The Static Failure of Adhesively Bonded Metal Laminate Structures: A Cohesive Zone Approach

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ABSTRACT

Adhesively bonded metal laminates are used in aerospace applications to achieve low cost, light weight design in the aerospace industry. Advanced structural adhesives are used to bond metal laminae to manufacture laminates, and to bond stringers to metal laminate skins. Understanding the failure behaviour of such bonded structures is important to provide an optimal aircraft design. In this paper, the static failure behaviour of adhesively bonded metal laminate joints is presented. A cohesive zone model was developed to predict their static failure behaviour. A traction-separation response was used for the adhesive material. Three joint configurations were considered: a doubler in bending, a doubler in tension and a laminated single lap. The backface strains and static failure loads obtained from experimental tests were used to validate the finite element models. The models were found to be in good agreement with the experiments.

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1. INTRODUCTION

The aim of today's aircraft design engineer is to achieve low cost, light weight structures which provide high safety and long service. To provide such a structural design solution, engineers resort to either new materials or combinations of materials. The usage of advanced structural adhesives and adhesively bonded metal laminates in aerospace industry is an example of an optimal structural design solution. Advanced structural adhesives are used to bond metal laminae to manufacture laminates, and to attach stringers to the metal laminate skin in order to increase the buckling resistance [1]. A schematic of an aircraft wing with adhesively bonded metal laminate skin and adhesively bond stringers is shown in Fig.1.

Despite its attendant advantages a possible concern when designing adhesively bonded structures is bondline failure that may be initiated by the high adhesive stresses that exist near the end of the joint overlap. Hence the failure behaviour of adhesively bonded metal laminates should be thoroughly understood to provide optimal aircraft design. Considerable experimental and numerical research work has been devoted to adhesively bonded structures in recent years. Experimental testing at the coupon level gives an insight into the failure behaviour of adhesives on the one hand, whereas, on the other hand, a comprehensive numerical model helps to understand the effects of different parameters, at both coupon and structural level, without exhaustive experimental testing. Currently more work is needed to fully establish confidence in the predictive modelling of such bonded structures. In this paper,

the numerical modelling of adhesively bonded metal laminates to predict and understand the static strength of such structures is presented.

The finite element method has been used by many researchers [2-5] to obtain either two- or three-dimensional stress distributions in adhesively bonded structures. However, a limitation of the use of standard finite element stress analysis is that it is not possible to incorporate the failure process in the model. Cohesive-zone models have been widely used in this last decade in many areas of computational mechanics which deal with delamination, debonding, and crack initiation and propagation [6-11]. In comparison with methods directly derived from fracture mechanics, cohesive-zone models are generally preferred when a non-negligible process zone exists on which the tractions gradually decrease from a peak value to zero. Unlike fracture mechanics based strategies, a cohesive zone approach can also be used for the analysis of crack initiation in adhesively bonded joints as damage initiates near free edges/fillet regions. When combined with FE analysis the cohesive zone approach provides an ideal means of modelling progressive failure in a single finite element analysis. Recently, this approach has been applied to adhesively bonded structures by a few researchers to assess the strength of both aged [12-13] and un-aged joints [14-15]. The current research extends these areas of application and investigates the relevance of cohesive zone parameters as unique material properties that can be used to deliver a consistent and reliable static strength prediction in structures containing single or multiple adhesive bondlines.

In this paper, a cohesive zone model is developed to predict the static failure behaviour of adhesively bonded metal laminate structures by using a traction-separation response for the adhesive material. The structures in question involve stringers made from Aluminium 2024-T3 that were bonded to Aluminium 2024-T3 laminate. The film adhesive Cytec FM73 was

used to bond the laminates and also to bond the stringer to the laminate. All the aluminium was pre-treated with a Chromic Acid Etch (CAE) followed by a Phosphoric Acid Anodise (PAA). The joint configurations that were extracted from this bonded panel structure for the current research were: a doubler in bending, a doubler in tension and a laminated single lap. The backface strains and static failure loads obtained from experimental tests were used to validate the finite element models.

2. COHESIVE ZONE THEORY

The Cohesive Zone Model (CZM) can be considered as an alternative to fracture mechanics-based methods without many of the limitations of fracture mechanics. The CZM has recently received considerable attention and has been employed for a wide variety of problems and in various homogeneous and inhomogeneous materials such as metals, ceramics, polymers and composites. The CZM was originally introduced by Barenblatt [16] based on the Griffith's theory of brittle fracture. Dugdale [17] considered the existence of a process zone at the crack tip and extended the approach to perfectly plastic materials. In both of their theoretical approaches, the crack was divided into two parts: a traction free part and a transmission part having finite tractions between the crack surfaces. Moreover, the tractions in the cohesive zone were assumed to follow a prescribed distribution as a function of the crack tip distance coordinate. The CZM was implemented by Hillerborg et al. [18] in the computational framework of the finite element method. A fictitious crack model was proposed for examining crack growth in cementitious composites. Contrary to previous work, the cohesive zone tractions were defined as a function of the crack opening displacement.

