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1 Damage in single lap joints of woven fabric reinforced polymeric composites subjected to transverse impact loading

R. S. Choudhry\textsuperscript{a,b}, Syed F. Hassan\textsuperscript{a}, S. Li\textsuperscript{c} and R. Day\textsuperscript{d},

\textsuperscript{a} College of E&ME Campus, National University of Sciences and Technology (NUST), Islamabad
\textsuperscript{b} Faculty of Engineering and Physical Sciences, The University of Manchester, Manchester, M13 9PL
\textsuperscript{c} Faculty of Engineering, University of Nottingham, Nottingham NG7 2RD
\textsuperscript{d} Glyndŵr University, Mold Road, Wrexham, Wales, LL11 2A

2 Abstract

Single lap joints of woven glass fabric reinforced phenolic composites, having four different overlap widths, were impacted transversely using a hemispherical impactor with different velocities in the low velocity impact range. The resulting damage was observed at various length scales (from micro to macro) using transmission photography, ultrasonic c-scan and x-ray micro tomography (XMT), in support of each other. These experimental observations were used for classification of damage in terms of damage scale, location (i.e. ply, interfaces between plies or bond failure between the two adherends) and mechanisms, with changing overlap width and impact velocity. In addition, finite element analysis was used to simulate delamination and disbond failure. These simulations were used to further explain the observed dependence of damage on overlap width and impact velocity. The results from these experiments and simulations lead to the proposal of a concept of lower and upper characteristic overlap width. These bounds relate the dominant damage pattern (i.e. scale, location and mechanism) with overlap width of the joint for a given impact velocity range.

3 Keywords:

Composite Joints; Impact Damage; Disbond; Delamination Modelling; X-ray Micro Tomography

1. Introduction

Joints formed through adhesive bonding or co-curing of composite laminates are often used to form part assemblies for various applications. An important aspect to consider when designing joints having
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composite adherends is that the joint or bond interface can have material properties similar to the
interfacial material properties of the adherends. This is particularly true for co-cured joints because in this
case the polymer matrix within adherends also acts as the adhesive for the joint. Owing to their similar
interfacial and joint properties, the failure for such assemblies, when subjected to a multi-axial stress state,
is not necessarily more likely to initiate from joint interface (i.e. joint failure). In fact, in some cases the
adherends may fail before the joint or the adherends and the joint may fail together. It is for this reason
that when discussing failure of such joints due to multi-axial stress state, multiple damage mechanisms,
which may occur within plies (fibre and matrix damage), at interfaces between plies (delamination) and at
the joint interface (joint failure) also need to be considered.

In recent years many authors have analyzed composite bonded joints from various perspectives. For
instance, Herszberg et al. [1, 2] undertook FE analysis and proposed a structural health monitoring system
for composite ship joints (T – joints) and other marine structures. Their FE analysis was limited to pre-
failure linear elastic stress analysis with a view to understand the stress distribution. Li et al. [3-5] used
mode I and mixed mode “cohesive zone” models to study the failure of adhesive joints of composites.
They used their model to predict joint failure due to lap-shear for a single lap joint. In addition they also
simulated the end notch flexure (ENF) test to study the effect of change in mode I failure toughness on
failure mechanism. They observed that depending on the mode-I toughness of the joint, the specimen may
or may not fail due to interfacial failure. In particular for a relatively strong interface, i.e. with higher
mode-I toughness, the composite may fail before the bond failure.

Failure of hybrid adhesive/mechanical joints of composites was modelled using a very unconventional
approach of Bond-Graphs by Gómez et al. [6]. This technique could only be used to access fail/safe status
of the joint and was not used for progressive damage modelling. It also excluded any possibility of
modelling failure in composite adherends and only focused on joint failure.

In addition to studying the joint failure in standard configurations for quasi-static cases, researchers have
also investigated fatigue failure for composite joints. For instance, Ashcroft et al. [7] used electronic
speckle pattern shearing interferometry (ESPSI) and x-radiography to experimentally observe tensile fatigue damage in an adhesively bonded layer. On the other hand, Wahab et al. [8] developed an FE model to predict fatigue life of adhesively bonded multidirectional composites. The model only considered adhesive failure as the dominant damage mechanism. Fatigue life of composite joints was also investigated experimentally by Potter et al. [9]. They studied the effect of fatigue loading using various paste adhesives with unidirectional carbon/epoxy adherends in a double lap joint configuration.

There is little work specifically on transverse impact damage of single lap joints of woven composites. There is however, a huge volume of literature available on low velocity impact of laminated composites, which has been an active area of research for over three decades. Since in this study co-cured joints are considered, a number of lessons can be learnt regarding experimental damage characterization and numerical modelling of damage in these joints by consulting this literature. In this regard, the reviews of Richardson et al. [10] and that of Hogg et al. [11] are very thorough and effectively summarise the experimental and numerical work before the start of twenty first century. Hogg et al. [11] in particular also discussed in his review the effect of reinforcement architecture on damage tolerance.

Transverse impact produces deformations in localized region around the impact zone (indentation) which may be elastic, plastic or may induce different form of local damage [11-13]. This indentation may subsequently lead to global flexural bending of the structure, penetration/perforation of the specimen or it may lead to a mix of both depending on impact velocity and relative masses of the projectile and the plate. For low velocity impact event, flexural response of the specimen is considered more important and Davies [14, 15] defined it as an impact event in which the through thickness stress wave did not play a role in stress distribution. It was shown by Olsson [16] that the ratio M/Mp (i.e. the ratio of mass of projectile to mass of plate being impacted) can also be used to characterize the type of response of composite square plates. He found that when this value is between ‘0.2’ and ‘2’, the response of plate is a result of interaction between the flexural wave and its boundary conditions. If this value is higher than ‘2’, then a quasi-static response is expected while for values less than ‘0.2’, the response is dominated by the flexural wave (without effect of reflection from boundaries). Thus for identical impact energies, the
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relative contribution of impactor mass and velocity lead to different damage mechanisms as these possibly excited different frequency modes [17].

Most of the studies cited above report damage area measurements using techniques such as 2D X-ray, ultrasonic C-Scan or Microscopy of selected specimens. More recently Richardson [18] experimentally studied low velocity impact induced non-penetration damage in pultruded glass fibre reinforced polyester (GRP) laminates using electron speckle pattern interferometry for live observation of damage. This is an excellent technique, however the main limitation is that it is a 2D technique and information about the exact depth and mechanism of damage is difficult to deduce from this technique alone. In recent years, the use of techniques such as stereoscopic X-radiography and X-ray micro-tomography (XMT) in support of other 2D techniques has gained prominence. As evidenced by [17, 19-22], XMT can provide more information about the damage in 3D and can be used for getting detailed information about damage mechanisms. It has a limitation however, that it is difficult to use it for live capturing of fast occurring events such as impact. Moreover, minute damage in carbon fibres is hard to pick up without the use of a die-penetrant due to low absorption of x-rays in carbon. Stereoscopic X-radiography offers an alternative to XMT for damage characterisation and recently Aymerich et al. [23] have demonstrated that this can be used very effectively to map interfacial impact damage area for each interface within a composite.

A number of methods exist for modelling the impact response of composite plates. Abrate [24] has extensively discussed the analytical approaches for studying the impact dynamics for composites and presents several models that can be used to estimate the peak forces and energy absorbed without detailed damage modelling. Most of these models however cannot account for the different boundary conditions and changes in specimen geometry and thus these cannot be directly used for current study where specimen geometry (i.e. in terms of the change in overlap area) and its effect on ensuing damage is under investigation. Different approaches to modelling of damage in composites can be found in literature such as continuum damage modelling (CDM) [25-32], micro-mechanics of damage (MMD) [29, 33], linear elastic fracture mechanics [34-36] cohesive zone models (CZM) [37-39] and synergistic methods [29].

