Development of Design Allowables for the Design of Composite Bonded Double-Lap Joints in Aerospace Applications

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Abstract

This paper describes progress towards developing design guidelines for a number of composite bonded joints in aerospace applications. The premise of a universal failure criterion is impractical given the number of adherend-adhesive configurations and layups available. However, for a finite number of joint configurations, design rules can be developed based on experimental test data and detailed finite element (FE) modelling. By using these techniques rather than the traditional overly conservative knock down factors, more of the performance of composite bonded joints can be accessed. The work presented here experimentally studied the effect of the substrate layup, adhesive type and adhesive thickness on double-lap joint (DLJ) strength. The corresponding failure surfaces were analysed and failure modes identified. Following this, detailed FE models were developed to identify the trends associated with altering joint parameters. Finally, the stresses and strains within the adhesive and substrate were analysed at the joints respective failure loads to identify critical parameters. These parameters can provide an insight as to the stress state of the joint at failure or near failure loads, and hence its true performance.

Keywords: joint design, composite, finite element stress analysis, microscopy

1. Introduction

The use of composite materials has grown significantly in recent years resulting in a demand for updated design protocols which better capture their performance. Adhesive bonding as a joining mechanism is used extensively, primarily due to the reduced mass penalty and more uniform distribution of load compared to mechanically fastened joints. Despite substantial research in the field, reliable failure criteria that can be used across multiple composite bonded joint configurations remains problematic [1]. Fibre reinforced plastic (FRP) bonded joints are inherently difficult to model due to the complex combination of potential failure modes present.

Early analytical investigations concerned with the mechanical response of bonded joints were developed by Volkersen [2] and Goland and Reissner [3]. Volkersen introduced the concept of differential shear. Goland and Reissner were the first to consider the effects of eccentric load paths and to include the adhesive peel stress. Several researchers have since contributed

to the refinement of closed-form solutions, most notably Hart-Smith [4], [5]. Da Silva et al. [6] presented a summary of the development of classical closed-form techniques since Volkersen.

Due to the maturity of the field, a significant amount of literature exists examining the behaviour of bonded joints. Single-lap joints (SLJ) and double-lap joints (DLJ) are commonly discussed in literature due to their widespread use in industry. However, variations of the traditional lap design have been heavily investigated in recent years by several authors due to their potential performance benefits. Most notably, Avila and Bueno (2004) investigated the novel design of a 'wavy' bonded joint using both numerical and experimental methods. They observed an increase in joint load bearing capacity of approximately 41% when compared to traditional SLJs of similar design [7].

Several researchers have also investigated the influence of varying joint parameters on composite bonded joint strength [8]. In these studies, researchers have investigated the effect of bonded surface preparation, adhesive thickness, spew fillet, adherend stacking sequence, adherend ply angle, environmental conditions etc [9], [10]. Banea and da Silva (2008) provide a useful review on the aforementioned influences on bonded joint performance [11]. Kanerva and Saarela (2013) studied the effect of peel ply surface treatment against plasma and blasting treatment for composite adherends. The authors found that while peel ply treatment does increase surface free energy compared to untreated substrates, the use of plasma or mechanical abrading treatments provide greater bond strength [12]. Belingardi et al. (2002) investigated the effect of the spew shape and size on adhesive stresses in adhesive joints. Peak stresses were found to be dependent on the size and shape of the spew fillet. Hence, careful consideration of spew fillet shaping can significantly reduce stress concentration [13]. Environmental factors are equally important factors when designing joints. The main environmental factors to consider are temperature and humidity, both in operation and manufacturing. Exposure to climates outside the design specification can lead to permanent chemical and physical changes to the adhesive. Parker (1990) studied the effect of adverse environmental conditions on CFRP bonded joints. The study showed that exposing dried laminates to humid environments prior to bonding can be detrimental to joint performance [14]. Meneghetti et al. (2012) studied the failure mechanisms associated with fatigue damage in composite bonded joints. The authors placed emphasis on the effect of surface ply orientation, stacking sequence, adhesive fillet geometry and overlap length. The authors observed failure to initiate at the adhesive-adherend interface with 0° surface plies. Failure progresses into the subsequent 90° layer leading to multiple inter/intralaminar failure paths [15].

Recently, research in this field makes use of complex techniques, employing cohesive zones or ductile damage material models to accurately predict the progression of failure in composite bonded joints [16]. These techniques are favoured over traditional failure criteria as researchers note the former offer less precise predictive capabilities [17]. Complicated failure criteria do exist, however, a considerable amount of experimental work is necessary before they can be deemed universal. In an industrial setting, complex and time-consuming techniques are not suitable where rapid prediction is required. Hence, engineers within industry may compensate for modelling uncertainties associated with simplified modelling techniques [18].