In contrast to other approaches, this phenomenological model has some advantages: (a) a pre-existing crack is not necessary, (b) the onset and growth of damage can be obtained as direct

outputs of the model without prior assumptions, (c) complex moving mesh techniques are not required to advance the crack front when the local energy release rates reach a critical value, (d) the infinite stress and strain in the vicinity of crack front leading to the use of singular elements and/or a highly refined mesh around the crack front can be avoided. In the past decade, cohesive-zone models have become very popular and have been recognised to be an important tool for describing fracture in engineering materials. Especially when the crack path is known in advance either from experimental evidence or because of the structure of the material, cohesive-zone models have been used with great success. In those cases, the mesh can be constructed such that the crack path, defined a priori, coincides with the element boundaries. By inserting interface elements between continuum elements along the potential crack path, a cohesive crack can be modelled exactly. This model can efficiently be utilised to study adhesively bonded joints. The concept of a cohesive zone and traction-separation is schematically illustrated for adhesively bonded laminates in Fig.2. The parameters involved in the traction-separation law are: separation, traction, fracture energy and the initial stiffness prior to the maximum traction

3. ADHESIVELY BONDED JOINTS

Aluminium 2024-T3 laminate bonded with Cytec FM73 adhesive to the flange of a monolithic stringer, which was made from Aluminium 2024-T3, was used in the current research. The joint configurations used were: a Doubler in Bending (DB), a Doubler in Tension (DT) and a Laminated Single Lap Joint (LSLJ). The targeted dimensions of each joint are given in Fig.3. For the DB and DT joints (Fig.3a and 3b) the adhesive layers in the laminate were 0.1 mm thick and the width of the joint was 15 mm. The bondline thickness between the stringer and the laminate was non-uniform (because of the curvature of the laminate) varying from 0.1 mm to 0.2 mm.

To create the LSLJ configuration a groove of 0.5 mm was made through the laminate (Fig.3c) providing an over lap length of 20 mm. The groove was made after the stringer was bonded to the laminate. As with the DB and DT joints, the adhesive layers in the laminate were 0.1 mm thick and the width of the joint was 15 mm. The bondline between the stringer and the laminate was non-uniform being 0.1 mm thick near the fillet and 0.2 mm at the tip of the groove.

As these laminates were made of adhesively bonded aluminium laminae of definite dimensions, adhesive butts were used in their manufacturing where two laminae join. These adhesive butts in the laminates can be a source of damage initiation. Hence the joint configurations that were considered for the current research were carefully chosen such that the locations of adhesive butts were located away from the overlap ends where the highest stresses were expected. However the effect of these butts in critical locations has been assessed numerically.

These aluminium alloys are used for high strength to weight ratio and good fatigue resistance which are the key requirements for aeronautic structural applications. Cytec FM73 is a toughened epoxy film adhesive which can provide good durability and structural performance within the temperature range of -55C to 82C. The aluminium was pre-treated prior to bonding according to adhesive manufacturer's recommendations. This involves a Chromic Acid Etch (CAE) followed by a Phosphoric Acid Anodise (PAA) and finally a BR127 corrosion inhibiting primer. This was applied to achieve maximum environmental resistance and bonding durability. BR127 is a modified epoxy primer and provides good corrosion inhibiting characteristics.

4. EXPERIMENTAL TESTS

Experimental tests were conducted on the three joint configurations to determine the static failure loads. An Instron 7512 hydraulic machine was used for the laboratory testing. All the static tests were performed at a rate of 0.1 mm/sec. The failure mode was found to be cohesive for all the tests, which suggested that the surface treatment used in bonding the tested joints was good and the full strength of the adhesive Cytec FM73 was optimally utilised. Strain gauges were attached to the substrates to obtain the backface strain variation during the static failure tests. The backface strains and static failure loads obtained from the experimental tests were used to validate the finite element models.

The static failure loads obtained from the experimental tests are given in Table 1 for the DB, DT and LSLJ. The average failure load for the DB joint was 5.86 kN with a scatter of ± 0.16 kN. The damage initiated at the overlap ends in all the tests. The bondline between the stringer and the laminate was found to fail cohesively. The DT, which was tested under axial loading, failed at 44.6 kN with considerable plastic yielding of the aluminium laminate. Only one test was conducted as the failure load was governed by large plastic strains in the aluminium laminae. The damage initiated near the overlap ends and adhesive butts in the laminate. The failure was because of delamination of the laminate and this delamination was cohesive in nature in the adhesive. The damage in the LSLJ initiated near the tip of the groove in the bondline and failed at an average load of 11.18 kN with a scatter of ± 0.28 kN. A cohesive failure of the bondline was observed. The failure surfaces of the LSLJ and the DB are shown in Fig.4.

