is matrix cracking (Joshi and Sun, 1987; Choi, Downs and Chang, 1991a,b). This mode initiates at low to moderate impact energies. The location of the initiating matrix cracks within the laminate seems to depend on its stacking sequence. Lin and Lee (1990) studied impact damage of laminated plates using relatively simple $[0_5/90_5/0_5]$ and $[90_5/0_5/90_5]$ laminates.

² A laminate plate is defined as thin where its thickness is small compared with its planar dimensions, yet it is capable of resisting bending and membrane forces (Megson, 1990).

(a) Principal stress resolution leading to crack initiation

$\alpha < \beta < \gamma$ (ply angle orientation)

(b) Crack initiation and delamination propagation

(c) Delamination bounded by cracks in indicated plies

Figure 2.2 - Crack Initiation and Delamination Growth (Gosse and Mori, 1988)

Figure 2.3 - Delamination Distribution Through Bottom 12 Plies of a $[-45/0/45/90]_{3s}$ Laminate (Gosse and Mori, 1988)

Figure 2.4 - Sectioned View of Matrix Cracking and Delamination Damage

Figure 2.5 - Contact and Back Face Damage

They found that matrix cracks initiated in the lower lamina and those cracks caused delaminations to subsequently initiate at the first ply interface³ of the plates. Choi *et al.* (1991a,b) reported that delamination and microcracks due to impact appeared to be initiated by the matrix cracks.

The creation of matrix cracks and the resulting development of delaminations in a laminate are described in Figure 2.2 from Gosse and Mori (1988). Figure 2.2(a) shows how the resolved principal stress leads to crack initiation within a ply. The crack grows in Figure 2.2(b) and allows for delamination to propagate within the laminate. The final figure, Figure 2.2(c) shows how the delamination is bound by transverse cracks in the indicated plies.

Based on their experiments, Choi *et al.* (1991a,b) claimed there existed an impact energy threshold associated with the energy required to initiate the first critical matrix crack. Abrate (1991) in his extensive review, reported the existence of a damage threshold above which the projected damage area was found to increase linearly with the kinetic energy of the impactor.

Dost, Ilcewicz and Gosse (1988), Lagace (1993) and Vietinghoff (1993) linked matrix cracks and delaminations by observing the transverse matrix cracks were found to connect delaminations, forming circular sublaminates that repeat with ply stacking sequence through the laminate thickness. Srinivasan *et al.* (1992) characterized delamination after impact as consisting of wedge-shaped damage areas spiralling through the thickness at each ply interface and causing the formation of numerous interconnected sublaminates. Daniel and Wooh (1990) observed that one of the principal characteristics of graphite/epoxy laminates subjected to impact damage was that there was an increase in the size of delaminations from the impacted side to the opposite side of the specimen.

³ The first ply interface is the lowest interface between plies where the ply orientation changes.

A possible explanation for this phenomenon is that matrix cracking damage occurs under primarily planar tensile stress conditions, thus giving rise to more damage sites towards the bottom of the specimen where delaminations may initiate.

Figure 2.3 from Gosse and Mori (1988) depicts an idealized top-down view of the delamination distribution through the bottom 12 plies of a $[-45/0/45/90]_3$ laminate⁴. The delamination pattern resembles a staircase-type progression through the laminate and the delaminations grow in size as the damage progresses down through the laminate. Figure 2.4 depicts the typical matrix cracking and delamination-type damage found in an impacted composite specimen as viewed from a section through the specimen. Often, the centre "core" of damage in an impacted specimen shows no delaminations.

At sufficiently high impact energies, Vietinghoff noticed the occurrence of fibre microbuckling in the vicinity of the local contact zone. This damage is depicted in Figure 2.5(a) along with plastic indentation damage. The upper surface indentation results from the local compression of the matrix material by the impactor while the microbuckling failure of the fibres on the upper surface is also likely due to the interaction of local compression by the impactor and bending compression of the entire plate. At high impact energy levels, fibre breakage occurs on the back face (tension side) of the laminate. Matrix cracking is also associated with this back face damage (Vietinghoff, 1993). Tests by Sjöblom, Hartness and Cordell (1988) on composites panels with epoxy (thermoset) and PEEK (thermoplastic) matrix materials showed similar back face damage modes as reported by Vietinghoff (1993). This damage is a localized effect due to the impactor pushing out the composite material directly beneath it. Figure 2.5(b) shows an idealized picture of damage at the back face. At a particular impact energy threshold, complete penetration of the specimen occurs and this is the effective upper energy limit for impact testing.

⁴ This convention for lay-up states that the $-45/0/45/90$ lay-up is repeated 3 times and is then mirrored symmetrically, resulting in a 24-ply lay-up.

The evolution of impact damage in a thin composite laminate with increasing impact energies can be summarized as follows:

1. At low impact energies, minor damage in the form of a small plastic indentation is formed on the impact surface;
2. At low to moderate impact energies, matrix cracking damage initiates in the lowest ply of the laminate. As the impact energy increases, delamination forms at the first ply interface. Matrix cracking and delamination damage subsequently spreads upwards through the laminate;
3. At higher impact energies, fibre microbuckling occurs on the impact face near the contact zone. Fibre breakage and matrix cracking damage begins to occur on the back face of the specimen directly below the impactor;
4. At a well-defined threshold value of impact energy, complete penetration of the specimen occurs; this is the upper energy limit for impact testing.

2.2.2 Material Effects

Experimental studies conducted by Swanson (1992) concluded that both delamination initiation and propagation were governed by fracture mechanics. Srinivasan, Jackson and Hinkley (1991), in their evaluation of 7 different material systems, determined that the matrix exerts a dominant influence on the impact resistance of composites. In general, the tougher the matrix material, the better the damage resistance performance of the composite. Morton and Godwin (1989) compared the low velocity impact responses of thermoplastic and toughened epoxy matrix composites and found the damage resistance of thermoplastic resin superior to that of the toughened epoxy systems. The superior damage resistance performance of thermoplastic resins may be due in part

to their ability to plastically absorb energy. Tobukuro, Odagiri, Ito and Nishimura (1988) studied the T800/3900-2 toughened epoxy composite system and noted improved impact performance over conventional epoxy systems as did Vietinghoff (1993) who concluded that the T800H/3900-2 system demonstrated significantly higher damage resistance compared to the brittle epoxy system, AS4/3501-6, although fibre performance may also have been a factor.

Cantwell, Curtis and Morton (1986) compared the low velocity impact performance of composites with standard high-strain carbon fibres to composites with improved AS4 high-strain carbon fibres. The matrix materials were similar for both composites, but not identical as the epoxy matrix material in the composite with the standard fibres had a thermoplastic additive. They observed that the damage size was considerably less in the AS4 laminates than in the laminates reinforced with standard high-strain fibres, which suggested that higher-strain fibres improved the damage resistance of a laminate.

A number of authors (Johnson and Mangalgiri, 1987; Tobukuro *et al.*, 1988; Sela *et al.*, 1989; Cantwell and Morton, 1991) have attempted to experimentally correlate the damage resistance performance of composites with their fracture mechanics material properties. As shown in Figure 2.6, mode I is the opening or peeling mode, mode II is the sliding or shearing mode and mode III is the tearing mode. G_I and G_{II} are the mode I and mode II strain energy release rates, respectively. G_{Ic} and G_{IIc} are the critical strain energy release rates (or fracture toughnesses) for modes I and II, respectively. A crack or flaw becomes critical, that it grows in an unstable manner, once the strain energy release rate exceeds the rate of increase in surface energy (Agarwal and Broutman, 1990).

Sela *et al.*, (1989) reviewed the subject of interlaminar fracture toughness and noted that reductions in brittleness could be achieved by toughening epoxy resins or by using thermoplastics for matrix applications. Johnson and Mangalgiri (1987) observed that fracture of brittle resin composites seemed to be controlled by the mode I strain

Figure 2.6 - Fracture Modes

energy release rate, G_I , while the fracture of tougher resin composites seemed to be controlled by the total strain energy release rate, $G_T = G_I + G_{II}$.

In contrast with these results, Tobukuro *et al.* (1988) concluded that the mode II critical strain energy release rate, G_{IIc} was a better indicator than the mode I critical strain energy release rate, G_{Ic} for predicting impact resistance of composites. Cantwell *et al.*, (1991) also found that the mode II fracture toughness of the matrix (G_{IIc}) appeared to determine the level of impact damage and hence the damage resistance of a composite.

Experimental and numerical research by Lee and Chang (1993) showed that toughened interleaves⁵ could improve the impact resistance of laminated composites. Interlaminar mode I fracture toughness seemed to affect the delamination initiation but interlaminar modes II and III fracture toughness became increasingly important as the

⁵ Interleaves are thin layers of matrix-type material placed between the laminae of a laminate to improve damage resistance of the laminate.

delamination grew away from the impacted area.

The consensus among researchers is that delamination damage is driven by fracture mechanisms; however the exact mode seems to depend upon material factors. There is a lack of consensus among researchers as to which material factors affect delamination damage.

2.2.3 Specimen Geometry and Test Fixture Effects

Cantwell and Morton (1985) studied the influence of target bending stiffness on low velocity impact damage in graphite/epoxy laminates using a drop-weight tester. Target stiffness was found to be a dominant parameter and controlled the mode of failure. At low velocities, thinner, more flexible targets responded primarily in bending which generated higher tensile stresses in the lowest ply. Matrix cracks resulted in the lowest ply, leading to delamination and the spread of damage upwards through the laminate. Thicker, stiffer targets had damage initiate on the impact surface as a result of high contact stresses. Cantwell and Morton (1989) further studied the effect of geometry on the low velocity impact response of graphite/epoxy laminates. Again they concluded that short, thick targets suffered top surface contact failure whereas long, thin laminates tended to suffer lower surface flexural failure. An optimum thickness was found whereby damage initiated simultaneously on the top and bottom layers and this thickness gave the highest damage resistance.

Currently, there is no standardized impact test and consequently, no standardized specimen geometry or lay-up. This lack of standardization makes the comparison of results between different groups of researchers difficult. Sjöblom *et al.* (1988) used circular specimens with diameters of 72 mm (2.8 in) and 123 mm (4.8 in.) in their experimental testing. Lee and Zahuta (1991) and Choi *et al.* (1991) both employed rectangular specimens for their impact testing; the former using 69 mm x 152 mm (2.7

in. x 6.0 in.) specimens while the latter used predominantly 76 mm x 100 mm (3.0 in. x 3.9 in.) specimens. Vietinghoff (1993) referred to Boeing specifications (Boeing, 1988) in order to define a suitable specimen geometry. The Boeing specification required the use of a rectangular specimen with dimensions 102 mm x 152 mm (4.0 in. x 6.0 in.). It does not appear that there will be a standardized specimen geometry in the near future, but it is likely that the standards will likely evolve from specifications set by larger organizations such as Boeing.

Results by Prandy, Boyd, Recker and Altstädt (1991) showed that the test fixture material had a large influence on the amount of impact energy absorbed by the test specimen. Using a wood fixture, some of the impact energy was absorbed by the fixture, whereas using a more rigid steel fixture, a higher proportion of the impact energy was absorbed by the specimen. Specimens tested on a more flexible fixture absorbed less impact energy and, therefore, suffered less damage.

2.2.4 Loading Rate Effects

Elber (1983) tested thin 8-ply quasi-isotropic graphite/epoxy composite plates to establish the degree of equivalence between low velocity impact and static testing. Both the deformation and resulting damage were found to be similar but not identical between the impact and static indentation tests. Elber found that static tests produced less fibre damage but slightly more matrix damage than impact tests. Lee and Zahuta (1991) also compared the impact and static loading behaviour of several toughened epoxy laminate systems. The parameters measured were impact force, damage size and energy absorption and they concluded that the static response was similar but not identical to the dynamic response.

Gillespie, Carlsson and Smiley (1987) examined fracture behaviour in graphite/epoxy (thermoset) and graphite/PEEK (thermoplastic) laminates over crosshead

loading rates ranging from 0.25-250 mm/min. The crosshead speeds spanned loading rates of quasi-static to low velocity impact. For the graphite/epoxy laminates, the load-deflection response was linear to fracture and stable; slow crack growth was observed for all rates tested. The progression of damage appeared to be relatively loading rate-insensitive for graphite/epoxy laminates. However, the crack growth in graphite/PEEK laminates was often unstable and showed 'stick-slip' behaviour, thus making the damage response rate-sensitive. The PEEK material showed rate-dependent behaviour most likely due to plastic and viscoelastic effects in the process zone around the crack tip. Morton and Godwin (1989) also concluded that the damage response of thermoplastic-based composites was more loading rate-sensitive than the response of epoxy-based composites.

With respect to the rate sensitivity of the damage response of thermoset-based composites, there is disagreement among researchers. Elber (1983) and Lee and Zahuta (1991) concluded that damage response did vary slightly with loading rate in thermoset composites, while Gillespie, Carlsson and Smiley (1987) did not observe rate sensitivity in their work on thermosets. Thermoplastic-based composites have damage response rate sensitivity based on the work of Gillespie, Carlsson and Smiley (1987) and Morton and Godwin (1989). The effect of rate sensitivity on the damage response of composites should be further investigated.

2.2.5 Stacking Sequence Effects

Clark (1989), Abrate (1991) and Cantwell *et al.* (1991) all reviewed the impact resistance of composites and observed that the fibre stacking sequence affects the damage resistance of the laminate. Clark (1989) described the delamination mechanism as occurring between plies of different orientations where the induced peel stresses promote the separation of the two laminae. It was also suggested by Liu (1988) and Lagace (1993) that delamination results from interlaminar stresses in the interply layer which arise from the mismatch in elastic constants from ply to ply. In fact, Liu (1988) proposed

that the mismatching of bending stiffness between two adjacent laminae could be an indicator of delamination in the composite laminates subjected to subperforation impact. Liu's results showed promise for relatively simple laminates.

2.2.6 Summary of Experimental Observations

Based on numerous experimental observations, it can be stated that the characteristic damage state of thin impacted composite laminates is a complex mixture of matrix cracking, delamination and fibre breakage. While matrix and fibre failure are driven by exceedence of strengths, evidence shows that delamination is driven by fracture mechanisms. Research has shown that composites with higher toughness matrix materials exhibit better damage resistance properties. However the exact mechanisms of delamination propagation remain uncertain. Standardized test procedures are required to facilitate comparison of research.

It appears as well that the impact resistance of composite materials using higher toughness thermoplastics is influenced by loading rate. Therefore, for thermoplastics in particular, quasi-static tests may not be sufficient to accurately characterize impact damage and accurate predictive modelling may need to take rate effects into consideration.

2.3 Failure and Post-Failure Theory

A number of theories have been formulated in order to predict the onset of failure in composites and their subsequent post-failure behaviour. The failure theories can be divided into two categories: intralaminar failure including fibre breakage and matrix cracking and interlaminar failure dealing with delamination damage. The intralaminar theories assessed here are maximum stress, Tsai-Hill, Tsai-Wu, Hashin and Chang and they are compared to each other using failure envelopes. Interlaminar criteria, namely

crack closure, Choi, quadratic delamination initiation and DYNA3D delamination are also discussed in this chapter. The three post-failure theories discussed are Hahn-Tsai, Petit-Waddoups and Chiu.

2.3.1 Intralaminar Failure Criteria

The intralaminar failure criteria may be divided in two categories: superimposed and interactive criteria. Superimposed criteria, such as maximum stress, assume simply that the failure modes in a composite depend on only a single stress whereas interactive criteria, such as Tsai-Hill, Tsai-Wu, Hashin and Chang, assume that the failure modes are dependent on at least two stresses. Controversy exists as to which philosophy is the correct one. Hart-Smith (1993a,b) stated that interactive failure models such as Tsai-Wu or Tsai-Hill had no relevance to the strength of conventional fibre/polymer composites and thus the diverse failure modes in heterogeneous fibrous composite laminates should be superimposed and not interacted.

The maximum stress criteria (Agarwal and Broutman, 1990) consider failure to occur through five independent failure modes: tensile and compressive fibre failure, tensile and compressive matrix failure, and shear failure. In the expressions for these criteria, given in Table 2.1, X_T and X_C refer to the fibre tensile and compressive strengths, respectively; Y_T and Y_C refer to the matrix tensile and compressive strengths, respectively; and S refers to the in-plane shear strength. Maximum strain criteria are identical to maximum stress criteria except that stresses are replaced by strains. The advantage of these criteria, in addition to their simplicity, is that the exact mode of failure is known when failure is predicted to occur. Quasi-static experimental tests using carbon/epoxy tubular specimens showed that the maximum tensile longitudinal stress criterion was a good predictor of tensile fibre failure in composites (Swanson and Christoforou, 1987a,b).

The Tsai-Hill failure criterion takes into account the interaction between stresses which differentiates it from the maximum stress/strain theory (Tsai, 1968; Hill, 1950). The difficulty with interactive criteria such as the Tsai-Hill is that discerning the exact mode of failure is not clear. In the expression for the criterion: X refers to the fibre strength, Y refers to the matrix strength and S refers to the shear strength. The criterion is also known as the maximum work theory.

