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# EFFECTS OF BONDLINE FLAWS ON THE DAMAGE TOLERANCE OF COMPOSITE SCARF JOINTS

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Scarf repairs to aircraft structures need to sustain design ultimate load in the presence of flaws due to manufacturing and impact by foreign object, in order to demonstrate compliance with airworthiness standards. This paper presents an investigation into the effect of disbonds on the load-carrying capacity of adhesively bonded scarf joints. Experiments were conducted on scarf joints containing disbonds of varying lengths. The results showed that the load-carrying capacity of scarf joints decreases with the size of the bondline flaw at a faster rate than the reduction in the effective bond area. Fractographic analysis showed that the fracture occurred in the composite matrix adjacent to the adhesive-composite interface, at a distance equal to a small fraction of ply thickness. Computational analyses using the virtual crack closure technique (VCCT) and the cohesive zone model (CZM) confirmed these experimental observations: model predictions using composite material properties were in better correlation with experimental results than those using adhesive properties. Furthermore, CZM is capable of predicting the effects of flaws of all sized being considered, while the VCCT model is only applicable to joints containing flaws greater than a certain size.

**Keywords**: *Adhesively bonded joints, strength prediction, cohesive zone model, virtual crack closure, interfacial fracture mechanics, damage tolerance*

## 1. Introduction

Airworthiness certification of adhesively bonded scarf repairs remains a significant challenge [1] due to the lack of non-destructive inspection techniques that can detect weak or kissing bonds. Existing airworthiness certification standards [2, 3] prescribe that safety-critical structures must meet the design limit load without repair, and the design ultimate load in the presence of damage larger than the detection limit [4]. In other words, bonded scarf repairs of safety-critical structures must be demonstrated, by experiments and analysis, to exceed the design ultimate load. Recent investigations have revealed that impact damage [5, 6] and pre-existing flaws [7] have a significant effect on a scarf joint's load-carrying capacity and fatigue endurance [8]. However, current scarf repair design methodologies [9-14] are exclusively based on the analysis of pristine joints that are free of any flaw or damage.

Damage tolerance and durability is a design philosophy used commonly in the aerospace industry. While the precise definitions of damage tolerance and durability may vary [15], the aerospace industry commonly refers to the damage tolerance concept as the ability of a structure to withstand large, discrete damage and still maintain design limit strength. On the other hand, the durability criterion of composite structures describes the need to retain its design ultimate strength in the presence of barely visible impact damage (BVID) [9]. In the case of composite repairs such as scarf repairs, the durability requirement means that a bonded repair must be designed to sustain the design ultimate load in the presence of detectable defects or disbonds [16]. Therefore it is important to develop validated methodologies that can accurately predict the effects of disbonds on the load-carrying capacities of adhesively bonded scarf joints and repairs.

The design and prediction of the strength in adhesively bonded repairs are traditionally based on the assumption that failure is cohesive, that is, cracking is entirely within the adhesive [17]. However, composite failure has been found as the major mechanism by which scarf repairs and joints fracture at room temperature [7]. With the increasing use of composite materials and structures, there is a need to study the fracture behaviour of adhesively bonded repairs and the effect of disbonds at the adhesive-composite interface on the fracture process. Due to the complexities of orthotropy and geometry discontinuities at ply interfaces in composite materials, existing stress or strain based failure criteria require calibration of the characteristic distance [13]. Fracture mechanics based methods, such as the cohesive zone model (CZM) and the virtual crack closure technique (VCCT), overcome this difficulty. The propagation of cracks at bimaterial interfaces occurs when the structure is loaded past the threshold strain energy release rate (SERR) [18]. This method has been used widely to determine the loads required for crack propagation [19-24]. While the CZM is relatively new in its application to bimaterial interfaces, the VCCT model is now an industry-standard for performing damage tolerance analysis. One particular issue of applying VCCT to a bimaterial interface is the oscillatory singularity at the tip of a bimaterial interface crack [25].

Scarf joints are two-dimensional representations of scarf repairs along the most highly loaded direction. It has been found that the stress concentrations along the bondline depend on the ply angle and are highest at the terminations of load-carrying plies [13, 26-28]. Furthermore, stress concentrations were also found near the feathered ends of a scarf joint, which could lead to damage initiation in the bondline [29] or in-plane damage in the composite adherends [4]. Numerical predictions of bonded scarf adherends have compared favourably to experiments in various publications using the CZM method [6, 26, 30, 31]. These publications explicitly modelled the crack path by embedding CZM elements that represented the unique mode of failure at that interface. Experimental studies of bonded scarf adherends have presented investigative techniques using microscopy to examine fracture surfaces [32, 33].