5. NUMERICAL MODELLING

The static failure behaviour of adhesively bonded joints was modelled by using the CZM approach. The experimentally tested joint configurations (DB, DT and LSLJ) were considered in the numerical study. A cohesive zone approach was employed to model the adhesive failure, and a bilinear traction-separation response was implemented for the joint configurations. Two and three dimensional models were developed using ABAQUS/Standard version 6.7 software.

To model the adhesive using a traction-separation law, some parameters need to be specified: initial stiffness prior to damage onset, peak value of traction, fracture energy in different modes and the shape of strain-softening. The initial stiffness should be chosen as high as possible so that the interface does not influence the overall compliance before damage initiation, but from a numerical perspective it cannot be infinitely large otherwise it leads to numerical ill-conditioning. The cohesive strength, although a material parameter which is related to the length of the process zone and to the tensile strength of the interface material, can be difficult to measure experimentally [19]. It has been shown [20-21] that if the cohesive strength is large enough, then it does not affect the solution of the problem. However, it is worth noting that choosing a very high value of cohesive strength will result in the need for a high mesh refinement which is computationally expensive [22]. Liljedahl et al. [23] studied the effect of the cohesive strength value on the failure load and divided the cohesive strength range into three regions. In the lower and higher cohesive strength value regions, the failure load is highly dependent on the cohesive strength but in the intermediate region, the failure load is essentially independent of cohesive strength. Furthermore, choosing a cohesive

strength in the higher region makes the analysis mesh dependent and computationally expensive. Thus, in this work peak tractions from the intermediate region were used.

Material and geometrical nonlinearities were included in the analysis as they play an important role in the static failure behaviour. Based on experimental testing and published literature Liljedahl [24], the yield stress, the ultimate strength, the Young's modulus and the Poisson's ratio of Aluminium 2024-T3 were considered to be 300 MPa, 450 MPa, 70000 MPa and 0.3, respectively. Further, the values of the Young's modulus and Poisson's ratio of FM73 were found to be 2000 MPa and 0.4, respectively. Unlike the values of the initial stiffness and tripping tractions, the fracture energy of structural adhesives is the most important and easily measurable parameter. This is often available in the literature or can be determined by means of some standard experimental tests, e.g. double cantilever beam test, end-notched flexure test, T-peel test etc. Based on experimental peel tests on the aluminium laminates used in the current study, the fracture energy of FM73 in mode-I was measured to be 1400 J/m^2 . However, the fracture energy of the adhesive in mode-II and mode-III were assumed to be 2800 J/m^2 as it is often observed that the mode-II fracture energy is higher than the mode-I fracture energy [25]. The peak traction values in mode-I, II and III were assumed to be 114 MPa, 66 MPa and 66 MPa based the strategy of Liljedahl et al. [23].

5.1 TWO –DIMENSIONAL MODELLING:

Initially, two-dimensional models were developed to minimise computational effort. Plane-strain conditions were employed to model the DB, DT and LSLJ. The adhesive fillet was excluded in the models.

The DB was modelled by considering the bond-line between the stringer and the laminate as a cohesive zone and the other adhesive layers as fracture-free zones. Moreover, the symmetry involved in the joint was used to minimize the computational effort. The boundary conditions used and the location of the cohesive zone are depicted in Fig.5a and 5b. The transverse displacement was constrained at the lower left boundary and a vertical load was applied at the right end of the joint. Plane strain four-node elements (CE4) were employed for the continuum adhesive layers and the aluminium laminae, and four-node cohesive elements (COH4) were used to model the adhesive between the laminate and the stringer (cohesive zone). The size of the cohesive element was between 0.2 mm x 0.2 mm and 0.2 mm x 0.1 mm as both uniform and non-uniform bondline conditions were analysed, see Fig.5(c). The average dimensions of the DB that were tested experimentally were employed in the models: (a) the thickness of the laminate was 8.3 mm, (b) the thickness of the stringer was 9.65 mm and (c) the thickness of the bond-line was 0.2 mm in the middle of the stringer and 0.1 mm at the edge of the fillet.

Similar to the DB model, the DT was modelled by considering the symmetry of the joint. The boundary conditions employed for the DT are given in Fig.6a. As the axial displacement of all the nodes on the right-side boundary must be equal, they were modelled using a kinematic coupling constraint. Every adhesive layer in the DT joints was modelled as a cohesive zone (see Fig.6b) because of the tensile loading condition, which, unlike the DB, resulted in high stresses in all adhesive bond-lines. This enabled damage to be modelled in all FM73 layers. Plane strain four-node elements (CE4) were employed for aluminium laminae, and four-node cohesive elements (COH4) for the cohesive zones.