Given the lack of fast and reliable composite bonded joint design tools, the following work aims to develop a novel methodology for identifying the performance of composite bonded joints in aerospace applications. The novelty in the work presented lies in the way in which relatively simple techniques can be consolidated to achieve a significant improvement in performance for a select number of composite bonded joints. While complex damage models are improving in terms of industrial applicability, they are not yet practical given the additional material data needed to conduct such analyses and the time penalty associated with non-linear modelling. Thus, in an industrial context, the approach adopted in this study was considered the most efficient and cost effective solution. The simple modelling techniques facilitate the rapid analysis requirement by maintaining simple linear-elastic modelling techniques (and avoiding complex non-linear damage models), whilst introducing improved design allowables.

Section 2 investigates the effect of adhesive modulus, substrate stiffness, substrate architecture and adhesive thickness on joint performance. Section 3 describes the development of detailed finite element models used to identify trends in stresses and strains associated with manipulating joint configuration. Section 4 uses the detailed numerical models to identify critical parameters at which failure is known to occur. 'Safe' values are also identified which confidently predicts a stress at which the material remains undamaged. Finally, the key outcomes and failure criteria developed from this study are summarised.

2. Experimental Study

2.1 Method and Materials

To develop more robust predictive techniques, an array of experimental work must first be carried out, which evaluates the real performance of various joint configurations. In this study, composite double-lap joints (DLJ) of varying composite substrate materials and layups and adhesive thicknesses and materials were investigated. The test programme was selected based on a commonly used joint configuration in the aerospace industry. All tests were carried out at room temperature in ambient conditions. The structural joints detailed in this study are designed to withstand the initial launch phase of a satellite into orbit. These structural components are typically shielded from the external environment. Consequently, environmental factors such as temperature and humidity are not the main focus of this study.

Each specimen was manufactured and tested to ASTM D3528-96 specifications, these being a nominal overlap length of 12.9 mm and joint width of 25.4 mm. Substrates are required to be cut from a single 300x300 mm CFRP panel as per the ASTM standard, to minimise variances that may be introduced from manufacturing multiple panels. The CFRP substrates were surface prepared using the glass-bead abrasion technique followed by a water-break test to ensure the surface has been adequately prepared for bonding. All adhesive fillets were controlled using PTFE rods of 2 mm diameter. The bondline thickness was controlled using bondwire placed between each specimen, which were then removed during the cutting process.

Two common aerospace carbon fibre reinforced plastic (CFRP) prepregs were used. The first consisted of an epoxy resin matrix system (MTM44-1) and intermediate modulus fibres (IMS65). The second, a cyanate ester resin system (HTM143) and high-modulus fibres (M55J). The prepreg woven plies consisted of a 2x2T architecture. The structural adhesives

were purposely chosen due to their distinctly different mechanical properties (3M 9323-low modulus, EA 9394-high modulus).

All tests are conducted under carefully controlled loading and environmental conditions. In the future, this approach could be extended to a wider range of operating conditions if necessary.

The joint configurations are outlined in Table 1, five repeats were tested for each specimen to identify variance. Apart from Test G, EA 9394 adhesive was used in all joint configurations.

Table 1: DLS test programme

Config	Substrate			Adhesive	
	Layup	Thickness (mm)	Material	Adhesive Material	Thickness (mm)
Test A	[0/45/90/45/-45/0/45/90] _s	2.05	Low modulus quasi-isotropic with surface ply orientated in the 0° direction	EA 9394	0.25
Test B	[90/45/0/45/-45/90/-45/0] _s	2.05	Low modulus quasi-isotropic with surface ply orientated in the 90° direction	EA 9394	0.25
Test C	[W/0/45/90/45/-45/0/-45/90] _s	2.46	Low modulus quasi-isotropic with 0/90° woven surface ply	EA 9394	0.25
Test D	[0/45/90/45/-45/0/45/90] _s	1.84	High modulus quasi-isotropic with surface ply orientated in the 0° direction	EA 9394	0.25
Test E	[90/45/0/45/-45/90/-45/0] _s	1.84	High modulus quasi-isotropic with surface ply orientated in the 90° direction	EA 9394	0.25
Test F	[W/0/45/90/45/-45/0/-45/90] _s	2.26	High modulus quasi-isotropic with 0/90° woven surface ply	EA 9394	0.25
Test G	[W/0/45/90/45/-45/0/-45/90] _s	2.46	Low modulus quasi-isotropic with 0/90° woven surface ply	3M 9323	0.25
Test H	[W/90/45/0/45/-45/90/-45/0] _s	2.46	Low modulus quasi-isotropic with 0/90° woven surface ply	EA 9394	0.40

The experimental setup consisted of an INSTRON 6025 5500R test machine, with mechanical wedge-action grips and a 100 kN load cell. A fixed loading rate of 1 mm/min was applied to all test specimens. Extension was measured across the overlap region using an extensometer of 75 mm gauge length and ± 0.00075 mm resolution.

2.2 Results and Discussion

The experimentally determined failure loads are presented in Figure 1.

Figure 1: Experimentally determined failure loads with minimum and maximum scatter

In all specimens tested, no apparent non-linear behaviour was observed (see Figure 2). Stiffness checks for each specimen were conducted using extensometer and load cell data to ensure sensible results were being obtained. Experimental stiffness was also compared to FE stiffness to ensure complete correlation between both sets of data.

