Abstract
Stiffened structures provide an efficient structure for engineering applications. During damage events debonding between the stiffener and the plate is commonly observed. In this paper the tolerance of top-hat stiffened composite structures is assessed considering debond damage between the stiffener and plate. The effect of prior debond damage on the failure modes and ultimate strength of the panel is investigated. A non-linear finite element model is verified with a global-local approach to assess both material degradation and crack initiation and propagation. A parametric study investigates the effect of both damage properties and the panel’s geometric properties on the failure modes, ultimate strength and its damage tolerance.

1 Introduction
Composites offer many advantages in engineering applications; ability to tailor properties to a specific design, favorable strength to weight ratio, resistance to corrosion and low thermal expansion. Stiffened top-hat shells are commonly used as the primary structure in the marine, aerospace and civil applications to reduce the unsupported span of the laminate and prevent lateral-torsional instability and panel buckling. Stiffened structures provide an efficient structure where the stiffeners are aligned with the in-plane loads. A number of stiffener cross-sections are utilized in this application including a number of open section and closed section stiffeners.

In all structural applications defects and damage events can occur. In damage events occurring in composite structures catastrophic failure is often avoided but can lead to cracks, delaminations and debonds. It is imperative that once damage has occurred that it can be rapidly assessed and suitable precautions taken to ensure safety of the structure and its users, but this must be done cost efficiently. To reduce expensive maintenance costs it is also imperative to know if damage must be fixed immediately, in the near future or that if it poses no current threat. At the design stage assessment of the effect of defects and damage leads to an optimized damage tolerant design which both avoids catastrophic failure following common damage events and provides an efficient use of materials and therefore optimized weight and fuel efficiency / load carrying capacity.

2.1 Literature Review
Many authors have investigated the damage progression of intact structures, with good correlation to experimental results. However, there is still limited research conducted on the residual capabilities of structures containing prior delaminations and associated damage.

Falzon [1] showed that mid bay delaminations and holes had minimal effect on the ultimate collapse load of a stiffened structure; however it has been shown by Meeks et al. [2] that delaminations present under the stiffener have a significant effect on the ultimate strength of the panel. Parametric studies have shown that delamination and debond size and location has a significant effect on the damage mode of the panel [3,4,5,6] however, these studies do not consider the interaction of failure modes as they omit material failure or damage propagation and therefore do not assess the ultimate failure of the structure. By assessing the ultimate strength of a structure the true margin of structural safety may be determined.

Literature on damaged stiffened structures is also primarily focused on T-stiffeners. Top-hat stiffeners have been shown to have increased torsional rigidity and an increased structural efficiency in comparison
to open section stiffeners However, the damage response of such panels is not well understood.

This paper uses a non-linear finite element model, in Abaqus, incorporating both material degradation and crack propagation to assess the effect of damage within the stiffened panel and the complex interaction of damage mechanisms leading to ultimate collapse. The effect of damage and design parameters is investigated on the damage tolerance of the structure.

3. METHODOLOGY

3.1 STRUCTURAL CONFIGURATION
A generic glass-vinylester top-hat stiffened plate is considered under in-plane compressive loading aligned with the stiffener direction. Under compressive loading the interaction of delamination growth, inter-frame and frame buckling leads to a complex interaction of damage mechanisms. Therefore, a multi-stiffened panel is considered with a damaged zone in the region of the central stiffener between the stiffener flange and plate. The following modeling methodology is outlined in order to accurately assess the progressive collapse of multi-stiffened panels for structures which are initially intact or contain idealized delaminations.

3.2 NUMERICAL MODELLING
A global-local methodology is used to optimize the efficiency of the model and is illustrated in figure 1. The global model is generated from 4 node shell elements (S4) which use using an equivalent single layer approach minimizing the computational effort required whilst providing an accurate assessment of the buckled and post-buckled response of the stiffened plate. The stiffeners and plate are modeled in separate parts and connected by multi-point constraints which restrict the degrees of freedom of the plate nodes to that of the connected flange nodes. To initiate the post-buckled response an imperfection is applied to the mesh using the first
eigenvalue buckling mode of the intact structure. This imperfection is scaled so the maximum out-of-plane imperfection is scaled to 10% of the lamina thickness. A non-linear analysis is then conducted using the Newton-Raphson method, an automatic incrementation scheme and a user-defined material which implements a strength based progressive damage methodology. The local analysis is driven by sub-model boundary conditions using this initial non-linear global analysis. A local model, representing the areas at risk of debonds initiating, is generated using a linear layer-wise, element per ply formulation using solid elements (C3D8). Therefore the through thickness stresses are modeled with increased accuracy to allow the use of a strength based delamination initiation criterion. A comprehensive review of such criteria compared to mixed mode experimental data is lacking in the literature and are not fully compared or validated. Therefore in this paper the most commonly used criterion is adopted to assess the location and load at which the debond initiates; Hashin [7] quadratic nominal stress criterion, which is a strength based criterion shown in equation 1.