Moreover, as was mentioned before, the butts in the laminates could influence the static behaviour of the joint configurations studied. To be able to investigate the influence, a detailed finite element model which can include the butts is required. From this viewpoint, all the adhesive near the butt and the butt itself needs to be modelled as regions where damage can be occur. By defining all the adhesive layers and butt joints as cohesive zones, a failure criterion for the adhesive, based on a traction-separation technique, was implemented for the DT under static loading. The details are shown in Fig.6c for the DT joint.

A fine mesh with a bondline element size varying from 0.2 mm x 0.2 mm to 0.1 mm x 0.1 mm was generated for the cohesive zones. As the cohesive element formulation does not include the in-plane (membrane) stiffness, the direction of peel stress in each cohesive zone in the model was defined such that the direction of the element peel separation was in the appropriate direction. For example, the butt in Fig.6d was divided into two regions. The left region is a cohesive zone and the right region is a damage-free zone. The top and bottom cohesive zones are along the laminate direction. When a tensile load is applied to the laminate, the direction of the maximum stress is along the laminate and thus the direction of element peel separation is defined such that it is along the laminate in the left cohesive zone and transverse to the laminate in all other locations. In this section, as the work was focused on the effect of butt joints and their positions on the static failure of the joints, the static analyses were performed for different butt positions to find out a critical location.

As the LSLJ did not exhibit any symmetry, it was modelled by considering the complete geometry. The boundary conditions and the cohesive zone used for the LSLJ are shown in Fig.7. The left-side boundary of the joint was clamped, and the displacements of all the nodes on the right-side boundary were kinematically coupled in order to apply an axial load. The adhesive layer between the bonded stringer and the laminate was modelled as a cohesive

zone, while all other adhesive layers in the laminate were treated as fracture-free zones. The average dimensions of the laminated SLJs that were tested experimentally were employed in the models: (a) the thickness of the laminate was 6.85 mm, (b) the thickness of the stringer was 9.65 mm, (c) the thickness of the bond-line was 0.2 mm at the edge of the slot and 0.1 mm at the edge of the fillet, and (d) the depth of the kinematically coupled boundary was 12.5 mm as the stringer was machined to this value to fit in the testing grips. Further, the distances between the left boundary and the edge of the fillet, and the edge of the slot and the right boundary were 18 mm and 14 mm, respectively (see Fig.3c). The Benzeggagh-Kenane (BK) [26] mixed-mode criterion provided by ABAQUS/Standard was used in the analyses. The mode mix of the deformation fields in the cohesive zone quantify the relative proportions of normal and shear deformation. The BK fracture criterion is particularly useful when the critical fracture energies during deformation purely along the first and the second shear directions are the same. The BK criterion is defined in Eq.1.

$$G_{Ic} + (G_{IIc} - G_{Ic}) \left(\frac{G_{II}}{G_I + G_{II}} \right)^\eta = G_I + G_{II} \quad (1)$$

In Eq.1 G_I and G_{II} are the energies released by the traction due to the respective separation in normal and shear directions, respectively, and G_{Ic} and G_{IIc} are the critical fracture energies required for the failure in normal and shear directions, respectively. The power η is a material parameter.

5.2 THREE –DIMENSIONAL MODELLING:

In order to investigate the influence of the plane strain approximation on the static failure behaviour, the joints were also analysed using three-dimensional models. The boundary

conditions and cohesive zone locations in the three-dimensional models were similar to the two-dimensional models. However, quarter models were used for the DB and DT joints and half models were employed for the LSLJ because of the symmetry involved in the joints. The dimensions shown in Fig.3 were used to model the joints. Three-dimensional continuum elements (C3D8 and C3D4) were used to model the aluminium layers and three-dimensional cohesive elements (COH3D8) were used for the cohesive zones. The size of the cohesive element in the joints was between 0.2 mm x 0.5 mm x 0.5 mm and 0.1 mm x 0.5 mm x 0.5 mm. The finite element mesh used for the 3D analysis of the DB is shown in Fig.8.

6. RESULTS AND DISCUSSION

The joints were analysed by using the 2D and 3D models to predict the static strength. The numerical results obtained from the models were compared with the experimental test data to validate the models.

6.1 DOUBLER IN BENDING:

The DB was analysed using both 2D and 3D models. Factors such as mixed-mode criteria for damage initiation and propagation, and variation in bond-line thickness, were included in the 2D model to study their influence. The static failure strengths for different combinations of the factors were obtained. The cohesive parameters used for the DB model were: (a) peak normal traction of 114 MPa, (b) peak shear traction of 66 MPa, (c) mode-I fracture energy of 1400 J/m², and (d) mode-II fracture energy of 2800 J/m². The variation in the predicted static strengths (in %) from the experimental static strength for each combination is given in Table 2.