The Tsai-Wu interactive criterion has a similar disadvantage as the Tsai-Hill criterion in that its complexity does not enable the mode of failure to be readily discerned (Tsai and Wu, 1971). Some researchers have ascribed failure to either the matrix or the fibre depending on the relative magnitude of the various terms. Another method has been to divide the original polynomial into two separate criteria, one for the fibre and the other for the matrix. The Tsai-Wu criterion for matrix failure, where $\sigma_{11} = 0$, was shown to compare well with experimental data derived from quasi-static tests using carbon/epoxy tubular specimens (Swanson and Christoforou, 1987a,b).

The Chang criteria (Chang and Chang, 1987a,b) are interactive criteria for fibre breakage, matrix cracking and matrix compression based on the Hashin criteria⁶ (Hashin, 1980). The Chang failure criteria are currently implemented in the LS-DYNA3D Composite Damage Material Model. The Chang criteria were developed for the finite element strength evaluation of graphite/epoxy bolted composite joints under tension loading and include α , a nonlinear shear stress parameter⁷. Chang and Chang found their predictions to be in good agreement with experiment. Murray and Schwer (1989) concluded that the Chang fibre breakage criterion was in reasonable agreement with test data; however, in order to properly evaluate the criterion for fibre failure, additional test data were necessary. They also concluded that the Chang matrix failure prediction did

⁶ S_T refers to the transverse shear strength in the Hashin criteria.

⁷ The non-linear shear stress parameter is experimentally measured for non-symmetric laminates; if a laminate is symmetric, $\alpha = 0$.

not adequately model failure in carbon/epoxy composites.

In conclusion, it can be stated that either the Chang or the maximum stress criterion predict tensile fibre failure reasonably well. The Tsai-Wu criterion was found to predict matrix failure well. However, more research needs to be done to further assess the accuracy of existing intralaminar failure theories for composites and the development of improved theories may be required.

A comparison of the theoretical failure envelopes for the intralaminar failure criteria is presented in Figures 2.7-2.9. Please note that the enclosed area of an envelope is known as the undamaged region whereas the area outside the envelope is known as the failed or damaged region. The Hashin and Chang criteria are presented together in the envelope figures as they are similar assuming the nonlinear shear stress parameter in the Chang criteria, $\alpha = 0$. The only other difference between the two criteria is that the Hashin criteria have an expression for compressive fibre failure. Therefore, for the biaxial failure envelope plot (Figure 2.7), the Hashin criteria are comprised of the entire shown envelope, while the Chang envelope is only defined for $\sigma_{11} \geq 0$.

Maximum Stress:	
Fibre Tension	$\sigma_{11} \geq X_T \quad (\sigma_{11} > 0)$
Fibre Compression	$ \sigma_{11} \geq X_C \quad (\sigma_{11} < 0)$
Matrix Tension	$\sigma_{22} \geq Y_T \quad (\sigma_{22} > 0)$
Matrix Compression	$ \sigma_{22} \geq Y_C \quad (\sigma_{22} < 0)$
Matrix Shear	$ \sigma_{12} \geq S$
Tsai-Hill:	$\frac{\sigma_{11}^2}{X^2} - \frac{\sigma_{11}\sigma_{22}}{X^2} + \frac{\sigma_{22}^2}{Y^2} + \frac{\sigma_{12}^2}{S^2} \geq 1$
Tsai-Wu:	$F_1\sigma_{11} + F_2\sigma_{22} + F_{11}\sigma_{11}^2 + F_{22}\sigma_{22}^2 + F_{12}\sigma_{11}\sigma_{22} + F_{66}\sigma_{12}^2 \geq 1$ $F_1 = \frac{1}{X_T} - \frac{1}{X_C} \quad F_2 = \frac{1}{Y_T} - \frac{1}{Y_C} \quad F_{11} = \frac{1}{X_T X_C}$ $F_{22} = \frac{1}{Y_T Y_C} \quad F_{12} = -\frac{1}{2}(F_{11}F_{22})^{\frac{1}{2}} \quad F_{66} = \frac{1}{S^2}$
Hashin:	
Fibre Tension	$(\frac{\sigma_{11}}{X_T})^2 + (\frac{\sigma_{12}}{S})^2 \geq 1 \quad (\sigma_{11} > 0)$
Fibre Compression	$ \sigma_{11} \geq X_C \quad (\sigma_{11} < 0)$
Matrix Tension	$(\frac{\sigma_{22}}{Y_T})^2 + (\frac{\sigma_{12}}{S})^2 \geq 1 \quad (\sigma_{22} > 0)$
Matrix Compression	$(\frac{\sigma_{22}}{2S_T})^2 + ((\frac{Y_C}{2S_T})^2 - 1)\frac{\sigma_{22}}{Y_C} + (\frac{\sigma_{12}}{S})^2 \geq 1 \quad (\sigma_{22} < 0)$
Chang:	
Fibre Breakage	$(\frac{\sigma_{11}}{X_T})^2 + \tau \geq 1 \quad (\sigma_{11} > 0)$
Matrix Cracking	$(\frac{\sigma_{22}}{Y_T})^2 + \tau \geq 1 \quad (\sigma_{22} > 0)$
Matrix Compression	$(\frac{\sigma_{22}}{2S})^2 + ((\frac{Y_C}{2S})^2 - 1)\frac{\sigma_{22}}{Y_C} + \tau \geq 1 \quad (\sigma_{22} < 0)$ $\tau = (\frac{\sigma_{12}}{S})^2 \left(\frac{1 + \frac{3}{2}\alpha G_{12}\sigma_{12}^2}{1 + \frac{3}{2}\alpha G_{12}S^2} \right)$

Table 2.1 - Intralaminar Failure Criteria for Composites

Figure 2.7 - Comparison of Theoretical Biaxial Failure Envelopes

Figure 2.8 - Comparison of Theoretical Fibre Failure Envelopes

Figure 2.9 - Comparison of Theoretical Matrix Failure Envelopes

The material properties used in the definition of the envelopes are those from the Toray T800/P2302-19 graphite fibre toughened resin system whose strength properties (Gaudert, Lee, Poon and Gould, 1993) are characterized in the following table.

Longitudinal Tensile, X_T	Longitudinal Compressive, X_C	Transverse Tensile, Y_T	Transverse Compressive, Y_C	Shear, S
2772.0 MPa	1482.0 MPa	79.3 MPa	231.0 MPa	132.8 MPa

Table 2.2 - T800/P2302-19 In-Plane Strengths

The differences between the superimposed and interactive failure philosophies are well illustrated by the three failure envelope figures. The superimposed criteria represented by maximum stress possess rectangular failure envelopes. The interactive criteria (Tsai-Hill, Tsai-Wu and Hashin/Chang) have quadratic-shaped failure envelopes and all generally sit within the space defined by the maximum stress criteria.

Figure 2.7 defines the failure envelopes for pure biaxial loading, σ_{11} vs. σ_{22} . The maximum stress criterion defines a rectangular envelope and the Tsai-Hill envelope fits within the former. The Tsai-Wu envelope extends slightly beyond the maximum stress envelope. Interestingly, the Hashin/Chang envelope has the same shape as the maximum stress criteria. The plot of the σ_{11} vs. σ_{12} envelopes (Figure 2.8), defined as the fibre failure envelopes, show the failure space under combined longitudinal and shear loading. Once again the maximum stress criteria define a rectangular envelope. Of note is the fact that all the interactive criteria envelopes not only lie within the rectangular envelope but that they are also in good agreement with each other. Figure 2.9 is the matrix failure plot, σ_{22} vs. σ_{12} . Both the Tsai-Hill and Hashin/Chang envelopes are similar and just fit within the maximum stress envelope. The Tsai-Wu envelope, considered the better matrix failure predictor, extends slightly from the rectangular envelope.

Overall, the failure envelopes are in general agreement with each other, with all envelopes sharing common intersection points on the axes, as would be expected. The implication of this general agreement among criteria is that the prediction of failure in laminates will not depend heavily the criteria chosen; all the criteria define roughly similar failure spaces.

2.3.2 Interlaminar Failure Criteria

The four interlaminar failure criteria dealt with in this section are all quite distinct from each other, hence it is difficult to compare them in the same way that the intralaminar criteria were compared using failure envelopes. The criteria examined include crack closure, Choi, quadratic delamination initiation and LS-DYNA3D quadratic delamination.

The crack closure technique has been used to predict delamination growth of laminated composites using a fracture mechanics approach. Crack growth theory can be

expressed as follows: if a crack extends by a small amount Δa , the energy absorbed in the process is equal to the work required to close the crack to its original length. The delamination crack length, a , is predicted using the relation $G(a) = G_c(a)$. Sun and Jih (1990, 1993) used finite element techniques to predict crack lengths in laminated composites. The expressions they used for energy release rates for Mode I and II delamination cracks are shown in Table 2.3.

Figure 2.10 shows a typical crack formulated using finite elements. F_z and F_y are the forces in the z - and y -directions required to hold nodes c and d together. The displacements V_c , V_d , W_c and W_d are the sliding and opening displacements on the crack face, respectively. Good agreement was found between measured and predicted crack lengths for high mass/low velocity impacts on graphite/epoxy laminates.

The Choi criteria consist of a criterion to predict matrix cracking and a criterion to predict delamination using finite element techniques (Choi and Chang, 1992). In a finite element simulation, a critical crack is predicted in a ply, the delamination criterion is activated and a delamination can be initiated from the crack. In the formula for the matrix cracking criterion, the subscript, n refers to the n th layer, Y refers to the matrix tensile or shear strength and S_i refers to the interlaminar shear strength within a laminate. The overbar in all stress terms refers to element averaged values. In the delamination criterion, D_{in} is an empirical constant determined by experiment that accounts for the fracture toughness of the laminate and the subscripts n and $n + 1$ correspond to the upper and lower plies of the n th interface. Experimental impact tests were conducted on graphite/epoxy specimens and simulations were performed using the Choi damage criteria (Choi and Chang, 1992). The simulation results agreed with the test data very consistently.

Figure 2.10 - Finite Element Crack Growth Model

The quadratic delamination initiation criterion was developed by Brewer and Lagace (1988) to predict only the *initiation* of delamination in composite laminates. As with the Choi criteria, this interactive strength of materials approach is simpler and more efficient than the strain energy (fracture) approach which requires the use of finite element analysis. In the formula, Z_{13} and Z_{23} refer to the interlaminar shear strengths for σ_{13} and σ_{23} , respectively. Z_{33} is defined as the interlaminar through-thickness strength. The overbars on the stresses refer to element averaged values. The quadratic criterion compared well with experimental uniaxial tension test results involving graphite/epoxy specimens.

Crack Closure: Mode I Mode II	$\frac{F_i(W_i - W_d)}{2\Delta a} \geq G_i$ $\frac{F_{ii}(V_i - V_d)}{2\Delta a} \geq G_{ii}$
Choi: Matrix Cracking Delamination	$\left(\frac{\bar{\sigma}_{22}^n}{Y^n}\right)^2 + \left(\frac{\bar{\sigma}_{21}^n}{S_i^n}\right)^2 \geq 1$ $D_n \left[\left(\frac{\bar{\sigma}_{21}^n}{S_i^n}\right)^2 + \left(\frac{\bar{\sigma}_{11}^{n+1}}{S_i^{n+1}}\right)^2 + \left(\frac{\bar{\sigma}_{22}^{n+1}}{Y^{n+1}}\right)^2 \right] \geq 1$
Quadratic:	$\left(\frac{\bar{\sigma}_{11}}{Z_{11}}\right)^2 + \left(\frac{\bar{\sigma}_{21}}{Z_{21}}\right)^2 + \left(\frac{\bar{\sigma}_{22}}{Z_{22}}\right)^2 \geq 1$
LS-DYNA3D:	$\left(\frac{\sigma_{11}}{X_i}\right)^2 + \left(\frac{\sigma_{12}}{S_i}\right)^2 \geq 1$

Table 2.3 - Interlaminar Failure Criteria for Composites

The LS-DYNA3D delamination criterion is a simple strength of materials criterion implemented in LS-DYNA3D software to predict interlaminar failure between stacked solid continuum elements. σ_{33} and σ_{12} refer to the interlaminar through-thickness and shear stresses while X_i and S_i refer to the interlaminar through-thickness and shear strengths. While Edlund (1993) used this criterion to predict delamination, he did not compare the results with experimentally observed damage.

The crack closure technique is not really suitable for predicting delamination in a general laminate. The technique requires the use of a fine mesh discretization using 3-D elements, a prohibitively expensive computational exercise. The quadratic delamination

initiation criterion is unproven for impact prediction and is useful for initiation prediction only. The LS-DYNA3D delamination criterion is experimentally unproven for usage in general laminates and requires more evaluation. The Choi delamination criterion offers the best prospect of an accurate, and yet relatively efficient means of predicting delamination in the finite element impact simulation of impacted composite laminates using its combined fracture and strength of materials approach.

2.3.3 Post-Failure Theories

Nahas (1986) reviewed post-failure theories for composite laminates. The Hahn-Tsai method (Tsai, 1965; Hahn and Tsai, 1974) postulated that any failed lamina can support the load which it is carrying when it fails, until total laminate failure occurs. This is known as the constant stress post-failure behaviour. The Petit-Waddoups method proposed that the failed lamina unloads gradually (Petit and Waddoups, 1969). Whereas the Chiu method proposed that unloading in a failed lamina should be instantaneous (Chiu, 1969). Figure 2.11 illustrates the aforementioned post-failure behaviours. The existence of the three rules reflect a lack of consensus in terms of understanding of what occurs after failure in a laminate. Murray and Schwer (1989) suggested that a general post-failure degradation rule be implemented into codes such as LS-DYNA3D. The user should be allowed to specify percent reductions in elastic constants for each failure mode and/or the degradation behaviour for each mode of failure, *e.g.* constant stress, gradual or instantaneous unloading.

Talreja (1985) found the post-failure behaviour of composites complex and classified the effects of transverse cracking on the stress-strain response of composite laminates. Reductions in ply properties due to matrix cracking depended on the stacking sequence, ply stiffness, cracking ply thickness and cracking density. Stiffness reductions from cracking in glass/epoxy and graphite/epoxy laminates were predicted and were found to be in good agreement with experimental results.

Humphreys (1981) defined more thoroughly post-failure rules for fibre failure, matrix cracking and matrix crushing (compression) damage in composites. Humphreys (1981) assumed instantaneous unloading once failure occurred. The rules are summarized in the following table.

Fibre Failure, $F_{\text{fibre}} \geq 1$	$E_1 = E_2 = G_{12} = \nu_1 = \nu_2 = 0$
Matrix Cracking, $F_{\text{matrix}} \geq 1$	$E_2 = G_{12} = \nu_1 = \nu_2 = 0$
Matrix Crushing, $F_{\text{comp}} \geq 1$	$E_2 = \nu_1 = \nu_2 = 0$

Table 2.4 - Humphreys Post-Failure Rules for Composites

More research needs to be done in the area on composite post-failure behaviour as there is a lack of knowledge in this area of composites behaviour.

Figure 2.11 - Composite Post-Failure Theories

2.4 Analytical Modelling

Efforts have been made in the area of analytical modelling of composites under transverse impact. Shortcomings in accurately characterizing the transverse shear deformation of composites have led to the development of improved deformation theory. Evaluations of both elastic and inelastic (damage) models are presented followed by an assessment of efforts in the area of dimensional analysis.

2.4.1 Development of Improved Deformation Theory

A number of researchers have worked to improve the accuracy of the plate deformation behaviour of laminated composite materials as classical plate theory was not felt to be sufficiently accurate in modelling the through-thickness behaviour of composite plates under transverse loading. In the preceding section, three of the delamination criteria were defined using at least one of the three transverse stresses. This inadequacy is most critical for modelling delamination.

Murthy (1981) derived an improved shear deformation theory for laminated anisotropic 2D plates under bending, eliminating the arbitrary Mindlin shear correction factor. Comparison with exact 3D results showed his theory to be more accurate than other theories of equivalent order. Reddy (1984) developed a higher-order shear deformation theory of plates. Reddy's theory predicts deflections, stresses and frequencies more accurately than the first-order deformation and the classical plate theory. Ambur, Starnes and Prasad (1993) developed an elastic low velocity composite impact model based on small-deformation theory with transverse shear effects. The predicted results compared well with experimental results.

2.4.2 Elastic Modelling

Qian and Swanson (1990) compared an analytical solution using displacement theory with an analytical solution using energy methods for elastic composite impact. In the displacement theory approach, the plate equations of motion used the shear correction factor introduced by Mindlin. The governing equations for the rotations, lateral displacements and the loads were solved using Fourier integral transforms. The transient response of the impact force from the deceleration of the impactor mass was calculated from the integral equilibrium equation between the impactor and the plate. The displacement equation and the equilibrium equation were combined and the Laplace Transform was taken to solve the impact problem.

Qian and Swanson (1990) also derived an analytical solution based on the Rayleigh-Ritz approach. The displacements of the plate were represented by a series of normal modes which are the solutions to the problem of free vibration of beams under appropriate boundary conditions. The equations for the potential and kinetic energies of the plate were given and expressed as Lagrange's equations of motion. The contact force was related to the impactor motion to couple the impactor and plate dynamics. The plate dynamics equation and the impactor dynamics equation were solved numerically using the Newmark integration method.