\[
\left( \frac{\sigma_{z3}}{S_{zz}} \right)^2 + \left( \frac{\sigma_{z2}}{S_{z2}} \right)^2 + \left( \frac{\sigma_{z1}}{S_{z1}} \right)^2 \geq 1. \tag{1}
\]

The calculated location and load at which a debond occurs is passed from the local model to the global model where the debond initiation is implemented at the appropriate load. The connections at the interface nodes at the debond location are removed to allow separation of the parts. A small sliding contact constraint is placed between the debonded plies to prevent penetration of the plies. The crack propagation is assessed using a kinematically compatible nodal release sequence based on the modified virtual crack closure technique. Under mixed mode loading the Benzeggagh-Kenane criterion [8] is used to assess the mixed mode failure criterion. The Benzeggagh-Kenane criterion for crack propagation is shown for the 3D case in equation 2 where \( \eta \) is a semi-empirical exponent applied to delamination initiation and growth and \( G_T = G_I + G_{II} + G_{III} \).

\[
G_{IC} + \left( (G_{IIc} - G_{IC}) \frac{G_{II}}{G_T} + \frac{G_{III}}{G_T} \right) \left( \frac{G_{II} + G_{III}}{G_T} \right)^\eta \geq G_T. \tag{2}
\]

For both the global and local models the strength based progressive damage is implemented through a user defined material using a limited discount approach. This incorporates a number of failure criteria as recommended by the World Wide Failure Exercise [9]. The Puck [10], Tsai-Wu [11] and Zinoviev [12] criteria are compared for each case to determine which is the most appropriate along with a combined criterion. For the combined criterion the limited discount approach is used if failure is deemed if any of the failure criteria is met for a specific failure mode. In this paper Zinoviev’s progressive damage methodology is applied to all criteria using an instantaneous degradation approach; following the onset of failure for a specific failure mode the affected material properties of the element are degraded instantaneously to 1% of the nominal value. To avoid numerical convergence issues a viscous regularization scheme is applied which causes the tangent stiffness matrix of the softening material to be positive for sufficiently small time increments.

### 3.2 MODEL VALIDATION

To validate the model and methodology, solutions are compared to selected experimental and analytical results available in the literature. A number of tests on rectangular plates loaded in uniaxial compression conducted by Starnes [13] are used to verify the buckled and post-buckled response. The VCCT nodal release method is verified against experimental results of mode I and mode II tests [14,15]. The inclusion of delaminations in composite structures is verified against through width delamination tests conducted by Kutlu and Chang [16] and embedded delamination tests reported by Riccio and Pietropaoli [17]. Finally, the elements are combined to test the damage progression in top-hat stiffened composite structures are verified against full scale tests conducted by Smith and Dow [18].

### 3.2 (a) Buckling of Rectangular Plates

A number of tests on GRP unidirectional rectangular plates, loaded in uniaxial compression, conducted by Starnes [13] are used to verify the buckled and post-buckled response. A typical load displacement plot
is shown in figure 2. In a convergence study the model is seen to have converged for ultimate load with approximately 400 quadratic elements (S8) and using a viscous parameter of 1E-5. The model shows excellent correlation to the post-buckled response of composite laminate plates; closely matching the initial stiffness and global buckling load for a number of experimental tests. For the case shown buckling of the panel is predicted at 42.9kN, within 8% of the experimental buckling load, and final failure predicted at 92kN, within 2% of the experimental failure load. The progressive damage approach shows excellent agreement for two of the three panels, and yields a conservative result by less than 9% for all panels tested. Comparison of the WWFE recommended failure criteria shows that a combined or Puck criterion yields the most accurate ultimate failure load across all panels and any deviation is conservative.