Configuration DB1 had a uniform bond-line thickness of 0.2 mm. The cohesive zone was modelled with a mode independent criterion for damage initiation and propagation. It failed at 5.36 kN, which was 8.53% less than the average experimental failure load. This suggests that the mode independent failure criterion based analysis initiated damage in the adhesive prematurely as the stresses reach either mode I or mode II peak traction value. Further, configuration DB2 was analysed for a mixed-mode criterion for damage propagation with a uniform bondline thickness. The predicted static failure load was 5.62 kN, which is 4.09% less than the test value. As the test specimens had a non-uniform bond-line thickness, configuration DB3 was accordingly modelled and analysed by considering a uniformly tapered cohesive zone. In this case, the static failure load was 5.68 kN with -3.07% variation from the test result. The configuration DB3 predicted a better static failure load as the stresses in the adhesive were more accurately analysed by including the non-uniform bondline thickness and the damage analysis followed a mixed-mode failure criterion. The phase angle of mode-mixity, $\varphi = \tan^{-1}(\sigma_s/\sigma_n)$, for the configuration DB3 was $23^\circ 3'$, which indicated that peel stresses in the adhesive layer played a dominant role on fracture.

Experimental tests revealed that damage initiated near the fillet region and propagated along the bondline. Similar to the experimental tests, the damage from the 2D model was predicted to initiate in the bondline near the free edge of the stringer. The von Mises stress distribution and damage propagation along the bondline in the DB is shown Fig.9. The process zone, where the value of the damage parameter (SDEG) varies from 1 to 0, ahead of the crack tip is also shown Fig.9b. The von Mises stress contour plots at a joint level indicate the adhesive crack tip as stress concentrations exist in the substrates near the crack tip region. Moreover,

the distribution of the damage variable (SDEG) in the bondline represents the damaged and undamaged adhesive bondline.

The numerical backface strains were also compared to the experimental backface strain values to validate the static response predicted by the 2D model. Strain gauges were attached to the laminate at 2 mm and 4 mm from the free edge of the stringer and the backface strains were experimentally measured. The comparison between the experimental and numerical backface strain data is shown in Fig.10. The load-backface strain curves show that the static response predicted by the 2D model is in correlation with the experimental data.

Using the parameters that were considered for configuration DB3 in Table 2, a three-dimensional analysis was performed and a static failure load of 6.02 kN was predicted, which was 2.73% greater than the experimental data. The von Mises stress distribution and the damage propagation are shown in Fig.11. It was observed that the length of the crack predicted on the symmetric boundary (the mid-plane of the joint) was greater than the crack length on the free boundary. This was caused by high peel stresses induced in the middle of the joint as plane strain conditions existed on the symmetric boundary. Plane-strain conditions cause a redistribution of stresses in the joint and stress triaxiality ahead of the crack tip. Further, at the free surface edge, the plane stress conditions result in a much larger plastic zone. Because of this the process zone was observed to be greater near the free edge of the adhesive layer as shown in Fig.11b. The phase angles of mode-mixity, $\phi_{I,2}$ (for mode-I and mode-II) and $\phi_{I,3}$ (for mode-I and mode-III) were $22^{\circ}5'$ and $4^{\circ}3'$, respectively. Lateral shear stresses were observed near the fillet region on the free surface of the adhesive layer.

The numerical backface strains were also compared to the experimental backface strain values. The comparison between the experimental and numerical backface strain data is shown in Fig.12. The load-backface strain curves show that the static response predicted by the 3D model is in correlation with the experimental data. This is similar to the observation made with the 2D models.

To compare the damage that was predicted in the DB joint by the 2D and 3D models, the static load versus crack length curves are shown in Fig.13. The predicted crack length from the 2D model was compared with the predicted crack lengths from the 3D model in the middle of the joint (*indicated as 3D model (M)*) and the free face of the joint (*indicated as 3D model (F)*). Though the ultimate static loads obtained from the 2D and 3D models were close to each other, the predicted damage propagation was different after the damage initiation.

6.2 DOUBLER IN TENSION:

Two and three dimensional static failure analyses were performed on the DT joint to predict the static failure loads. Factors such as mixed-mode criteria for damage initiation and propagation and variation in bond-line thickness, were included in the 2D model to study their influence. The cohesive parameters that were used for the DB model were also employed for the DT.

The variation in the predicted static strengths (in %) from the experimental static strength for each combination is given in Table 3. The DT1 configuration had a uniform bond-line

thickness of 0.2 mm. The cohesive zone was modelled with a mode independent criterion for damage initiation and propagation. It failed at 42.93 kN, which was 3.74% less than the experimental failure load. Further, a mixed-mode criterion with uniform bondline thickness was considered for configuration DT2. The predicted static failure load was 43.11 kN, which is 3.34% less than the test value. However, to accurately calculate the adhesive stresses a non-uniform bondline thickness and mixed-mode criterion were included in the analysis for the DT3. The non-uniform bondline was modelled as a uniformly tapered cohesive zone. In this case, the static failure load was 43.95 kN, a 1.46% variation from the test result. The phase angle of mode-mixity, $\phi_{1,2}$, for the configuration DT3 was $49^{\circ}6'$, indicating a more shear dominant behaviour than the DB specimen.