This paper presents an experimental investigation into the effect of interfacial disbonds on the load-carrying capacity of adhesively bonded scarf joints. Scarf joints with embedded disbonds of varying lengths, as shown in Figure 1, were tested to failure in tension. A scanning electron microscope (SEM) was used to investigate the failure surfaces and determine the fracture path. Representative scarf joints were analysed with finite element (FE) models in conjunction with an average stress criterion, a linear elastic fracture mechanics (LEFM) approach, and damage progression models using the CZM and VCCT. With the CZM and VCCT models, various combinations of damage interfaces and material properties were investigated to verify and predict the failure strength and fracture path in the scarf joints.

## 2. Experiments and results

The experimental investigation used VTM264/T700 [34] unidirectional carbon/epoxy prepreg for composite coupons and composite scarf joint adherends. This prepreg material has a nominal ply thickness of 0.22 mm and a curing temperature of 120°C for 1 hour. For bonding, an epoxy-based structural adhesive VTA260 [35] was used, which has a curing temperature of 120°C for 1 hour. The material properties of the composite material and the adhesive, as provided by the manufacturers [36, 37], are presented in Table 1 and Table 2.

Experimental tests were conducted to determine the critical mode I and II strain energy release rate, $G_{Ic}$ and $G_{IIc}$, of the composite and adhesive in accordance with the standards [38, 39]. The double cantilever beam (DCB) and three-point bend end notched flexure (ENF) tests were used for mode I and II, respectively. Each specimen had a width of 25 mm, and five specimens were used for each test. To determine the properties of the composite, specimens were manufactured using 16 plies of unidirectional lamina, with fibres being along the length of the specimen. For each specimen, a polytetrafluoroethylene (PTFE) insert was embedded at the mid-plane of composite laminar or directly adjacent to the adhesive layer, to create an initial flaw. Some typical load-displacement curves for DCB and ENF tests are shown in **Error! Reference source not found.**. The critical mode I and II strain energy release rate of the composite material and the adhesive were calculated in accordance with the standards [38, 39]. The values of mode I and mode II strain energy release rates for the composite and the adhesive are summarised in Table 3. Also included in this table are the interlaminar tensile and shear strength of the composite, and the tensile and shear strength of the film adhesive, both provided by the manufacturer [36, 37]. In Table 3, the parameter $\sigma_T$ refers to the through-thickness tensile strength for the composite, and the tension strength of the adhesive, and $\tau_f$ refers to the transverse shear strength of the composite, and the shear strength of the adhesive.

Scarf joints were manufactured with an embedded flaw size, *a*, referring to Figure 1, of 3, 6, and 12 mm in length along the bondline. Joints were also manufactured without an initial flaw ($a$ = 0) to characterise the performance of the pristine joints. A lay-up of $[45°/0°/0°/90°/-45°]_{2S}$ was used to manufacture two separate panels of VTM264/T700 composite. The cured panels were cut into coupons of 25 mm in width. Scarfing was carried out by tilting the coupons at 5º to a milling machine, producing a taper with a "feathered" end as shown in Figure 1. The scarfed surfaces were cleaned by light sanding and solvent. The scarfed adherends were then bonded with VTA260 adhesive and cured in accordance with the manufacturer's recommended curing process. Scarf joints produced in this manner are representative of the situation where the damage material is removed through machining and a repair patch is machined from a cured laminate. At least three specimens were manufactured for each set of embedded flaw condition. The scarf joints were loaded under quasi-static tension at a displacement rate of 1.0 mm/min until failure, where failure was determined as the loss of load-carrying capability and the failure strength was taken as the maximum recorded load.

The experimental results, the strength and extension across the grips at failure, are summarised in **Error! Reference source not found.**. During the tests, minor cracking sounds were heard, followed by catastrophic fracture that is typical of highly loaded brittle structures. From **Error! Reference source not found.**, it can be seen that as the flaw size increased, the stress and extension at failure decreased. This means that as the flaw size increased, less energy was dissipated during joint fracture.

A detailed inspection of the fracture surfaces was conducted, using visual inspection, optical microscopy and SEM. An example of the fracture surfaces is given in **Error! Reference source not found.**, where the fractured sides of a scarf joint with an embedded flaw (a = 12) and a pristine joint are shown. The feathered ends of each adherend can be seen on both fracture surfaces. This indicates that for specimens both with and without flaws, fracture occurred at the feathered end of one adherend, travelled along the bondline, then crossed the adhesive, and propagated along the bondline again towards the feathered end of the other adherend.

