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Failure analysis in postbuckled composite T-sections

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Abstract

Blade-stiffened skin designs made of composite materials have the potential to produce highly efficient structures, when the large strength reserves in the postbuckling range are utilised. This paper investigates the failure under postbuckling deformations of T-section specimens cut from a blade-stiffened panel, by comparing experimental results to finite element models. In the experimental work, T-section specimens with a particular lay-up and geometry were tested to failure in antisymmetric and symmetric loading rigs. These loading rigs simulate deformations on skin-stiffener interfaces during panel postbuckling. For the numerical analysis, two-dimensional models of the interface cross-section were used with a strength-based criterion that monitored failure within each ply. The use of a zero-thickness layer of cohesive elements has also been investigated in order to simulate the delamination behaviour. The numerical predictions are compared to the experimental results in terms of the failure load, specimen stiffness and specimen behaviour. The analysis approach is shown to be capable of predicting the critical damage locations and initiation loads for both antisymmetric and symmetric loadings. The successful prediction of failure in skin-stiffener interfaces can be linked to a global-local approach for efficient analysis of large, fuselage-representative composite structures.

Keywords: Buckling; Postbuckling; Stiffened panels; Skin-stiffener damage; COCOMAT

1. Introduction

Postbuckling is the phenomenon in which structures continue to carry loads higher than the critical buckling load. Stiffened skin panels made of composite materials have the potential to produce highly efficient structures, when the large strength reserves in the postbuckling range are utilised. Such composite structures are ideal for fuselage and wing skin panels on aircraft. Postbuckling design has been used with metallic aircraft for decades to reduce weight. However, to date the application of postbuckling design with composite structures has been limited, owing to concerns relating to the durability of the composite structures. Unlike metals, composites do not yield locally under the high local stress field set up during the postbuckling phenomenon. Furthermore, there are concerns regarding the high through-thickness stresses that are set up and the development of defects within the laminates is not well understood and cannot be predicted accurately using current design tools.

When blade-stiffened composite panels are loaded in compression, generally two types of buckling deformation patterns are observed, as shown in Figure 1. One is “local” buckling of the skin between the stiffeners shown in Figure 1(a), and the other is “global” buckling along the panel length as shown in Figure 1(b). Failure under these types of deformations usually occurs at nodal or anti-nodal lines in the structure, where either the bending or twisting moments are at their maximum [1]

In experimental testing of stiffened composite structures in compression, failure typically involves delaminations at the stiffener base or at the edge of the stiffener flange [2]. The onset of delamination typically leads to catastrophic collapse, resulting in a rapid decline in the load carrying capacity of the structure. It is therefore important to develop design tools to predict such failures and thus lead to improved panel design.

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Figure 1: Postbuckling deformation patterns (a) Antisymmetric (b) Symmetric.

In finite element (FE) analyses of postbuckling stiffened structures, it has been shown that good definition of the global structural response can be obtained by relatively coarse modelling with shell elements [3]. However, to accurately determine the correct failure mechanisms requires fine models of the skin-stiffener interface with a large number of elements, which is infeasible in the analysis of large structures. An alternative approach is to use a two-step approach, in which a coarse global model is combined with the analysis of local models at regions of potential failure based on the global panel buckling patterns. This approach can be applied for the rapid analysis of large structures, and has been developed and demonstrated in experimental and numerical investigations of fuselage-representative composite structures [4]

This paper describes the failure analysis of T-sections from a stiffened composite panel loaded with typical postbuckling deformations. In previous work, experimental and numerical investigations of T-sections

specimens with ply drop-offs were conducted [5]. In this work, the analysis approach is extended to study specimens without ply drop-offs, and in an additional loading case. Antisymmetric and symmetric postbuckling deformations have been investigated, where the symmetric loading consisted of both pull and push tests. A strength-based approach is used to predict the initiation of interlaminar damage, in which stresses in the critical directions are compared to material strengths and combined into a single failure index. FE analysis was conducted using the nonlinear solver in MSC.Marc (Marc) and a user subroutine written to determine the failure index for each element. The FE models are compared to the experimental results in terms of the specimen behaviour, specimen stiffness and failure load. Recommendations are then given for the application of this approach for the analysis of fuselage-representative structures.