Figure 2: Load-displacement curve for Test G

In the case of Test B, where the surface fibres are orientated perpendicular to the loading direction (90°), a reduction in joint strength is observed compared to a similar joint configuration with 0° surface fibres (Test A). Damage appears to propagate through the surface ply into the 45° ply below (see Figure 3).

Figure 3: Failure surfaces of Test B

Much of the damage can be considered matrix cracking, which is observed as the separation of fibres from the matrix, in both the surface 90° ply and the subsequent 45° ply. As the surface fibres are orientated in the 90° direction, loading is primarily carried by the inherently weak matrix, consequently, failure initiates in the form of matrix cracking. Although matrix cracking can be considered the initial failure mode, it does not define the critical failure mode, which determines joint strength. A crack in the surface ply will promote delamination between the 90° and 45° plies, as seen in Figure 3. This is known as “interaction between matrix cracking and delamination”, whereby the matrix cracking within the surface 90° ply propagates along the ply thickness until impeded by the adjacent 45° ply. Subsequently, the crack degenerates into a delamination failure mode between the 90° and 45° ply.

Test A exhibited signs of minor cohesive failure, alongside what appears to be predominantly interfacial and surface intraply failures (see Figure 4). As expected, the strength of the joint is higher compared to Test B, as the highest strained ply is now aligned with the load and not perpendicular to it.

Figure 4: Failure surfaces of Test A

The crack appeared to initiate at the interface between the centre substrate and adhesive, then progressed through the bondline to the outer substrate, which is observed as small regions of cohesive failure. Visually inspecting the surface, a significant amount of fibre transfer is apparent, which may be considered as surface intraply failure. However, upon observing a cross-section of the failure surfaces using reflected light microscopy (RLM), only light fibre transfer was apparent (see Figure 5). These accumulations of fibres are approximately 20 μm in thickness, below what might be typically considered intraply failure.

Figure 5: Failure surface of the outer substrate of Test A, demonstrating light fiber transfer ([a] 7x magnification; [b] 35x magnification)

Test C, in which a woven surface ply is used, presented a mixture of minor cohesive and major interfacial failures (see Figure 6). Joint strength has reduced, compared to an equivalent joint configuration with a 0° surface ply (Test A), despite the addition of a woven ply resulting in a stiffer substrate. Woven surface plies orientated in the $\pm 0/90^\circ$ direction are particularly susceptible to light-fibre transfer which promotes an interfacial failure mechanism. The 90° ply below the woven surface ply in Test H further contributes to the reduced joint strength as the laminate flexural stiffness is reduced compared to Test C. Unlike Test C, the failure mode of Test H is entirely interfacial, which may also explain the reduced

joint strength, as interfacial failure is characteristically a weaker failure mode compared to a cohesive failure.

Figure 6: Failure surfaces of Test C

Test G presented cohesive failure throughout (see Figure 7). Unlike the failure surfaces in Test C, the lower stiffness adhesive in Test G does not exhibit interfacial failure. This is due to the increased compliancy of the adhesive, thus reducing peak stresses. As the failure is entirely cohesive, and 3M 9323 is known to have a higher tensile strength compared to EA 9394, Test G exhibits the highest strength of all joints tested.

Figure 7: Failure surfaces of Test G

The high-modulus substrates of Test D, E and F all exhibit far lower strengths compared to their low-modulus counterparts. The reason is probably due to the low fracture toughness of the cyanate ester HTM143 resin system. This is identified in the subsequent failure surfaces, all of which present significant fibre and resin transfer.

Figure 8: Failure surfaces of Test F

To verify the transfer of fibre and resin, a cross section of the substrate was examined using RLM. In Test F evidence of the woven surface ply was present on both the centre and outer substrates (see Figure 8). Figure 9 and Figure 10 illustrate two regions of the woven surface ply on the outer substrates.

Figure 9: Evidence of fiber failure in the loading direction for Test F ([a] 3.5x magnification; [b] 1.5x magnification)

Figure 10: Evidence of fiber failure in the transverse direction for Test F

Figures 9 and 10 are typical of what is observed along the interface of the outer substrates. A clear indication of fibre failure is present in both the transverse and loading direction fibres of the 2x2T woven surface. Given the low fracture toughness of the cyanate ester resin system, a combination of through-thickness peel and in-plane shear stresses may be sufficient to cause the initiation and propagation of an intralaminar crack. This intralaminar crack can displace fibres in the transverse direction (see Figure 10), resulting in a transfer of fibres from one failure surface to another. Fibre failure for fibres in the loading direction is unlikely as fibre strength is known to be considerably higher than local stresses in this region, but such failure is highly localised. Across the entire failure surface, loading direction fibres appeared to fail in the same manner. A thin layer of fibres at the surface of the weave, in contact with the bonded interface, have clearly been separated from the weave.

The thin layer of fiber separation is associated with the poor fracture toughness of the resin system. Figure 11 illustrates the corresponding failure surface of the centre substrate.