Visual inspection of the adherends showed that the bulk of the adhesive remained attached to one adherend. This initially suggested that adhesive failure, or failure along the composite-adhesive interface, was the principal damage mode. However, detailed SEM analysis revealed the presence of fibres on both fracture surfaces. This is illustrated in **Error! Reference source not found.**, where SEM images of both fracture surfaces are presented, in comparison with the adherend surface after machining but prior to bonding. As the scarf plane passes through each ply, the fracture surface characteristics changed with ply orientation, which is illustrated in **Error! Reference source not found.**. On the 0° and 45° ply surfaces, the adhesive was covered with a thin layer of composite peeled off from the other adherend, which, unsurprisingly, exhibited complete composite fracture without any sign of adhesive. These results showed that the fracture path was not at the composite-adhesive interface, but inside the composite adherend. Furthermore, it can be concluded that the distance between the fracture path and the composite-adhesive interface was comparable to the fibre diameter. The presence of matrix heckles on the majority of the fracture surface indicated that the fracture was largely driven by shear failure of the matrix. In addition, there were occasional instances observed on the fracture surfaces of pull-out of the adhesive carrier scrim; an example is shown in **Error! Reference source not found.**. The adhesive carrier scrim serves to maintain a constant bondline thickness. During adhesive bonding, compression of the adherends led to the adhesive carrier scrim contacting the adherends. The low bonding strength between the adhesive and the polyester scrim contributed to the formation of imprints on the 0 degree ply terminations. For the 90° plies, both fracture surfaces showed loose fibres, which again suggests that the crack travelled through the composite ply. Heckles were not observed on the fracture surfaces, which indicates the failure was predominantly driven by tension.

A summary of the crack path observations is presented in **Error! Reference source not found.**, which shows a schematic of the crack path through each ply, and a simplified schematic of the way in which the crack migrated through the adhesive at some point along the bondline. The flaw size was not found to affect the crack path, or the characteristics of the fracture plane on any of the plies. In general, it was concluded that the fracture occurred in the composite adherends, with tensile fracture in the 90° plies and interlaminar shear failure in the 0° and 45° plies. These results suggested that failure was controlled by the fracture properties of the composite material, instead of the adhesive. These experimental observations will be used to guide the development of FE models and the identification of the appropriate material parameters, which is described in the following section.

## 3. Numerical modelling of scarf joints

### *3.1. Finite element model*

Finite element models were developed in Abaqus 6.10 [40], for use with four different strength analysis techniques: Average stress; LEFM; VCCT; CZM. Plane strain four-node orthogonal (CPE4) and three-node triangular (CPE3) elements were used to model the adhesive and the composite adherends by individual plies. In the scarf region, the laminate of each adherend is no long balanced and symmetric. The deformation across the width of the specimen produces a complex three-dimensional field. As a conservative approach, the scarf joints were modelled as plane strain, as it was unclear whether generalised plane strain would be applicable. Material properties for the composite plies and the adhesive were taken from Table 1 and Table 2, respectively. The properties of +/- 45º plies were derived using ply coordinate transformation equations, considering only the components in the plane of the model [13]. Boundary conditions were applied on both ends of the scarf joint to replicate experimental testing constraints, and consisted of constrained displacements in all degrees of freedom, except for the loading displacement at one end. A non-linear implicit numerical analysis was performed using Abaqus/Standard.

Based on the fractographic observations described in the previous section that the fracture occurred in the composite at a small distance comparable to the fibre diameter away from the composite-adhesive interface, the onset and propagation of cracks was assumed to be along the composite-adhesive interface. This ignored the extremely thin layer of resin-fibre material removed from the composite adherend, as the influence of this layer was considered negligible. To model the crack path, damageable interfaces were embedded along critical regions in the joint, as shown in **Error! Reference source not found.**. Almost all models used a single damageable interface along the composite-adhesive interface. Some CZM models instead used a single damageable interface in the mid-plane of the adhesive, as explained in a subsequent section. The fracture properties of the composite-adhesive interface were taken from the composite properties in Table 2, except for some models where the use of adhesive properties was investigated.