Experimental tests showed that damage initiated near the fillet region and the adhesive butts which were present in the laminate, and propagated along the bondline. However, in the current model no attempt was made to include the adhesive fillet, though the butts in the laminate were studied using a different model. Similar to the experimental tests, the damage was predicted to initiate in the bondline near the free edge of the stringer from the 2D model. The von Mises stress distribution and damage propagation along the bondline in the DB are shown in Fig.14. The process zone ahead of the crack tip is also shown Fig.14b.

Using the conditions that were used for configuration DT3 in Table 3, a three-dimensional analysis was performed and a static failure load of 45.12 kN was predicted, which was 1.16% greater than the experimental data. The von Mises stress distribution and the damage propagation are shown in Fig.14c and 14d, respectively. It was observed that the length of the crack predicted on the symmetric boundary was smaller than the crack length on the free boundary because of higher shear stresses near the outer surface of the laminate. The lateral shear stresses were observed near the fillet region on the free surface of the adhesive layer

because of the contraction of the aluminium laminae. The phase angles of mode-mixity, $\phi_{1,2}$ (for mode-I and mode-II) and $\phi_{1,3}$ (for mode-I and mode-III) were $45^{\circ}1'$ and $21^{\circ}6'$, respectively. To compare the damage that was predicted in the DT joint by the 2D and 3D models, the static load versus crack length curves were plotted and shown in Fig.15. The predicted crack length from the 2D model was compared with the predicted crack lengths from the 3D model in the middle of the joint (*indicated as 3D model (M)*) and the free face of the joint (*indicated as 3D model (F)*). Although the ultimate static loads obtained from the 2D and 3D models were close to each other, the predicted damage initiation and propagation were rather different because of the observed mod-mixity. This suggests that a three-dimensional analysis should be considered for accurate damage predictions.

As mentioned before, the adhesive butts may influence the static failure behaviour of the DT, depending upon their location in the laminate. To investigate this aspect, the influence of adhesive butts in the laminate was studied using the 2D model. The boundary and loading conditions were similar to the static model without the adhesive butts, see Fig.16a. A number was assigned to each position of adhesive butt, and the numbering system that was used in the analyses is given in Fig.16b.

The DT joints were analysed with butts to obtain their static failure loads. A limited number of butt positions, that were assumed to be critical – although a butt joint could be anywhere in the joint – were considered in the study. From the analyses performed on the DT joints, it was clear that introducing a butt joint in the model reduced their static strengths. The variation in the static failure load with different butt positions is given in Table 4. The static strength obtained experimentally from a specimen with butts not at critical locations was 44.6 kN. Moreover, when butts were not modelled in the DT, the static strength predicted was 43.95

kN, which was 1.45 % less than the experimentally obtained value. The predicted static strength of the DT with a butt at position 11 was 41.53 kN – 3.0% less than the experimental value. Similarly, the DT was analysed for butts at positions 3,5,7,2 and 6. The static strengths predicted and the variations from the experimental value were tabulated. It was found that the DT with a butt at position 1, which is in the top lamina near the fillet region, had failed at 34.77 kN and was critical. A reduction of 22.0 % in the static strength was predicted in this case. The implication of this modelling is that considerable care should be taken over the position of the butt when fabricating the structure.

From the DT joints analysed, the von Mises stress and damage distributions for the DT with butts at positions 6 and 1 (see Fig.16b) are shown in Figs. 17. It was observed that the length of the crack in the laminates along the butt joints was greater than the one in the overlap. It can be seen that in these cases the adhesive layers in the laminate are more prone to failure than the bond-line between the stringer and the laminate.

6.3 LAMINATED SINGLE LAP JOINT:

Similar to the DB and DT joints, two and three dimensional static failure analyses were performed on the LSLJ to predict the static failure loads. As the LSLJ were cut from a wing panel with double curvature, they have initial geometric irregularities – leading to a complex stress distribution. To incorporate these initial irregularities, a detailed three-dimensional modelling approach is required, which includes the effect of initial stresses in the adhesive layer because of bending and twisting moments. However, in the current models, two different boundary conditions were considered by changing the transverse displacement condition at the right-side boundary (see Fig. 7a) – i.e., either completely constrained or released (the latter might compensate the initial stresses in the adhesive layer due to the curvature). The cohesive parameters that were used for the DB model were also employed for

the LSLJ. The variation in the static strengths predicted by the 2D model from the experimental data for each combination is given in Table 5.