Both sets of results were in agreement with FEM results and series solutions in literature. Comparisons of both sets of results with experimental data on impact of carbon/epoxy plates were reasonably good. Both sets of results showed that elastic impact can be accurately modelled using analytical techniques.

2.4.3 Damage Modelling

Shivakumar, Elber and Illg (1983) analyzed clamped circular composite plates for static loads assumed to be equivalent to impact loads. The analysis was based on the minimum-potential-energy method and strain-displacement relations. The ply failure region was calculated using the Tsai-Wu criterion. Matrix and fibre failure modes were determined using the maximum stress criteria. Reduced moduli were then used in the failed region in subsequent displacement increments of the analysis. The calculated damage region agreed with selected static test results from the literature.

The analytical composite impact model developed by Cairns and Lagace (1990) used Reissner-Mindlin kinematics to account for laminate shearing deformation in the plate equations of motion. Local contact was predicted using a Hertzian-type contact spring and the local strain field was determined with a Fourier-Bessel series. Damage was predicted using the maximum strain ply failure criterion and the model compared reasonably well with observed damage.

Sun and Jih (1990) proposed an analytical method for predicting impact-induced delamination suitable for low velocity impact with heavy impactors. Static delamination fracture toughness was used to predict delamination crack growth under impact conditions. The contact force history was measured and the peak force used to determine the delamination crack length. The predictions were found to agree very well with experiment. Jih and Sun (1992) performed further work in the area of analytical delamination prediction and found that curing stresses played a significant role in evaluating the fracture toughness of some laminates.

The above summary shows that the capability to predict damage using analytical techniques exists. However as previously stated, the problem with analytical damage prediction techniques is that they lack the generality to model complex geometries and

boundary conditions.

2.4.4 Dimensional Analysis

A number of researchers have tried to apply dimensional analysis to the analysis of fibre composite laminates under impact. The goal was to use lab scale impact results to predict damage in larger structures. Qian and Swanson (1989) found that scaling rules could indeed be used in some instances for non-damage-inducing elastic energy levels. However, Morton (1987) and Swanson (1993) concluded that because of size effects, scaling small-scale damage in coupons to large-scale damage in structures was not possible. The reasoning for this conclusion was as follows; while the scaling effects with respect to specimen size are well understood with respect to deformation, delamination propagation on the other hand is governed by fracture mechanics principles and thus has an absolute size effect.

2.4.5 Summary of Analytical Models

Greater accuracy in the modelling of composite transverse deformation using plate theory has been achieved through the use of improved deformation theory. Analytical techniques have been shown to be capable of modelling elastic impacts with accuracy. A number of authors have reported success in correlating predicted fibre and matrix damage with experimental results and success has been reported in modelling delamination using analytical techniques. The main shortcoming of analytical impact models however, is that they are limited to relatively simple geometries and boundary conditions. Scaling techniques were found not to be viable for the accurate modelling of composite damage.

2.5 Numerical Modelling

Considerable work has been performed by researchers in the area of numerical modelling of impact on composites. As in the case of analytical modelling, efforts have been made to improve the transverse deformation behaviour of numerical simulations in order to improve the accuracy of the predicted solution; those efforts are summarized here. A review of both elastic and inelastic (damage) simulation work has also been conducted.

2.5.1 Improved Element Deformation Formulation

Efforts have been made by researchers to formulate computationally efficient finite elements that accurately predict transverse shear deformation. Harding (1989) was critical of the traditional composite FEM approach which has often been based on plate theory that essentially ignores the transverse normal strain and the two transverse shear strain components. Classical plate theory may be reasonable in many circumstances, however the out-of-plane shear stresses under impact loading may be sufficiently large to initiate or control failure.

DiSciuva (1985) created a multilayered anisotropic flat plate element which included the effects of transverse shear deformation by making use of an improved displacement formulation. Comparison with exact analytic solutions showed that the element was very efficient in predicting the responses of thin and thick laminates. Averill and Reddy (1992) developed an FEM model based on a third-order expansion of displacements through the thickness of the shell, thus allowing for both transverse normal and shearing deformation to be accounted for.

Robbins and Reddy (1993) used the layer-wise laminate theory of Reddy (1987) to develop a layer-wise, 2D displacement-based FEM element for composite plates that

assumes piecewise continuous distribution of transverse strains through the laminate thickness. The resulting element is capable of computing interlaminar stresses and other localized effects with the same degree of accuracy as a 3D model.

2.5.2 Elastic Modelling

The low velocity elastic response of graphite/epoxy laminates was investigated both experimentally and numerically by Tan and Sun (1985). A 9-node isoparametric element based on classical laminate theory along with an empirical contact law was used in the finite element analysis. The impact force response and the strain response of the finite element model were found to be in agreement with experimental data. Wu and Chang (1989) presented a transient FEM scheme using brick elements for studying the elastic response of composites due to transverse impact. Results compared well with existing analytical solutions.

Sankar (1989) used the finite difference method to analyze the axisymmetric problem of smooth contact between a rigid indenter and a laminated circular plate clamped at the edges. Results showed that the interlaminar shear stresses in the vicinity of contact were much different from classical plate theory solutions with shear stress corrections. It was shown that low-modulus interleaves could help in reducing the maximum interlaminar shear stresses due to static indentation.

2.5.3 Damage Modelling

Humphreys (1981) and Edlund (1993) stated that any accurate impact damage model would have to include the response-damage coupling on the basis that the dynamic response affects the stresses, the stresses affect the failure modes and locations, which in turn affect the dynamic response. Humphreys (1981) implemented a FEM model to predict damage and growth in composite laminates subjected to low mass, low velocity

impacts. To model damage, failure and post-failure rules were implemented for matrix cracking, fibre breakage and delamination damage. The analysis results were found to be in agreement with experimental data. Later work by Humphreys and Goering (1983) resulted in the development of a shear flexible plate bending element. They concluded that matrix cracking and fibre breakage occurred as a result of high flexural modes.

Choi and Chang (1992) used a combined numerical and experimental approach to understand the impact damage mechanism and mechanics of laminated composites due to impact. The numerical analysis involved using a 3D transient dynamic code developed by Wu and Chang (1989) called 3DIMPACT. A matrix failure criterion and delamination criterion were proposed for predicting the initial damage and extent of delamination, respectively. The failure criteria for this program were previously addressed in Section 2.3. The experimental testing was performed under quasi-static loading. Overall, the delamination predictions based on the model agreed with the test data.

Liu and Chang (1992) developed a nonlinear 3D FEM model for analyzing damage evolution inside a graphite/epoxy cross-ply composite under quasi-static transversely concentrated loading. Initial matrix cracking and delamination growth were modelled using the crack closure technique. It was concluded that initial matrix cracking significantly affected delamination growth, that the surface matrix crack induced the delamination to grow into a slender peanut shape and that mode I fracture contributes significantly to the growth of delamination.

Montemurro, Hansen and Houde (1993) developed an FEM code for plate impact. The program used Mindlin/Reissner plate theory with a Hertzian-based contact law. A mesh optimization algorithm showed that discretization can affect damage extent and progression. The choice of failure criteria, boundary conditions, and strain displacement relationships were found to influence damage prediction. Transverse shear strength appeared to be important at higher energies.

Banerjee (1992) used LS-DYNA3D to study the impact response of a composite plate using shell elements. For a low velocity impact of a steel sphere on a graphite/epoxy laminate, the contact force profile as well as the displacement history compared fairly well with predicted results from earlier investigations. However, out-of-plane shear stresses were not accurate because of the assumption of constant stress through the laminae and thus, delamination could not be modelled with the shell elements. Transverse matrix cracking was the initial planar mode of failure. This research did not correlate the LS-DYNA3D results with experimentally observed results.

Edlund (1993) modelled impact on composites using the LS-DYNA3D code as well and compared the results with experimental tests. Both shell and brick elements were used in the analysis. Good matching of the numerical impact force results with experimental results was achieved. The fibre and matrix damage modes were predicted using the Chang (Chang and Chang, 1987a,b) criteria with shell elements and delamination damage was predicted using a quadratic delamination criterion with solid elements. No comparison between experimentally observed damage and the DYNA3D results was made.

2.5.4 Summary of Numerical Models

Improvements in the transverse deformation behaviour of finite elements have been achieved. As in the case of analytical modelling, numerical modelling is capable of predicting, with relatively good accuracy, the elastic response of a composite plate subject to impact. Efforts to model fibre, matrix and delamination damage using numerical techniques have been made with researchers reporting some success. Some work has been done using LS-DYNA3D as a tool for composite transverse impact modelling; however, the previous work has not made extensive use of experimental comparison.

2.6 Summary

More standardized experimental testing needs to be performed to better understand the failure mechanisms in transversely impacted composite specimens. The existing failure theories for fibre and matrix failure require more experimental validation. Delamination failure theory requires extensive development; however, the consensus among researchers is that delamination is primarily a fracture-driven mechanism. More research must be conducted to better understand composite post-failure behaviour.

Researchers working with analytical and numerical composite impact models have sought to incorporate accurate transverse stresses and strains into their composite deformation behaviour with the goal of improving the accuracy of their overall results. Accurate delamination prediction is tied to accurate transverse stress prediction. The accurate prediction of damage necessitates the coupling of the damage and post-failure behaviour into the model response.

The numerical approach to the solution of composite impact has a distinct advantage over the analytical approach in that the numerical approach can be extended to problems with much more complex geometries and boundary conditions. The disadvantage of the numerical approach is that it is more computationally intensive.

2.7 Present Research

The goal of this thesis was to produce accurate low and high energy composite impact simulations. To assess the accuracy of the LS-DYNA3D simulations, extensive comparison was made with experimental data. The experimental parameters gathered for use in this thesis were impact force history, impact energy history, strain history and damage area.

The acquisition of the experimental data for this thesis is described in *Chapter 3 - Experimental Testing*. The results of both low and high energy impact simulations are presented in *Chapter 5 - Results*.

The effect of physical parameters such as the support base and the tup assembly and numerical parameters such as hourglassing control and contact stiffness are addressed in *Chapter 4 - Theoretical and Model Development*. The simulations performed in this chapter were purely elastic as it was not desired to evaluate the effect of damage at this stage. The effect of discretization on both the low and high energy impact simulations are addressed in *Chapter 5 - Results*. The effects of reduced fibre strength and of modifiable fibre post-failure rules in the high energy impact simulations are also discussed in the same chapter.

3.0 Introduction

This chapter describes the experimental work performed in support of the numerical simulations performed in this thesis. The composite coupons were instrumented with strain gauges and were impacted in a rig equipped with a load cell and velocity sensor. The coupons that suffered significant damage underwent non-destructive evaluation, *i.e.* visual, ultrasonic and radiographic inspection. The damaged coupons then underwent destructive evaluation, *i.e.* fractography. The various data gathered from the coupons served as a basis for verification of the numerical simulations.

3.1 Test Coupons

A set of 4 composite coupons were used in the experimental testing. The composite material selected was the Toray T800H/P2302-19 graphite fibre/toughened resin system. A 24-ply $[-45/0/45/90]_6$ quasi-isotropic lay-up was used for the coupons. The coupons were made according to Boeing Specification BSS 7260 (Boeing, 1988) which stated that the specimens must have planar dimensions of 152.4 mm x 101.6 mm (6.0 in. x 4.0 in). The coupons were cut from a larger panel which was made by precutting plies from a prepreg roll and laying them up by hand. The panel was then vacuum-bagged, cured in an autoclave in accordance to the manufacturer's instructions, and precisely cut to the required dimensions. The average thickness of the coupons was found to be 4.44 mm, corresponding to an average ply thickness of 0.185 mm. The in-plane orthotropic material properties of the Toray material are given in Tables 3.1 and 3.2 and were measured by Gaudert, Lee, Poon and Gould (1993).

Longitudinal Modulus, E_1	Transverse Modulus, E_2	Shear Modulus, G_{12}	Poisson's Ratio, ν_{12}
152.4 GPa	10.7 GPa	4.1 GPa	0.35

Table 3.1 - T800/P2302-19 In-Plane Elastic Properties

Longitudinal Tensile, X_T	Transverse Tensile, Y_T	Transverse Compressive, Y_C	Shear, S
2772.0 MPa	79.3 MPa	231.0 MPa	132.8 MPa

Table 3.2 - T800/P2302-19 In-Plane Strengths

The four coupons were instrumented with six 120 Ω strain gauges installed in one of two separate configurations. Both coupon configurations had identical dimensions, material and lay-ups. Two specimens were instrumented according to the first configuration (C1), which had 3 biaxial gauges mounted on the top surface of the coupon as shown in Figure 3.1(a). The two other specimens were instrumented according to the second configuration (C2) as shown in Figure 3.1(b), which had six uniaxial gauges mounted on the top surface of the coupon aligned in the fibre direction.

3.2 Test Set-Up

The impact testing system comprised the Dynatup 8200 instrumented drop weight system which was made up of the following components: a load-cell equipped tup with a GRC 730-I instrumentation package, a drop tower and a base assembly conforming to the BSS 7260 Class 1 Impact Specification. Different impact energies were generated by varying the release height of the instrumented tup. A special latch prevented repeated impacts.

Figure 3.1 - Composite Coupon Configurations

The load cell in the tup measured the load continuously and the PC connected to the load cell sampled 1024 data points over a 10 ms time interval. The velocity just prior to impact was measured using an infrared scanner. The scanner also served as a switch to start the load cell data acquisition program. The energy absorbed during impact was also calculated by the PC using the integral of the product of the impact force and the tup displacement. The tup displacement was calculated from the tup acceleration, which in turn was determined by dividing the impact force by the mass of the tup. The tup assembly had a mass of 5.84 kg, equal to the sum of the individual masses of the impactor, load cell and extra weights. The composite coupons were centred on the aluminum/plywood base by means of guide pins. The coupons were held in place by means of rubber-tipped clamps.

The acquisition of the strain gauge data required the use of six Pacific Instruments Model 8255 transducer amplifiers to amplify the strain signals. Strain gauges 1 - 4 were connected to NRC's 4-channel Nicolet Pro 20 storage oscilloscope while strain gauges 5 - 6 were connected to Carleton University's 2-channel Nicolet Pro 30 storage

oscilloscope. The Nicolet Pro 20 sampled 500 data points per channel in 10 ms (50 kHz/channel) and the Nicolet Pro 30 sampled 5000 data points per channel in 10 ms (500 kHz/channel). A schematic of the entire test set-up is presented in Figure 3.2 and a close-up drawing of the coupon, tup and base is presented in Figure 3.3. Photographs of the experimental apparatus are shown in Figures 3.4 - 3.6. Figure 3.4 shows the entire tup, Figure 3.5 shows a close up of the composite coupon and base and Figure 3.6 shows the data acquisition system used in the experiment.

The impact force and impact energy history data were useful for observing the overall impact response of the composite coupon while the strain history data was useful for observing the specimen response at discrete locations on its upper surface.

3.3 Testing

Two different energy levels were used in the experiments: a 5.4 J (4.0 ft-lb) low-energy level; and, a 30.4 J (22.4 ft-lb) high-energy level. From prior testing made by Vietinghoff (1993) at these energy levels, it was expected that the 5.4 J energy level would create minimal damage; however, the damage expected from the 30.4 J tests was expected to be considerably greater. A slight misalignment of 1.6 mm was noted between the centre of the coupon and the tup, along the minor axis of the coupon.

A Configuration 1 and a Configuration 2 coupon (Figure 3.1) were impacted at each of the two energy levels, thus totalling four tests. Repeatability *between* tests at a given energy level was assessed using the impact force history data, the impact energy history data and the strain signal from Gauge 4, which was in the same location and orientation for both coupon configurations. While only two tests were performed at each energy level, there was considerable confidence in the repeatability of those tests based on the work by Vietinghoff (1993). Repeatability *between* the results presented in this thesis and those presented by Vietinghoff (1993) was assessed by comparing damage area.

The Toray material properties were very consistent, as required by Boeing specifications.

3.4 Damage Characterization

Both coupons impacted at the high-energy level had their damage characterized using a variety of techniques. Surface damage on the specimens was visually assessed and internal damage was characterized using ultrasonic C-scans, radiography and fractography.

3.4.1 Visual Assessment

The front and back sides of the specimens were inspected for damage using the unaided eye. Damage characteristics were carefully noted and black and white photographs were taken of the specimens.

3.4.2 Ultrasonic Inspection

Ultrasonic inspection was employed to characterize the delamination damage within the specimens. The technique employed was the pulse-echo C-scan method coupled with Time-Of-Flight (TOF) software. With the TOF method, the time for the signal to travel to and return from a damage area is compared to the time it takes the signal to travel to and return from the back face of the specimen. The difference in the two time intervals allows the depth of the damage to be determined as well as the planar area of damage. The limitation of the technique is that the ultrasonic waves reflect only from the first damage area detected and thus cannot detect damage below the initially detected area. Specimens were scanned from both front and rear in order to partially overcome this limitation. Additionally, TOF is insensitive to small flaws such as fibre or matrix failure and, therefore, only detects delamination.