The virtual crack closure technique (VCCT) and the cohesive zone model (CZM) are numerical models that are dependent on the mesh density for numerical accuracy. The effect of the CZM interfacial strength and mesh density on the accuracy of numerical simulations has been discussed in [41, 42]. The mesh size of the cohesive zone elements needs to be sufficiently small to capture the effect of stress concentrations or cohesive damage zones at the crack tip. Given the magnitude of the interface stresses and the requirement to capture the cohesive zone with at least three elements, it was found that an element length of approximately 0.15 mm in the crack growth direction was required in the present investigation. Similarly, guidelines for applying the VCCT at a bimaterial interface are provided in the literature [25]. Mathematical solutions of the SERR have been shown to oscillate at very small flaw sizes ($a \rightarrow 0$). A range of element sizes were studied to ensure a converged FE solution with a mesh that was coarse enough to avoid oscillating results [25]. It was found that a bondline mesh length of 0.15 mm in the crack growth direction was suitable. Thus, a bondline mesh length of 0.15 mm was maintained for both CZM and VCCT models. The mesh scheme of the finite element model is shown in **Error! Reference source not found.**.

### *3.2. Average stress*

Adhesive stresses along a scarf plane vary significantly as the ply stiffness is strongly affected by its orientation [13]. Traditionally, scarf joints are designed using identical isotropic adherends where the stresses along the adhesive are uniform. An elastic FE analysis was performed to determine the shear stress distribution of the current scarf joint lay-up in the adhesive along the bondline, and the results are shown in **Error! Reference source not found.**. These results show that high shear stresses occur at the ends of 0° plies, which is similar to results of Wang and Gunnion [13].

An average stress failure criterion is based on using the average of all peel or shear stresses along the bondline. The average stress criterion is a very crude instrument. Since this technique is currently used in repair designs to size scarf repairs, it has been chosen as a comparison. Maximum stress may be more appropriate for brittle adhesives in the case of pristine joints. For joints containing disbond, the maximum stress is unbounded, due to the stress singularity at the crack tip. Consider a scarf joint between identical adherends of thickness, *t*, and tapered at angle, *θ*. The average shear stress and the average peel stress for a given disbond *a* can be determined using the load-equilibrium method by Erdogan and Ratwani [43],

$$\frac{\tau_{av}}{\sigma_{app}} = \frac{\frac{1}{2}\sin(2\theta)}{1 - \frac{a\sin\theta}{t}} \tag{1}$$

$$\frac{\sigma_{av}}{\sigma_{app}} = \frac{\sin^2\theta}{1 - \frac{a\sin\theta}{t}} \tag{2}$$

where the averaged peel stress, $\sigma_{av}$, and average shear stress, $\tau_{av}$, denote the averaged quantities along the mid-plane of the adhesive layer. These values are normalised using the applied (far-field) stress, $\sigma_{app}$. **Error! Reference source not found.** presents a comparison of these solutions with the results of a FE analysis, in which flaws of various sizes were embedded at the composite-adhesive interface. In the FE model, the composite-adhesive interface was set as a bonded contact, and disconnecting the nodes in the relevant region generated the required flaw. It is seen that the numerical models and analytical equations for the average stresses in the adhesive are in good agreement.

### *3.3. Linear elastic fracture mechanics*

Linear elastic fracture mechanics considers the crack to grow once the SERR at the crack tip reaches a critical value. The application of LEFM at the composite-adhesive interface requires consideration of the bimaterial properties of this crack tip. The mismatch in elastic properties is commonly expressed in terms of the Dundurs second parameter, *β*, under plane strain conditions [34], which is given by

$$\beta = \frac{1}{2}\left[\frac{\mu_1(1-2\upsilon_2) - \mu_2(1-2\upsilon_1)}{\mu_1(1-\upsilon_2) + \mu_2(1-\upsilon_1)}\right] \tag{3}$$

where  and  are the shear modulus and Poisson’s ratio respectively, subscripts 1 and 2 refer to the two dissimilar materials surrounding the crack tip. From this, the crack tip singularity parameter, ε is given by [35],

$$\varepsilon = \frac{1}{2\pi} \ln\left(\frac{1-\beta}{1+\beta}\right) \tag{4}$$

The strain energy release rate, *G*, at the bimaterial interface is given by [22]:

$$G = \frac{K_I^2 + K_{II}^2}{E_{eff} \cosh^2(\pi\varepsilon)} = \frac{1-\beta^2}{E_{eff}}\left(K_I^2 + K_{II}^2\right) \tag{5}$$

where *K* denotes the stress intensity factors, subscripts *I* and *II* refer to parameters pertinent to the peel and shear loading modes, and $E_{eff}$ is the bimaterial effective modulus given by

$$\frac{1}{E_{eff}} = \frac{1}{2}\left(\frac{1-\upsilon_1}{E_1} + \frac{1-\upsilon_2}{E_2}\right) \tag{6}$$