Figure 11: Failure surface of the centre substrate demonstrating fiber transfer ([a] 7x magnification; [b] 35x magnification)

It is apparent that a thin film of fibres orientated in the transverse direction have transferred to the opposite failure surface. This suggests that joint strength is not dominated by the strength of the woven surface ply, rather the strength of the resin system. This is reflected in the test results where the low-modulus epoxy composite resin systems failed at greater strengths than their high-modulus cyanate ester counterparts.

Table 2 summarises the observed failure modes and their corresponding failure loads for each joint. The presence of each failure mode is characterised by the observed surface area affected. A clear trend of increased joint strength is apparent amongst joints which exhibit cohesive failure. Delamination and intraply failures exhibit the lowest joint strengths. Hence, the results suggest that even in the presence of minor cohesive failure, a significant rise in joint strength is achieved. Increasing the adhesive thickness results in an entirely interfacial failure, although this may be due to reduced laminate flexural stiffness and light fibre failure in the woven surface ply. The use of a high-stiffness, low fracture toughness substrate significantly reduces joint strength as failure migrates into the substrates in the form of intraply failures. It is clear that in all joint tested, the initial failure mode is fundamental in understanding joint performance. All subsequent failure modes stem from this initial region of failure. Nonetheless, the final failure mode often defines joint strength which is of great interest in this study.

Table 2: Summary of observed failure modes

	Cohesive	Intraply	Delamination	Interfacial	Failure Load (kN)
Test A	20%	-	-	80%	17.2
Test B	-	-	90%	10%	7.96
Test C	20%	-	-	80%	10.2
Test D	-	70%	-	30%	4.09
Test E	-	-	100%	-	5.57
Test F	-	70%	-	30%	6.88
Test G	100%	-	-	-	20.6
Test H	-	-	-	100%	7.05

3. Finite Element Modelling

Representative detailed 3D ply-by-ply FE models were produced in ABAQUS to replicate the loading condition of each joint at failure. The aim is to determine critical stresses and strains in the adhesive and substrate. In doing so, a failure criterion for each failure mode can be determined, which can be used across multiple joint configurations. Prior to extracting critical stresses, each joint configuration will be modelled under the same loading conditions to identify trends and validate the models.

Consequently, a detailed model of the substrate is needed, where the stresses and strains can be captured on a ply-by-ply basis. The use of ply-by-ply modelling greatly increases computation time; therefore, each joint was modelled as a half-joint with symmetry boundary conditions applied as shown in Figure 13. The adhesive and substrate thicknesses vary for each configuration (see Table 1 and Table 3). Only the joint width (25.4 mm) and overlap

length (12.9 mm) remain constant. Table 1 summarises the substrate and adhesive thickness for each joint configuration.

The material properties used in these analyses were taken from existing experimental data provided by Surrey Satellite Technology Limited (SSTL). Suitable assumptions were also made where appropriate. Material data for woven and unidirectional (UD) plies used are outlined in Table 3.

Table 3: Woven and UD ply data used in the first test campaign

	MTM 44-1 IMS65 UD	MTM 44-1 IMS65 Woven	HTM 143 M55J UD	HTM 143 M46J Woven
E₁ (MPa)	155520	62690	286420	100610
E₂ (MPa)	9010	9010	5780	5780
E₃ (MPa)	9010	62690	5780	100610
v₁₂	0.287	0.287	0.183	0.183
v₁₃	0.287	0.057	0.183	0.022
v₂₃	0.40	0.287	0.40	0.183
G₁₂ (MPa)	3710	3570	3730	2443
G₁₃ (MPa)	3710	4262	3730	2827
G₂₃ (MPa)	3128	3570	2064	2443
Thickness (mm)	0.128	0.204	0.115	0.211

Where the ‘2’ direction represents through thickness ply properties.

Here it is assumed that the UD through-thickness modulus (E₂) is matrix dominated, thus similar to experimentally determined transverse in-plane modulus (E₃), and the through-thickness modulus (E₂) for the woven ply is matrix dominated. Both UD and woven plies use the same epoxy resin matrix system, hence the through-thickness stiffness is taken to be the same.

The adhesive properties for 3M 9323 and EA 9394 are given in Table 4 and Table 5.

Table 4: 3M 9323 adhesive material data at 24°C

Tensile strength (MPa)	59
Tensile Modulus (MPa)	886
Poisson ratio (MPa)	0.35

Table 5: EA 9394 adhesive material data at 25°C

Tensile strength (MPa)	46
Tensile Modulus (MPa)	4237
Poisson ratio (MPa)	0.37

The load-displacement plots for joint configurations using both adhesives obtained during testing showed little evidence of non-linear material behaviour, hence a linear-elastic adhesive material model was used throughout. Figures 12 and 13 illustrate the boundary conditions for all DLJs.