The most well-established of these methods for impact damage modelling are the Continuum damage
modelling (CDM) based approaches [40, 41] and their recent extensions in the form of cohesive zone models (CZM) that combines the elements of CDM and fracture mechanics [20, 23, 31, 32, 42, 43]. In CDM approaches, onset of failure is usually predicted using a ply level failure criterion [44-47] and the effect of damage growth is reflected through degradation of ply level properties of the material using empirical hardening/softening equations set up in terms of additional material parameters. These additional parameters are adjusted to the model through experimental measurement of loss of stiffness because from the view point of thermodynamics the damage variables are the internal state variables and thus are not measurable directly [29]. Whether the loss of stiffness is sensitive enough or not, to a particular damage mechanism, is arguable [30]. CDM with smeared crack approach [48] has been used more commonly for modelling ply failure mechanisms such as intra-ply matrix cracks (in-plane and transverse) and fibre failure [41], whereas CZM have been used primarily for modelling of delamination failure [29, 39]. CZM models use strength or strain based criterion to either model failure initiation in the same way as traditional CDM or alternately in terms of traction and separation law, and after that, damage propagation is governed by the mode mix of failure and corresponding critical energy release rate ERR [45, 49]. In recent years CZM models have also been used to model intra-ply failure mechanisms in addition to delamination modelling [20, 43]. The main limitation of such models is that the preferred crack paths need to be defined at prior, the computational cost is high and the appropriate calculation of the nine material parameters for each mode of failure being represented by these models is a challenge. CDM models and CZM models have also been used effectively in support of each other [23, 39, 50, 51]. In these models the intra-ply failure mechanisms are dealt with using pure CDM approach and delamination is modelled using CZM. Although authors have reported an excellent agreement with experiments in these cases, the general applicability of this approach has the limitation that a significantly high computational cost is required and mesh dependency cannot be completely eliminated. More over the fundamental problems with CDM such as the disagreement about the failure initiation criteria for composites and assumptions about the stiffness degradation schemes remain unresolved. In addition to these other types of synergistic models which combine the elements of other modelling strategies such as
CDM and MMD [29] have also been used for modelling damage in composites, however their application to modelling impact damage in composites is yet to be demonstrated to the best of author’s knowledge.

Unlike the general impact problem for composite plates, the specific question of transverse impact damage in single lap joints of woven composites has been addressed by relatively few researchers. The most notable in these is the work of Kim [52] who specifically looked at damage formation mechanisms in single lap joints having woven glass-epoxy adherends under transverse impact loading. The major limitations of his work were that the experimental portion of his work relied only on ultrasonic C-Scan (2D) and the finite element model was limited to pre-failure analysis. Another directly relevant work was carried out by Bhamare [53]. In this study, the author studied single-lap joints of quasi isotropic and cross-ply laminates under transverse impact. The damage mechanism observation was limited to visual analysis and the model was based on shell element representation of laminates. Delamination failure within adherends was not modelled. The adhesive layer was not modelled physically; instead a tie constraint was used to model the bond. Tie-break failure was then used to model bond failure. Ply failure in this study was modelled using Tsai-Wu criteria for plane stress. As opposed to Bhamare’s work [53] a unique aspect of Kim’s [52] numerical work was the analysis of both, bond failure and the adherend failure. The study did not simulate progressive failure however, and only delamination initiation was modelled using an empirical quadratic failure criteria. The experimental evidence in that study was limited to two dimensional ultrasonic C-Scans of the samples and thus his work did not provide sufficient experimental data related to through thickness distribution of damage. Other recent studies of adhesively bonded lap joints include work by Quaresimin and Ricotta [54, 55] Odi and Friend [56] and Kim [57].

Based on literature discussed above, it can be asserted that the damage mechanisms and the interaction between them for lap joints of composites depends on multiple factors such as joint geometry (e.g. area of overlap region and its thickness), material properties of the laminate and the adhesive, impact velocity, impact energy, location of impact relative to joint geometry and boundary constraints. The review given above is representative of the various approaches that have been followed for analysing damage in bonded composites in recent years. It highlights that firstly only a few authors have specifically looked at
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transverse impact induced damage in composite lap joints. Thus for such cases, the change in damage mechanisms with changing impact velocity and joint geometry is not well understood. Secondly, it also highlights that previous researchers have mainly focused on 2D techniques such as C-Scan for damage characterization and damage area measurements. Thirdly, the FE models for most of the previous work on the topic is not sufficiently detailed and the reason for this simplification can often be attributed to the fact that modelling of impact induced damage in composites is a contentious subject area and unlike metals, there is no fundamental agreement on the choice of modelling methodology and failure criterion for composites [10, 11, 58-60]. The disagreement is at various levels, from fundamental understanding of damage mechanisms to choice of modelling methodologies. The issue becomes more complicated with the presence of a joint, since in this case bond failure and its interaction with the composite failure also needs to be considered.

It is understood that the damage mechanisms may also be affected by multiple factors such as impactor shape, impactor material, lap joint thickness, type of adhesive and boundary conditions to name a few. Discussion of all contributing factors is not possible in a single paper, however. Thus for single lap joints of composites, impacted transversely by a hemispherical tip impactor; the study aims at clearly explaining the dependence of damage mechanisms and its extent on two of the main contributing parameters, which are impact velocity and overlap width. Experimental methods and FE simulations were both employed to achieve this aim. Experimental methods included transmission photography, ultrasonic c-scan and x-ray micro tomography (XMT). This allowed for observing damage in both 2D and 3D, enabling authors to explain damage mechanism observations in terms of ply, interface and bond failure; taking stock of both macro and micro failure mechanisms. The experiments were supplemented by finite element analysis which was used to simulate impact damage at bond interface and within adherends. Progressive failure through delamination propagation within adherends and the bond layer was modelled using cohesive zone approach [40, 45, 49, 61], while ply failure, i.e. other matrix and fibre failure mechanisms were only evaluated to the point of failure initiation based on different ply failure criteria. The numerical predictions were useful in explaining the experimental observations of damage.
2. The Methodology

Experimental and numerical investigations were carried out as detailed below.

2.1 Details of Experiments

The experimental work was carried out to physically observe transverse impact induced damage in single lap, co-cured joints of woven glass/phenolic composite with a view to characterize the resulting damage. Experiments were carried out in the low velocity regime using a hemispherical impactor, which was much heavier than the lap joint and therefore the results relate with frequently encountered in-service impact scenarios such as tool drop. The choice of velocities investigated was mainly driven by the consideration that while remaining in the low velocity regime a wide range of damage should be observable (i.e. ranging from barely visible impact damage (BVID) to visible impact damage (VID)). The lap joint test specimens were made from Primco-SL246/40, which is a glass fibre/phenolic pre-preg. The pre-preg is based on an 8 harness satin weave fabric impregnated with phenolic resin mix (proprietary modified phenolic resin). The specimens were made using hand lay-up and vacuum bagging using a single side tool. The curing was carried out using Quickstep™ plant at Northwest Composites Centre (NWCC), The University of Manchester (The Quickstep™ process has been described in [62]). Control over thickness variation was ensured by application of vacuum pressure and by the presence of metal support strips that were placed above and below each adherend in the non-overlap region. The average thickness of the samples in overlap region was 2.4 mm with a standard deviation of 0.09. The average volume fraction calculated using the Burn-off method (ASTM D3171) was 40.1% and void content was 4.5%. Each adherend of the lap-joint was made from four layers of the pre-preg. Thus the overlap region consisted of 8 layers in each case. The joints were co-cured and the resin in the adherends acted as the adhesive; no special surface treatment was required. The layup for each adherend was done in a way that the plies were stacked in the warp direction back to back like flipped pairs. This would result in the layup in each adherend to be semi-symmetric, i.e. $[0/0_{f}]_2$, where the subscript ‘$f$’ refers to the flipping of the alternating lamina and the 0 direction is taken to be along the warp direction. The flipping stacking sequence has been described in more detail in [63].
Four different overlap widths (olw) i.e. 21, 25, 36 and 46 mm were tested for a velocity range of 4.0 ms\(^{-1}\) to 9.6 ms\(^{-1}\) (which corresponds to the energy range of 1.6 J to 8.0 J) in a horizontal spring loaded impact gun using a hemispherical impactor with tip radius of 7.5 mm and total mass of 201.4 grams. The corresponding ratio of mass of projectile to mass of lap joint for the 21, 25, 36 and 46 mm overlap width joints was 4.4, 4.2, 3.8 and 3.4 respectively. The impactor was not instrumented and through this apparatus it was only possible to measure the velocity before impact (based on a laser diode trigger connected to an oscilloscope), which was then used to calculate the impact energy. No direct impact force or specimen deflection measurements were possible using this apparatus, however peak pressure generated on the test specimen at the point of impact was measured using a pressure sensitive thin film placed under the impact location. The working principle of the film is explained in [64] and the calibration and data reduction has been discussed in [65].