The factors $K_I$ and $K_{II}$ can be expressed in terms of the basic solution for a crack in an infinite body [19], after introducing two factors $y_I$ and $y_{II}$ to characterise the geometric configuration shown in Figure 12. This gives

$$K_I + iK_{II} = \left[ y_I\left(\frac{a}{L}\right)\sigma_{av} + iy_{II}\left(\frac{a}{L}\right)\tau_{av} \right](1+2i\varepsilon)\sqrt{\pi a}\,(2a)^{-i\varepsilon} \tag{7}$$

where *a* is the crack length, $\sigma_{av}$ and □av are the average peel and shear stresses given by equations (1) and (2). With regards to a loaded scarf joint, Equation (7) can be expressed in terms of the far-field applied stress, $\sigma_{app}$ [43]:

$$K_I + iK_{II} = \sigma_{app}\left[\frac{1}{2} y_I\left(\frac{a}{L}\right)\sin(2\theta) + iy_{II}\left(\frac{a}{L}\right)\sin^2\theta\right](1+2i\varepsilon)\sqrt{\pi a}\,(2a)^{-i\varepsilon} = \sigma_{app}\left(Y_I + iY_{II}\right)\sqrt{\pi a} \tag{8}$$

where $Y_I$ and $Y_{II}$ are the geometry factors of the scarf joint with a crack at a bimaterial interface. Now the strain energy release rate can be written as

$$G = \left(\frac{1-\beta^2}{E_{eff}}\right)\left(Y_I^2 + Y_{II}^2\right)\sigma_{app}^2 \pi a \tag{9}$$

The solution (9) requires the geometry factors ($Y_{\text{I}}$ and $Y_{\text{II}}$) of the scarf joint to be known. Because of the complex geometries, inhomogeneous and anisotropic material properties, no analytical expressions are currently available. In the present investigation, finite element analysis was employed to first determine the strain energy release rate, *G,* for a given applied stress, and then determine the values of *Y* from equation (9). For each flaw size, a unit stress of 1.0 MPa was applied, the mode I and mode II *G* values were determined using the finite element model, and the geometry factors were determined in Equation (9). **Error! Reference source not found.** presents the geometry factors as a function of the normalised flaw size. The largest values correspond to the cases when the material around the crack tip is surrounded by 0° plies. It should be noted that these geometry factors are specific to the particular layup and geometry of the specimen and cannot be generalised. However, the factors can be determined for any specimen configuration using the approach outlined.

With the results shown in Figure 12, it is now possible to predict the onset of crack propagation, defined as when the SERR, $G$, approaches the critical value, $G_C$:

$$\frac{G}{G_C} \geq 1 \tag{10}$$

In a mixed mode loading condition, the critical SERR can be defined by the Benzeggagh-Kenane (B-K) fracture criterion [44] as given by

$$G_C = G_{IC} + \left(G_{IIC} - G_{IC}\right)\left(\frac{G_{II}}{G_I + G_{II}}\right)^{\eta} \tag{11}$$

where the exponent, $\eta$, is an empirical parameter that needs to be experimentally calibrated.

### *3.4. Virtual Crack Closure Technique*

The VCCT is a well-established numerical technique used on composite structures that calculates the strain energy release rate at a crack tip. It is a linear elastic fracture mechanics analysis based on the product of the nodal forces, $F$, at the crack tip and nodal displacements, $v$, behind the crack tip for a given area of width, $b$, and element length, $d$. Thus, for a given fracture mode:

$$G = \frac{Fv}{2hd} \tag{12}$$

Within Abaqus, the VCCT is incorporated into a progressive damage model that allows for automated modelling of crack propagation in a non-linear analysis. In this model, a damageable interface is defined as a bonded contact between two surfaces. A pre-existing disbonded region is defined, which involves only a touching contact, and this is used to automatically define the crack tip. At the end of every increment in a non-linear analysis, Equation (12) is assessed at a crack tip node. If crack growth, defined by equation (10), is deemed to occur, then the bonded contact at that node is converted to a touching contact for the next increment. In this way, automated crack progression can be captured, allowing for the simulation of stable crack growth, or crack growth occurring in a non-catastrophic manner. In this paper, the term “VCCT model” is used to refer to the Abaqus crack growth methodology incorporating the VCCT Equation (12).