This work is part of the European Commission Project COCOMAT (Improved MATERIAL Exploitation at Safe Design of Composite Airframe Structures by Accurate Simulation of Collapse), an ongoing four-year project that aims to exploit the large strength reserves of composite aerospace structures through more rapid and accurate prediction of collapse [6-7].

2. Experimental Investigation

A fuselage-representative blade-stiffened composite panel was manufactured at Israel Aircraft Industries and tested at the Aerospace Structures Laboratory at Technion as part of the COCOMAT project. The panel was manufactured from IM7/8552 carbon fibre unidirectional prepreg. Thin strips were cut from these panels consisting of the skin and a single stiffener, each with a width of 25 mm. The ply lay-up and specimen geometry is shown in Figure 2.

Figure 2: Specimen geometry and ply lay-up.

A total of 25 specimens were tested in two test rigs to simulate three postbuckling deformations as shown in Figure 1: antisymmetric, symmetric pull and symmetric push. There were ten specimens each for the antisymmetric and pull tests, and five specimens for the push tests.

Figure 3: Antisymmetric test rig.

The antisymmetric deformations were simulated using the test rig in Figure 3. The skin clamps were on a rotating fixture, one end of which was connected to a load cell. The piston moved vertically downwards and the blade and skin clamps translated accordingly, with the skin clamps also rotating about the axis of rotation due to their connection to the load cell. The moment applied to the specimen was measured by multiplying the reaction force at the load cell with the distance between the circular segment and the axis of rotation. This applied moment was then normalised by dividing by the specimen width. The applied energy could be obtained by multiplying the moment with the angle of rotation. However, in this work the normalised moment is used as the defining parameter for the antisymmetric tests.

For the pull tests, a modified version of the antisymmetric rig was used whereby the skin clamps were connected directly to the translating piston without the rotating assembly. The blade clamp was connected to the load cell and the piston translated downwards with the skin clamps. The applied load was taken from the MTS machine, and was multiplied by the displacement of the piston to obtain the applied energy. This energy was then normalised by dividing by the specimen width.

From previous work, specimen failure was classified according to the failure location, as shown in Figure 4 [5]. Mode 1 or bend failure involved delaminations that occurred in the skin-stiffener junction. Mode 2 failures occurred in the stiffener blade. Mode 3 or flange failure occurred at the flange edge and involved delamination

and separation of the stiffener and skin. Mode 4 or core failure occurred at the core region underneath the stiffener, generally at the interface with the skin.

For the specimens in this work, flange failure was the dominant mode for the antisymmetric and pull tests, which was expected given the high stress concentration at the flange edge. Bend failure occurred for some antisymmetric and pull specimen, and whilst not common, occasionally involved lower failure energies than flange failure. The pull tests all failed exclusively in the core region.

One critical aspect of the experimental results is the slipping seen from the grips in the push tests, as shown in Figure 5. From the test, a displacement of roughly 4 mm of slip was seen after 12 mm axial displacement loading, which was seen in all push tests. Though slipping was not seen in the other tests, as the clamp conditions for all tests were similar, some degree of slipping is expected to have occurred.

Figure 4: Failure mode classification.

Figure 5: Push test, estimated slip at skin clamp at 12 mm axial displacement load.

Plots of normalised moment or energy versus piston loading displacement are given for the pull, antisymmetric and push tests in Figure 6, Figure 7 and Figure 8 respectively. Note that the results are

presented as a shaded region indicating the experimental data range. From this data, an outlier was identified in the antisymmetric test based on unusual specimen stiffness indicating poor skin clamping. Another outlier for the same test was considered due to its relatively high failure energy when compared to the rest of the test data.

Figure 6: Pull tests, data range and failure points.

Figure 7: Antisymmetric tests, data range and failure points.

Figure 8: Push tests, data range and failure points.

For the antisymmetric tests, the experimental curves showed an initial delay between increasing displacement and increasing moment, prior to a roughly linear region of increasing moment. As each of the specimens showed a different amount delay, the experimental curves were shifted so that the delay was removed.

3. Numerical Analysis

3.1. FE Modelling

FE models of the T-sections were created in MSC.Mentat v2005r3 and solved using the Marc nonlinear solver [8], on a 1.6 GHz AMD Sempron processor. Two-dimensional (2D) models were created using 2D generalised plane strain elements, which were validated to give similar results to 3D solid elements in previous work [5]. The six-noded generalised plane strain elements are quadrilateral elements with two added nodes for out-of-plane displacement and rotations. Figure 9 gives the geometry and boundary conditions for the antisymmetric and symmetric tests, and the models are summarised in Table 1.