Figure 3.4 - Photograph of Tup Assembly

Figure 3.5 - Close-Up Photograph of Composite Coupon and Base

Figure 3.6 - Photograph of Instrumentation Set-Up

The set-up developed by Vietinghoff (1993) was used. The system employed for the TOF scans was the portable NDT Instruments Portascan PS1-3000. The system consists of a hand operated mini-scanning frame coupled with a Novascope 3000 ultrasonic thickness gauge and a personal computer. Immersion of the specimen and the transducer in water helped to alleviate problems associated with surface contact problems due to surface irregularities. A transducer of 5 MHz frequency and 5/16 inch diameter was chosen (NDT Instruments Model C14).

TOF C-scans are interpreted in the following way. Damage depth increments are represented by a colour bar. The colour bars and corresponding depth scales are shown underneath each set of scans. Clarity of scans performed from the rear surface is affected by signal loss attributed to the increased matrix damage towards the rear face and damage protrusion. The signal is lost in areas of extensive matrix damage because the signal is not reflected.

3.4.3 X-Radiography

X-radiography was used because it is an effective technique for detecting matrix cracks and, therefore, was an effective means of predicting matrix damage. An opaque penetrant was applied to the specimens in order to improve the X-ray images. The penetrant was applied by swabbing both sides of the specimen with the solution and letting the specimens sit at 50°C for 24 hours. The specimens were washed with water to remove any of the penetrant remaining on the surface. Standard X-ray film was used for the procedure (Kodak Industrex MAA-2).

3.4.4 Fractography

Fractography is a destructive evaluation technique used to characterize internal damage by sectioning impacted specimens, enabling delamination and matrix cracking

damage to be observed along a cut line. The specimens were sectioned longitudinally along three lines passing near the impact centre. The resulting sectioned faces were polished, inspected and photographed. The specimens were slowly cut with a diamond saw, this process did not cause any additional damage.

A fluorescent dye-penetrant coupled with ultraviolet light was used to highlight the damage. The dye-penetrant was swabbed on the polished specimen side and was wiped off just prior to photography. A long exposure under ultraviolet light captured the damage image while a second exposure under normal light superimposed the specimen image onto the damage image.

3.5 Material Characterization

It was determined from preliminary numerical analysis that additional material information would be required to more realistically model the impact event. Specifically, the use of shell elements to model the composite laminate, while computationally efficient, did not account for the local through-thickness contact stiffness of the composite. As well, the use of rigid shell elements to model the supporting base was not deemed realistic as the actual supporting base had an aluminum/plywood construction and thus was thought to have potentially significant compliance. Therefore, the two material parameters that were sought were the composite contact stiffness, k_{comp} and the plywood through-thickness modulus, E_{ply} .

A set of four composite mini-specimens were made with nominal dimensions of 32 mm x 32 mm. The mini-specimens were cut out of a larger $[-45/0/45/90]_3$, 152.4 mm x 101.6 mm specimen. They were mounted on a rigid support and placed under a 7.9 mm radius hemispherical tup head mounted on an Instron Model 1125 uniaxial multi-purpose testing machine. The specimens were loaded in contact compression up to approximately 20 kN while the force versus displacement curves were plotted. The slopes

were taken from the relatively linear middle portion of the curves, the average of the slopes gave an approximate value for the contact stiffness, $k_{\text{comp}} = 27.8 \text{ MN/m}$.

A similar procedure was used to determine the plywood through-thickness modulus. A set of four plywood specimens were made from the same batch of material used in the supporting base. The nominal dimensions of the specimens were 32 mm x 32 mm with a thickness of 32 mm. The specimens were mounted in the Instron machine and were compressed with a flat faced head overlapping the surface area of the entire specimen. The maximum load applied was 5 kN. The stiffness of each specimen was determined from the linear middle section of the slope of the force - displacement curve and the averaged E_{ply} was calculated to be 225 MPa using the relation $E = kL/A$.

3.6 Experimental Results

3.6.1 Impact Force, Impact Energy and Strain Histories

The impact force histories of the two low and high energy impacts are presented in Figure 3.7. The curves for the low energy impacts are in very good agreement with each other. The C2 (Configuration 2 coupon) force curve for the high energy impact shows higher oscillations after damage as compared to the C1 (Configuration 1 coupon) high energy impact force curve. It should be noted, however, that both curves have very similar profiles. The force history results presented here suggest that the impact testing produced repeatable results, especially at the lower impact energy level. Figure 3.8 shows the averaged impact force histories which are used for comparison with the simulation results later in the thesis.

The impact energy history curves for all four impacts are presented in Figure 3.9. As in the case of the low energy impact force history curves, the energy curves for low energy impact are in very good agreement with each other. The curves did not return to

zero because of energy losses due to friction in the impact mechanism and due to the formation of plastic indentations on the coupons. The impact energy curves for the high energy impacts also showed agreement with each other implying repeatability between tests. Their large residual energies reveal the formation of significant damage within the coupons. Figure 3.10 shows the averaged energy histories which are used in later comparison with predicted results.

The strain history curves for the low energy impacts on both the C1 and C2 coupons are plotted in Figures 3.11 and 3.12. The high energy impact strain curves are presented in Figures 3.13 and 3.14. It is interesting to note that the strain responses for gauges 1, 2 and 3 for coupon C2 under high energy impact (Figure 3.14) are not smooth parabolas compared to their low energy counterparts in Figure 3.12. The gauges may have been damaged due to the high energy impact loading; however, visual inspection did not reveal any debonding or other damage to the gauges. Furthermore, had any gauge been broken or debonded, its signal would have been lost or gone to zero, respectively. The strain signals from gauges 1, 2 nor 3 neither disappeared nor returned to zero. Therefore, the unusual strain histories in the C2 coupon likely reflected the true strain histories within the coupon at the specified locations. The observed strain jump in both gauges was probably the result of an unknown failure effect related to tensile fibre failure.

The gauge 4 location was common for all specimens and this enabled the repeatability of the strain signal to be assessed at both energy levels as shown in Figure 3.15. Both the low and high energy strain curves are in agreement with each other. The averaged gauge 4 results are presented in Figure 3.16.

Based on comparison of the impact force histories, impact energy histories and the gauge 4 strain gauge histories, it can be stated that the repeatability between the two low energy impacts existed and that repeatability between the two high energy impacts existed.

3.6.2 Visible Damage

Photographs of the impact faces of the C1 and C2 coupons subjected to the 30.4 J impacts are shown in Figures 3.17 and 3.18 respectively. In Figure 3.17, two cracks caused by fibre microbuckling can be seen to be emanating from the impact centre indicated by the cross in the middle of the picture. Figure 3.18 clearly shows a crack near the impact centre and plastic indentation near the cross mark.

Photographs of the back faces of coupons C1 and C2 are shown in Figures 3.19 and 3.20 respectively. Fibre breakage and matrix cracking are clearly visible in both photos. The approximate lengths of the fibre damage cracks are approximately 0.8 cm.

3.6.3 Ultrasonic Inspection

The delamination damage of the back and impact faces as detected by the C-scan for the C1 coupon impacted at 30.4 J is shown in Figures 3.21 and 3.22, respectively. The delamination damage for the C2 coupon is shown in Figures 3.23 and 3.24. A staircase pattern of damage is not clearly discernable from any of the figures with the possible exception of Figure 3.22. The scans for the other figures are characterized by large regions of black where the ultrasonic signal simply was not reflected. The delamination areas for all four scans are presented in Table 3.3. The projected damage areas, the greater of the back and impact face damage areas, compare well with the projected areas from Vietinghoff (1993) presented in Table 3.4, showing that repeatability exists between both sets of data.

	Back Face (mm ²)	Impact Face (mm ²)	Projected Area (mm ²)
C1 Coupon (30.4 J)	520	505	520
C2 Coupon (30.4 J)	509	530	530

Table 3.3 - Delamination Damage Area

	Projected Area (mm ²)
1 (30.4 J)	480
2 (30.4 J)	510
3 (30.4 J)	520
4 (30.4 J)	530

Table 3.4 - Vietinghoff Delamination Damage Area

3.6.4 X-Radiography

The C1 and C2 coupon X-radiography damage from the 30.4 J impacts is shown in Figures 3.25 and 3.26 respectively. In both figures, the heavily darkened central area reveals extensive matrix damage. Close inspection of both figures shows the fine lines of matrix cracks in the various ply orientations. The damage shape and area is similar for both specimens, damage area results are presented in the table below.

	Projected Area (mm ²)
C1 Coupon (30.4 J)	388
C2 Coupon (30.4 J)	408

Table 3.5 - Matrix Damage Area

3.6.5 Fractography

Fractography was the only means of destructive evaluation used to inspect the C1 and C2 coupons and was therefore performed last. Figures 3.27 and 3.28 show the results of the sectioning of the damaged specimens. In both figures, extensive matrix cracking and delamination damage is observed through the impact centre. These figures show how matrix damage links delamination damage.

Figure 3.7 - Experimental Impact Force Histories

Figure 3.8 - Averaged Experimental Impact Force Histories

Figure 3.9 - Experimental Impact Energy Histories

Figure 3.10 - Averaged Experimental Impact Energy Histories

Figure 3.11 - C1 Coupon Experimental Strain Histories (5.4 J)

Figure 3.12 - C2 Coupon Experimental Strain Histories (5.4 J)

Figure 3.13 - C1 Coupon Experimental Strain Histories (30.4 J)

Figure 3.14 - C2 Coupon Experimental Strain Histories (30.4 J)

Figure 3.15 - Gauge 4 Experimental Strain Histories

Figure 3.16 - Averaged Gauge 4 Experimental Strain Histories

Figure 3.17 - C1 Coupon Impact Face Visible Damage (30.4 J)

Figure 3.18 - C2 Coupon Impact Face Visible Damage (30.4 J)

Figure 3.19 - C1 Coupon Back Face Visible Damage (30.4 J)

Figure 3.20 - C2 Coupon Back Face Visible Damage (30.4 J)

Figure 3.21 - C1 Coupon Back Face C-Scan Image (30.4 J)

Figure 3.22 - C1 Coupon Impact Face C-Scan Image (30.4 J)

Figure 3.23 - C2 Coupon Back Face C-Scan Image (30.4 J)

Figure 3.24 - C2 Coupon Impact Face C-Scan Image (30.4 J)

Figure 3.25 - C1 Coupon X-Radiography Image, Full Scale (30.4 J)

Figure 3.26 - C2 Coupon X-Radiography Image, Full Scale (30.4 J)

All dimensions in mm.
Specimen thick. = 4.4 mm

Figure 3.27 - C1 Coupon Fractographic Inspection (30.4 J)

Section A - A

Section B - B

Section C - C

Section D - D

44 44
51

All dimensions in mm.

Specimen thick. = 4.4 mm

Figure 3.28 - C2 Coupon Fractographic Inspection (30.4 J)

CHAPTER 4 THEORETICAL AND MODEL DEVELOPMENT

4.0 Introduction

The purpose of this chapter is to present an overview of the theory behind the dynamic finite element software employed in this thesis and to evaluate the effect of various physical and numerical parameters on the model response. The knowledge gained from the studies was used to generate an improved model for accurate analysis of low energy elastic impact simulation. The effect of damage on the model response was intentionally not addressed in these early studies as efforts were focussed on understanding only the parameters that affected the elastic response of the model. The effect of damage on the model response is examined in the following chapter.

4.1 Finite Element Software

The numerical modelling of the experimental impact tests was performed with the LS-DYNA3D (Hallquist, 1991) finite element code. LS-DYNA3D is a vectorized explicit 3-D finite element program designed for the large deformation dynamic analysis of inelastic solids. The equations of motion are explicitly integrated with respect to time using the central difference method. The contact algorithm is a key aspect of the code and it permits sliding and gaps along defined interfaces.

4.1.1 Time Integration

The solution of the equations of motion requires the use of time integration which is performed using the finite difference approximation. The solution is subject to the given initial and boundary conditions. Numerical integration in finite element codes is

performed either implicitly or explicitly. Implicit methods are used most often in applications involving low-frequency motions, while explicit methods are used for modelling events involving high-frequency modes such as short-duration impact loading. The explicit method is particularly suitable for nonlinear material modelling and high-speed computing and is the method used in LS-DYNA3D.

Explicit methods are conditionally stable meaning that the time step must be smaller than a critical value to ensure stability of the solution. LS-DYNA3D uses the central difference method to estimate the velocity and acceleration in the neighbourhood of time, t . For the central difference method, the critical time step is given by:

$$\Delta t \approx \frac{2}{\omega_{\max}} \quad (4.1)$$

where $\omega_{\max}$ is the highest natural frequency of the system which is estimated using the highest element frequency at each time step (Hallquist, 1991). The critical time step is the size of the time step at which LS-DYNA3D marches through a simulation.

4.1.2 Contact Algorithm

Impact conditions are modelled in LS-DYNA3D by defining sliding interfaces along which the contacting bodies must slide without penetrating. One side of the interface is defined as a master side with its corresponding nodes designated as master nodes and the other interface is defined as a slave side with its corresponding nodes designated as slave nodes. A penalty method is used to model the interaction between both interfaces. The method involves placing normal interface springs between all penetrating nodes and the contact surface. At every time step, each slave node is checked for penetration through the master surface. When penetration is detected, a restoring force is generated and acts to move the penetrating node back to the free surface. The magnitude of this force is proportional to the extent of penetration, thus simulating the action of an interface spring. The interface spring stiffness of element i , λ_i , is based on

the volume, face area and bulk modulus of the element that contains the master segment:

$$\lambda_i = \frac{\xi_i K_i A_i^2}{V_i} \quad (4.2)$$

where K_i is the bulk modulus, A_i is the face area, V_i is the volume and ξ_i is a scale factor called the Contact Stiffness Penalty Factor that can be used to adjust the contact stiffness to ensure a stable solution. Using this approach, momentum is exactly conserved without the necessity of imposing impact and release conditions (Hallquist, 1991).

4.1.3 LS-DYNA3D Element Formulations

The composite coupons were modelled using Belytschko-Tsay four-noded quadrilateral shell elements with single point quadrature per lamina (Belytschko, Lin and Tsay, 1984). The Belytschko-Tsay elements were chosen for modelling the composite coupons because of their high computational efficiency and good accuracy compared to elements of higher order. The shell element model is based on point stress-strain relations, instead of stress or moment resultants. This approach also allows anisotropic materials to be modelled, where the material properties, stresses and strains are a discontinuous function of thickness. The shell element has six degrees of freedom at each node and is based on Mindlin-Reissner plate and shell theory (Reissner, 1945; Mindlin, 1951).

Single-point integrated Belytschko-Tsay elements are susceptible to hourglassing, which refers to zero energy vibration modes which arise due to element-centred in-plane integration of first-order isoparametric finite elements. Identification of these modes is difficult as they do not generate stresses and may be identified only by close inspection of the deformation behaviour of the entire mesh under consideration (Flanagan and Belytschko, 1980). A simple but effective hourglass control algorithm which is orthogonal to the rigid body modes on an element level is implemented in DYNA3D and does not compromise the consistency of the equations. The hourglassing modes can be

resisted by either a viscosity- or a stiffness-based control scheme and the degree of hourglass control can be set with by the control factor, ϕ . The default control mode is the viscosity-based scheme with ϕ set to 0.10.

It should be noted that Belytschko-Tsay elements do not accurately model the two transverse shear stresses and do not consider the through-thickness normal stress. As a consequence, a major limitation of these elements with respect to composite modelling is that delamination behaviour cannot be modelled.

Shell elements were also employed to discretize the impactor and supporting base for the initial impact model. The elements were defined using single-point quadrature. The Type 20 Rigid Material Model was used to define the constitutive material properties of both components. The use of the rigid model avoided the need to fully characterize the material properties of tup and base. This was felt to be a reasonable approach for the initial model.

Eight-node solid continuum hexahedron elements were employed to model the tup assembly and the supporting base. The hexahedron elements employed utilized single-point quadrature resulting in substantial savings in computing time over eight-point integration. As in the case of the Belytschko-Tsay shell elements used in this thesis, hourglassing control was required to resist the zero-energy modes (Hallquist, 1991). The same viscosity-based hourglassing control scheme was employed for the hexahedron elements for the impact models in this thesis. The Type 1 Elastic Material Model was used to define the constitutive material behaviour of the tup assembly and supporting base hexahedron elements.

4.2 Composite Damage Material Model

4.2.1 Model Formulation

The composite coupon material properties were defined using the LS-DYNA3D Type 22 Composite Damage Material Model. The composite shell elements were defined with a single integration point for each lamina, totalling 24 through-thickness integration points per element. At each integration point, a lamina orientation was assigned to the shell model in accordance with the specified $[-45/0/45/90]_s$ lay-up. The planar stiffness of the coupon was calculated by LS-DYNA3D based on classical composite lamination theory.