A schematic view of the impact test setup is shown in figure 1. The specimens were mounted in the impact rig using a window frame type fixture that consisted of two steel frames between which the specimen is placed (see figure 1). After placing the specimen in the fixture, baffles (cut from the same material as the adherends and having thickness equal to each adherend) were placed around the periphery so that the lap joint was uniformly constrained in the window frame (see figure 1(b) and (c)). Once the specimen and baffles were placed in the fixture, the bolts around the frame (not shown in figure) were fully tightened giving a fully clamped boundary condition (i.e. all translational and rotational DOF = 0) at edges while allowing for bending of the lap joint during test. The free breadth, i.e. taking in account the constrained portion of the lap joint, was around 96 mm. In the above velocity range, tests were carried out at five different velocities with at least three repeats for four of these velocities and a single test for each specimen at lowest velocity.

Each sample was examined for damage using ultrasonic C-Scan and through transmission photographs (imaging) after the impact tests. The system used for performing C-Scan was a 2-axis computer controlled water jet inspection system from ‘Midas NDT’ (used in through transmission mode with unfocussed, 10 MHz probes) and imaging was done using Nikon D200 Camera (The backside illumination was achieved by placing the sample flat on the glass top of a standard slide projector whose
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diffuser had been removed to allow for maximum light). The damaged area as percentage of overlap area was calculated using a comparison of the pre-impact and post impact scans and images. Ultrasonic C-Scan and imaging offered comparable results of damage in 2D owing to the transparent nature of glass fibres and the fact that the thickness was also less (2.4 mm). These results albeit useful were not sufficient to describe the damage mechanisms in 3D and at micro level. Thus, a number of samples were chosen for damage mechanism observation using X-ray micro-tomography (XMT).

**Figure 1:** Schematic representation (not to scale) of impact testing setup and specimens.

XMT is one of the computed tomography (CT) techniques and refers to reconstructing a volume from its cross sectional projections. The cross sectional projections are obtained from x-ray transmission data. Since different materials absorb x-rays to different extent the internal micro-structure of an object can be revealed using the contrast difference. The XMT system used for this study was HMXST 225 supplied by X-Tek systems Ltd. Details of the equipment were discussed in [65] and details of technique can be read from [66]. In case of glass phenolic composites, XMT works particularly well because the level of
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attenuation offered by both the constituents is significantly different. This technique allowed for observing damage at any location within the specimen. Virtual cross sectional images and 3D surface views were generated at any required depth and angle without any need for physical cutting. Multi-planar, cross-sectional views were used to investigate the connectivity of damage in three dimensions. As an example of the XMT carried out in this study, figure 2a shows three orthogonal slices through the overlap region of a 21 mm overlap width joint impacted at 6.7 ms\(^{-1}\) velocity. Figure 2b shows for the same scan, a single oblique slice through the specimen, at a location where both delaminations can be seen simultaneously. XMT scans results in hundreds of such slices through the specimen (depending on resolution and area scanned). It can be seen from this example that this technique enabled the authors to determine the location, extent and connectivity of damage within a sample. The scan shown in figure 2 was performed at a resolution of 20.3 μm voxel size. Using scans at higher resolution (up to 10 μm) even more detailed damage features were studied. For each specimen XMT was done at various resolutions, thus revealing damage features ranging from millimetres to micro metres.
Figure 2: Sectional views using X-ray micro tomography for identifying the damage mechanisms in 3D

(a) Three orthogonal slices from an XMT scan (resolution 20.3 μm) (b) Oblique section view from the same scan showing delamination and disbond simultaneously.

In addition to these tests, microscopy of some selected samples was also carried out to observe the surface at delaminated interface. The remaining samples were tested for residual bond strength. Besides the impact tests, other tests such as the mixed mode bending tests for determining the fracture energies at various mode mixes (from pure mode I to pure mode II), three rail shear test to determine the shear modulus and tensile tests for finding tensile modulus were also performed. These tests were used for specifying the properties in FE model and were performed following applicable standard test methods. The details of these tests can be found in [65].
2.2 Details of Simulations

Explicit time integration was used to carry out a dynamic simulation of the actual impact event using finite element analysis software package ABAQUS/Explicit.

2.2.1 Model Geometry and Boundary Conditions

The model geometry and boundary conditions were specified as shown in figure 3. These boundary conditions closely approximated the boundary conditions described earlier for the actual experimental setup. FE simulations were carried out for all the lap joint geometries and impact velocities used in experiments. The impactor was modelled as an ‘analytically rigid’ part and velocity was specified as a ‘pre-defined field variable’. The projectile was constrained to move only along z-axis, thus not allowing for any slip of projectile during impact. The dynamic interaction between the projectile and the lap joint was modelled as frictionless ‘hard contact’ [67] in Abaqus/Explicit.

2.2.2 Mesh details

Each ply within the adherends and each interface between adjacent plies, was meshed using a separate layer of elements. Similarly, the bond interface (joint) between the two adherends was also meshed using separate layer of elements. This has been shown in zoomed view of mesh in figure 3. Thus each ply was meshed using a single layer through the thickness of reduced integration continuum shell elements (i.e. 8 node, reduced integration, hexahedron (SC8R) and 6 node reduced integration wedge (SC6R) [68]). Each of the layers (i.e. each ply) was connected to other through interface layer that was meshed using 8 and 6 node, three dimensional, cohesive zone elements (COH3D8 and COH3D6) [69]. The joint interface (bond layer) in the overlap region was also modelled using similar cohesive elements. The cohesive layer was generated using an offset mesh, meaning that the in-plane density of the mesh for the interfaces and the plies was the same; in fact, they had shared nodes at mating surfaces. Each continuum shell element had one element integration point and three section integration points (numerical integration of shell section using Simpsons rule), while each cohesive element had one element integration point. Thus in total the mesh had fifteen integration points through the thickness for the lap joint in the overlap region for the converged mesh (i.e. one for each ply and one for each cohesive zone). For the continuum shell elements, the outputs were requested at three section integration points for each ply (i.e. the top, mid and bottom...
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portion of each element) and hence, the outputs were available at thirty one points through the thickness.

Through thickness mesh density, similar to the one used in this study, has been found adequate to capture the bending and indentation response by other authors [20, 23]. In order to ascertain this however, comparable simulations having two stacked 3D continuum solid elements (C3D8R) for each lamina were also run. These models had twenty three integration points through the thickness as opposed to the fifteen discussed earlier. The results however, did not show any appreciable change in the measured damage area or peak contact force history, thus the mesh with one continuum shell elements and one cohesive element per ply was used for the remaining simulations. The choice for using continuum shell elements instead of solid elements was guided by the fact that as opposed to solid elements these did not have any restriction on aspect ratio.

![Mesh details and boundary conditions](image)

**Figure 3: Mesh details and boundary conditions (only half lap joint is shown for visualization purposes)**

The problem being modelled had three main sources of mesh dependency. The first one was due to modelling of contact between the lamina outer surface and the projectile outer surface. The mesh in this case had to be refined enough in the impact zone to prevent penetration of projectile into the lamina. This was successfully achieved and validated. The second source of mesh dependency was due to the reason
This is an author’s version of post print (final draft after referring) of the journal article in International Journal of Impact Engineering, Volume 80, June 2015, Pages 76-93, https://doi.org/10.1016/j.ijimpeng.2015.02.003 that contact between each lamina was handled by cohesive zone elements rather than by explicitly defining contact surfaces. The cohesive elements even after failure resist penetration under normal compression and small amount of shear loading. When the shear loading is large (relative to element dimensions), the elements may distort and allow interpenetration of plies [65, 70]. This was prevented by having a sufficiently refined mesh in the impact zone and around it. The third source of mesh dependency was related to the energy dissipation during the strain softening phase of modelling progressive damage in continuum elements. This was resolved by using a characteristic length in the formulation of cohesive element [65, 70]. Due to this characteristic length it was possible to define the damage propagation using a stress – displacement relation instead of stress-strain relation. Thus in this case, the energy dissipated during the damage process was specified per unit area rather than per unit volume. This allowed for a direct relationship between this energy and crack propagation displacement in a manner similar to the fracture mechanics approach of using critical energy release rate. Use of similar formulation has also been reported by [20, 23] and was found to greatly reduce the mesh dependency.