Figure 9: FE models. Top: Antisymmetric. Bottom: Symmetric.

Table 1: FE nominal model parameters

	Symmetric	Antisymmetric
No. elements	4295	4329
No. nodes	4607	4637

Figure 9 shows the system of rigid links, multiple point constraints and displacement boundary conditions that replicated the testing rig for the antisymmetric and symmetric tests. Since a boundary condition cannot be imposed on a slave node in a rigid-body link, a 2D beam with properties of a nominal aluminium bar was used to represent the load cell arrangement in the antisymmetric model, thus allowing a fixed boundary condition to be set at the load cell node.

Separate models were created using a layer of zero-thickness cohesive elements between the skin and stiffener. The cohesive elements used the exponential cohesive law as implemented in Marc [8], which describes the traction-displacement behaviour with an exponential relationship.

The material properties were taken from characterisation tests on IM7/8552 unidirectional specimens within the COCOMAT project. These properties were the average taken from tests in three separate laboratories. From these tests, the variation between material properties ranged within 5% for the stiffness data. Furthermore, there was a variation of 10% from the mean in the strength data, but the variation between data sets of the transverse tensile strength was up to 25% from the mean. The ply material was assumed to be transversely isotropic, so that the through-thickness properties were taken equal to the transverse properties. The material definition is given in Figure 10, where the core was given properties of 0° plies surrounded by resin, as in the test specimens this region consisted of rolled up prepreg tape.

Figure 10: Element orientations at stiffener bend and skin.

To model the effect of the skin slipping out of the clamps, friction was included as part of the boundary conditions, which required modelling the skin and blade clamps with aluminium blocks. A friction coefficient of 0.6 was found to give suitable properties across all loading types. Though slipping was not noted for the antisymmetric and pull tests experimentally, from the numerical models the introduction of friction for these tests resulted in the skin slipping out of the clamps by only 0.25 mm. Though undiscernible in the experiment, the numerical analysis illustrated that this degree of slipping has a significant effect on the specimen stiffness, as is shown later in the results.

Another observation from the testing was that the antisymmetric specimens were subject to lateral translation, in addition to the axial translation of the testing machine. An example of this is shown in Figure 11, where the lateral translation corresponded to an x-displacement, which was introduced into the boundary condition of the clamps. As with friction, the introduction of the x-displacement had significant influence on the specimen stiffness.

As a result of these considerations, the nominal models were updated to account for friction and lateral displacement, where the latter was only applied to the antisymmetric specimens. It was found that the introduction of friction in all models and x-displacement in the antisymmetric models was required in order to get good comparison with the stiffness of the experimental specimens, which agrees with findings of the previous work [5] and of other researchers investigating stiffened composite joints [9].

Figure 11: Lateral displacement of the antisymmetric specimens under loading (Test #9). Top left: Unloaded specimen. Top right: Final loaded displacement. Bottom: Schematic showing x and y displacement.

To determine the initiation of delamination, a user subroutine was written for the PLOTV routine within Marc. The PLOTV subroutine allows for the calculation of user-defined element output values, and is called by every integration point at every layer of each element. As shown in Figure 10, orientations were assigned to each element to align the first material direction with the through-thickness direction. Delamination was predicted with the ‘degenerated Tsai’ criterion [10], given by

$$\left(\sigma_x/X_t\right)^2 + \left(\sigma_z/Z_t\right) + \left(\tau_{yz}/S_{yz}\right)^2 \geq 1 \quad (1)$$

where:

σ_x = Stress in longitudinal direction

σ_z = Interlaminar (through-thickness) stress

τ_{yz} = Interlaminar (through-thickness) shear stress

X_t = Tensile strength in the longitudinal direction

Z_t = Interlaminar tensile strength

S_{yz} = Interlaminar shear strength

Failure was deemed to occur when the average of all four values at the element integration points satisfied this criterion.

The analysis of failure at the end of the flange required the above approach to be modified, to account for the stress concentration at the blunt edge. As a result, an approach based on a characteristic area was applied, where the area of failed elements was used to determine the onset of failure. From a comparison of the experimental and numerical results, it was found that a characteristic area of 0.15 mm² gave suitable failure predictions for all loading types considered. It should be noted that the characteristic area approach was only applied for the analysis of interlaminar damage initiation at the flange edge, and that the element-based detection approach was applied for all other locations.