Figure 12: Boundary conditions used for DLJ FE model

Figure 13 (a): Fixed end of DLJ restricting axial movements but allowing for Poisson's contractions

Figure 13 (b): Symmetry face of DLJ

Figure 13 (c): Loading face of DLJ where load is applied as a uniform pressure

The left-end is fixed in the axial (X) direction ($UX=0$) to oppose the loading force. To allow for Poisson's contractions, two nodes on the RHS are pinned in the through-thickness direction ($UY=0$) and in the in-plane orthogonal direction ($UZ=0$). The load is applied to the RH end as an equally distributed pressure. As a half joint is being modelled, the mid-plane of the centre substrate is assigned symmetry conditions ($UY=0$).

To verify the ply-by-ply modelling method used for the laminates, a CFRP laminated substrate was generated using the modelling techniques mentioned previously and loaded in tension. The results were compared to laminate plate theory (LPT) and experimental data. Complete correlation was found between FE and LPT ply stresses and laminate stiffness and good correlation was found with experimental stiffness. A mesh dependency study was also carried out to identify optimal meshing parameters. All stresses were extracted at the mid-plane of an element (centroid) to avoid interfacial stress singularities. Various mesh densities and higher order elements were investigated; however, no significant impact was made to the accuracy of the results being considered when increasing the number of elements in the model. The mesh simplifies each ply and adhesive layer to a single element thickness (see Figure 14). Mesh checks were also carried out to ensure suitable mesh quality. The final mesh consisted of 653395 3D reduced integration 8-noded brick elements.

Figure 14: Final FE Mesh density

Prior to identifying critical parameters at the failure load, each FE joint model was placed under a load of 5kN. The resultant adhesive stresses were analysed to determine if the models replicated what is typically expected when altering the properties of a double lap joint.

Alongside the FE models, a Hart-Smith analytical model was implemented. Hart-Smith was chosen over more sophisticated models as it is relatively straightforward to compute, whilst still an improvement over Volkersen or Goland and Reissner analytical models. Complex analytical models exist, however the purpose of the analytical solutions was simply to ensure sensible FE results were being achieved. Comparisons were made between Hart-Smith analytical and FEM solutions for mid-plane adhesive layer stresses in one of the joint configurations (Test A) (see Figure 15). As the FE model is three-dimensional, stresses were extracted along the centre and edge of the adhesive (Figure 16), always on the adhesive mid-plane (centroid), not the interface.

Figure 15: Peel and shear stress plot comparison between FE and analytical solutions for Test A

Reasonably good correlation is observed between FE and analytical shear stresses, regardless of location. FE peel centre stresses and analytical peel stresses are also in good agreement. Interestingly, maximum FE peel stresses are greater at the centre than the edge, whereas maximum FE shear stresses are higher at the edge than at the centre.

Figure 16: Regions from which adhesive stresses were extracted

Table 6 (a): Summary of maximum peel and shear stresses in the adhesive layer for both analytical and FE solutions

	FE Stresses (MPa)				Analytical Stresses (MPa)	
	Centre		Edge			
	Peel	Shear	Peel	Shear	Peel	Shear
Test A	18.52	15.54	11.55	16.11	18.43	22.70
Test B	19.49	12.99	16.66	13.69	18.43	22.70
Test C	19.90	14.15	13.08	13.87	16.87	20.29
Test D	22.14	15.47	16.67	14.51	13.31	18.41
Test E	15.90	10.58	13.95	10.99	13.31	18.41
Test F	16.94	12.00	11.22	11.54	12.65	16.83
Test G	11.84	10.22	9.10	10.46	7.43	11.41
Test H	17.65	11.82	12.07	12.24	12.82	16.64

Table 6 (b): Summary of maximum von-Mises and principal stresses in the adhesive layer for FE solutions

	FE Stresses (MPa)			
	Centre		Edge	
	von-Mises	Max Principal	von-Mises	Max Principal
Test A	-	30.31	-	24.77
Test B	-	28.87	-	27.22
Test C	-	30.38	-	24.31
Test D	-	33.88	-	28.93
Test E	-	22.99	-	21.53
Test F	-	25.18	-	19.54
Test G	18.24	-	18.90	-
Test H	-	25.10	-	20.04

Referring to Table 6, the analytical stresses follow the expected trends when altering joint parameters, with the exception of joint configurations that change the orientation of the UD surface ply (Test A, B, D and E). The analytical model does not take flexural stiffness or in-plane surface ply stiffness into account, hence, the adhesive stresses are identical regardless of surface ply orientation. These expected trends are that adhesive stresses reduce for increasing substrate stiffness (thickness or modulus) and decreasing adhesive stiffness (thickness or modulus).

FE models are able to account for the change in surface ply orientation and so should better reflect the stresses in the actual joints. Shear stresses appear to be particularly sensitive to changes in in-plane stiffness of the surface ply. The discrepancy compared to analytical models increases as the surface ply moves towards a 90° orientation. The analytical solutions appear to be in better agreement with FE solutions for high-modulus substrates and low modulus adhesives. This is expected as the analytical solutions assume that the substrate stiffness is much greater than the adhesive stiffness.