As indicated earlier, the mesh in XY plane in the overlap region and in particular in the indentation zone (i.e. the region with maximum possibility of damage) was denser than other regions. Such biasing of mesh for explicit analysis has also been reported in [20, 23, 39]. Since, explicit integration scheme was being used therefore it was expected that this biasing may cause some deviation of results as in certain regions stress wave propagation may not be captured adequately. This biasing was inevitable however, due to the limitation of computational resources required for the large number of cases to be run. If the entire mesh was made with a uniform mesh size of the smallest element edge length (i.e. 0.375 mm) then the mesh for 21 mm overlap joint (the smallest mesh case) had over a million elements (1,050,112 elements: 591,872 SC8R and 458,240 COH3D8) as opposed to around fifty thousand elements (51,328 elements: 27,304 SC8R; 784 SC6R; 22588 COH3D8 and 652 COH3D6) required for the biased mesh. The results of the sensitivity analysis showed that even with the use of over a million elements the maximum damage area did not change more than 3% and the differences in peak contact force and displacement were even smaller.
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Based on this, it can be concluded that the final mesh selected had to be a compromise between the quality of output and the computation time required for each case. This was considered acceptable because the variation in experimental damage area measurements (as commonly happens in composites) was also of a higher degree and because the intent of study was to explain the damage modes rather than generating design parameters.

2.2.3 Material model and failure criteria for adherends excluding interfaces and joint

The material model for each ply (i.e. each layer of continuum shell elements) was written in FORTRAN programming language and implemented via the user subroutine interface VUMAT in ABAQUS/Explicit. This material model was modified from the built in material model type 'lamina', which was a plane stress transversely isotropic (orthotropic) elastic material model. The modified material model followed the approach described by Li et al. [71] and thus took in account, in-plane shear non-linearity of the composite, by using a piece-wise bilinear approximation to the non-linear shear stress-strain curve. The material model can be described by the following set of equations,

\[
\begin{align*}
\tau_{11 i} &= \tau_{11 i-1} + (Q_{11} \ast \Delta \varepsilon_{11 i} + Q_{12} \ast \Delta \varepsilon_{22 i}) \\
\tau_{22 i} &= \tau_{22 i-1} + (Q_{21} \ast \Delta \varepsilon_{11 i} + Q_{22} \ast \Delta \varepsilon_{22 i}) \\
\tau_{33 i} &= 0 \quad \text{(The actual out of plane stresses are not zero as will be explained subsequently.)}
\end{align*}
\]

\[
\begin{align*}
\tau_{12 i} &= \tau_{12 i-1} + G_{12} \Delta \gamma_{12 i} \quad \text{for } |\tau_{12 i}| \leq |\tau_{nl}| \\
\tau_{12 i} &= \tau_{12 i-1} + G_{nl}^{12} \Delta \gamma_{12 i} \quad \text{for } |\tau_{12 i}| > |\tau_{nl}| \\
\Delta \gamma_{12 i} &= \gamma_{12 i} - \gamma_{12 i-1}
\end{align*}
\]

Where, \(i\) represents the \(i^{th}\) increment for which computation is being carried out in ABAQUS/Explicit and ‘1’ axis is taken along the warp direction and ‘2’ axis is taken along the weft (fill) direction.

\[
\begin{align*}
Q_{11} &= \frac{E_1}{1 - \nu_{12} \cdot \nu_{21}} ; \quad Q_{22} = \frac{E_2}{1 - \nu_{12} \cdot \nu_{21}} ; \quad Q_{12} = \frac{\nu_{21} E_1}{1 - \nu_{12} \cdot \nu_{21}} ; \quad Q_{21} = Q_{12} \quad \text{and} \quad \nu_{21} = \frac{E_2}{E_1} \nu_{12} ; \quad G_{12} \text{ is the shear modulus in the linear range and } G_{nl}^{12} \text{ is the linear approximation for the shear modulus in non-linear range and } \tau_{nl} \text{ is the stress level after which the non-linear shear behaviour was observed in a three-rail shear test as described in [65]. In the above equation the out of plane stress component (\(\tau_{33}\)) has been set to zero.}
\end{align*}
\]
This apparent anomaly is because for continuum shell elements like all shell elements the assumed stress state for constitutive relation is plane stress and thus out of plane stress component is reported as zero for these elements in ABAQUS. This however does not mean that the actual out of plane normal and shear components are zero as these are calculated based on the shell section properties as explained in detail in ABAQUS analysis user manual [72] and hence their definition is not repeated here. The input material properties used for defining each lamina for FE analysis are given in table 1. Note that in this table the out of plane section modulus was taken equal to the out of plane modulus of composite laminate while the transverse shell stiffness was specified as a function of ply thickness as recommended in the ABAQUS theory manual [73].

The material model used in this study does not take in account the strain rate sensitivity of composite and the adhesive layer. As opposed to CFRP materials, GFRP composites are known to be somewhat rate sensitive [20]. In absence of reliable material data at different strain rates however, the authors were compelled to make a judicious choice between either running the model with assumed data; or not to consider the rate effects at all. In this regard the paper of Heimbs et al. [74] was consulted. He experimentally evaluated the strain rate sensitivity of phenolic woven-glass fibre reinforced composites for the strain rates ranging from $10^{-4}$s$^{-1}$ to 50s$^{-1}$. He found out that there was an 88% increase in peak uniaxial tensile strength value in warp direction, 53% increase in weft direction and around 33% increase in shear strength but there was little change in elastic modulus. Thus the authors concluded that by ignoring the rate effects the model will be conservative and may predict more damage than the experimentally observed damage (which was verified later). Since the intent of modelling in this paper is not to generate design allowable rather it is to help understand the damage process, this simplification was preferred over running the model with assumed material data.

It is pertinent to mention that unlike delamination modelling, progressive failure was not modelled for the ply or lamina failure mechanisms. Only failure initiation stresses were evaluated by comparing three failure theories. These were the LARC03 (Langley research centre criteria 03) [45], Tsai-Wu criterion [46] and Max Stress theory [46]. The equations defining the failure indices for all these theories were implemented as part of the user subroutine mentioned earlier. There were a number of reasons for not
Sensitivity: Internal

modelling the ply failure in this case. Firstly, there is no consensus amongst researchers that which progressive damage modelling methodology should be adopted. Secondly, if the author had opted for continuum damage modelling (CDM) approach there is no consensus on which failure criteria should be used to derive the model required for modelling the ply failure. Thirdly even if a CDM based on LARC03 or Hashin damage is adopted [23, 45], even then the experimental effects of damage in woven composite in most cases cannot be adequately captured by these models which are primarily aimed at unidirectional or at best multi-directional laminates. Although there are better approaches in literature for modelling damage in woven composites [75], the material data required for these models was difficult to obtain and this may be taken up as future work in a study that improves the current model. Thus, the consequences of this simplification were weighed against the quality of results obtained. Experimental observations revealed that for the smaller overlap width joints, delamination and disbond were the dominant damage mechanisms. Thus, in these cases it was expected that deviation of simulation results from experimental values will be small. Even for cases of larger overlap width joints, for the velocity range under consideration, complete penetration of projectile or complete splitting of adherends was not observed experimentally, therefore, the results without modelling delamination failure were considered acceptable.

The consequences of this assumption have been discussed further in the results section.