3.2 (c) Mode I and II Crack Propagation
The crack initiation and propagation methods are verified against experimental results of mode I, double cantilever beam, and mode II, end notch failure coupon, tests conducted by Reeder and Crews [14,15]. The Load Displacement curves are compared to the experimental data for the DCB and ENF tests shown in figure 3. The model shows that reasonable correlation with the experimental results for the crack propagation in modes I and II can be obtained from the VCCT method using shell elements in a 3D model. The idealization of the fracture surface leads to an underestimation of the maximum load in the mode I case by 8.8% which suggests that fiber bridging may be present behind the crack tip experimentally. Whereas, in the mode II case the maximum load is overestimated due to a gradual build of matrix cracks and damage at the crack tip prior to crack advancement, which is not captured by this modeling technique.

3.2 (d) Through Width Delaminations
The model's ability to follow combined crack propagation and buckling is verified against the experimental work of Kutlu and Chang [16]. The authors investigated the response of composite coupons in compression containing through width delaminations positioned centrally across the coupon. The delamination in the case presented here is positioned 4 plies from the front face of a 20 ply coupon. In a convergence study the model is seen to have converged for ultimate load and buckling loads with 1570 quadratic elements (S4). A global imperfection is seeded representing the first buckling mode with a maximum out-of-plane deformation of 0.1% of the lamina thickness which is the minimum to initiate a global post-buckled response. An additional imperfection is seeded in the
outer lamina of 0.01mm representing the offset generated by the Teflon insert. This combination has been shown parametrically to most closely match the experimental results. For this model the combined failure criterion is used to ensure conservative results. Figure 4 shows the load-strain response for the upper delaminated ply and the lower laminate, where the strain data is read from the central outer laminates of the coupon. The initial stiffness, buckling load of the delaminated ply and transition to the post-buckled state are well captured by the model; shown by the linear load strain response up to a load of 6kN and the transition from negative to positive strain on the outer edge of the front ply. However, the global buckled response and ultimate collapse is significantly over estimated by the model. The maximum load, which is coincident with the global buckling load, is predicted at 13.4kN representing an overestimation by 32%.

Figure 4: Through Width Delaminations
Load-Strain Response

Figure 5 compares the deflection at the center of the front and back plies against applied load for the current three dimensional shell element method and is compared to the 2D numerical models of Liu et al. [19] and finite strip method implemented by Wang and Zhang [20]. The current three dimensional shell element model shows good overall correlation with the alternative methodologies; all three numerical models overestimate the global buckling load suggesting this overestimation may be attributed to unrealistic boundary condition assumptions or imperfections or flaws within the material. The boundary is assumed to be ideally clamped whereas the experimental fittings are unlikely to restrict all rotations and would lead to increased deflection of the back laminate experimentally compared to the finite element model.

3.2 (e) Embedded Delaminations

The models accuracy in representing the progressive collapse of an embedded delamination is verified against the experimental work of Riccio and Pietropaoli [17]. Riccio et al. investigated laminates under compressive loading containing a central circular delamination. The case discussed here is that of a delamination positioned 3 plies from the front face of a 35 ply laminate. The out-of-plane deflection of the central point of the delaminated zone for the front and back plies with increasing load is illustrated in figure 6. The model reasonably replicates the experimental observations; the buckling load of the delaminated ply and the post-buckled response is adequately replicated by the model although there is an over estimation of the peak out-of-plane displacement. For this model the minimum imperfection required to generate the post-buckled response generates a more gradual transition to the local buckled state and instigates an increase in the out-of-plane displacement during global buckling.

The delamination propagation load is predicted well by the model and as the delamination propagates the stiffness reduction triggers global buckling. During
global buckling the model predicts tensile matrix and shear failure in the lower ply of the delaminated layer however with a lack of information from the experimental test on the damage propagation this cannot be verified. The delamination growth is modeled well; delamination growth initiates at the center of the panel, on the symmetry line and propagates perpendicular to the loading direction, as reported experimentally. 