The configuration LSLJ1 was modelled with a uniform bond-line thickness of 0.2 mm and the transverse displacement was constrained at the right-side boundary, see Fig. 7a. It failed at 12.30 kN, which was 10.02% greater than the average experimental failure load. A non-uniform bondline thickness and mixed-mode criterion were included in the analysis for the configuration LSLJ2 with the transverse displacement constrained at the right-side boundary. In this case, the static failure load was 12.78 kN showing a +14.31% variation from the test result. Further, the transverse displacement at the right-side boundary was released in the configuration LSLJ3 and the predicted static failure load was 10.98 kN, which is 1.78% less than the test value. Finally, in the configuration LSLJ4 a non-uniform bondline thickness was considered and the transverse displacement was released. A static failure load of 10.54 kN was predicted with a variation of -5.72% from the test data. It can be observed that by releasing the transverse displacement at the right boundary a better static failure load was predicted. Experimental tests revealed that damage initiated near the tip of the groove and propagated along the bondline. Similar to the experimental tests, the damage was predicted to initiate in the bondline near the groove from the 2D model. The von Mises stress distribution and damage propagation along the bondline in the LSLJ is shown Fig.18. The phase angle of mode-mixity, $\phi_{I,2}$, for the configuration LSLJ4 was $29^{\circ}5'$, indicating a peel dominant behaviour, similar to that found in the DB configuration.

The numerical backface strains were compared to the experimental backface strain values to validate the static response predicted by the 2D model. Strain gauges were attached to the stringer at 5 mm and 7 mm from the groove along the overlap and the backface strains were

experimentally measured. The comparison between the experimental and numerical backface strain data is shown in Fig.19. The load-backface strain curves show that the static response predicted by the 2D model is in correlation with the experiments.

Using the conditions that were considered for the configuration LSLJ4 in Table 5, a three-dimensional analysis was performed and a static failure load of 11.31 kN was predicted, which was 1.45% greater than the experimental data. The von Mises stress distribution and the damage propagation are shown in Figs.20a and 20b, respectively. It was observed that the length of the crack predicted on the symmetric boundary was greater than the crack length on the free boundary because of higher peel stresses caused by local plane strain conditions. Lateral shear stresses were also observed near the groove on the free surface of the adhesive layer because of the contraction of the adhesive layer. The phase angles of mode-mixity, $\phi_{1,2}$ (for mode-I and mode-II) and $\phi_{1,3}$ (for mode-I and mode-III) were 30°6' and 8°4', respectively. This indicated a peel dominant behaviour. The numerical backface strains were compared to the experimental backface strain values to validate the static response predicted by the 3D model. The comparison between the experimental and numerical backface strain data is shown in Fig.20. To compare the damage that was predicted in the LSLJ joint by the 2D and 3D models, the static load versus crack length curves were plotted and shown in Fig.21. The predicted crack length from the 2D model was compared with the predicted crack lengths from the 3D model in the middle of the joint (*indicated as 3D model (M)*) and the free end of the joint (*indicated as 3D model (F)*). This suggests that the 2D plane strain model predictions were in correlation with the 3D model.

7. CONCLUSIONS

The static failure behaviour of adhesively bonded metal laminates was investigated by comparing two and three dimensional finite element model predictions with experimental data. A cohesive zone approach with a bilinear traction-separation response was employed to model and predict the static failure behaviour of adhesively bonded metal laminates. An aluminium 2024-T3 laminate with a bonded 70 series aluminium stringer was used for the current research. All bonding was made using FM73 adhesive. Numerical investigations were performed on three joint configurations: a doubler in bending, a doubler in tension and a laminated single lap. The backface strains and static failure loads obtained from the experimental tests were used to validate the models. By considering a mixed-mode criterion and non-uniform bondline thickness, the static damage and failure loads were predicted to an accuracy of 3% for the DB. The backface strains measured from the tests were in correlation with the backface strains predicted. The location of the damage initiation and propagation also agreed well with the experiments. Moreover, using the same failure model, the static failure obtained for the other two joints (DT and LSLJ) were in correlation with the experiments (less than 2% variation). The strategy used in this paper to model adhesively bonded laminates can be used to investigate the effects of adhesive butts on static failure behaviour. It was shown that such an interaction can cause a significant reduction in predicted static strength if the butt is located in a critical region.

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FIGURES:

Figure 1: The usage of adhesively bonded metal laminates in aerospace structures: (a) an aircraft wing with adhesively bonded aluminium laminated skin and (b) adhesively bonded stringer

Figure 2: The cohesive zone in an adhesively bonded laminate

Figure 3: The joint configurations that were used: (a) DB (width 15 mm), (b) DT (width 15 mm), and (c) LSLJ (width 15 mm)

Figure 4: The failure surfaces obtained from static testing on: (a) the LSLJ and (b) the DB joints

Figure 5: The DB joint: (a) the boundary conditions, (b) the cohesive zone location and (c) the mesh details

Figure 6: The DT joint: (a) the boundary conditions and (b) the cohesive zone location, (c) a butt in the laminate and (d) the detail to model the butt

Figure 7: The LSLJ joint: (a) boundary conditions and (b) the cohesive zone location