4.2.2 Failure Envelopes

The material model failure criteria are based on the work of Chang and Chang (1987a,b) and were defined in Chapter 2. In order to verify the numerical model, a set of failure envelopes was plotted to compare the numerical implementation of the Chang criteria with theory. A single element was defined with a single 0° ply. Thus, the σ_{11} stress was defined in the longitudinal or fibre direction and the σ_{22} stress was defined in the transverse direction. The elements were loaded under displacement-controlled boundary conditions. The test elements for each envelope are shown in Figure 4.1. The strength values employed for the envelopes were those of the T800/P2302-19 material (please refer to Table 3.2).

Figure 4.1 - Test Element Load Cases

The theoretical and numerical biaxial failure envelopes, σ_{11} vs σ_{22} , are compared in Figure 4.2 and are in good agreement with each other. The comparison of the fibre failure envelopes, σ_{11} vs σ_{12} are presented in Figure 4.3. The predicted envelope is in reasonable agreement with the theoretical failure envelope; however, LS-DYNA3D tends to underpredict the shear failure stress as the pure shear stress state is approached. It is possible that the combination of the 0° orientation of the ply of the composite test element and the associated boundary conditions was not conducive to accurate modelling of shear failure. In practice, the fibre failure occurs towards the right side of the envelope as the great majority of the ply load is carried by the fibre. Thus, it is more important that the right side of the fibre failure envelope is in better agreement than the left side. The matrix failure envelope, σ_{22} vs. σ_{12} , as shown in Figure 4.4, is also in reasonable agreement with theory. Overall, it appears that the LS-DYNA3D implementation of the Chang failure criteria is good.

Figure 4.2 - Comparison of Predicted and Theoretical Biaxial Failure Envelopes

Figure 4.3 - Comparison of Predicted and Theoretical Fibre Failure Envelopes

Figure 4.4 - Comparison of Predicted and Theoretical Matrix Failure Envelopes

4.2.3 Model Post-Failure Rules

The existing post-failure rules implemented in the Type 22 model for fibre failure, matrix cracking (tensile) and matrix crushing (compression) are directly tied to the Chang failure criteria (Chang and Chang, 1987a,b) and are based on a paper by Humphreys (1981). The stress unloading is effectively instantaneous for all modes as shown in Figure 2.11(c). The rules are summarized in Table 4.1.

Fibre Failure, $F_{\text{fibre}} \geq 1$	$E_1 = E_2 = G_{12} = \nu_1 = \nu_2 = 0$
Matrix Cracking, $F_{\text{matrix}} \geq 1$	$E_2 = G_{12} = \nu_1 = \nu_2 = 0$
Matrix Crushing, $F_{\text{comp}} \geq 1$	$E_2 = \nu_1 = \nu_2 = 0$

Table 4.1 - Composite Damage Model Post-Failure Rules

4.3 Initial Model Formulation

The formulation of the finite element composite impact model is described. The simulations were models of preliminary tests carried out at the NRC in June 1992 using coupons with a $[-45/0/45/90]_s$ lay-up and made of the Toray material (Straznicky, 1992). The impact energy level that was simulated was 5.2 J, which was considered low enough to generate a primarily elastic response with little damage. The mass of the tup was 5.44 kg and the impact velocity was 1.38 m/s.

The simple elastic impact model, as shown in Figure 4.5, was defined using three parts: the tup head, the supporting base and the composite coupon. Both the tup head and the supporting base were defined using Belytschko-Tsay shell elements and the Type 20 Rigid Material Model; the composite coupon was defined using Belytschko-Tsay shell elements and the Type 22 Composite Damage Material Model. The composite coupon was defined using 28×42 uniformly-sized elements giving an element size of approximately 2 mm x 2 mm. For discussion purposes, the model was designated *Rigid Tup+Base* as the tup and base were both modelled as rigid.

To reduce computational time in these preliminary simulations, quarter symmetry was employed in the model definition. The quarter symmetry did not in fact exist in the laminate due to the existence of $\pm 45^\circ$ plies orientations, but the use of quarter symmetry was considered acceptable for these preliminary elastic analyses. The displacement boundary conditions enforcing quarter symmetry for all three components along the $y = 0$ were $d_x = \theta_x = \theta_z = 0$ and along the $x = 0$ were $d_y = \theta_y = \theta_z = 0$. The tup head was given a mass equal to one quarter of 5.44 kg and an initial velocity of 1.38 m/s. The supporting base was further displacement constrained such that $d_z = 0$. All three parts were defined to be initially in contact with each other. Contact interfaces were defined between the tup and the coupon upper surface and between the coupon lower surface and the base upper surface.

The predicted impact force history for the simple elastic impact model is presented in Figure 4.6 and shows that the software had the capability to model elastic impact.

4.4 Parametric Studies

The objective of this thesis was to generate accurate low energy elastic and high energy damage-inducing simulations. As part of this objective, it was necessary to identify the key physical and numerical parameters which influenced results of the low energy simulations. The high energy simulations would not be accurate if the low energy simulations were not accurate. A series of low energy parametric simulations was performed to better understand the physical dynamics of the problem and the effect of various numerical parameters on the impact simulation. The results from these parametric studies are presented in the following sections.

4.4.1 Physical Parameters

Considerable uncertainty existed initially regarding which physical elements of the impact system were important to model and so a study was undertaken to determine the effect of different physical details on the dynamics of the system. The details considered included the dynamic response of the composite coupon alone, the effect of introducing a flexible tup and base and the effect of the composite through-thickness compliance. The effect of the rubber-tipped clamps used to grip the coupon and the effect of the compliance of the tup head were also evaluated. The global parameter chosen for evaluating the effect of the various physical details on the model response was the impact force history.

Figure 4.5 - Rigid Tup and Base Model - Exploded View

Figure 4.6 - Impact Force History for Rigid Tup and Base Model (5.2 J)

The first model (*Rigid Tup+Base*) has been previously discussed and is shown again in Figure 4.7(a) for comparison purposes. This simplest model solely evaluated the response of the coupon as it was the only part of the system modelled as flexible. Each of the next three subsequent models represented an increase in the complexity of the model and an increase in fidelity to reality compared to the previous one.

The next model (*Hex Base*) consisted of the same coupon and rigid tup meshes as the first model, however the rigid supporting base elements was replaced by a mesh utilizing flexible hexahedron elements to model the response of the aluminum/plywood base more accurately, as shown in Figure 4.7(b). Thus, in this model, both the coupon and the base were flexible.

The third model (*Hex Base+Simp Tup*) was a modification of the *Hex Base* model by adding a cylindrical rod representing the tup mass and a numerical spring to model the local through-thickness stiffness of the Toray material, as shown in Figure 4.7(c). This spring was needed as the shell model of the coupon had no stiffness in the transverse direction (see Section 3.5); the stiffness of the spring, k_{spring} , was made equal to the experimentally determined composite contact stiffness, k_{comp} .

$$k_{comp} = k_{spring} = 27.80 \text{ MN/m} \quad (4.3)$$

The spring was shaped as a quarter of a disc and placed between the tup head and the cylinder containing the mass. The radius of the spring was 7.9 mm, the same as the tup radius, and the spring was defined with a thickness of 2.0 mm. From the relation $E = kL/A$, the spring modulus, E_{spring} , was set equal to 283.6 MPa.

In the *Hex Tup+Base* model, virtually the entire tup was discretized using solid elements to accurately model the elastic response of the tup, as shown in Figure 4.7(d). Both the through-thickness spring and the rigid tup head were retained from the *Hex Base+Simp Imp* model. This model was subsequently selected as the *Baseline* model for further parametric simulations.

The impact force histories of these four simulations along with the experimental data are plotted in Figure 4.8. It is evident from the curves that the increasing physical complexity of the system resulted in a more compliant force history response. The influence of each parameter on the model can be deduced from Table 4.2. The results show that the flexible base, the through-thickness spring and the flexible tup models all contributed significantly to rendering the system more compliant.

	Max. Force (kN)	Period (ms)
<i>Rigid Tup+Base</i>	6.7	3.6
<i>Hex Base</i>	6.2	4.1
<i>Hex Base+Simp Tup</i>	5.5	4.6
<i>Hex Tup+Base - Baseline</i>	5.2	4.6

Table 4.2 - Maximum Force and Period Comparison between Models

Two additional simulations investigating the effects of physical detail were performed. The *Baseline* model was modified to evaluate the effect of the rubber-tipped clamps on the impact force history response of the model. The spindles served to lightly clamp the coupon in place on the supporting base. It was found that there was no difference between the impact force history results of the model with the spindle as compared to the *Baseline* model. This suggests that the spindle had a minor or no effect on the system dynamics. As well, the influence of a more detailed and computationally expensive hexahedron element-meshed tup head was investigated in which the rigid tup head of the baseline model was substituted with a modified tup head. The use of the solid continuum elements for the tup head slightly reduced the maximum impact force but did not reduce the period. This slight softening of the force response was expected.

(a) Rigid Tup and Base

(b) Hex Element Base

(c) Hex Element Base and Simplified Tup

(d) Hex Element Tup and Base

Figure 4.7 - Discretization of Models

Figure 4.8 - Effect of Physical Details on Impact Force History (5.2 J)

4.4.2 Numerical Parameters

Various numerical parameters were investigated with a view to better understand their effect on the numerical model. The effects of refining the coupon mesh, modelling the entire geometry, different hourglass control schemes and varying the contact stiffness within the impact model were examined. Prior to evaluating the hourglassing control and contact stiffness parameters, an investigation was conducted into the strain state within the coupon mesh.

The effect on the model response of a more refined mesh in the coupon was investigated by comparing the impact force history results of a model with a 50 x 74 element mesh with elements approximately 1 mm x 1 mm, with the results of the 28 x 42 *Baseline* mesh with elements approximately 2 mm x 2 mm. Figure 4.9 shows that the maximum impact force of the more refined model was slightly lower than that of the

Baseline model suggesting the more refined mesh generated a slightly softer response.

The results of modelling the full geometry are shown in Figure 4.10 where the fully defined model also a slightly lower peak impact force and a slightly longer period than the quarter symmetry *Baseline* model. The increased compliance can be attributed to the $\pm 45^\circ$ plies being fully modelled rather than artificially imposing quarter symmetry.

The strain versus position behaviour of the 28 x 42 *Baseline* coupon mesh was investigated in order to evaluate the strain response of the mesh and to identify any anomalies that may exist. It was hoped that this evaluation would reveal evidence of hourglassing behaviour, should any be occurring in the mesh. As previously mentioned, hourglassing modes are often difficult to identify. The strain versus position behaviour was observed along five so-called cuts in the coupon mesh. Those cuts are illustrated in Figure 4.11. On the subsequent strain versus position graphs, the element numbering always commences from either the left or the bottom edge of the mesh as shown in Figure 4.11, depending on the cut. The time at which the strain plots were taken was at 2.0 ms after impact. This point in time was chosen as the coupon was close to its maximum deflection.

Figure 4.12 shows the global upper surface x- and y-strains along Cut 1. The x-strain is relatively smooth; however the y-strain, while lower in magnitude than the x-strain, does not exhibit smooth behaviour along the cut. Figure 4.13 shows the upper surface strains along Cut 2. Both the x- and the y-strains exhibit non-smooth behaviour as well. Along Cut 3 (Figure 4.14), both the x- and y-strains are very smooth with the maximum strains occurring at the impact centre. The strains predicted through Cuts 4 and 5 were smooth as well (Figure 4.15). The lack of smoothness in the first four strain curves reveal the existence of hourglassing in the coupon mesh. The strain versus position results from Cuts 3, 4 and 5 were smooth and this likely was related to the fact that the cuts emanated in a radial direction from the impact centre.

Figure 4.9 - Effect of Coupon Mesh Refinement on Impact Force History (5.2 J)

Figure 4.10 - Effect of Full Model Discretization on Impact Force History (5.2 J)

A parametric study into the effect of various hourglassing control schemes on the *Baseline* model's behaviour was conducted using both impact force histories and strain versus position plots. The y-strain versus position through Cut 1 was used to investigate the strain behaviour as this strain plot exhibited typical hourglassing behaviour.

The default hourglass control setting on the *Baseline* model was viscosity-based hourglassing control with a control factor, ϕ , set to 0.10. The effect of varying the control factor from 0.10 to 0.15 was investigated using both viscosity- and the stiffness-based hourglass control. The hourglass control factor, ϕ , had a recommended maximum value of 0.15 to avoid potential instabilities (Hallquist, 1993). Figure 4.16 shows that the type of hourglass control had more effect on the global impact force response than the ϕ value, with the stiffness-based scheme markedly stiffening the model response. Figure 4.17 shows that stiffness-based hourglass control slightly improved the smoothness of the y-strain versus position plot through Cut 1. However, this slight improvement came at the expense of measurably stiffening the global impact force response of the model. Decreasing the element size within the coupon from approximately 2 mm x 2 mm (28 x 42 mesh) to approximately 1 mm x 1 mm (50 x 74 mesh) had a much more marked effect on smoothing the y-strain behaviour through Cut 1, as shown in Figure 4.18. The benefit of this approach was that the global model response was only slightly softened; however, increased computational expense was a natural consequence of mesh refinement.

An investigation into the effect of the contact stiffness on the global and local strain response of the impact model was also conducted. High contact stiffness is known to excite hourglass modes in some impact simulations. The effect of reducing the Contact Stiffness Penalty Factor, ξ , on the y-strain versus position behaviour through Cut 1 was dramatic. With $\xi = 0.01$, the y-strain curve response was rendered completely smooth when compared to the default value $\xi = 1.0$, as shown in Figure 4.19. The effectiveness of using reduced contact stiffness on the y-strain behaviour further reinforced the conclusion that hourglassing was occurring in the mesh. Figure 4.20 showed that decreasing the Contact Stiffness Penalty Factors between the impact interfaces excessively

softened the model's global impact response.

Stiffness-based hourglassing control was ineffective because it modified the model's global response but did not significantly reduce hourglassing in the model. Reducing the Contact Stiffness Penalty Factors between the interfaces reduced hourglassing significantly in the mesh; however, this caused an unacceptable decrease in the global stiffness of the model. Overall, mesh refinement appears to be the most effective way of decreasing the effect of hourglassing in the model without modifying the global response. The disadvantage of this approach is that it is more computationally expensive.

Figure 4.11 - Baseline Composite Coupon Mesh - 28 x 42 Elements

Figure 4.12 - Global Upper Surface Strains Along Cut 1 @ 2.0 ms (5.2 J)

Figure 4.13 - Global Upper Surface Strains Along Cut 2 @ 2.0 ms (5.2 J)

Figure 4.14 - Global Upper Surface Strains Along Cut 3 @ 2.0 ms (5.2 J)

Figure 4.15 - Global Upper Surface Strains Along Cuts 4 and 5 @ 2.0 ms (5.2 J)

Figure 4.16 - Effect of Hourglass Control on Impact Force History (5.2 J)

Figure 4.17 - Effect of Hourglass Control on Y-Strain Along Cut 1 @ 2.0 ms (5.2 J)

Figure 4.18 - Effect of Mesh Refinement on Y-Strain Along Cut 1 @ 2.0 ms (5.2 J)

Figure 4.19 - Effect of Contact Stiffness on Y-Strain Along Cut 1 @ 2.0 ms (5.2 J)

Figure 4.20 - Effect of Contact Stiffness on Impact Force History (5.2 J)

4.5 Full Model Configuration

Based on the parametric studies performed on the numerical elastic impact model, several observations were made:

- The supporting base and the instrumented tup, including the tup head, should be modelled with solid continuum elements and not rigid elements in order to account for the flexibility of these components;
- The local indentation stiffness of the composite specimen should be accounted for with a numerical spring made of solid elements as the composite coupon shell elements themselves had no transverse flexibility;
- The modelling of the rubber clamp had no effect on the results;
- Modelling the entire geometry, while increasing the computational cost, resulted in a small increase in model compliance;

- Stiffness-based hourglassing control in the composite coupon mesh tended to increase the stiffness of the response without appreciably reducing the level of hourglassing;
- Reduction of the contact stiffness reduced the level of hourglassing in the coupon mesh, but at the expense of excessively softening the global response;
- Refinement of the composite mesh slightly increased the global compliance of the model and reduced the level of hourglassing in the coupon, but at higher computational expense.

It was decided that a full model instead of a quarter symmetry model would be needed since any impact damage in the composite would be non-symmetric with the particular lay-up used, therefore requiring full discretization. In addition, a full model would permit consideration of a slight misalignment of the tup with the centre of the specimen that occurred during the experiments (discussed in Chapter 3).

Based on the above observations, an improved finite element was defined and incorporated the following elements:

- A solid element tup and supporting base;
- A numerical spring simulating the composite local indentation stiffness;
- The clamping spindles were not modelled;
- Default viscosity-based hourglass control;
- Default contact stiffness;
- Full symmetry.

This final model is illustrated in Figure 4.21.

Figure 4.21 - Discretization of the Improved Model

5.0 Introduction

This chapter presents the results from the low energy and high energy impact simulations using the improved model defined in Chapter 4. Three sets of simulations are presented in this chapter. The first set of low and high energy simulations was performed to assess the convergence of the impact solution by modifying the composite coupon mesh discretization. The second set of simulations was conducted to evaluate the effect of fibre strength on the high energy model response. The last set of simulations examined the effect of modified fibre post-failure rules within the LS-DYNA3D Composite Damage Material Model on the high energy model response. The predicted results were compared to experimental impact force histories, impact energy histories, strain histories and damage areas.