3.2 (f) Damage Evolution in Top-Hat Stiffened Panel

Finally, the damage progression in top-hat stiffened composite structures is used to verify the global-local methodology monitoring delamination initiation and propagation in a post-buckled structure against full scale tests conducted by Smith and Dow [18] on a large grillage structure. A single stiffener parallel to the loading direction and located at the center of the grillage is modeled using symmetric boundary conditions on the unloaded edges and clamped condition on the loaded and reaction edges. For this model the imperfection is seeded with the maximum out-of-plane imperfection represents 10% of the lamina thickness, as measured experimentally by Smith [18]. A convergence study is conducted on the global and local models showing that both models have converged for an element size of 10mm. The maximum out-of-plane displacement with increasing load is shown in figure 7 comparing the experimental data and the global-local methodology. Quantitatively the initial stiffness, buckling load and plate deformation are reasonably captured by the model given the boundary condition assumptions. The delamination initiation is predicted at a 981kN which is comparable to that reported experimentally of 920kN. However, the model predicts a rapid progression of damage with instantaneous crack growth down the length of the flange-plate interface which reduces the load carrying capability of the panel significantly whereas Smith et al. observed a residual capability following delamination propagation leading to ultimate failure at 1090kN. This underestimation of the ultimate failure load by 10% could be attributed to the boundary condition assumption or due to an assumption made in the material properties which in this case are taken from the literature.

In this model the effect of the relative size of the local model is investigated on the stress distributions and shows that the length of the local model has little effect on the through thickness tensile and shear stresses however the sub-model width must extend beyond the flange edge to ensure accuracy in the stress distribution. The use of the sub-models provide a significant reduction in run time (10%-50%) compared to the computational time for the full solid model. By reducing the sub-model width around the flange-plate interface considerable time saving can be achieved. However, a reduction in length of the sub-model provides the greatest reduction in computational expense; however, the failure location must be known to successfully use...
this method. The sub-model method has been shown to be a robust method capable of reducing the computational expense when compared to a full solid model of approximately 50%.

4 Parametric Studies
The progressive collapse mechanisms are investigated examining the effect of delamination size, location and the design geometry of the stiffened panel.

4.1 Model Configuration
Under compressive loading the interaction of delamination growth, inter-frame and frame buckling leads to a complex interaction of damage mechanisms. A triple stiffened panel is considered with dimensions and boundary conditions as shown in figure 8. The panel is loaded in longitudinal compression with the loaded and reaction ends of the panel fully clamped. The panel is considered to be part of a larger grillage and therefore symmetric boundary conditions are applied to the unloaded transverse edges. A layup is considered using Glass Vinylester balanced woven roving (WRE580) with the fiber direction aligned with the stiffener direction and a fiber volume fraction of 0.33. The table of the stiffeners is reinforced with a central additional central unidirectional Glass Vinylester ply (UE500). The material properties are shown in Table 1 and are taken from manufactures data or approximate values taken from the literature. Material properties are expressed in the directions 1, 2, 3 which refer to the longitudinal, transverse and through thickness material directions respectively. The critical strain energy release rates are mixed mode test data from the literature [21].The shear interface strengths are approximated for the lower range of material parameters reported by Junktikka [22]. The normal interface strength is taken as the transverse strength of the unidirectional laminate as the tensile failure of the fiber-resin interface represents the mode of failure in this test. To initiate the post-buckled state an imperfection is seeded onto the structure where the maximum out-of-plane deflection is equivalent to 10% of ply thickness. This scale of imperfection
is observed in large scale manufacture as measured by Smith [18].

The analysis is stopped when the panel reaches its ultimate load and undergoes gross yielding or overall collapse. Buckling of the intact plate is determined when a 5% change in stiffness is observed from the initial linear state which corresponds to an out-of-plane deformation of 3.75mm which in this case is equivalent to 50% of the plate thickness. Local plate buckling is defined as when the deflection under the debonded stiffener area corresponds to an out-of-plane deformation representing 50% of the plate thickness. Flange buckling is determined when a marked deviation is observed in the out-of-plane deformation of the flange.

Convergence of the model is assessed using a number of damaged cases. For larger debonds, greater than 13% of the panel length, the analysis has converged for a mesh size of 10mm corresponding to 560 elements in the debond area, as shown in figure 9a. For smaller debonds, less than 13% of the panel length, a more refined mesh is required to capture buckling of the debonded flange, the radial nature of crack growth and clarity of the crack front. An element size of 5mm is chosen, equivalent to 2250 elements in the debond area as a compromise between efficiency and accuracy; the solution has converged to within 5% change in ultimate load and crack propagation load as shown in figure 9b.