The following trends can be observed amongst the FE stresses; lower peak adhesive shear stresses are observed at the centre in most specimens; higher peak adhesive peel stresses are always observed at the centre; maximum principal and von-Mises stresses are consistently lower at the edge compared to the centre, suggesting that any cohesive failure for both types of adhesive is likely to initiate at the overlap end in the centre and not the edge of the joint. Given the variation in both sets of data, it is sensible to use peak stresses from both the centre and edge where necessary when identifying critical parameters.

The following conclusions can be drawn from the FE results presented in Table 7.

Adhesive Stiffness Comparison:

- Taking Test C as a baseline, moving to a more ductile adhesive (Test G) reduces adhesive stresses as the adhesive is more compliant. This is true for stresses extracted at both the centre and edge.

Low-Modulus Layup Comparison:

- In the low-modulus substrate joints (MTM 44-1), adding a woven surface ply (Test C) has a minor impact on adhesive stresses compared to Test A and B. Comparing LPT derived modulus values for each laminate shows only a slight change in in-plane stiffness (Test A and B = 55 GPa, Test C = 59 GPa) which may be the reason why only a small change in adhesive stresses is observed.
 - Comparing Test A, B and C; Test B presents the lowest shear stress at the centre; peel stresses appear to be similar in each configuration at the centre which seems sensible given through-thickness stiffness of the first ply has not changed but flexural stiffness of the laminate has increased slightly; larger shear stresses in Test A compared to Test C seems to correlate to the slightly lower in-plane stiffness (loading direction) of the woven ply compared to the UD ply.
 - Stresses on the edge present a similar trend with the exception of Test B peel stresses. Extracting stresses from the edge appears to capture the reduced flexural stiffness of Test B.

High-Modulus Layup Comparison:

- Focusing on the high stiffness substrate joint configurations (Test D, E and F);
 - Including a woven surface (Test F) clearly reduces adhesive stresses at both the centre and edge compared to Test D.
 - Rotating the surface ply 90° in Test E significantly reduced the adhesive stresses over Test D. The shear stresses in the low modulus substrate joints also reduce (see earlier discussion), however the peel stresses offer a conflicting trend. This could be due to the low through-thickness modulus of the HTM substrate compared to MTM substrate (approx 5 GPa to 9 GPa), and the stiffness matching effects with the EA 9394 adhesive (approx 4.5 GPa).

High/Low Modulus Layup Comparison:

- Comparing Test B to E, adhesive stresses are lower for E which uses high-modulus substrate. A similar trend is observed comparing Test C to F.
- In configurations which use a 90° surface ply (Test B and E), the lower through-thickness and axial modulus can be considered similar to the adhesive, thus in both cases effectively increasing the adhesive thickness and lowering adhesive shear stresses at the joint ends.

The key point to take away from this study is that the adhesive stresses are much more affected by changes in localised, surface ply properties than in global substrate properties. These localised effects cannot be included in the analytical solutions where the input is a global substrate stiffness.

4. Developing Critical (and Safe) Parameters

In all joints tested experimentally, failure remained within the region of the bondline and the two adjacent plies. The crack running along the overlap region did not progress further into

the laminate in any of the specimens tested. Hence, the following FE models are focused on extracting stresses in these regions only, at their respective failure loads.

To extract critical parameters, the models must first be loaded to their experimentally determined failure loads. Table 7 summarises the loads applied to each joint configuration.

Table 7: Failure loads and pressures used in FEM

	Strength (kN)	Stress (N/mm²)
Test A	17.2	330
Test B	7.96	152
Test C	10.2	164
Test D	4.09	88
Test E	5.57	118
Test F	6.88	120
Test G	20.6	330
Test H	7.05	113

4.1 Adhesive Parameters

Test G presented entirely cohesive failure. Hence, extracting adhesive stresses from the equivalent FE model of Test G would likely provide an indication as to the critical stress values for the adhesive EA9323 which would cause cohesive failure. Table 8 summarises the critical adhesive stress parameter. The critical stress is taken as the maximum stress at either the centre or edge.

Table 8: Critical adhesive stress to cause cohesive failure in 3M 9323

	von-Mises (MPa)
9323	78

The critical stresses at the centre seem more consistent with the known tensile strength of the adhesive. No joint configurations using the high-modulus adhesive presented cohesive failure, either complete interfacial or a mixture of cohesive-interfacial was present. Hence, developing criteria for strictly cohesive failure is not possible for the EA 9394 adhesive. Instead, ‘safe’ parameters can be identified where failure is known not to occur.

4.2 Interfacial Parameters

EA9394-MTM interfacial FEM parameters were taken from Test H, where interfacial failure was prominent throughout. Maximum adhesive peel and shear stresses of 25 (centre) and 17 (edge) MPa respectively were observed. These stresses are significantly lower than those predicted for Test A, where failure is observed as a mixture of interfacial and cohesive. From the data presented in Table 6, the surface ply has a significant impact on the predicted peak stresses of the adhesive. However, this may also suggest that interfacial failure is a weaker failure mode, and that the presence of cohesive failure dramatically increases joint strength. A trend of predominantly interfacial failure can be seen in configurations with low modulus woven surface plies, with exception of Test G, which uses the low modulus adhesive. Table 9 summarises the critical adhesive parameters for the onset of interfacial failure for a woven surface ply and a subsequent 90° ply.