### Table 1: Material properties for the individual ply used in FE models for Continuum Shell elements

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density of composite</td>
<td>1566.3 kg.m⁻³</td>
</tr>
<tr>
<td>$E_1$: Tensile modulus in 1 direction (Warp)</td>
<td>24.2 GPa</td>
</tr>
<tr>
<td>$E_2$: Tensile modulus in 2 direction (Weft)</td>
<td>23.1 GPa</td>
</tr>
<tr>
<td>$\nu_{12}$: Poisson ratio</td>
<td>0.2</td>
</tr>
<tr>
<td>$G_{12}$: (in-plane shear modulus in linear range)</td>
<td>3.85 GPa</td>
</tr>
<tr>
<td>$G_{12}^{nl}$: (in-plane shear modulus non-linear range)</td>
<td>1.04 GPa</td>
</tr>
<tr>
<td>Out of plane section modulus</td>
<td>7.71 GPa</td>
</tr>
<tr>
<td>$K_{11} = K_{22}$: transverse shear stiffness</td>
<td>0.482 MPa</td>
</tr>
</tbody>
</table>

**Lamina Strength**

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$X_t$: Tensile stress limit in warp direction</td>
<td>336.6 MPa</td>
</tr>
<tr>
<td>$X_c$: Compressive stress limit in warp direction</td>
<td>-298.4 MPa</td>
</tr>
<tr>
<td>$Y_t$: Tensile stress limit in weft direction</td>
<td>295.8 MPa</td>
</tr>
<tr>
<td>$Y_c$: Compressive stress limit in weft direction</td>
<td>-309.4 MPa</td>
</tr>
<tr>
<td>$S_{12}$: Shear strength in the X–Y plane</td>
<td>57.2 MPa</td>
</tr>
</tbody>
</table>
Material model for the joint interface and the interfaces within adherends

The joint interface (bond interface), the interfaces within adherends and their subsequent disbonding and delamination were modelled using cohesive zone elements. The material model used for these elements is based on traction-separation description of the interface [76, 77]. This approach allows for failure initiation prediction using a stress or displacement based failure criterion while the propagation is controlled by comparing the energy release rate (ERR) with the experimentally determined critical energy release rate \( (G_c) \) for this material. Since in this problem the mode of failure (i.e. normal, shearing or tearing) was not known at prior, a mixed mode cohesive model as described in [26, 77, 78] was used.

The interfacial tractions (\( \tau_i \)) were defined as \( \tau_i = K_i^\circ \cdot \delta_i \) where \( i = I, II, III \) represented the three modes of crack propagation (i.e. normal, shearing or tearing), and \( \delta_i \) were the corresponding separations between the opposite faces of the cohesive zone elements. The interface behaviour was assumed linear elastic up to crack initiation and damaging elastic thereafter. \( K_i^\circ \) is the penalty stiffness value and following Zhou et al. [25], it was defined as \( K_i^\circ = K_i \cdot \tau_i^c \) where \( \tau_i^c \) (i=I,II,III) are the interlaminar tensile and shear strengths respectively and the constant \( K_i \) can be assigned any value between 1E5 mm\(^{-1}\) to 1E7 mm\(^{-1}\). In this study \( K_i \) was fixed at 1E6 mm\(^{-1}\) while the values of \( \tau_i^c \) (as reported in table 2) were estimated from the tensile lap shear tests performed on single lap joints. Thus based on these values the penalty stiffness was evaluated using the previously defined equation \( K_i^\circ = K_i \cdot \tau_i^c \). It may be pointed out that the choice of this penalty stiffness also satisfies the requirement posed by ABAQUS for stable time increment [79]. The failure index \( (FI) \) for damage onset in the interface zone was calculated using quadratic nominal stress criterion defined in ABAQUS [70]. This was preferred over the maximum stress criterion because the polymer matrix due to transverse impact is under considerable multi-axial stress state, and the quadratic criterion is interactive and can thus more effectively take in account the interaction of different stress components.

After failure initiation the traction is progressively reduced using a scalar damage parameter. The evolution of this damage parameter (i.e. the damage evolution law), depends on how the ERR, for the damaged element (from FE analysis), relates with the critical energy release rate \( (G_c) \) for that mode mix.
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This is an author’s version of post print (final draft after referring) of the journal article in International Journal of Impact Engineering, Volume 80, June 2015, Pages 76-93, https://doi.org/10.1016/j.ijimpeng.2015.02.003 (based on experiments). The dependence of $G_c$ on mode mix was specified using the Benzeggagh-Kenane (BK) criteria [78]. The BK criteria can be mathematically expressed as,

$$G_c = (G_{IIc} - G_{IC}) \left( \frac{G_s}{G_T} \right)^\eta \quad (2)$$

Where, $G_{IC}$ is the pure mode I critical energy release rate and $G_{IIc}$ is the pure mode II critical energy release rate. $G_s = G_{II}$ is the mode II component of the energy release rate for a mixed-mode situation and $G_T = G_I + G_{II}$ is the sum of mode I and mode II components of energy release rate for a mixed mode problem. $\frac{G_s}{G_T}$ defined the mode mix for which the $G_c$ value had to be approximated and $\eta$ is a material parameter that gave the best fit to the experimentally determined $G_c$ values measured from standard mode-I and mixed mode bending tests (details of these test can be found in [65]). The results of these tests and BK criteria fit to the test data for two different values $\eta$ is shown in figure 4. The material data input for the cohesive zone model is given in Table 2. It should be pointed out that since no material data was available for mode III, therefore in keeping with the usual practice $G_{IIIc}$ was assumed to be equal to $G_{IIc}$, while implementing the model in ABAQUS.

![Figure 4: Experimentally measured fracture energies for various mixed mode ratios and BK-criteria fit to the data with two different values of parameter $\eta$.](image)
Table 2: Material properties for the cohesive zone elements in FE models

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density of resin (to be used for cohesive zone)</td>
<td>1085.0 kg.m$^{-3}$</td>
</tr>
<tr>
<td>$K_{nn} = K_{Ia}^0$ : Penalty Stiffness in mode I</td>
<td>4440 GPa</td>
</tr>
<tr>
<td>$K_{ss} = K_{IIa}^0$ : Penalty Stiffness in mode II</td>
<td>2220 GPa</td>
</tr>
<tr>
<td>$K_{tt} = K_{IIIa}^0$ : Penalty Stiffness in mode III</td>
<td>2220 GPa</td>
</tr>
<tr>
<td>$\tau^c_{I}$ : Inter-laminar tensile strength</td>
<td>44.4 MPa</td>
</tr>
<tr>
<td>$\tau^c_{II} = \tau^c_{III}$ : Inter-laminar shear strength</td>
<td>22.2 MPa</td>
</tr>
<tr>
<td>$G_{Ic}$</td>
<td>425 J.m$^{-2}$</td>
</tr>
<tr>
<td>$G_{IIc}$</td>
<td>905 J.m$^{-2}$</td>
</tr>
<tr>
<td>$\eta$</td>
<td>4.8</td>
</tr>
</tbody>
</table>

3. Discussion of Results

Different combinations of multiple damage mechanisms, such as indentation, matrix cracking, delamination, bond failure (disbond), tow splitting, fibre fracture (weave failure) and bulging were observed to varying extents for the lap joints impacted at different velocities. It was also observed that there is a strong dependence of observed damage mechanisms and area of damage on overlap width and impact velocity.

3.1 Damage area measurement

Damage area was calculated both experimentally using C-Scan and theoretically using FE simulations. Figure 5 shows a representative C-Scan for a test specimen and a corresponding FE simulation result. The red zone in Figure 5(i) indicates damaged area as measured using ultrasonic C-Scan. The value of experimental percentage damage area from C-Scan plotted in figure 6 was calculated by taking average of up to three tests results like the one shown for each impact velocity and overlap width. In figure 5(ii), the plot of damage variable (SDEG) for the cohesive zone elements at the bond interface, (i.e. failure of cohesive zone between lamina 4 and 5) measured from FE simulation, is shown. The red region shows elements which are more than 90% degraded. Such plots were drawn for all interfaces, overlap widths and impact velocities. It was observed that in each case the most severely delaminated interface is the bond interface. Thus based on such plots the damage area measured from FE analysis was calculated for each case. It may be pointed out that the damage area measurements for C-Scan were taken after the completion of test (i.e. once the elastic spring back had taken place) and required physically removing the sample from the rig, while for simulation results the area was measured after 1.81ms of the first impact. By this stage, although the specimen had not become completely stationary, it had rebounded and the...
Sensitivity: Internal

Vibrations were slowly decaying and damage area was not changing appreciably. There can be an argument that the simulation could have been allowed to run longer i.e. until the specimen had completely stopped, but this would have required much longer computational time without any appreciable increase in damage area.

Figure 6 compares the damage area values expressed as percentage of overlap area in each case for both experiments and FE simulations. This graph shows that the FE analysis predicts more damage than observed experimentally, however for both, the data trend, i.e. damage area reduces with increasing overlap width, is consistent. Thus in general, the data trend for experiments and simulations agree and the model results can be considered as conservative. The over prediction of model results can be explained with the help of x-ray micro tomographic evidence. XMT shows that there was significant interaction between inter-laminar and laminate failure mechanisms.