4.2 Debond Size

The effect of debond size is assessed for a centrally located debond between the central stiffener and the plate. The debond size is described as a percentage of the plate length. The methodology as described in section 3 where the local model is used to assess delamination initiation in the outer stiffeners only.

The local model has shown that for the intact case and smallest delamination case, 6.7%, a debond initiates in the outer stiffeners within 5% of the ultimate load without effecting the ultimate load of the panel. For larger initial debonds the outer stiffeners bond interfaces remain intact.

Figure 10 shows the trends in the ultimate failure load and the initiation load of different failure modes: damage initiation load, crack propagation load and the local plate and flange buckling loads and the global buckling load. The loads are represented as a percentage of the ultimate failure load of the intact structure.

Examining the intact case it is shown that a considerable post-buckled strength exists as the buckled load and damage initiation load occur at 54% and 83% of the ultimate load respectively.
The ultimate failure load drops to 70% of the intact ultimate failure load for debonds greater than 20% of the panel length and is largely unaffected by the size beyond a debond size representing 20%. The proportion of the post-buckled strength relative to the ultimate strength reduces to 25% at a debond length of 13% but as the initial debond size increases the buckling load decreases whilst the ultimate strength remains constant.

The results are discussed with regard to three regions to highlight the change in failure mode. In region 1, where the debond represent less than 20% of the panel length, global buckling of the plate occurs prior to local buckling of the stiffener or plate. Crack propagation is driven by plate buckling and is predominantly mode I. Ultimate failure of the plate is driven by the buckling of the flange in the first mode which leads to shear failure in the flange and then in the web of the stiffener. In the second region, where debonds represent 20-40% of the panel length, damage propagation is driven by local buckling of the plate under the debond. Mode I crack propagation occurs due to this local plate buckling which is followed by buckling of the flange into a single half sign wave. In the third region, where debonds are greater than 40% of the panel length, the plate initially buckles locally under the debonded stiffener however crack propagation initiates due to flange buckling where the flange buckles into a three half sign wave. For these cases crack growth is inhibited by global buckling of the plate as the crack front lies close to the global buckling anti-node line. The positive peak of the buckled zone at the end of the plates inhibits mode I crack growth and crack propagation is initiated under mixed mode I and II for a debond length of 53%. Further crack propagation is also inhibited by failure of the central stiffener. As the debond length increases to 66% buckling of the central stiffener occurs prior to crack propagation where localized material degradation occurs in the stiffener flange and centrally within the web which reduces the load at the crack tip and inhibiting crack propagation.

4.3 Debond Position

The effect of the debond position along the panel length is investigated for a number of debond sizes. The debond location is described as the distance from the panel end to the center of the debonded area as a ratio of the panel length. Debond sizes of 50mm (6.7%), 150mm (20%) and 300mm (40%) between the stiffener and the plate are presented here in figure 11 a, b and c respectively. For these cases the local model checks delamination initiation in the outer stiffeners. For a debond size of 50mm,
it is shown that the debond location has little effect on the global buckling load of the panel. The damage initiation load drops from 83% to 70% as the debond moves closer to the center of the panel. The ultimate failure load is reduced by less than 10% from the intact load for a debond location at the outer thirds of the panel and only reduced to 78% for central delaminations located within the central half sine wave of the globally buckled plate. Only for the centrally located delamination are localized buckling and crack propagation observed. For a debond size of 150mm, representing 20% of the panel length, the effect of the debond location has a greater effect on both the ultimate load and the failure mechanism. For a central debond the crack propagates and damage initiates as the panel buckles both globally and locally at approximately 50% of the intact ultimate load. The panel maintains a significant post-buckled strength which represents a further 50% increase in load from buckling to ultimate failure as damage and crack propagation occurs as the panel buckles however these propagate gradually. An offset delamination in the outer thirds of the panel can maintain a significant increase in load from buckling to ultimate failure with irreversible damage occurring at a further increase in load of 40%. A transition point is seen at a debond location 35% along the panels length where shear damage initiation and propagation from the anti-node line becomes the dominant failure mode as in the intact case. For an offset delamination in the outer thirds of the panel the buckled load remains unaffected, the delamination initiation load reduces by 9.5% and the ultimate load is reduced by less than 7.5% relative to the intact case.