### *3.5. Cohesive Zone Model (CZM)*

The CZM is a numerical method that assumes a cohesive damage zone developing at the crack tip at the onset and propagation of fracture. A zero-thickness cohesive element with a bilinear traction-separation law is applied. The bilinear law consists of a linear elastic response before damage initiation and propagation as shown in **Error! Reference source not found.**. The linear elastic opening displacement for mode I and II respectively, $v_I^o$ and $v_{II}^o$, are defined as:

$$v_I^o = \frac{\sigma^o}{k}; \quad v_{II}^o = \frac{\tau^o}{k} \tag{13}$$

where $\sigma^o$ and $\tau^o$ are the traction strengths for Mode I and II respectively, and $k$ is the penalty stiffness parameter that ensures a stiff connection between the surfaces. The displacements for complete fracture for mode I and II respectively, $v_I^f$ and $v_{II}^f$, are defined as:

$$v_I^f = \frac{2G_{IC}}{\sigma^o}; \quad v_{II}^f = \frac{2G_{IIC}}{\tau^o} \tag{14}$$

For mixed mode damage evolution, the B-K fracture criterion, Equation (11), was used. The stress for damage initiation at a particular mode mixture is determined using the quadratic stress criterion, given by

$$\left(\frac{\tau}{\tau^o}\right)^2 + \left(\frac{<\sigma>}{\sigma^o}\right)^2 = 1 \tag{15}$$

Damage progression in cohesive elements is dependent on the relative displacements and tractions at the interface which surrounds it [45]. Thus, the CZM approach is capable of predicting fracture of pristine joints without pre-existing disbond. Similar to the VCCT model, the crack plane must be pre-defined as a damageable interface. It is reported in the literature [41, 45] that the values for the stiffness of cohesive elements are required to be sufficiently high for composite materials. This is to prevent a reduction in material elasticity at the interface due to excessive deformation of the cohesive elements without causing numerical errors. Although the appropriate value would depend on the geometry of the specimen and the layup, the stiffness is commonly chosen as a penalty parameter, which balances a high stiffness requirement against computational issues. The present investigation employed a value of $k = 1 \times 10^6$ N/mm$^3$ as recommended in [45].

## 4. Strength prediction of scarf joints

### *4.1. Average stress*

A first order estimate of the load-carrying capacity of scarf joints containing a disbond can be estimated using the average shear stress criterion [13, 43]: fracture occurs when the average shear stress reaches the shear strength of the adhesive. From Equation (1), the ultimate strength $\sigma_{ult}$ of a scarf joint containing a disbond of length, $a$, can be expressed in terms of the shear strength as:

$$\sigma_{ult} = \frac{\tau_f}{\frac{1}{2}\sin(2\theta)} \times \left(1 - \frac{a\sin\theta}{t}\right) \tag{16}$$

**Error! Reference source not found.** shows a comparison of the predicted strength using the average stress criterion and the experimental results, plotted against the respective flaw size. It is clear that the strength of joints, measured in the experiments, decreased at a faster rate than that predicted by the average stress criterion. It can be concluded that the reduction in joint strength is greater than the expected reduction due to the loss of bond area, and that the average stress criterion is non-conservative and hence unsuitable as a design criterion.

*4.2. Linear elastic fracture mechanics*

Based on the linear-elastic fracture mechanics analysis presented in Section 3.3, the load-carrying capacity of a scarf joint containing a disbond can be determined as the stress that produces the critical SERR, $G_C$. From Equation (9), the joint strength is given by

$$\sigma_{ult} = \frac{1}{\sqrt{Y_I^2 + Y_{II}^2}} \sqrt{\frac{E_{eff} G_C}{(1-\beta^2)\pi a}} \tag{17}$$

In this work, the geometry factors $Y_I$ and $Y_{II}$ have been determined using a numerical approach in Section 3.3, and the results are shown in **Error! Reference source not found.**. The critical SERR is a function of the mode mixity at the crack tip, which can be determined from the B-K fracture criterion, referring to Equation (11),

$$G_C = G_{IC} + (G_{IIC} - G_{IC})\left(\frac{Y_{II}}{Y_I + Y_{II}}\right)^{\eta} \tag{18}$$

Equation **Error! Reference source not found.** can now be employed to determine the joint strength for any flaw size. This approach assumes that the joint experiences catastrophic fracture as soon as the critical SERR is first reached. A comparison between this prediction and the experimental results is shown in **Error! Reference source not found.**. The LEFM predictions are in fairly good correlation with the experimental results, and are conservative. In particular, the LEFM predictions are reliable for flaw sizes of over 3.0 mm, but significantly over-predicted the joint strength for smaller flaws. This is due to the strain energy release rate approaching zero as the flaw size approaches zero.