5.1 Mesh Convergence Simulations

Based on the analysis of the results from the previous chapter, a final configuration model was adopted as defined in Chapter 4. A further mesh convergence study was performed using a set of low energy and high energy impact simulations. The simulations were based on experimental tests conducted at the NRC in November 1993, which were discussed in Chapter 3. A set of four LS-DYNA3D simulations were run at the 5.4 J impact energy level ($v_i = 1.37$ m/s). Four simulations were also run at the 30.4 J impact energy level ($v_i = 3.23$ m/s) which were identical to the low energy simulations with the exception of the higher initial impact velocity of the tup. Note that the mass of the tup assembly increased to 5.88 kg for the November 1993 tests due to a procedure change. The simulation parameters are summarized in Table 5.1. The element

dimensions listed in the table are approximate.

The purpose of the first three runs at each energy level was to compare the influence of mesh discretization on the convergence of the impact force solution. Simulations 3L and 3H utilized the finest mesh with the size and location of eight elements arranged to coincide with the size and location of the strain gauges of the actual experimental coupons. This design enabled direct comparison of the strain data between the simulations and the experiment. The mesh design was efficient in that the locations of the strain gauges of both the Configuration 1 and the Configuration 2 coupons were captured by a single mesh. In Simulations 4L and 4H, failure was prevented by artificially increasing the strengths by a factor of 1000. This fourth pair of simulations was used as a reference for the simulations which considered failure.

	Number of Elements	Approximate Element Size	Failure Implemented
Simulation 1L (5.4 J)	216	8 mm x 8 mm	YES
Simulation 2L (5.4 J)	864	4 mm x 4 mm	YES
Simulation 3L (5.4 J)	3800	2 mm x 2 mm	YES
Simulation 4L (5.4 J)	3800	2 mm x 2 mm	NO
Simulation 1H (30.4 J)	216	8 mm x 8 mm	YES
Simulation 2H (30.4 J)	864	4 mm x 4 mm	YES
Simulation 3H (30.4 J)	3800	2 mm x 2 mm	YES
Simulation 4H (30.4 J)	3800	2 mm x 2 mm	NO

Table 5.1 - Mesh Convergence Simulations

Figure 5.1 shows the relative sizes of the elements used in the simulations listed above.

Figure 5.1 - Element Sizes Used for Coupon Discretization (Actual Size)

5.1.1 Impact Force Histories (5.4 J)

The measured and the predicted impact force results for the 5.4 J impact energy level are presented in Figure 5.2. At this low-energy impact level, mesh convergence of the predicted impact force history results was achieved. The Simulation 1L (216 elements) curve has a slightly lower peak force and a slightly longer impact period as compared to the Simulation 3L (3800 elements) curve. Both of the 3800 element solutions, Simulation 3L (with failure) and Simulation 4L (without failure) appear to have identical force curves, suggesting no significant damage was predicted. This was in general agreement with the experiments. A small amount of matrix damage was predicted in the vicinity of the impact centre. The peak impact force predicted by Simulation 3L is 5.0 kN and the predicted period is 4.7 ms. The averaged experimental peak impact force is 5.2 kN and the averaged period is 5.0 ms. Therefore, the period is underpredicted by 6% and the peak force is underpredicted by 4% as compared to experiment.

5.1.2 Impact Energy History (5.4 J)

The experimental impact energy history for the low energy impact has a peak value of approximately 5.6 J and a residual energy of approximately 1.0 J. The simulations predicted a peak impact energy of 5.4 J and a residual energy of 2.0 J. The residual energy in the simulations may be due to entrapped vibrational energy.

5.1.3 Strain Histories (5.4 J)

Comparing the strain response of the Simulation 3L model (3800 elements) to the experimental data shows a more precise picture of the predicted local response to the impact. The comparisons of only the first two gauges for both coupon configurations are presented; the comparisons are representative of the agreement between experiment and model. Comparisons of the experimental and predicted strain histories for gauges 1 and 2 of the Configuration 1 specimen are presented in Figures 5.4 and 5.5. The predicted and experimental strain histories for gauges 1 and 2 of the Configuration 2 specimen are shown in Figure 5.6. Based on comparison of the experimental and predicted strain histories, LS-DYNA3D mildly underpredicts the period for all the gauges presented and tends to mildly overpredict the peak strain near the impact centre.

Figure 5.2 - Effect of Coupon Discretization on Impact Force History (5.4 J)

Figure 5.3 - Effect of Coupon Discretization on Impact Energy History (5.4 J)

Figure 5.4 - Simulation 3L Strain History for Gauge 1 of C1 Coupon (5.4 J)

Figure 5.5 - Simulation 3L Strain History for Gauge 2 of C1 Coupon (5.4 J)

Figure 5.6 - Simulation 3L Strain Histories for Gauges 1 & 2 of C2 Coupon (5.4 J)

5.1.4 Impact Force Histories (30.4 J)

The experimental and the numerical impact force results for the 30.4 J impact energy level are presented in Figure 5.7. The Simulation 1H (216 elements) and Simulation 2H (86+ elements) impact history force curves show no evidence of failure, although matrix cracking damage was observed in the coupon elements of both simulations. In contrast, the Simulation 3H (3800 elements) curve shows clear evidence of failure as evidenced by the sharp drop in load at 1.9 ms. Extensive fibre and matrix damage was observed in the coupon elements. The Simulation 4H (3800 elements) run with failure disabled gave a response similar in profile to those of the Simulation 1H and 2H runs. From these results, it is apparent the global response of the model is dependent on the size of element used to discretize the coupon. Therefore, convergence of the impact force response, defined as the approach to a stable solution with increasing coupon discretization, was not attained.

5.1.5 Impact Energy Histories (30.4 J)

The impact energy curves are plotted in Figure 5.8. The residual energy for Simulations 1H and 2H is approximately 12.0 J. This value likely reflects the presence of extensive matrix damage, as well as the entrapment of vibrational energy in the various system components. The residual energy of Simulation 3H was higher, at approximately 16.0 J, reflecting the creation of additional damage in the coupon in the form of fibre breakage.

5.1.6 Strain Histories (30.4 J)

Strain histories from Simulation 3H (3800 elements) were compared to experimental strain histories obtained from the 30.4 J impacts. As with the predicted low energy results, the strains from the first two strain gauge locations of each configuration

are presented as typical results. Comparisons between the experimental and predicted strain histories for gauges 1 and 2 of the Configuration 1 specimen are presented in Figures 5.9 and 5.10, respectively. Figures 5.9 and 5.10 clearly show predicted failure; the predicted strain responses for both gauges 1 and 2 match the experimental curves quite closely until failure. After failure, the predicted gauge 1 and 2 responses are highly oscillatory, likely due to the complete loss of stiffness at failure within the numerical model. Comparisons of the experimental and predicted strain histories for gauges 1 and 2 of the Configuration 2 specimen are presented in Figures 5.11 and 5.12, respectively. The predicted strain gauge responses also show failure and while highly oscillatory, they qualitatively match the shape of the experimental strain curve.

5.1.7 Impact Element Stress Histories (30.4 J)

The top and bottom ply fibre and transverse stress histories for a coupon element located at the impact centre are presented in Figure 5.13. The histories enable the effect of the failure and post-failure rules, as implemented in the Composite Damage Model, to be observed. At point 1 on the figure, the bottom ply transverse stress, which was positive, drops to zero after failure, indicating matrix cracking failure. At point 2, the top ply fibre stress exceeds the indicated fibre compressive strength. No failure occurs as no compressive fibre failure criteria exists within the Composite Damage Model. Points 3 and 4 show bottom ply fibre tensile failure and top ply matrix crushing failure, respectively, occurring virtually simultaneously. Point 5 shows the top ply fibres absorbing load in the form of higher stress, due to the load being shed by the failed lower surface ply/plies.

Figure 5.7 - Effect of Coupon Discretization on Impact Force History (30.4 J)

Figure 5.8 - Effect of Coupon Discretization on Impact Energy History (30.4 J)

Figure 5.9 - Simulation 3H Strain History for Gauge 1 of C1 Coupon (30.4 J)

Figure 5.10 - Simulation 3H Strain History for Gauge 2 of C1 Coupon (30.4 J)

Figure 5.11 - Simulation 3H Strain History for Gauge 1 of C2 Coupon (30.4 J)

Figure 5.12 - Simulation 3H Strain History for Gauge 2 of C2 Coupon (30.4 J)

Figure 5.13 - Impact Element Stress Histories (30.4 J)

5.2 Modified Fibre Strength Simulations

The high energy impact force history results presented in the preceding section showed a lack of convergence with mesh refinement. The results were dependent on the amount of discretization employed for the composite coupon mesh. The element size controlled the failure threshold of the coupon. It was thought that if the failure threshold depended on the level of coupon discretization, then the failure threshold for the coupon must also certainly vary with material strengths. The most important material strength with respect to the overall strength of a composite is the tensile fibre strength. Therefore, a parametric study was performed to examine the effect of reducing the tensile fibre strength on the failure threshold of the coupon. This artificial reduction in failure threshold was analogous to further reducing the size of the coupon elements. Further simulations utilizing coupon elements smaller than 2 mm x 2 mm were not performed due to considerations of computational expense. A further motivation for evaluating the effect of reduced fibre strength was the fact that the predicted failure point was delayed compared to the experimental failure point (see Figure 5.8). It was of interest to determine if the predicted failure threshold could be artificially lowered to match the experimental threshold.

Simulations were performed using Simulation 3H meshed with the 3800 elements under high energy impact (30.4 J). The tensile fibre strength was artificially lowered to 75% and 50% of the measured fibre strength. The resulting impact force histories are presented in Figure 5.14. The impact force failure point for the simulation with full tensile fibre strength is 12.4 kN at 1.9 ms; the failure point for the simulation with 75% tensile fibre strength is 8.8 kN at 1.1 ms; and the failure point for the simulation with 50% tensile fibre strength is 5.5 kN at 0.7 ms. The experimental failure point is 11.2 kN at 1.7 ms, which is significantly below that of the simulation with full fibre strength. Figure 5.15 shows the residual energy increase, *i.e.* increased damage, with a decreasing failure threshold.

It is clear that the impact force history results are very dependent on the failure stress. An examination of the predicted fibre failure on the lower surface of all three simulations at 3.0 ms, shown in Figure 5.16, demonstrates the fibre damage increasing in extent with decreasing fibre strength. The mesh becomes "unzipped" along the longitudinal axis due to lowered failure thresholds. The fibre failure initiation occurs in the bottom ply as a result of the high fibre stress concentration about the impact centre as shown for Simulation 3H/1.00 X_T in Figure 5.17. The large region of zero stress in the corresponding matrix stress plot for Simulation 3H/1.00 X_T in Figure 5.18 indicates that considerable matrix failure has preceded fibre failure. Based on the observation that considerable matrix damage preceded fibre damage and that the slope of the impact force curve is relatively unchanged until fibre failure occurs, it can be stated that matrix damage didn't alter the impact force response of Simulation 3H/1.00 X_T . The fibre damage appears to propagate in directions of the maximum strain gradient based on the lower surface strain distributions shown in Figures 5.19 and 5.20.

Figure 5.14 - Effect of Reduced Fibre Strength on Impact Force History (30.4 J)

Figure 5.15 - Effect of Reduced Fibre Strength on Impact Energy History (30.4 J)

(a) 3H/1.00 X_T

(b) 3H/0.75 X_T

(c) 3H/0.50 X_T

Bottom Ply (-45°) - Simulation 3H Set @ 3.0 ms

Figure 5.16 - Effect of Reduced Fibre Strength on Fibre Breakage (30.4 J)

Bottom Ply (-45°) @ 1.8 ms

Figure 5.17 - Fibre Stress Fringe Plot from Simulation 3H/1.00 X_T (30.4 J)

Bottom Ply (-45°) @ 1.8 ms

Figure 5.18 - Transverse Stress Fringe Plot from Simulation 3H/1.00 X_T (30.4 J)

$T = 1.8 \text{ ms}$

Figure 5.19 - Bottom Surface X-Strain Fringe Plot of Simulation 3H/1.00 X_T (30.4 J)

$T = 1.8 \text{ ms}$

Figure 5.20 - Bottom Surface Y-Strain Fringe Plot of Simulation 3H/1.00 X_T (30.4 J)

5.3 Modified Post-Failure Simulations

5.3.1 Development of the Modified Post-Failure Model

The lack of convergence of the impact force solution in the high energy impact simulations spurred the development of a modified post-failure model. If the fibre failure threshold was low enough, either due to small element size or a lowered tensile fibre strength, then the fibre damage would spread catastrophically. The reason why fibre damage would spread too rapidly, essentially overpredicting damage, was felt to be partially due to the severity of the fibre post-failure rules which require all stresses and moduli of an integration point within an element to be set to zero at failure. The damage progression problem was compounded by numerical artifacts due to the discrete nature of the meshing. The fibre post-failure rules are listed below:

$$\sigma_{11} = \sigma_{22} = \sigma_{12} = \sigma_{23} = \sigma_{31} = 0 \quad (5.1)$$

$$E_1 = E_2 = G_{12} = \nu_1 = \nu_2 = 0 \quad (5.2)$$

To investigate the effect of post-failure rules, the Composite Damage Model was modified such that the user could define the fibre post-failure behaviour. A set of parametric simulations was performed in order to assess the effect of post-failure behaviour on the model response.

The failure rules were modified such that the post-failure fibre stress $(\sigma_{11})_{\text{post-fail}}$ and the post-failure fibre modulus $(E_1)_{\text{post-fail}}$ could be independently set by two parameters. The fibre post-failure stress and modulus parameters, α_s and α_E , respectively, were defined to affect *only* σ_{11} and E_1 , respectively, after fibre failure. The parameters are defined as follows:

$$\alpha_s = \frac{(\sigma_{11})_{post-fail}}{X_T} \quad (5.3)$$

$$\alpha_E = \frac{(E_1)_{post-fail}}{E_1} \quad (5.4)$$

This set of rules allowed the user to simulate the effect of undamaged fibres remaining after failure at an integration point (ply or lamina) within an element. The parameters essentially allowed the residual fibre strength and stiffness to be set after failure. The transverse and shear stresses are still set to zero after fibre failure as in the unmodified Composite Damage Model. This modified fibre post-failure behaviour is an idealization; however, it does allow the effect of fibre post-failure rules to be examined.

	No. of Elements	$\alpha_s = (\sigma_{11})_{post-fail} / X_T$	$\alpha_E = (E_1)_{post-fail} / E_1$
Simulation 1H-00	216	0.0	0.0
Simulation 1H-05	216	0.05	0.05
Simulation 1H-10	216	0.10	0.10
Simulation 1H-20	216	0.20	0.20
Simulation 2H-00	864	0.0	0.0
Simulation 2H-05	864	0.05	0.05
Simulation 2H-10	864	0.10	0.10
Simulation 2H-20	864	0.20	0.20
Simulation 3H-00	3800	0.0	0.0
Simulation 3H-05	3800	0.05	0.05
Simulation 3H-10	3800	0.10	0.10
Simulation 3H-20	3800	0.20	0.20

Table 5.2 - Impact Simulations with Modified Post-Failure Rules

A set of parametric simulations was performed to assess the influence of the modified post-failure rules on the mesh convergence of the impact solution response at high impact energy. Simulations 1H, 2H and 3H were used as a basis for the parametric runs and the simulations were conducted are summarized in Table 5.2. The results from the simulations were compared with experiment. It is worth noting that simulations with $\alpha_s = \alpha_E = 0.00$ had identical results compared to the unmodified Composite Damage Model.

5.3.2 Impact Force Histories (30.4 J)

Figures 5.21 through 5.23 show the impact force histories for the simulations with 216, 864 and 3800 elements within the coupon, respectively. Each figure presents force curves for four different fibre post-failure rules varying from 0% to 20% undamaged fibre after failure. The only simulations exhibiting sensitivity to variations in the fibre post-failure behaviour were those using the 3800 element mesh (Simulation 3H Set), since this was the only set of simulations with elements small enough to predict fibre failure. Figure 5.23 clearly shows the effect of increasing the amount of undamaged fibre after failure, the impact force history response shows less and less damage occurring. With 20% undamaged fibre after failure, the impact force history looks elastic, virtually undamaged.

Based on observations of the impact force history results for all simulations presented in the preceding three graphs, it was decided to evaluate the results from Simulation 3H-05 more closely. This particular simulation was sufficiently discretized to have failure initiation and the 5% setting of the fibre post-failure parameters appeared to be sufficient to limit the progression of fibre damage. The maximum predicted impact force from Simulation 3H-05 was 12.4 kN and the predicted period was 4.6 ms. The experimental peak impact force was estimated to be 11.7 kN with a period of 5.1 ms.

The peak force from Simulation 3H-05 was 6% overpredicted compared to experiment and the Simulation 3H-05 period was 10% underpredicted compared to experiment. There is a considerable difference, however, in the shapes of the predicted and experimental force curves, suggesting very different failure processes.