Figure 5: C-Scan of overlap area of one of the test specimens and a corresponding FE simulation result showing contour plot of damage at bond interface.
Figure 6: Comparison of FE damage area prediction and average experimental damage area values from C-Scan expressed as percentage of total area of overlap in each case for the scenarios where impact velocity was 4.0 m/s\(^{-1}\), 5.5 m/s\(^{-1}\), 6.7 m/s\(^{-1}\), and 7.9 m/s\(^{-1}\).

It will be explained in more detail later that for the higher velocity cases of wider joints significant macro laminate failure mechanisms were observed. Significant energy may be dissipated in these failure mechanisms and in addition to this the failed or collapsed weave sometimes created physical barriers to delamination crack opening. The consequence of this damage mechanism interaction is that regardless of the severity of damage in the damage zone the actual extent of delamination area (which is what C-Scan is measuring) will be less for such cases. On one hand, this highlights the limitation of using C-Scan and other similar 2D techniques alone because they only measure the 2D projection of damage area which does not necessarily always reflect the severity of damage. On the other hand, this also points out the limitation of the model that unless the model completely captures the damage mode interaction it will fail to capture the true extent of delamination. A model which only models delamination failure may predict a larger damage area than the one actually observed in experiments because firstly there is no possibility of modelling the physical barrier that the collapsed or pushed-out weaves from adjoining plies create for...
Sensitivity: Internal

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Secondly, in such a model the kinetic energy of the projectile must either be converted into elastic strain energy of the laminate or be used for advancing the delamination crack front. This will result in a larger delamination area as compared to a model, in which part of the kinetic energy of the projectile had also been utilized for advancing the damage in the adjoining ply itself. Such limitation of a model that only accounts for delamination has also been recently reported by Aymerich et al. [23]. Despite the limitations, the FE simulations gave useful insight into the damage propagation mechanism; and when reviewed together with the experimental observations these were also useful for explaining the evolution of various damage mechanisms.

### 3.2 Characterization of damage mechanisms

The presence of multiple damage mechanisms made it difficult to identify a pattern or trend in the evolution of damage with change in overlap width and impact velocity. The pattern however became more evident once damage mechanisms were categorized under the two well-known broad classes. That is, the **laminate failure mechanisms**, which included both fibre and matrix failure mechanisms at micro and macro length scales (such as indentation, matrix cracking, fibre fracture, weave failure etc.), and **inter-laminar failure mechanism** which included delamination within adherends and disbond (which is defined as the delamination that occurred specifically at joint interface between the two adherends in overlap region). The damage mechanism observations for all sets of experiments have been summarised in table 3 and will be discussed subsequently.

#### 3.2.1 Laminate failure mechanisms

The laminate failure mechanisms can be further classified in terms of length scale at which these occur i.e. **Micro** or **Macro** failure mechanisms.

(a) **Micro Laminate failure mechanisms**

Once the hemispherical projectile impacted the top surface of the adherend; before the lap joint could bend significantly as a whole structure; local deformation under the nose of impactor took place. The severity of local indentation for a composite panel was previously shown to be linked with mass of projectile [17]. In this study the mass of impactor was kept constant throughout and only velocity was...
Sensitivity: Internal

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varied. The results showed that for single lap joint having composite adherends the overlap width played a significant role in determining whether the overall flexural response or the local indentation dominated. As summarized in Table 3, for cases where either the impact velocity was low (6.7 ms\(^{-1}\) or less) or where the overlap width was small (the 21 mm case), the local indentation lead to multiple micro laminate failure mechanisms. The observed failure mechanisms that have been classed under micro laminate failure mechanisms were micro indentation, fibre tow splitting or loosening, micro matrix cracking and localized fibre tow rupture within the weave.

The cumulative effect of all the micro damage mechanisms was observed using C-Scan while the detailed observations were carried out using X-ray micro Tomography. Thus Figure 7 explains how the C-Scan and tomography were used in support of each other to carry out multi-scale damage mechanism observations. In this case, figure 7(a) shows C-scan of a 25 mm overlap width specimen, impacted at a velocity of 6.7 ms\(^{-1}\) (4.5 J). This shows micro damage which was barely visible on visual inspection. Figure 7(b) shows for the same specimen XMT view (xy-plane) of the inside of the specimen. This shows fibre tow splitting and flattening due to micro indentation at interface zone of lamina 5 and 6 (counting from impact side). Similarly in figure 7(c) for the same specimen xz-view along the plane cut by marked line shown in figure 7(b) is presented. This shows fibre tow splitting and inter-laminar matrix cracks – the dots in tows (out of plane) show extensive fibre tow splitting throughout thickness.

By comparing tomography scans of specimens at various impact velocities it was identified that in the indentation zone the fabric started bending locally under the influence of impacting projectile. The matrix allowed for the fibres to push on the underlying laminae. The bending of fibre weave and the shearing against the matrix resulted in fibre tow splitting (loosening and consequently flattening). Depending on the compactness of the lamina in the indentation zone (compactness in terms of resin, fibre and void content.) and the impact velocity level the matrix directly under the impact load yielded locally and was squeezed out from underneath the pressing fibres. Due to the weave however, such resin shear deformation remained highly localized as the resin packed in the interstices locations. This made these zones denser – and hence the cumulative effect of micro indentation and related damage mechanisms was picked up in C-Scans and in transmission photography as a near circular disc or ring (this is also shown in
For the cases where the extent of weave deformation was higher than the extent to which the matrix could yield, local micro cracks at fibre resin interface and between plies developed.

**Figure 7**: A representative composite image obtained by combining a C-Scan and two XMT views of an impacted specimen, showing micro-laminate failure mechanisms.

The results (summarized in Table 3), indicated that once the velocity was higher than a minimum threshold in each case micro indentation phenomenon was observed. The minimum threshold varied with the overlap width. If the velocity was lower than the minimum threshold then there was only perfectly elastic response. The 21 mm overlap joint had lesser effective strength as compared to the 46 mm overlap width joint due to smaller overlap area. Thus for the smaller overlap width case, the joint delaminated without significant local indentation and at lower peak impact force. It will be shown with the aid of simulation results later that as the projectile rebounded, for the smaller overlap width joint, the overall bending of the joint rather than indentation became more pronounced and the effect of peel and compressive stresses at the opposite joint free edges became apparent.
(b) Macro Laminate failure mechanisms

For those specimens that had sufficient overlap area to prevent disbonding followed by micro indentation, the macro laminate failure mechanisms were observed. The details are as follows:

a. **Macro indentation and bulging**: For higher velocity cases, the observed macro indentation and bulging was an amplified form of micro indentation phenomenon due to the increase in absorbed energy in the absence of delamination. The underlying mechanisms, i.e. ‘matrix yielding’, ‘fibre tow splitting’, ‘matrix cracking’ and ‘fibre push out’ were the same as in micro indentation, however, it was the scale that was magnified.

b. **Matrix cracking**: In-plane and out of plane matrix cracks developed in the indentation zone. These generally resulted in complete loss of support to fibres.

c. **Fibre push out**: The fibres in the indentation zone were pushed out of the matrix. Further increase of impact velocity lead to catastrophic failure/rupture of fibre tows in many cases.