From **Error! Reference source not found.**, the LEFM predictions show that at a flaw size of 5.2 mm the joint strength starts to increase with increasing flaw size, until a flaw size of about 7.0 mm is reached, after which the joint strength decreases with flaw size. The local peak of joint strength at 7.0 mm is roughly equal to the joint strength at 4.0 mm. This means that at flaw sizes from 4.0 mm to 7.0 mm, a stable crack growth region would be expected. As such, the LEFM results would be too conservative, and a methodology that captures progressive crack growth would be required. This type of analysis is presented in the VCCT model and CZM results in the following sections.

### *4.3. Virtual crack closure technique method*

The VCCT model was employed to model crack propagation along the composite-adhesive interface. The numerical model was analysed under increasing displacement until failure and the ultimate strength of the joint was calculated from the maximum applied load. Disbonds of various lengths were embedded at the interface, by modifying the properties of the contact surface and the location of the crack tip. Two different sets of material properties were investigated to study whether using composite or adhesive fracture properties, presented in Table 3, would yield better correlation with experimental results. The models using adhesive or composite fracture properties for the crack growth interface are labelled VCCT-adhesive and VCCT-composite, respectively.

As shown in **Error! Reference source not found.**, The VCCT model joint strength predictions using composite fracture properties provided excellent correlation with experimental results for flaw sizes greater than 3 mm in length. The better correlation using composite fracture properties over adhesive fracture properties is consistent with the SEM observations that fracture propagated in the composite adherends, and that the composite properties controlled the crack growth. However, as the flaw size decreases below 3.0 mm, the VCCT model predicted increasingly high strengths, exceeding the strength of pristine joints.

A comparison between the stress at initiation of cracking and the joint strength using the VCCT-composite model is given in Table 4, where the crack initiation was detected as the first instance of crack growth. From these results, it can be seen that for most flaw sizes the initiation of cracking occurred at stress levels very close to the joint strength. This agrees with the experimental observations, where only slight cracking noises were heard just before catastrophic failure for the flaw sizes investigated. The results in Table 4 also show that for flaw sizes of 5.2 mm and 6.0 mm there was a significant difference between the crack initiation and joint strength, indicating a larger stable period of crack growth. This agrees with the LEFM results presented previously that identified stable crack growth for flaw sizes between 4.0 mm and 7.0 mm, and confirms the benefit of applying a progressive damage model for failure predictions.

### 4.4. Cohesive zone model

The CZM was applied to model progressive crack growth and failure in the scarf joints. To investigate the sensitivity of the location of the cohesive zone within the adhesive layer, models were created with the CZM elements embedded at the composite-adhesive interface and adhesive mid-plane as shown in **Error! Reference source not found.**. Three models were investigated: “Composite” – failure along the composite-adhesive interface using composite properties; “Adhesive, interface” – failure along the composite-adhesive interface using adhesive properties, and; “Adhesive, mid-plane” – failure along the adhesive mid-plane using adhesive properties. The numerical models were analysed under displacement control until failure and the joint strength was computed as the total load applied at the loaded end of the joint at maximum load.

The results of the CZM predictions for all three models in comparison with the experimental joint strengths are shown in **Error! Reference source not found.**. The CZM predictions of all three models at flaw sizes of less than 2.5 mm were observed to be generally similar. It was found that experimental results for small flaw sizes (3 mm or less) showed better agreement with the "Adhesive, Interface" rather than "Composite" model predictions. This suggests that fracture in joints at flaw sizes of 3.0 mm or less is likely dominated by cohesive failure of the adhesive. At larger flaw sizes, the model with composite properties gives the best correlation with the experimental results, and followed the experimental results closely. This agrees with the results from the VCCT model, and confirms the conclusion that the composite properties controlled the joint strength in the experimental results. There was very little difference between the choices of interface for the two models using adhesive properties. Unlike the VCCT model, the CZM was able to provide reasonable predictions of joint strength at small flaw sizes and the pristine joint, though the predictions were slightly higher than the experimental joint strengths.