3.2. Analysis Results

The results for the nominal and updated numerical models are given in Figure 12 and Figure 13. As previously mentioned, the nominal models displayed much stiffer specimen behaviour than the test results, and the introduction of friction for all models and x-displacement in the antisymmetric models was required in order to get good comparison with the experimental stiffness.

For the pull tests, the updated numerical model predicted failure at the flange at 50 J/m. In comparison, the experimental results show both flange and bend failure, where flange failure was seen for seven out of the ten specimens. The specimens with flange failure had failure energies banded in two groups, with four results around 54 J/m and three results around 77 J/m. The numerical prediction shows good agreement with the four lower energy flange failures, and close agreement with the average normalised failure energy of 62 J/m for all seven flange failure results. In the numerical model, failure in the bend region was predicted to occur at a normalised energy of 110 J/m, which is significantly higher than the average value of 51.5 J/m seen in the three experimental bend failures.

For the antisymmetric tests, the updated model showed excellent agreement with the test results. Flange failure was predicted at a normalised moment of about 2000 Nm/m which showed good correlation with the average value of 1948 Nm/m for all the five flange failures observed. In the numerical model, bend failure was predicted to occur at a normalised moment of 2830 Nm/m, compared to the average experimental bend failure value of 2230 Nm/m. This indicates that the bend was not a critical region for the antisymmetric tests, even with the effects of friction and lateral displacement included. Reason for the five instances of bend failure in the antisymmetric tests, three of which were not outliers, is discussed in a later section.

For the push tests, though the introduction of friction coefficient into the numerical models improved the agreement with the experimental stiffness, the numerical models still predicted significantly stiffer behaviour, particularly towards the end of the loading. The updated FE model gave a slip distance of only 1 mm after 12 mm axial loading, in comparison with the experimental value of 4 mm. This indicates that the friction model could not accurately represent the complex sliding behaviour occurring in the grips for large amounts of slipping. The updated numerical model predicted failure in the bend at around 173 J/m, which did not agree well with the experiment where failure was seen in the core region with an average value of around 500 J/m. Note that the jagged nature of the results at large displacements is due to model experiencing large slips out of the clamp, and further illustrates that the clamp modelling was not suitably representing the complex friction behaviour.

Figure 12: Normalised applied load (energy or moment) versus loading displacement. Top: Pull tests. Middle: Antisymmetric tests. Bottom: Push tests.

Figure 13: Delamination failure index at first failure: Top: Pull and antisymmetric tests (characteristic failure area 0.15mm^2). Bottom: Push tests.

The results of the models with the layer of zero-thickness cohesive elements are given in Figure 14 and Figure 15. These results are all for updated FE models, where friction was included in all models and 1 mm of x-displacement was applied with the antisymmetric models.

Figure 14: Pull test, FE model with cohesive elements.

Figure 15: Antisymmetric test, FE model with cohesive elements.

The behaviour of the cohesive models was different than the previous models, due to the capability of the cohesive elements to model progressive failure of the skin-stiffener interface, as shown in Figure 16.

Figure 16: Pull test, delamination progression captured using cohesive elements

For the antisymmetric and pull models, failure initiated at the flange edge and progressed inwards along the skin-stiffener interface. Both models displayed similar behaviour, in that failure of the cohesive elements showed stable crack growth up until some critical point, after which rapid delamination or unstable crack growth was seen in the skin-stiffener interface. This critical point corresponded to around 0.8 mm of failed elements for both models, and corresponded to a significant change in the specimen stiffness. The unstable crack growth following this critical point often led to convergence issues with the nonlinear solver.

For the cohesive models, the stresses in the bend region were very low, and bend failure was not predicted to

occur. In fact, for crack lengths longer than the critical crack length, continued failure in the skin-stiffener interface led to a reduction in bend stresses due to the change in deformation of the specimen.

For the push models, the introduction of the cohesive layer did not affect the specimen behaviour and critical mode, and the results for the push cohesive model were almost identical to those given in the bottom figure of Figure 12, with bend failure predicted. Failure did occur in the cohesive layer at one flange edge at around 11.4 mm applied displacement, and led to unstable crack growth and convergence issues.