Table 9: Critical adhesive stresses to cause interfacial failure in EA 9394-MTM interface.

	Peel (MPa)	Shear (MPa)
9394*	25	17

*for woven surface ply and subsequent 90° ply

Minor amounts of interfacial failure are observed in specimens using the high-modulus woven surface ply (Test F). Microscopy reveals an intraply failure which removes the surfaces fibres from the corresponding failure surface. This appears to be a characteristic of the high-modulus substrate which suffers from poor fracture toughness. Failure is observed to be near the adherend-adhesive interface, hence the adhesive stresses provide an insight as to the stresses in this region at failure. The peak adhesive peel and shear stress for Test F are outlined in Table 10, which is only valid for a woven surface ply.

Table 10: Critical adhesive stresses to cause interfacial failure in EA 9394-HTM interface.

	Peel (MPa)	Shear (MPa)
9394*	23	17

*for woven surface ply

The interfacial parameters are similar to those outlined for the low-modulus woven surface ply (see Table 9). As the adhesive is the only common material between Test F and Test H, it can be suggested that interfacial failure is dependent on the adhesive properties rather than the adhesive-adherend interface.

4.3 Low Modulus Substrate Parameters

The low-modulus woven surface ply remains undamaged in all the tested specimens, with the exception of very light-fibre transfer. This may not provide an indication as to the strength of the ply in the context of a bonded joint, but it does provide ‘safe’ parameters, up to which failure is known not to occur. Beyond these stated values, the performance of the woven ply is unknown. Table 11 describes these ‘safe’ stress values. These stresses are taken as the maximum stress across the overlap region for every configuration. All ply stresses were extracted at the element centroid to avoid singularities.

S11 describes stresses in the loading direction, S22 is the through-thickness direction and S12 is the in-plane shear direction.

Table 11: ‘safe’ stresses for the woven MTM ply

	S11 (MPa)	S22 (MPa)	S12 (MPa)
MTM W	449	42	45

The failure mode of Test A was determined to be predominantly interfacial. Fibre transfer was also observed, but subsequent microscopy proved that the transfer of fibres was minimal and not an indication of intraply failure. Hence, extracting the surface 0° ply stresses will provide an indication as to the materials resistance to failure. Table 12 summarises the ‘safe’ parameters.

Table 12: ‘Safe’ stresses for the UD 0° MTM ply

	S11 (MPa)	S22 (MPa)	S12 (MPa)
MTM 0°	822	42	41

Matrix cracking of a surface 90° ply occurred in Test B and E. The crack appears to propagate through the surface 90° ply to the subsequent 45° ply. Table 13 describes the critical stress for the 90° UD MTM ply.

Table 13: Critical stress for the UD 90° MTM ply

	S11 (MPa)	S22 (MPa)	S12 (MPa)
MTM 90°	54	-	-

The FE predicted stresses are greater than the known transverse strength of the UD ply. This may be due to the adjacent 45° ply reinforcing the matrix of the 90° ply. The 45° ply in this configuration also fails in the matrix, however the order in which the failure modes progress is unknown. Interestingly, the stresses observed in the 45° ply of Test A (0° surface ply) are noticeably greater than Test B (90° surface ply), but failure is not observed. This suggests that the 90° surface ply is detrimental to the performance of the adjacent 45° ply. Hence, the following conclusions can be made; sub-surface 45° plies require a separate criterion depending on the surface ply orientation; the adjacent ply below the 90° surface ply serves to reinforce the matrix, hence increasing the FE predicted stresses beyond the known transverse stress of the ply.

Table 14 summarises the parameters for the 45° plies. As failure does not occur in Test A, where a 0° surface ply is used, the parameters outlined below are not critical, but ‘safe’ values.

Table 14: Safe and critical stresses for the UD 45° MTM plies

	S22 (MPa)	S12 (MPa)
MTM 45° (0° Surface) – ‘safe’	37	34
MTM 45° (90° Surface)	26	26

4.4 High Modulus Substrate Parameters

The high-modulus substrates consist of M55J fibres set in a cyanate ester resin system. The resin system has significantly lower fracture toughness compared to the epoxy matrix system used in the low modulus substrate. This has a significant impact on the observed failure modes, and subsequently the joint strengths. Prior to failure at the interface or in the adhesive, the substrate surface ply is typically the first point of failure. The woven surface plies exhibit predominantly fibre-transfer. Microscopy revealed the fibre-transfer to be significantly more than what is considered light-fibre transfer. Hence, the parameters below describe the critical values for the onset of intraply failure in the high-modulus woven surface ply. These values can be used in conjunction with those stated in Table 10 to identify a heavily mixed failure mode.

Table 15: Critical stresses for the woven HTM ply

	S11 (MPa)	S22 (MPa)	S12 (MPa)
HTM W	177	18	16

A trend similar to the low-modulus substrates is observed in Test E and D. Separate criteria are necessary for the 45° ply depending on the orientation of the surface ply. From the FEM, the peak 90° and 45° ply stresses were extracted and tabulated below.