In all cases the buckled plate forms a negative half sine wave at the center of the panel forcing the plate away from the stiffener and initiating crack propagation. A transition in failure mode occurs as the central crack front approaches the anti-node line. Crack propagation is mode I driven as the buckled shape of the plate forces the stiffener and plate apart. As the central crack front moves away from the central node-line the deformations at the crack tip reduce, as does the energy available to open the crack.

For the 300mm debond size, representing 40% of the panel length, the debond is too large to remain
between two node lines and location trends are similar to that of the 150mm case. A transition point in failure mode is again observed between 0.3 and 0.35. At 0.3 the debond sits midway between two node lines which represent a positive and negative peak. As the panel buckles globally the local buckling of the plate is inhibited by the sine wave produced by global buckling. Therefore the plate deformation at the more central crack tip is reduced compared to those where local plate buckling is dominant and crack growth is inhibited. At 0.35 the outer crack front passes beyond the outer node line further enhancing this effect.

4.3 Stiffener Spacing
The effect of stiffener spacing is investigated with regard to the effect of debond size. The stiffener spacing is presented as the distance center to center between stiffeners as a percentage of the panel length. The stiffener spacing is investigated from 33% to 60% for a central debond. Figure 12 illustrates the decrease in the global buckling load for increasing stiffener spacing whilst the ultimate strength remains largely constant. Therefore the post-buckled strength increases as the stiffener spacing increases. For a stiffener spacing representing 33% the panel exhibits minimal post-buckled strength however the post-buckled strength increases linearly with increasing stiffener spacing an at a stiffener spacing of 60% the buckling load is 38% of the ultimate load.

Considering the effect of debond size for a central stiffener similar trends are seen to the reference case considered in the previous section, in which the stiffener spacing represents 50% of the panel length. For all spacing’s there exists an almost linear drop in the ultimate strength up to a debond size of 20%. For larger debonds the ultimate strength remains constant however, this plateau in ultimate strength occurs at increasing load, relative to the specific intact case, for increasing stiffener spacing, as shown in Figure 13. This is due to a significant change in failure mode from the intact to the large debond case for the smallest stiffener spacing. For the smallest stiffener spacing the intact case exhibits very limited post buckled strength. The introduction of the debond changes the failure mode most significantly introducing large displacements as the plate buckles locally below the debond area causing localized damage and therefore reducing the ultimate strength most significantly. For larger stiffener spacings both the intact and debond case exhibit significant deformation during buckling causing localized damage and therefore resulting in a less significant reduction in ultimate strength and a greater damage tolerance.

4.4 Stiffener Height
The effect of stiffener height is investigated for a squat, square and tall stiffener, representing a stiffener height of 50mm 75mm and 100mm respectively width all other dimensions fixed as in Figure 10. The stiffener height can assist in
preventing buckling of the plate but adds more resistance in sustaining a higher ultimate load. For the intact case the ultimate load increases for increasing stiffener height by 20% from squat to square and 9.5% from square to tall whilst the buckling load increases by 5.3% and 3.5% respectively. This is also reflected in the damaged case; the increased stiffener height and therefore stiffness also increases the ultimate strength of the panel. However, this fails to prevent damage and crack propagation in the localized debond area which is driven by plate and flange buckling. A reduction in ultimate strength in regions 2 and 3 from the intact case is observed to 67%, 70% and 77% respectively for the squat, square and tall stiffener types however the local buckling, crack propagation and damage initiation load remain constant.

5. Conclusions
This paper presents an analysis investigating the effect of damage parameters on the residual capability of multi-stiffened composite structures. Detailed non-linear finite element models are assessed utilizing a global-local approach which was validated to accurately assess the crack initiation and propagation, progressive damage and ultimate collapse of the structure. The analysis shows that the failure mode of the panel and interaction of failure modes changes significantly as the debond size increases. Global buckling dominates for a debond size representing 20% of the panel length whereas local plate buckling for larger debonds. Debond location relative to the plates buckled node lines has been shown to be critical in assessing the damage tolerance. Considering geometric parameters it is shown that smaller stiffener spacing results in a greater reduction in ultimate strength for a large debond compared to a larger stiffener spacing due to a more significant change in failure mode. Considering the stiffener height it is shown that the ultimate strength of the damaged panel may be increased by increasing the height of the stiffeners however localized damage in the region of the debond occurs at constant loads.

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References