5.3.3 Impacted Energy Histories (30.4 J)

The effect of the modified post-failure parameters on the energy history response of the simulations utilizing the 3800 element mesh for the coupon is presented in Figure 5.24. As the extent of damaged fibre was reduced, the predicted residual energy in the models decreased, reflecting this reduction in damage. The experiment had a residual energy of 12.2 J. Simulation 3H-05 predicted residual energy of 12.8 J while Simulation 3H-00 predicted a residual of 16.1 J. This suggests that the overall amount damage in Simulation 3H-05 was in good agreement with experiment and that the modified post-failure rules improved the overall damage prediction.

5.3.4 Strain Histories (30.4 J)

Figures 5.25 and 5.26 compared the predicted strain histories from Simulation 3H-05 with experimental results for gauges 1 and 2 of the Configuration 1 coupon. The strain predictions using the modified post-failure rules are in better agreement with the experimental results than the strain predictions made by the standard Composite Damage Model. The predicted strain responses of Simulation 3H-05 for gauges 1 and 2 of the Configuration 2 coupon appear to be better controlled and less oscillatory than the strains of the unmodified model, as shown in Figures 5.27 and 5.28, respectively. If the failure threshold were artificially reduced, the agreement between the model and experiment would likely have been better.

5.3.5 Damage Area Plots (30.4 J)

Plots of predicted fibre failure damage and matrix cracking damage within the specimens using post-failure parameters set to 5% are presented in Figures 5.29, 5.30, 5.31 and 5.32. Predicted matrix cracking damage was plotted for the second lamina from the bottom (0° ply) as this was the ply that exhibited the most predicted matrix damage. The damage areas for each simulation are presented in Table 5.3. Figure 5.33 shows the distribution of matrix cracking through the bottom eight lamina of the Simulation 3H-05 run. The matrix damage is clearly larger for the lower laminae and appears to be oriented with the lamina direction.

Experimental fractography showed matrix damage and delamination damage to be closely linked and the matrix damage areas from the X-radiography compared well with the delamination damage areas. Therefore, delamination and matrix damage areas should be approximately similar. Comparing the *projected* matrix damage from Simulation 3H-05 (1149 mm^2) with the experimental projected delamination damage (525 mm^2), the predicted matrix damage area is more than double that of the delamination area observed in experiment, as shown in Figure 5.34. A fibre damage length of 1.4 cm was predicted, while a fibre damage length of approximately 0.8 cm was experimentally observed, as shown in Figure 5.35.

	Simulation 1H-05 216 Elements (Approx. 8mm x 8mm)	Simulation 2H-05 864 Elements (Approx. 4mm x 4mm)	Simulation 3H-05 3800 Elements (Approx. 2mm x 2mm)
Fibre Failure (Bottom Ply, -45°)	-	-	37 mm^2
Matrix Cracking (2nd Bottom Ply, 0°)	1004 mm^2	968 mm^2	1047 mm^2

Table 5.3 - Predicted Damage Areas for Simulations with $\alpha_s = \alpha_e = 0.05$

Figure 5.21 - Effect of Post-Failure Mods. on Impact Force History of 1H Set (30.4 J)

Figure 5.22 - Effect of Post-Failure Mods. on Impact Force History of 2H Set (30.4 J)

Figure 5.23 - Effect of Post-Failure Mods. on Impact Force History of 3H Set (30.4 J)

Figure 5.24 - Effect of Post-Fail. Mods. on Impact Energy History of 3H Set (30.4 J)

Figure 5.25 - 3H Set Strain Histories for Gauge 1 of C1 Coupon (30.4 J)

Figure 5.26 - 3H Set Strain Histories for Gauge 2 of C1 Coupon (30.4 J)

Figure 5.27 - 3H Set Strain Histories for Gauge 1 of C2 Coupon (30.4 J)

Figure 5.28 - 3H Set Strain Histories for Gauge 2 of C2 Coupon (30.4 J)

(a) $T = 0.0 \text{ ms}$

(b) $T = 1.0 \text{ ms}$

(c) $T = 2.0 \text{ ms}$

(d) $T = 3.0 \text{ ms}$

2nd Ply from Bottom (0°)

Figure 5.29 - Simulation 1H-05 Matrix Cracking Damage (30.4 J)

(a) $T = 0.0 \text{ ms}$

(b) $T = 1.0 \text{ ms}$

(c) $T = 2.0 \text{ ms}$

(d) $T = 3.0 \text{ ms}$

2nd Ply from Bottom (0°)

Figure 5.30 - Simulation 2H-05 Matrix Cracking Damage (30.4 J)

(a) $T = 0.0$ ms

(b) $T = 1.0$ ms

(c) $T = 2.0$ ms

(d) $T = 3.0$ ms

2nd Ply from Bottom (0°)

Figure 5.31 - Simulation 3H-05 Matrix Cracking Damage (30.4 J)

(a) $T = 0.0$ ms

(b) $T = 1.0$ ms

(c) $T = 2.0$ ms

(d) $T = 3.0$ ms

Bottom Ply (-45°)

Figure 5.32 - Simulation 3H-05 Fibre Breakage Damage (30.4 J)

(a) Ply 1 (-45°)

(b) Ply 2 (0°)

(c) Ply 3 (45°)

(d) Ply 4 (90°)

(e) Ply 5 (-45°)

(b) Ply 6 (0°)

(c) Ply 7 (45°)

(d) Ply 8 (90°)

$T = 3.0 \text{ ms}$

Figure 5.33 - Simulation 3H-05 Matrix Cracking Damage - Bottom 8 Plies (30.4 J)

Simulation 3H-05, Projected Matrix Damage @ 3.0 ms

Figure 5.34 - Comparison of Predicted Matrix and Experimental Delamination Damage (30.4 J)

Simulation 3H-05, Bottom Ply (-45°) @ 3.0 ms

Figure 5.35 - Comparison of Predicted and Experimental Fibre Damage (30.4 J)

6.1 Theoretical and Model Development

The single element failure envelopes of the LS-DYNA3D Composite Damage Model were found to be in good overall agreement with the theoretical failure envelopes of the Chang criteria (Chang and Chang, 1987a,b), as shown in Figures 4.2 through 4.4. Agreement was found between the model and theory for the biaxial failure envelope σ_{11} vs. σ_{22} . The agreement between the model and theory was not as good for the fibre failure envelope, σ_{11} vs. σ_{12} , as the model underpredicted the failure shear stress, S , for decreasing values of σ_{11} . It proved difficult to generate a pure shear stress state in the single element model and it was thus difficult to generate a pure shear failure. The agreement between theory and the model for the matrix failure envelope, σ_{22} vs. σ_{12} , was good.

The cause for the disagreement between the model and theory for the fibre failure envelope is difficult to ascertain. It is possible that there was a problem with the implementation of the failure criteria in the model. However, a problem with implementation of the criteria would have revealed itself in the other envelopes. It is more likely that the combination of displacement-based loading boundary conditions and the 0° orientation of the ply of the composite test element was responsible for the difficulty in generating a combined uniaxial/shear and a pure shear loading on the element.

The consequences of this disagreement, assuming the test element was behaving correctly, was that for stress conditions that resemble the σ_{11} vs. σ_{12} envelope, fibre damage would be predicted to occur prematurely for lower values of σ_{11} . However, the

stress state at any given integration point is dominated by fibre stress as opposed to matrix or shear stress (see Figure 5.13) and therefore a situation where tensile fibre failure is initiated because the shear stress is significant in relation to fibre stress would be very unlikely. Tensile fibre failure occurs primarily in the far right of the σ_{11} vs. σ_{12} envelope.

The absence in the Composite Damage Model of a compressive fibre failure criterion is notable as fibre compression damage is a common damage mode and was observed in the high energy experimental impact tests conducted for this thesis. This absence can be traced to the Chang criteria themselves (Chang and Chang, 1987a,b).

As discussed in the literature review, the intralaminar failure criteria can be divided into superimposed and interactive categories. There exists controversy as to which category, let alone which criteria are the most accurate (Hart-Smith, 1993a,b). Uncertainty also exists as to which of the interlaminar failure criteria is best. Considerably more research needs to be done in the area of failure criteria and the optimum criteria will likely depend on the specific application. There is currently a lack of knowledge regarding composite post-failure behaviour and, therefore, this is also an area where more research is required. The current implementation in this research of the Chang failure criteria (Chang and Chang, 1987a,b) and the Humphreys post-failure rules (Humphreys, 1981) in the Composite Damage Model may be considered starting points for further investigations.

The accurate modelling of the physical components of the impact apparatus was important for correctly simulating the global impact force history under elastic impact. The effect on the global response of modelling the aluminum/plywood supporting base with compliant elastic hexahedron elements was significant. This result is consistent with the findings of Prandy, Boyd, Recker and Alstädt (1991) who observed that the test fixture material influenced the impact response. The elastic tup assembly contributed to a small softening of the impact response of the model.

The difficulty in modelling the through-thickness stiffness of the composite coupon, defined with shell elements, was overcome using a numerical spring having the same stiffness as the measured indentation stiffness of the composite coupons. The effect of this spring on the model compliance was of equal significance as the effect of the entire elastic base. Therefore, consideration of the composite through-thickness stiffness is important for accurately modelling the impact force history.

A more refined composite coupon mesh was found to be more compliant than the baseline coupon mesh. A fine mesh was found to be necessary for properly capturing damage initiation. Using full model discretization also rendered the model response more compliant as compared to the baseline model which employed quarter symmetry. The assumption that quarter symmetry existed in the $[-45/0/90/45]_3$ lay-up is, of course, not correct on account of the $\pm 45^\circ$ plies, so use of full discretization in the elastic model is required. As damage is rarely symmetric in composites, full discretization was also required to model damage accurately. Finally, full symmetry allowed a slight misalignment in the actual tup to be accounted for in the model. The penalty of mesh refinement and full discretization is higher computational cost.

Investigations into the effect of the hourglassing control and contact stiffness numerical parameters were conducted. The philosophical approach adopted in dealing with such numerical parameters was that the LS-DYNA3D default values for hourglassing control and contact stiffness would be used in order to preserve the predictive nature of the model, unless modifications of those parameters resulted in a substantial improvement in the performance of the model.

The existence of hourglassing modes was identified in the stress response of the composite coupon under elastic impact. The hourglassing modes were observed only along particular sections within the coupon. It was desirable to minimize the effects of hourglassing not only to preserve the integrity of the simulation, but also to reduce the artificial variation in strain from element to element to improve the overall displacement

accuracy of the model. Use of stiffness-based hourglass control reduced hourglassing in the model somewhat, but at the expense of unacceptably stiffening the coupon. Increasing the value of the control factor, ϕ , was ineffective for either viscosity- or stiffness-based control modes. Therefore, the default viscosity-based control formulation was preferred.

It appears that the hourglassing modes were excited by the contact between the coupon and the tup head. Reduction of the contact stiffness in the model greatly reduced the effect of hourglassing in the coupon mesh; however, the model stiffness was unacceptably reduced. As in the case of the hourglassing control, the cure was worse than the affliction, as reduction of the contact stiffness excessively softened the global model response.

6.2 Improved Impact Simulations

6.2.1 Low Energy Simulations

Based on the recommendations made in Chapter 4, an improved impact model was defined. A series of low energy impact simulations were performed to evaluate the effect of coupon mesh refinement on the convergence of the impact force solution and to compare the impact force, impact energy and strain energy responses of the simulations with experiment.

The low energy impact force solutions showed convergence implying an adequate mesh discretization of the composite coupon as shown in Figure 5.2. The predicted peak impact force was underpredicted by 4% and the period was underpredicted by 6% in Simulation 3L, the model with the most refined mesh. Based on the impact force response, the global response of the low energy model using the finest mesh discretization was in agreement with the experimental data.

Possible sources of error in terms of the global model response include:

- The simplifying assumptions required to model the local indentation stiffness as a numerical spring;
- The composite coupon element plane stress constitutive formulation;
- The modelling of the supporting base and tup assembly;
- Variability in the composite material properties, including rate effects.

The numerical spring simulating the through-thickness stiffness of the composite had a constant stiffness because the experimental stiffness was linearized. Had the true stiffness shape function been used, the predicted impact force history would have been more compliant. As the effect of the spring on the impact force response of the model was significant, a more accurate spring stiffness shape function would have a small, but noticeable effect on the model behaviour.

Had a more accurate composite element formulation that included through-thickness and transverse shear effects been used, the model global impact force response would have probably been more compliant due to more degrees of freedom within the element. Such a formulation would have rendered the need for a numerical spring redundant, but would require accurate values for the transverse moduli, E_{33} , G_{23} and G_{31} , of the composite material.

The stiffness of the plywood in the tup was also linearized; in reality, plywood may not behave exactly in a linear-elastic manner. The geometry of the tup could have been improved, thus modifying its stiffness; however, the parametric studies showed that the overall effect of variations in the model of the tup assembly on the predicted impact force history was small. The combined effect of all the above improvements would have been an overall increase in the compliance of the model. As a result, the period would have increased marginally, more closely matching the experimental period, while the peak force would have decreased slightly, less closely matching experiment. The cause for the underprediction of the peak impact force is more difficult to conjecture, but the actual

aluminum/plywood base may have been more rigid than assumed as the assembly was bound in compression with its fixing bolts. Higher stiffness in the base would have caused a stiffer impact force response.

It is possible that the effects of variability in the properties of the composite material may have affected the accuracy of the predicted response. Statistical variations in the elastic properties of the composite material were not considered and may have affected results. Loading rate effects were neglected in the composite material formulation and may have also been a source of error.

The residual energy, the steady state energy after impact, is a useful global parameter for quantifying the amount of energy absorbed by damage mechanisms. The predicted residual energy after impact decreased slightly with increasing coupon discretization, as shown in Figure 5.3. The experimental energy losses were 1.0 J and could be attributed to sliding friction in the system and energy absorbed by the coupon in the form of the plastic contact indentation. The elastic simulations had residual energies of approximately 2.0 J, which was a relatively high proportion of the total energy of the impact event. In theory, the predicted residual energy should have been close to zero as there was no mechanical friction modelled, *i.e.* no slideline friction, and there was little damage predicted. However, it is likely that some energy was trapped in the model in the form of vibrational energy and some energy was absorbed by matrix damage.

The comparison of the Simulation 3L strain results with the Configuration 1 experimental data showed that the predicted strain responses were in good agreement with experiment, as shown in Figures 5.4 and 5.5. The predicted peak strains were generally very close to the actual strain gauge values; however, the measured periods were slightly longer than the predicted periods.

The comparison of the simulation strain results with the Configuration 2 experimental strain data showed that the simulated peak strains close to the impact centre

were overpredicted in comparison to experiment, as shown in Figure 5.6. The predicted periods were again less than the experimentally observed periods. While the spring had a beneficial effect on the global response of the model, it must be remembered that the presence of the spring could not completely improve the accuracy of the predicted stress state near the impact centre of the coupon as plane stress shell elements were being used in the coupon. A finite element formulation that accounted for the through-thickness and transverse shear stresses would probably distribute the impact load more evenly throughout the composite coupon mesh, potentially reducing strain overprediction near the impact centre and reducing strain underprediction away from the impact centre. Such formulations have already been suggested by authors such as DiSciuva (1985), Averill and Reddy (1992) and Robbins and Reddy (1993).

Overall, the low energy simulations were in agreement with the experimental data based on the impact force, impact energy and strain data comparisons, respectively. The consequence of the low energy model behaving accurately is that the elastic response of the higher impact energy simulations can be considered accurate and that large inaccuracies may then be attributed primarily to the nonlinear response due to the onset of damage.

6.2.2 High Energy Simulations

Based on the impact force results from the high energy simulations, it is apparent that the high energy FEM solution did not converge. Simulations 1H and 2H did not predict significant fibre damage, while Simulation 3H predicted extensive fibre damage, as shown in Figure 5.7. As the only variation between these simulations was the element size for the composite coupon, the only conclusion that could be drawn is that the simulation behaviour under damage-inducing impact was mesh dependent. As a consequence, the solution did not converge and therefore, the model was not predictive.

In the review of research dealing with the numerical modelling of composite impact damage, only the paper by Montemurro, Hansen and Houde (1993) made mention of the fact that mesh discretization can affect damage extent and progression. However, researchers using finite element techniques for failure modelling in metals have also noted mesh sensitivity in their results (Addessio and Johnson, 1993).

Based on the predicted high energy impact force results, it can be stated that the threshold for significant damage, *i.e.* fibre failure, in the coupon was a function of element size. The explanation for this phenomenon is as follows. Under impact loading conditions, there is stress concentration in the coupon in the vicinity of the coupon centre of impact, as shown in Figure 5.17. Because the stress concentration is high in that area, the average stress state seen by smaller elements close to the impact centre is significantly higher than that seen by larger elements in the same location. By virtue of having a higher stress state, fibre failure will occur earlier in smaller elements than larger elements in the vicinity of the coupon impact centre.