Table 16: Critical stresses for the UD 90° and 45° HTM ply

	S11 (MPa)	S22 (MPa)	S12 (MPa)
HTM 90°	24	-	-
HTM 45° (90° Surface)	-	15	16

With regards to Test D, the failure modes appear to be predominantly intraply. Failure is not observed in the 45° ply, hence the following values can be used as critical and ‘safe’ stresses in this configuration.

Table 17: Critical and ‘safe’ stresses for the UD 0° HTM ply and 45° ply respectively

	S22 (MPa)	S12 (MPa)
HTM 0°	16	16
HTM 45° (0° Surface) – ‘safe’	15	28

Joint strength between Test E (90° surface) and Test D (0° surface) is not too dissimilar, whereas in the low-modulus substrates a large difference was observed. The poor fracture toughness of the cyanate ester resin system plays a significant role in causing the early onset of failure in the laminates.

The following table summarises the critical parameters developed above from a combination of experimental and numerical work. At these stresses, failure is known to occur within the substrate, adhesive or interface.

Table 18: Critical DLS parameters

Adhesive Parameters			
	Peel	Shear	von-Mises
9323 (Cohesive)	-	-	78
9394-MTM Interface*	25	17	-
9394-HTM Interface**	23	17	-

*woven interface with subsequent 90° surface ply

**woven interface

Substrate Parameters		
	S11 (MPa)	
MTM 90°	54	

HTM 90°	24	
	S22 (MPa)	S12 (MPa)
MTM 45° (90° Surface)	26	26
HTM 45° (90° Surface)	15	16
HTM 0°	16	16

The following ‘safe’ values have been identified below. At these stresses, failure is known not to occur, but the margin to failure is unknown. These values are instead used to verify current and future models.

Table 19: ‘Safe’ DLS parameters

‘Safe’ Parameters			
	S11 (MPa)	S22 (MPa)	S12 (MPa)
MTM 0°	822	42	41
	S11 (MPa)	S22 (MPa)	S12 (MPa)
MTM W	449	42	45
HTM W	177	18	16
	S22 (MPa)	S12 (MPa)	
MTM 45° (0° Surface)	37	34	
HTM 45° (0° Surface)	15	28	

5. Conclusions

The following key conclusions can be drawn from the experimental and numerical work presented in this paper.

Experimental Study:

- Joints which exhibit any signs of cohesive failure present increased joint strength compared to interfacial, intraply and delamination failures.
- The addition of a $\pm 0/90^\circ$ woven surface ply in low modulus substrates reduces joint strength compared to an equivalent configuration with a UD surface ply with fibres orientated in the loading direction. This is due to light-fibre transfer promoting interfacial failure prematurely.
- Fracture toughness of the matrix system plays a significant role in joint strength. The high-modulus adherends, which use a low fracture toughness cyanate ester resin system, perform poorly compared to their epoxy resin matrix system counterparts. Failure is observed to migrate towards the substrate in the form of intraply failures, whereas in the low-modulus substrate configurations, failure remains within the adhesive or adhesive-adherend interface.

Numerical Modelling Study:

- Modelling the substrate in a ply-by-ply manner has shown that adhesive stresses are highly sensitive to local changes in surface ply properties (stiffness and orientation) compared to bulk changes to the substrate itself. Analytical models are unable to capture these effects as the substrate is modelled using a global stiffness value generated from LPT.

Identification of Critical Parameters:

- Only the low-modulus adhesive exhibits complete cohesive failure, hence a von-Mises criterion is used to identify the critical stress. In other configurations where failure occurs at the interface, a peel and shear stress criteria is used.
- Substrate failure parameters have been identified for the following:
 - Matrix cracking in surface 90° plies in both high and low modulus materials.
 - Delamination of 45° plies (assuming a 90° surface ply) for both materials.
 - Intraply failure of the high-modulus surface 0° ply.
- A separate failure criteria is necessary for the 45° ply depending upon the adjacent surface ply. The crack in a 90° surface ply appears to propagate through into the 45° ply, causing matrix cracking and greatly reducing its strength. This is not observed in configurations with a 0° surface ply.
- Safe parameters have been identified for all other plies where failure does not occur.

Several critical and ‘safe’ parameters were successfully extracted from the FE modelling work. However, the reality is far more complex due to the combination of failure modes observed during testing. A single criterion can be extracted for a combination of cohesive and interfacial failure, which is predominantly observed in joint configurations using high-modulus adhesive. However, the progression of failure in the substrate is more difficult to capture. Instead, design rules can be adopted in which failure is migrated towards the adhesive and interface, and away from the substrate. In doing so, simple and reliable failure parameters can be used, alongside simplified modelling where the substrate no longer needs to be modelled on a ply-by-ply basis.

Following this study, additional joint configurations will be tested and analysed. The data gathered from these studies will be consolidated with existing DLJ data. The consolidated data will form the novel predictive tool. The predictive tool will be validated against a different set of joints to assess its applicability

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