Figure 8: The 3D finite element mesh in the DB joint

Figure 9: Failure prediction of the DB (configuration DB3) from the 2D models: (a) the von Mises stress distribution and (b) the damage in the bondline

Figure 10: Comparison of the load versus backface strain curves for the DB joint from the 2D model (configuration DB3)

Figure 11: Failure prediction of the DB from the 3D model: (a) the von Mises stress distribution and (b) the damage in the bondline

Figure 12: Comparison of the load versus backface strain curves for the DB joint from the 3D model

Figure 13: Load versus crack length curves for the DB joint from the 2D and 3D analyses

Figure 14: Failure prediction of the DT: (a) the von Mises stress distribution, (b) the damage distribution from the 2D model (configuration DT3), (c) the von Mises stress distribution, and (d) the damage distribution from the 3D model

Figure 15: Load versus crack length curves for the DT joint from the 2D and 3D analyses

Figure 16: The doubler in tension: (a) symmetric boundary condition and kinematic coupling, and (b) numbering system used for butt joints

Figure 17: The DT behaviour at failure load with adhesive butts: (a) the von Mises stress distribution, (b) the damage distribution for a butt at position 1, (c) the von Mises stress distribution and (d) the damage distribution for a butt at position 6 (see Fig. 16)

Figure 18: Failure prediction of the LSLJ (configuration LSLJ4) from the 2D model: (a) the von Mises stress distribution and (b) the damage distribution in the bondline

Figure 19: Comparison of the load versus backface strain curves for the LSLJ from the 2D model (configuration LSLJ4)

Figure 20: Failure prediction of the DT from the 3D model: (a) the von Mises stress distribution and (b) the damage distribution in the bondline

Figure 21: Comparison of the load versus backface strain curves for the LSLJ from the 3D model

Figure 22: Load versus crack length curves for the LSLJ joint from the 2D and 3D analyses

TABLES:**Table 1:** The static failure loads obtained from laboratory testing for different joints

Joint	DB	DT*	LSLJ
Failure load (kN)	5.86 ± 0.16	44.6	11.18 ± 0.28

*Only one test was conducted as the failure observed was governed by large plastic strains in the aluminium laminae

Table 2: The static failure loads obtained for the DB joints from the 2D models for different parameters

Joint	Traction peel(shear) (MPa)	Fracture energy G_{Ic} (G_{IIc}) J/m^2	Thickness of the bondline	Mode-mix criteria	Static strength (kN)	Variation from tests %
DB1	114 (66)	1400 (2800)	uniform	✗	5.36	-8.53
DB2	114 (66)	1400 (2800)	uniform	✓	5.62	-4.09
DB3	114 (66)	1400 (2800)	non-uniform	✓	5.68	-3.07

Table 3: The static failure loads obtained for the DT joints from the 2D models for different parameters

Joint	Traction peel(shear) (MPa)	Fracture energy G_{Ic} (G_{IIc}) J/m^2	Thickness of the bondline	Mode-mix criteria	Static strength (kN)	Variation from tests %
DT1	114 (66)	1400 (2800)	uniform	✗	42.93	-3.74
DT2	114 (66)	1400 (2800)	uniform	✓	43.11	-3.34
DT3	114 (66)	1400 (2800)	non-uniform	✓	43.95	-1.46

Table 4: The static failure loads obtained for the DT joints from the 2D models for different butt positions

Butt joint location	Traction Peel (shear) (MPa)	Fracture energy G_{Ic} (G_{IIc}) J/m2	Static strength (kN)	Variation %
DT: Experimental	114 (66)	1400 (2800)	44.6	-
DT: No butt	114 (66)	1400 (2800)	43.95	-1.45
DT: Butt 11	114 (66)	1400 (2800)	41.53	-3.07
DT: Butt 3	114 (66)	1400 (2800)	39.41	-11.63
DT: Butt 5	114 (66)	1400 (2800)	38.69	-13.25
DT: Butt 7	114 (66)	1400 (2800)	38.05	-14.69
DT: Butt 2	114 (66)	1400 (2800)	37.82	-15.21
DT: Butt 6	114 (66)	1400 (2800)	36.33	-18.54
DT: Butt 1	114 (66)	1400 (2800)	34.77	-22.04

Table 5: The static failure loads obtained for the LSLJ for different parameters

Joint	Traction peel(shear) MPa	Fracture energy G_{Ic} (G_{IIc}) J/m2	Boundary condition	bondline	Static strength (kN)	Variation from tests %
LSLJ1	114 (66)	1400 (2800)	$U_2=0$	uniform	12.30	+10.02
LSLJ2	114 (66)	1400 (2800)	$U_2=0$	non-uniform	12.78	+14.31
LSLJ3	114 (66)	1400 (2800)	$U_2 \neq 0$	uniform	10.98	-1.78
LSLJ4	114 (66)	1400 (2800)	$U_2 \neq 0$	non-uniform	10.54	-5.72