Comparison of the experimental and predicted impact force curves yielded some interesting insights. All the predicted force curves behaved largely elastically up to the point of failure observed in Simulation 3H. It is worth noting that Simulation 3H suffered extensive matrix failure (see Figure 5.18) just prior to fibre failure initiation. The occurrence of matrix damage did not significantly affect the impact force history response of the model. The experimental force curve revealed the onset of damage within the coupon long before the labelled "failure point". The occurrence of damage can be seen in the reduced slope and the small force drops on the loading side of the experimental impact force curve. Fibre damage appeared to occur gradually and progressively in the experimental coupon up to the arbitrarily defined failure point, whereas fibre failure occurred suddenly and catastrophically in Simulation 3H. Slight rate effects on matrix damage progression have been observed by Lee and Zahuta (1991) in their work on toughened epoxy laminate systems; however, these effects would not have been significant enough to account for the fundamental difference in the occurrence of failure

in the experiment and in the model. Work by Elber (1983) suggested that the fibre damage threshold decreased slightly with increasing loading rate; however, once again, the effect would not have been large enough to completely account for the difference between experimental and predicted impact force histories.

This difference between the experimental and simulation failure processes may be attributed to the inherent difficulty that arises in attempting to model a composite laminate using a discrete number of elements. The actual coupon consists of twenty-four lamina in a quasi-isotropic lay-up with each lamina containing hundreds of thousands of individual fibres and accompanying matrix material. Each of those fibres have some variability in elastic and strength properties. Conversely, the coupon defined in the model is discretized into elements of a finite size with each lamina being represented by an integration point. Uniform material properties are assumed throughout each integration point of each element. Thus, the failure threshold for any actual fibre may vary considerably, while fibre failure stress for an integration point of an element is fixed. Therefore, fibre failure will tend to occur suddenly in the finite element model, while fibre failure will tend to occur more progressively in experiment due to the stochastic variations in individual fibre strengths.

Comparison of the strain histories from the Configuration 1 experimental impact test and Simulation 3H (Figures 5.9 and 5.10) showed that the predicted strains were in mixed agreement with experimental results. The C1 coupon predicted strains reflected the occurrence of damage in the coupon; however, the experimental strains did not reflect the occurrence of any damage. This suggests that the damage was overpredicted by the model. The comparison of the experimental results from the Configuration 2 impact test and the predicted results along the -45° direction from the impact centre show that the Simulation 3H results did approximately match the shape of the experimental strain curves, as shown in Figures 5.11 and 5.12. The experimental C2 coupon strains decreased suddenly in magnitude at approximately 1.0 ms, suggesting the onset of damage in the coupon long before the predicted failure point of approximately 2.0 ms. The lag

in the predicted strain failure points compared to experiment suggests that the predicted failure threshold was too high. The sudden jump in the experimental strains, which LS-DYNA3D also predicted, was probably the result of an unknown failure effect in the coupon related to tensile fibre breakage.

The predicted strain curves which were presented all showed oscillating behaviour after failure. It is possible that this behaviour was related to the presence of elastic stress waves in the coupon. Fibre and matrix stress data from the top and lower surface of an impact element (see Figure 5.13) were sampled at high frequency in order to detect the presence of stress waves in the coupon. No evidence of stress waves was detected. It is likely that any stress waves that were present in the coupon due to impact were small in magnitude and were quickly attenuated (Kolsky, 1963). Therefore, the solution can be categorized as falling within the structural dynamic regime. It is more likely that the oscillations observed in the predicted strains after failure were the result of a complete loss of stiffness in the fibre damage zone of the coupon.

Overall, the shape of the predicted impact force history curve was not in good agreement with experimental data, implying fundamental differences in the predicted and actual failure processes. The predicted strain history curves gave poor results as compared to experiment. The simulation, while predicting excessively large regions of failure, did seem to generally capture the effect of failure on the strain response near the impact centre.

6.2.3 High Energy Simulations with Modified Fibre Strengths

Based on observations of the Composite Damage Model under high energy damage-inducing impact, the predicted impact force response of the model was found to be element size dependent. More specifically, the failure threshold of the model, the load and point in time where significant damage occurred, was found to be element size

dependent. Based on element size considerations and the fact that the predicted failure threshold was too high compared to experiment, it was desired to further investigate the influence of the failure threshold on the model response. The high computational expense of the 2 mm x 2 mm elements precluded the use of smaller elements. Therefore, the failure threshold of the Simulation 3H model with 2 mm x 2 mm elements was artificially lowered by reducing the tensile fibre strength to 75% and 50% of its actual value; this was felt to be analogous to using smaller elements. The effect of the reduced strength on the damage response was dramatic as shown in Figure 5.14. At 75% tensile fibre strength, the impact force solution softened considerably, while at 50% tensile fibre strength, the mesh failed completely.

The effect of increasing fibre damage due to the decreasing failure threshold is clearly shown in the residual energy values from the impact energy history plots (see Figure 5.15). The effects of the reduced fibre strengths on the fibre damage in the bottom lamina of the Simulation 3H are shown in Figure 5.16; the fibre damage is more extensive with decreasing fibre strength.

Based on the evidence gathered, the mechanics of the failure process in the coupon is similar to rapid fracture. The damage propagation mechanism was related to the failure threshold in that the lower the threshold, the more energy is fed into the fibre failure process from the descending tup, the more the fibre damage propagates. The stress plots, Figures 5.17 and 5.18, show the fibre and matrix stress states, respectively, for Simulation 3H/1.00 X_T just prior to fibre failure. Of note is the large matrix damage region in the matrix stress plot where the stresses are zero. Fibre failure is initiated at the fibre stress concentration at the impact centre. The fibre failure then appears to proceed in the directions of maximum strain gradient. The relationship between damage propagation and strain distribution can be seen by comparing the fibre damage plots (see Figure 5.16), with the global x- and y-strain plots (see Figures 5.19 and 5.20). Although it is possible that the maximum strain gradients drove the direction of damage propagation, it is also possible that the mesh merely "unzipped" along element-oriented rows following the

establishment of a crack.

6.2.4 High Energy Simulations with Modified Post-Failure Rules

The effects of element size and the failure threshold on the initiation and propagation of fibre damage in the composite mesh have been addressed. The tendency for fibre failure to propagate unchecked at sufficiently low failure thresholds was thought to be related to the severity of the fibre post-failure rules. The original rules stipulated an instantaneous shedding of all load from the integration point in the element where the fibre has failed. This requires that the other integration points within the failed element and within adjacent elements immediately pick up all the shed load from the failed integration point. If the elements are small enough, then the shed load is redistributed more locally among the elements adjacent to the failed element. This creates a situation where the integration points within those adjacent elements can, themselves, overload and fail. The process then quickly repeats itself, leading to a domino failure process within the mesh, which then leads to catastrophic failure of the mesh. It was felt that modifying the fibre post-failure rules would be an effective way to limit the unchecked propagation of damage within the mesh.

Observing the impact force history curves presented in Figure 5.7, it can be seen that the post-failure behaviour of the experiment and the model are completely different. The experimental failure point was arbitrarily defined at 1.7 ms at 11.2 kN because of the large force drop at this point. As failure proceeded, there was a clear oscillation in the impact force response. This oscillation was likely the result of successive fibre failures, leading to a local unloading which resulted in a drop in the impact force. However, immediately after this impact force drop, the toughened matrix material then locally absorbed the shed load, thus causing a rise in the impact force as the coupon redistributed load within itself. This process continued, giving the impact force curve its oscillating behaviour. It should be noted that some authors have attributed the oscillations to

specimen vibration. The post-failure behaviour in Simulation 3H consisted of a large, sudden force drop resulting from a large amount of fibre damage. The drop suggested that the load from failed elements was not being redistributed in a physically realistic manner.

The dependency of the high energy simulations on the coupon element size was addressed by modifying the fibre-post failure rules. Modifications to the Composite Damage Model were made that essentially enabled the user to limit the progression of fibre damage in the mesh by specifying the amount of residual strength and stiffness remaining in a failed integration point of an element. This technique was thought to be effective because it prevented all the fibre load from being transferred from a failed integration point. The post-failure fibre stress, $(\sigma_{11})_{\text{post-fail}}$, and the post-failure fibre modulus, $(E_1)_{\text{post-fail}}$ were specified using the normalized parameters, α_s and α_E , respectively. Using a simplified model, a range of normalized fibre post-failure behaviour between 0.0 and 1.0 was evaluated. The amount of fibre failure and the fibre failure sequence of the integration points (laminae) within an element was found to be very much dependent on the degree of undamaged fibre specified in post-failure rules.

Using the high energy simulation models, a set of parametric simulations was performed. The effect of the modified post-failure rules on different coupon meshes was observed; the range of values employed for the normalized post-failure parameters, α_s and α_E , was between 0.0 and 0.20. In other words, the effect of the post-failure rules were evaluated for 0% to 20% residual strength and stiffness after failure. The impact force simulations for the Simulation 1H and 2H Sets showed no significant evidence of failure, as shown in Figures 5.21 and 5.22, for the entire range of post-failure parameter values. This is not unexpected as the element sizes in these simulations were too large to initiate failure. The Simulation 3H Set showed the most sensitivity to the value of the post-failure parameters (Figure 5.23), because the coupon elements were small enough to fail and exhibited the effect of the modified post-failure rules. The sensitivity of the Simulation 3H Set to the values of the fibre post-failure parameters indicates the

importance of the post-failure behaviour in the modelling of the damage-inducing impact response of composite materials.

The simulation which was felt to most closely match experiment was Simulation 3H-05. The predicted period from the impact force history was underpredicted by 10% as compared to experiment, while the peak force was overpredicted by about 6% as compared to experiment. Overall, the predicted impact force response of the modified Composite Material Model was somewhat improved over the unmodified material model. However, the shape of Simulation 3H-05 impact force curve still differed significantly from the experimental curve. As previously discussed, this difference is attributable to differences in the failure initiation and post-failure mechanics between the model and the actual coupon. As the fibre post-failure strength and stiffness were increased (less fibre damage), the propagation of damage was effectively controlled, as shown in the impact force histories of Figure 5.23.

Simulation 3H-05 was in good agreement with experiment with respect to the residual impact energy and the simulation showed marked improvement over the Simulation 3H-00 prediction (Figure 5.24). The experiment had a residual energy of 12.2 J, Simulation 3H-05 predicted residual energy of 12.8 J whereas Simulation 3H-00 had a residual of 16.1 J. This suggests that the overall amount damage in Simulation 3H-05 was in good agreement with experiment and that the modified post-failure rules improved the overall damage prediction.

Comparison was made with the Simulation 3H-05, the Simulation 3H-00 (identical to Simulation 3H) and experimental strain history data for the both the C1 and C2 coupons (Figures 5.25 - 5.28). The Simulation 3H-05 results were found to be in better agreement with the experimental data than the Simulation 3H-00 data for the C1 coupon. Simulation 3H-05 also predicted better strain behaviour with less oscillation after failure for the C2 strain gauge locations as compared to Simulation 3H-00. Had the failure threshold of the simulations better matched that of the experiment, the strains predicted

by Simulation 3H-05 would have much better matched those from experiment.

The matrix damage plotted for the bottom eight lamina of Simulation 3H-05 shows the decreasing damage area in each lamina from bottom up, as well as the relationship between matrix damage and lamina direction (Figure 5.33). Both of these observations are similar to the delamination behaviour observed by Gosse and Mori (1988) and Vietinghoff (1993). However, the projected matrix cracking damage was overpredicted by a factor of more than two compared to the ultrasonic projected damage area (Figure 5.34). There are two possible reasons why the matrix damage was so overpredicted: the failure criterion was inaccurate and/or the post-failure rules were too severe. The back face fibre damage predicted by Simulation 3H-05 was in good agreement with the experimentally observed fibre damage (Figure 5.35).

Overall, the implementation of the modifiable fibre post-failure behaviour contributed to improved accuracy of the impact force, impact energy, strain and damage area predictions of the high energy impact simulations.

6.3 Summary

The response of the numerical model was found to be sensitive to two key parameters, namely the failure threshold and the fibre post-failure behaviour. Failure initiation will always be highly mesh dependent for elastic/brittle materials such as composites. Failure propagation was also found to be very mesh dependent with failure propagating along element-oriented rows in the directions of maximum strain gradient. The use of modified fibre post-failure rules was shown to be able to limit failure propagation to physically reasonable levels. Further parametric studies should enable a failure threshold and fibre post-failure rule to be defined for a given element size that will reasonably match experimental impact force history and resultant damage areas for damage-inducing impact. Unfortunately, the present state of the art suffers this mesh size

dependency and limits the true predictive nature of this type of model. The need exists for a method to eliminate mesh dependency, such a method, based on a characteristic length scale, has been postulated by Addessio and Johnson (1993).

6.4 Recommendations for Future Work

A primary thrust of future research in the area of numerical modelling of composite impact should be to reduce mesh dependency in the simulations. The failure threshold and damage propagation mechanisms were established to be mesh dependent behaviours. Therefore, the first task would be to create a composite damage model that has the same failure threshold irrespective of element size. Once failure initiation is rendered independent of element size, then the fibre post-failure rules should be modified such that they also would be independent of element size. The result of these modifications would be an impact force history in high energy damage-inducing simulations that would converge quickly and would be independent of element size.

Once the challenges of mesh dependency are resolved, the intralaminar failure rules should more closely investigated and modified, if necessary. The fibre damage failure prediction was quite accurate; however, the matrix damage was significantly overestimated. The matrix damage overestimation may have been a result of the failure criterion itself and/or its post-failure being too severe. Furthermore, a failure criterion and post-failure rule for compressive fibre failure should be incorporated as this is a significant failure mode.

The approach taken in modifying fibre post-failure behaviour was relatively simple in that after fibre failure, the fibre stress was reduced and the fibre modulus was degraded to reflect the amount of undamaged fibre remaining after failure. This approach did not account for failure progression at the integration point after failure. Therefore, an algorithm to further degrade failed integration points should be considered. A much more

sophisticated approach to post-failure modelling would involve accurately characterizing the interplay between the matrix and the fibre after failure, such that the oscillating response observed in the experimental high energy impact force history could be replicated.

Interlaminar failure criteria for delamination should be incorporated into the model; the criteria postulated by Choi and Chang (1992), which include the effects of fracture toughness, would be a good starting point. However, such a criterion would require the accurate prediction of through-thickness and transverse shear stresses. Methods to predict those stresses in shell element have been outlined by DiSciava (1985), Averill and Reddy (1992), and Robbins and Reddy (1993).

The question remains as to whether LS-DYNA3D can be anything more than a computationally expensive research tool for the analysis of composite impact. The capability to quickly and accurately predict composite impact damage would be very useful in a real world engineering design environment. Two steps can be taken to significantly reduce the run time required to perform impact simulations similar to those conducted in this thesis. First of all, simplifying the mesh slightly by using rigid shell elements in the tup head instead of elastic hexahedron elements would increase the time step by a factor of up to four, thus reducing the run time by the same factor. The penalty of this approach would be a global impact force response of slightly reduced accuracy. The use of a graded mesh with no more than 1000 composite elements could provide almost the same level of discretization near the impact centre as a mesh using 3800 uniformly-sized elements. Thus, further run-time savings of a factor of up to four could be realized. However, the use of a graded mesh must await the modifications to the Composite Damage Model that will eliminate or reduce the mesh dependency observed in the response. Therefore, the possibility exists to reduce run times from five days to approximately six hours on a Challenge-type Silicon Graphics machine for a typical 24 ply coupon subjected to impact low velocity impact.

7.1 Conclusions

The overall conclusions reached from the work performed in this thesis are:

1. The numerical software proved capable of accurately modelling low energy elastic impact on composite coupons. The predicted impact force history, impact energy history and strain histories were found to be in good agreement with experiment;
2. The Composite Damage Model exhibited mesh dependency in high energy damage-inducing impact simulations. The model response was found to be dependent on (i) the failure threshold and (ii) the fibre post-failure behaviour;
3. After modifications were made to the fibre post-failure behaviour in the material model, improvements were noted in the prediction of the impact force history, the impact energy history, the strain response and the amount of fibre damage; however, the matrix damage area was overpredicted;
4. Mesh dependency remains an issue to be addressed.

7.2 Future Research

Areas of future research that should be pursued in the area of numerical modelling of impact on composite materials include:

1. The mesh dependency of the Composite Damage Model should be resolved with

the implementation of an algorithm into the model which takes element size into consideration in the fibre failure and post-failure processes;

2. Improvement of the existing intralaminar failure and post-failure criteria should be investigated, with particular attention paid to the matrix failure. Fibre compression damage should be considered for incorporation into the model. More realistic formulations for the fibre post-failure behaviour should also be evaluated;
3. Delamination criteria including the effects of material fracture toughness should be implemented in the Composite Damage Model. The implementation would require improved transverse stress prediction capability within the shell elements.

7.3 Summary of Contributions

The contributions made in the area of numerical modelling of impact on composite coupons are:

1. Identification of key numerical and physical parameters in the impact simulation problem;
2. Accurate modelling of low energy elastic impact on composite coupons;
3. Identification of mesh dependency in the Composite Damage Model under high energy damage-inducing impact;
4. Implementation of modified fibre post-failure behaviour in the Composite Damage Model and improved accuracy in the simulation of high energy impact as a result of the modified post-failure rules.

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