**Table 3: Relative severity of damage mechanisms for changing velocity and overlap-width**

<table>
<thead>
<tr>
<th></th>
<th>Case 1</th>
<th>Case 2</th>
<th>Case 3</th>
<th>Case 4</th>
<th>Case 5</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Damage mechanism</strong></td>
<td>1.64J / 4.0ms⁻¹</td>
<td>3.01J / 5.5ms⁻¹</td>
<td>4.5J / 6.7ms⁻¹</td>
<td>6.2J / 7.9ms⁻¹</td>
<td>8.01J / 9.6ms⁻¹</td>
</tr>
<tr>
<td><strong>Surface VID¹</strong></td>
<td>None</td>
<td>None</td>
<td>Barely visible (BV)</td>
<td>BV (&lt; case 3)</td>
<td>BV (&lt; case 4)</td>
</tr>
<tr>
<td><strong>Micro laminate²</strong></td>
<td>Some</td>
<td>Dominant</td>
<td>Dominant</td>
<td>Significant</td>
<td>Significant</td>
</tr>
<tr>
<td><strong>Macro laminate³</strong></td>
<td>None</td>
<td>None</td>
<td>Some</td>
<td>None</td>
<td>None</td>
</tr>
<tr>
<td><strong>Inter laminar⁴</strong></td>
<td>None</td>
<td>Joint Free edge</td>
<td>Dominant</td>
<td>Dominant</td>
<td>Dominant</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th></th>
<th>Case 1</th>
<th>Case 2</th>
<th>Case 3</th>
<th>Case 4</th>
<th>Case 5</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Surface VID¹</strong></td>
<td>None</td>
<td>None</td>
<td>BV</td>
<td>BV</td>
<td>Clearly visible (CV)</td>
</tr>
<tr>
<td><strong>Micro laminate²</strong></td>
<td>Some</td>
<td>Dominant</td>
<td>Dominant</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td><strong>Macro laminate³</strong></td>
<td>None</td>
<td>None</td>
<td>Some</td>
<td>Dominant</td>
<td>Dominant</td>
</tr>
<tr>
<td><strong>Inter laminar⁴</strong></td>
<td>None</td>
<td>Joint Free edge</td>
<td>Joint Free edge</td>
<td>Dominant</td>
<td>Dominant</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th></th>
<th>Case 1</th>
<th>Case 2</th>
<th>Case 3</th>
<th>Case 4</th>
<th>Case 5</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Surface VID¹</strong></td>
<td>None</td>
<td>BV</td>
<td>BV (&gt; case 2)</td>
<td>CV</td>
<td>Severe</td>
</tr>
<tr>
<td><strong>Micro laminate²</strong></td>
<td>None</td>
<td>Dominant</td>
<td>Dominant</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td><strong>Macro laminate³</strong></td>
<td>None</td>
<td>None</td>
<td>Some</td>
<td>Dominant</td>
<td>Dominant</td>
</tr>
<tr>
<td><strong>Inter laminar⁴</strong></td>
<td>None</td>
<td>None</td>
<td>None</td>
<td>Some</td>
<td>Some</td>
</tr>
</tbody>
</table>

Sensitivity: Internal
Figure 8 shows two sections of the lower surface of ply 1 (lamina 1), i.e. the back face lamina of a 25 mm overlap specimen. This was impacted at 9.6 ms\(^{-1}\) velocity and suffered a combination of macro laminate failure mechanisms and inter-laminar failure (delamination/disbond). In this figure, only macro failure mechanisms have been discussed. In the first view in figure 8, the effect of matrix plastic deformation that led to fibre push out has been shown. In the second view (figure 8), extensive fibre tow splitting can be seen.

**Figure 8:** (a) Tomography slice ply 1 (i.e. bottom most ply) – note how the weave has been spaced out due to impact – This damage mechanism is being called fibre push out (b) Tomography slice ply 1 at bulge location – see the extensive two splitting in pushed out fibres

### 3.2.2 Inter-laminar Failure Mechanisms - Delamination and Disbond

Delamination for the purpose of XMT observations was defined as an inter-laminar matrix crack that extended continuously over a significant area and caused a separation between interfaces that was greater than 0.1 mm. Such a definition was necessary to distinguish between inter-laminar micro cracks in matrix from delamination. A disbond was defined as a delamination that took place at the joint interface (i.e.
Sensitivity: Internal

between the top and bottom adherends in the overlap region). A strong influence of overlap width on
delamination / disbond was observed. For co-cured, centrally impacted, single lap joints, this study found
that the maximum delamination damage occurred at joint interface and varying degree of delamination
damage occurred at other interfaces. The delamination damage was classified based on initiation and
propagation mechanism and the location (joint interface or adherend interfaces) at which it took place.
Thus a disbond took place through two mechanisms either independently of each other or in combination,
depending on the joint width.

a. It propagated outwards from the region directly under the impactor in indentation zone. These
cracks generally stopped propagating or deflected when the collapsing weave from adjacent layers
blocked their path. Based on XMT and micrographs, it appeared that the mechanisms occurred
under mixed mode with type II component being more significant. The mechanism was more
pronounced for joints with greater overlap width.

b. Delamination/disbond initiating from back side joint free edge and propagating across the
interface without deflection. This is mainly due to type I loading and was more commonly
observed for narrower overlap joints.

Delamination at ply interfaces other than the bond interface was observed to take place through the first
of these two mechanisms with the exception of a case where the impactor had hit the joint nearer to the
constrained edges. In that case multiple delamination fronts starting from joint free edge were observed.

It was observed that whenever impactor velocity was 6.7 m/s (4.5 J impact energy) or greater, the
dominant failure mechanism for the 21 mm overlap width joints was disbonding. In case of 25 mm
overlap width joints, delamination was first observed when the impactor velocity was 7.9 m/s (6.2 J
impact energy). It was not the dominant mechanism, however, and occurred only when the applied
loading was off centre. When the impactor velocity was further increased to 9.6 m/s (8 J impact energy)
or higher, delamination became the dominant damage mechanism for this overlap width. In case of 36
and 46 mm overlap specimens no disbond and delamination was observed beyond the indentation zone
This is an author’s version of post print (final draft after referring) of the journal article in International Journal of Impact Engineering, Volume 80, June 2015, Pages 76-93, [https://doi.org/10.1016/j.ijimpeng.2015.02.003](https://doi.org/10.1016/j.ijimpeng.2015.02.003) for impactor velocity of less than 9.6 ms\(^{-1}\) (impact energy less than 8 J. In these cases, the delamination strongly interacted with other failure mechanisms.

In general, for woven composites the value of mode II fracture energy is about two to three times higher than the mode I fracture energy [80, 81]. For the specimens in this study the experimental value of mode I toughness \(G_{IIc}\) was 425 Jm\(^{-2}\) and mode II toughness \(G_{IIC}\) was 905 Jm\(^{-2}\). Thus, type II delamination failure was only observed when the loading conditions excluded significant mode I presence. In the current problem the smaller the overlap region the greater will be the effect of peel stresses at the backside free-edge of the joint and hence mode I type failure will dictate. This was especially true for the smallest overlap (the 21 mm) case. In these joints no macro laminate failure mechanisms was observed. Even for impactor velocity as high as 9.6 ms\(^{-1}\) (i.e. impact energy 8 J) very little surface and laminate damage and almost total disbond failure was observed.

As an example the C-scan in figure 9a shows a 21 mm overlap width specimen (velocity 7.9 ms\(^{-1}\) or impact energy 6.2 J) with central micro damage zone and the trapezoidal delamination zone. Using tomography (for example figure 9b) the major delamination was verified to be present at joint interface (lamina 4-5 interface). Similarly the micro indentation zone could also be identified.

**Figure 9:** (a) C-scan showing a 21 mm overlap width specimen – (impactor velocity 7.9 ms\(^{-1}\)) – note the central micro damage zone and the trapezoidal delamination (lines added at end of trapezoid for making the shape prominent) (b) (c) Tomographic views of the same specimen showing failure at joint interface (disbond) and micro indentation.
3.3 Further explanation of damage based on FE simulation

The damage area predicted by FE simulations has already been shown and discussed using figure 6. In this section the deformed shapes and corresponding damage states for 21 and 46 mm joints have been compared to explain the observed dependence of damage on overlap width. Figure 10 to 14 plots the deformation and damage in lap joints of 21 mm and 46 mm overlap width for impact velocity of 5.5 ms$^{-1}$ at different instances of time. In these figures in the first plot in each case the actual value of deformation in meters is plotted against the x-distance along the overlap width for each case. Here x = 0 represents the point of first impact/contact and positive distance is measured from x = 0 to impact-side free edge of the lap joint, while negative distance is measured from x=0 to backside free edge of the joint. The second graph in each case shows the same deformation values plotted on a normalized scale. The normalization is with respect to maximum out of plane deformation calculated for this impact velocity.