The cohesive energy was the most critical parameter for the cohesive models, as it controlled the amount of energy dissipated in the cohesive softening process. This value is taken to be equal to the critical strain energy release rate for the ply material. The behaviour of the 2D cohesive element is defined in terms of the separate loading actions of opening (displacement normal to the skin-stiffener interface) and shear (displacement parallel to the skin-stiffener interface), which in fracture mechanics are referred to as the mode I and mode II crack openings. In the implementation of cohesive elements within Marc v2005r3, the cohesive energy value used corresponds to the total energy dissipated as a result of the combined loading actions of opening and shear. However, from fracture mechanics characteristic tests it has been seen that the critical strain energy release rate in shear (mode II) is around double the value in opening (mode I). As a result, the average of the two critical values in mode I and mode II was used as the cohesive energy in the model, to more accurately reflect the combined action of the two crack opening displacements.

3.3. Parametric Study

Parametric studies were conducted to investigate the sensitivity of the model to various parameters. These parameters included the core region material property, blade and skin clamp sizes, friction coefficient between the skin and clamps, antisymmetric friction between the left and right skin clamps as well as the material strength values used in defining the failure criteria. Similar trends were observed to those described in earlier work done by Orifici et al. [5]. As in previous studies, the material properties had the most significant influence on the failure predictions. A reduction in the strength parameter Z_i by 25% reduced the predicted

The experimental specimens also showed varying amounts of resin overflow at the flange edge. This included overflow regions of varying lengths, as shown in Figure 17, and specimens with either no resin overflow, or overflow on one or both flange edges. The experimental results showed no clear correlation between resin overflow and failure energy or failure mode, in that failures were seen at flange edges both with and without an overflow region with different failure energies. For the numerical models, the influence of the resin overflow was investigated by including triangular regions of resin material at the flange edge in small and large sizes, as shown in Figure 17. This was implemented for the antisymmetric and pull models, where the only configuration considered was identically sized overflow regions on both flange edges. Models with the cohesive layer and resin overflow regions were also investigated, where the cohesive layer was extended to the edge of the resin region/

edge. For the resin overflow models, it was not possible to determine the first onset of flange failure. This was due to the fact that offsetting the stress concentration away from the flange led to only a few skin elements showing failure, as failure of the resin itself could not be predicted. In spite of this, the results clearly demonstrated that the presence of the overflow region increased the criticality of the bend stresses and promoted bend failure

4. Discussion

The majority of experimental specimens in the pull and antisymmetric tests failed by delamination in the skin-stiffener interface, which initiated at the flange edge as a result of the high stress concentration. However, a

few of the antisymmetric and pull specimens exhibited failures in the bend, and the cause of these failure modes, which for the antisymmetric tests had a lower failure energy, is unclear. The occurrence of different failure modes for nominally identical specimens and the large degree of experimental scatter in general are both problematic in terms of validating the numerical approach.

The experimental scatter could be due to a number of factors. The specimens indicated signs of variation in geometry between specimens, which may have occurred during manufacture. These variations included differing stiffener flange geometries, tapering in the stiffener blade, as well as asymmetrical bend geometries with different bend radii. These geometric differences affect the stress distribution throughout the specimens and thus have a strong influence on the failure mode. This is particularly significant as the specimens are quite thin and the geometric differences would not be “averaged out” as they would be for longer specimens and panels. Furthermore, there were indications of irregular boundary conditions during the experimental tests such as slippage of the skin from the clamp and horizontal displacement of the blade during the tests. Any taper in the stiffener blade, which likely results from rotation or deformation of the stiffener mandrels in manufacture, would also have complicated the blade grip condition and promoted further slipping. The numerical analyses demonstrated that the specimens were highly sensitive to boundary conditions, and that the introduction of friction and lateral displacement had a significant affect on the specimen stiffness.

The failure predictions were also highly dependent on the strength parameters that were defined for the failure criterion, particularly the through-thickness tensile strength which is a difficult parameter to obtain experimentally. A significant degree of variation of up to 27% was observed in the material characterisation tests for the transverse tensile strengths [5], and this was shown to affect the failure predictions by around 15% to 20%. Furthermore, the accuracy of the assumption of transverse isotropy is unknown. As a result, for any strength-based failure analysis it is important to study the effect of material strength parameter variation on the failure predictions.