## 5. Discussion

Experimental results have shown that the fracture of composite scarf joints would propagate into the composite laminate adherend. Fractographic analysis showed composite materials composed mainly of loose, broken fibres and sheared matrix on the fracture surfaces of both adherends. Numerical models, calibrated to mode I and II interlaminar critical SERR, predicted the strength of the scarf joint accurately. The successful application of these composite properties to the numerical models would suggest that the fibres did not play a significant part in resisting crack propagation. Furthermore, the application of adhesive properties along the composite-adhesive interface or the adhesive mid-plane provided poorer predictions of joint strength. This suggests that the strength of scarf joints was controlled by the properties of the composite matrix. Furthermore, this confirms observations that the fracture plane contained a matrix layer that was smaller than the ply thickness and of the order of the fibre diameter.

The damage tolerance and durability is critical for composite structures due to its susceptibility to delaminations and disbonds. Through linear elastic fracture mechanics approaches shown in previous sections, it was found that the geometry factors for a range of crack lengths was capable of identifying the state of crack propagation. By designing for steady state crack propagation, composite plies and structures can be made tolerant against catastrophic failure during operation by ensuring the crack size smaller than the detectable limit would not cause premature failure.

Experimental results showed that the loss of approximately a quarter of the bond length resulted in the loss of more than half the joint strength. In scarf repairs, as the disbond size increases, the stresses in the scarf is transferred to the surrounding adherend to cause disbond growth [13]. The analysis in this paper has been focused on the interfacial fracture propagation along the bondline of the scarf joint using unidirectional composite plies. As scarf joints are designed with scarf angle aspect ratios between 1:20 and 1:40, fracture behaviour in the joints will likely involve interlaminar delaminations and in-plane ply fracture. Further research is needed to address the crack branching phenomenon and the effects of the load-carrying capability of scarf joints containing flaws.

## 6. Conclusions

This paper has investigated the strength of secondarily bonded composite scarf joints with different bondline flaw sizes through a series of experimental testing, analytical modelling and numerical simulation. Experimental results showed that the strength for complete fracture of scarf joints with flaws is dependent on the ply angle adjacent to the crack tip and the size of the flaw. Through fractographic analysis, it has been found that the fracture of composite scarf joints occurred in the composite adherend, at a distance that is a very small fraction of the ply thickness. This failure near the composite-adhesive interface was dominated mainly by matrix shear failure in the 0° and 45° plies and matrix peel failure in the 90° plies. Numerical analyses using composite material properties along the composite-adhesive interface gave better predictions than when adhesive properties were used. This is consistent with the experimental observation that the fracture was within the composite, rather than by cohesive failure of the adhesive. For scarf joints of pristine conditions or containing flaws, the CZM was capable of accurately predicting the ultimate strength. The VCCT and the LEFM approaches were able to provide equally accurate predictions of the ultimate strength for flaws greater than around 3.0 mm. The predictive model using the cohesive zone model offers a robust technique to account for the effect of disbond on the ultimate strength of scarf joints and repairs.

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Table 1: Material properties of VTM264/T700 composite

| $E_{11}$ | $E_{22} = E_{33}$ | $\nu_{12} = \nu_{13}$ | $\nu_{23}$ | $G_{12} = G_{13}$ | $G_{23}$ |
|---|---|---|---|---|---|
| 120 GPa | 7.5 GPa | 0.32 | 0.33 | 3.9 GPa | 2.3 GPa |

Table 2: Material properties of VTA260 adhesive

| $E$ | $\nu$ | $G$ |
|---|---|---|
| 3 GPa | 0.35 | 1.1 GPa |

Table 3: Fracture properties of VTM264/T700 and VTA260

| | VTM 264/T700 | VTA 260 |
|---|---|---|
| $G_{I\,c}$ (J/m$^2$) | 462 | 1302 |
| $G_{II\,c}$ (J/m$^2$) | 1603 | 7750 |
| $\sigma_T$ (MPa) | 45 | 65.8 |
| $\tau_f$ (MPa) | 85 | 38 |

Table 4: Loads at the onset and propagation of flaw

| Flaw size (mm) | Crack initiation (MPa) | Joint strength (MPa) |
|---|---|---|
| 1.0 | 1396 | 1396 |
| 2.0 | 481 | 486 |
| 3.0 | 324 | 326 |
| 5.2 | 199 | 254 |
| 6.0 | 225 | 254 |
| 8.0 | 222 | 224 |
| 12.0 | 145 | 150 |