Figure 10 shows the deformation and damage state at t=0.04 ms after the first contact (i.e. t=0). At this stage the joint had just started to experience indentation damage below the tip of impactor. As shown at this stage, none of the interfaces was completely degraded and the deformation could be considered entirely elastic. As expected, the deformed shapes for both overlaps at this stage were almost identical and based on the normalized plot it can be seen that at this instance the out of plane deformation was around 20% of its peak value.
Figure 10: FE based deformation and damage plots for lap joints of 21 mm and 46 mm overlap width for impact velocity of 5.5 ms\(^{-1}\) at 0.04 ms after initial contact.

Figure 11 presents the deformation and damage state at \(t = 0.1\) ms. Till this stage the projectile had moved further down and the indentation at this stage was around 40\% of the peak indentation experienced by the joints for this velocity (shown in the second plot in Figure 11). By this stage all interfaces had experienced indentation failure just below the point of impact. It is interesting to note that the shape of damaged zone below the first interface is in the form of a near circular ring and this was in keeping with the experimentally observed shape of damage zone for low velocity impacts.

As the projectile moved further down the peak deformation for this impact velocity was experienced at \(t = 0.87\) ms after the initial contact. This has been shown in figure 12. It is interesting to note that for the 21 mm joint the impact side joint free edge has slightly greater downward deformation than the backside joint free edge. This indicates that the peel separation of joint is more likely from the back side joint free edge and the impact side joint free edge is acting almost as a virtual hinge (pivot) point for the opening at the opposite free edge. A closer look revealed that by this stage for 21 mm joint a delamination/bond failure initiating from the backside joint free edge had appeared and this was in addition to the damage of interface that had expanded outwards from below the indentation zone.
**Figure 11:** FE based deformation and damage plots for lap joints of 21 mm and 46 mm overlap width for impact velocity of 5.5 ms\(^{-1}\) at 0.1 ms after initial contact.

**Figure 12:** FE based deformation and damage plots for lap joints of 21 mm and 46 mm overlap width for impact velocity of 5.5 ms\(^{-1}\) at 0.87 ms after initial contact.
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The difference in bending shape became much more pronounced after this stage, as the projectile rebounded. This has been shown with the help of figure 13 and 14 which correspond to t=1.41ms and 1.81ms respectively. For the 21 mm joint, the joint edges had undergone considerable deformation and thus delamination that initiated from these edges could propagate towards the point of initial impact. On the other hand for the 46 mm joint, the only delamination front that propagated was from the impact location towards joint free edges. This resulted in significantly greater percentage delamination with respect to overlap width for 21 mm joint. Since the simulation did not cater for macro laminate failure mechanisms, the delamination that started from either of these points propagated purely on the basis of mode-mix of fracture for the cohesive zone. In reality, as explained earlier, for larger joints the macro laminate failure mechanisms limited the extent of damage area. In such cases the damage may be limited to a smaller area but it is more severe in terms of mechanisms involved (e.g. local weave failure). It can be seen from figure 14 that during the rebound phase, once the stress wave had reflected, the return path for deformation profile was not the same in both cases because of the greater degradation of overlap region for the 21mm joint. In order to sum up the discussion figure 15 shows the damage at each interface of both the joints at the same instance as discussed in figure 14.
**Figure 13:** FE based deformation and damage plots for lap joints of 21 mm and 46 mm overlap width for impact velocity of 5.5 ms$^{-1}$ at 1.41 ms after initial contact.

**Figure 14:** FE based deformation and damage plots for lap joints of 21 mm and 46 mm overlap width for impact velocity of 5.5 ms$^{-1}$ at 1.81 ms after initial contact.
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**Sensitivity: Internal**

<table>
<thead>
<tr>
<th>Interface of Lamina 7-8</th>
<th>Interface of Lamina 6-7</th>
<th>Interface of Lamina 5-6</th>
<th>Interface of Lamina 4-5</th>
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**Dimensions:**
- Interface of Lamina 7-8: 21mm x 87.5mm
- Interface of Lamina 6-7: 21mm x 87.5mm
- Interface of Lamina 5-6: 21mm x 87.5mm
- Interface of Lamina 4-5: 21mm x 87.5mm

**DAMAGE VARIABLE**

- 1.00
- 0.00

**Additional Information:**
- 46mm x 75mm
3.4 Generalization of results and the concept of characteristic overlap width

Grouping the results discussed in previous section in terms of change in dominant damage mechanism with increasing impact velocity for a given overlap width, highlights three discerning patterns. These are;

**Pattern 1:** In pattern 1, for velocities up to 5.5\(\text{ms}^{-1}\), ‘Micro damage mechanisms’ were dominant whereas if the impact velocity lied between 5.5 \(\text{ms}^{-1}\) to 6.7 \(\text{ms}^{-1}\) then ‘Localized indentation related delamination in impact zone’ became dominant. On increasing the impact velocity further (i.e. 6.7 \(\text{ms}^{-1}\)to 9.6 \(\text{ms}^{-1}\)), ‘Delamination at joint interface (disbonding)’ became the dominant failure mechanism.

**Pattern 2:** In pattern 2, for velocities up to 6.7\(\text{ms}^{-1}\), the damage pattern was similar to Pattern 1, whereas if the impact velocity was increased further (i.e. 6.7 \(\text{ms}^{-1}\)to 9.6 \(\text{ms}^{-1}\)) then ‘Macro Laminate failure mechanisms’ became dominant at velocities higher than 6.7\(\text{ms}^{-1}\) (i.e. no significant global delamination and disbonding took place)

**Pattern 3:** In pattern 3, again for velocities up to 6.7\(\text{ms}^{-1}\), the damage pattern was similar to Pattern 1 and 2, whereas, if the impact velocity was increased further (i.e. 6.7 \(\text{ms}^{-1}\) to 9.6 \(\text{ms}^{-1}\)) then instead of one dominant damage mechanism a balanced mix of ‘Disbond and Macro laminate failure mechanisms’ was observed.

In summary, these patterns were observed as follows:

a. Pattern 1 was mainly observed for 21 mm overlap joint

b. Pattern 2 was observed for 36 and 46 mm overlap width joint.

c. Pattern 3 was observed for the 25 mm overlap width joint.

Thus these observations pointed to the fact that in the low velocity/energy regime (the one observed in this study) there appeared to exist a lower characteristic overlap width such that if the overlap width was less than the characteristic width the dominant damage mechanism was always a combination of micro
damage under the impactor nose and a delamination/disbond that initiated from back side joint free edge and propagated across the joint interface, primarily due to bending, resulting in peel type loading of joint (pattern 1). Similarly for the same energy and velocity range, an upper characteristic width could also be defined such that if the overlap width was greater than this upper characteristic width; the dominant failure was always a combination of micro and macro laminate damage mechanisms, with no or little inter-laminar failure (i.e. pattern 2). Interfacial matrix cracks, however, existed in the indentation zone but these did not appear to propagate in a continuous manner. For the energy and velocity range investigated in this study, the lower characteristic width in this case was found to be around 21 mm while the upper characteristic width was found to be around 35 mm. In between the two limits pattern 3 dominated. This characteristic width is specific to a particular energy range, joint geometry and boundary conditions. In future work, it may be possible to show the relationship of this width with the impactor tip diameter, tip shape and material.

4. Conclusions

The paper has given a detailed account of impact induced damage mechanisms observed for composite lap joint having different overlap widths (areas), which were impacted with increasing velocities. The damage mechanism observations were carried out at various length scales using mainly a combination of x-ray micro-tomography and ultrasonic C-scan. The damage mechanism observations were supplemented with detailed delamination modelling (FE analysis) to aid the understanding of how the damage mechanisms changed for different widths in response to increasing velocities.

Based on these observations and understanding from FE analysis it has been suggested that the dominant damage mechanism for a given velocity range is a function of overlap width. Thus, if the overlap width is less than the lower characteristic width, then disbonding dominates as a failure mechanism, for velocities that can cause visible impact damage. Whereas, if the overlap width is greater than the upper characteristic width then macro laminate failure mechanisms rather than delamination and disbonding dominates for comparable velocities as in previous case. If the overlap width is between these two limits then a balanced mix of delamination/disbonding and laminate failure mechanisms take place.
Further work is required to explore if this parameter can be used as a design guide for such joints based on residual strength of such impacted joints. It is also acknowledged that the choice of boundary condition (in particular the free breadth of joint) and the choice of impactor shape and material (e.g. if the impactor is of a material that is softer than the joint material) may change the observed damage pattern.

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References

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