The delamination of the stiffener from the skin was well captured by the models with the layer of cohesive elements. However, the use of cohesive elements

introduces a weaker region within the model, and so failure is predetermined to occur at this interface. This therefore limits the model to predicting the delamination, and cannot capture the high stresses that may develop in the stiffener bend. Additionally, the failure progression seen in the cohesive models, in which elements progressively failed prior to a rapid crack growth, differed from the experimental behaviour in which only rapid crack growth was seen. This indicates that the cohesive models may not have accurately represented the specimen stiffness, due to the introduction of this soft region at the skin-stiffener interface.

For the push tests, the analyses predicted failure in the flange, which was at lower levels than the core failure seen in the experiment. The experimental push test specimens also demonstrated a significant degree of slippage, and the inability of the numerical models applied to accurately represent this complex friction behaviour. However, as the failure energies for the pull tests were around three times those for the other tests, and considering the highly nonlinear slipping scenario, the push test is not expected to be a critical or even applicable loading scenario for postbuckling skin-stiffener interfaces.

In general, the FE models gave a good representation to the specimen behaviour, though some adjustment of the model was required in order to suitably represent the clamping condition and rig movement. The numerical models for the antisymmetric and pull tests predicted collapse due to delamination of the stiffener from the skin at the flange edge, which agreed well with the experiments where this was the dominant failure mode. Though some antisymmetric and pull specimens failed in the bend region, this was shown to be likely due to the high sensitivity of the specimens boundary conditions, material strengths and resin overflow at the flange edge.

5. Conclusion

Prediction of failure in postbuckled composite stiffened panels representative of fuselage structures is important for the development of design tools. An experimental and numerical investigation was carried out into the failure mechanisms in the skin-stiffener interface of T-sections. The experimental investigation involved representing local and global postbuckling phenomena through antisymmetric and symmetric deformations.

Despite variation in the experimental results, very good agreement was seen between the experimental and numerical results, in terms of specimen behaviour and failure mode. In the FE models, the failure was predicted to occur at the flange edge, which agreed with the experimental results where this was the dominant failure mode. Some experimental specimens showed failure in the bend, and numerical analysis demonstrated that both resin-rich areas at the flange edge and the high variability in the strength parameters could promote failure in this region. Models with cohesive elements were also analysed, and though the introduction of the softening elements at the skin-stiffener interface modified the specimen behaviour, good agreement with the experimental results was seen and the models were also able to represent damage progression as well as initiation.

Future work on the numerical approach applied will focus on experimental and numerical investigations of specimens with differing stringer geometries and dimensions. The analysis approach for 2D models in this work has also been extended to the analysis of large fuselage-representative structures, which is described in a companion paper [11]. This involves the use of a global-local analysis approach, where deformations from a coarse shell model are used as boundary conditions on local models of the skin-stiffener interface similar to the models in this work.

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References

1. Stevens KA, Ricci R, Davies GAO. Buckling and postbuckling of composite structures. *Composites* 1995;26:189–99.
2. Meeks C, Greenhalgh E, Falzon BG. Stiffener debonding mechanisms in post-buckled CFRP aerospace panels. *Compos Struct* 2005;36:934–46.
3. Orifici AC, Thomson RS, Gunnion AJ, Degenhardt R, Abramovich H, Bayandor, J. Benchmark finite element simulations of postbuckling composite stiffened structures. In: *Proceedings of the 11th Australian International Aerospace Congress*, Melbourne, Australia, 13–17 March 2005.
4. Orifici AC. Degradation models for the collapse analysis of composite aerospace structures. PhD thesis, Royal Melbourne Institute of Technology; 2007.
5. Orifici AC, Herszberg I, Thomson RS, Weller T, Kotler A, Bayandor J. Failure in stringer interfaces in postbuckled composite stiffened panels. In: *Proceedings of the 12th Australian International Aerospace Congress*, Melbourne, Australia, 19–22 March 2007.
6. Degenhardt R, Rolfes R, Zimmermann R, Rohwer K. COCOMAT – improved material exploitation of composite airframe structures by accurate simulation of postbuckling and collapse. *Compos Struct* 2006; 73: 175–8.
7. COCOMAT Home Page, www.cocomat.de; 2007.
8. MSC.Marc User Manuals Version 2005r3, MSC.Software Corporation, Santa Ana, CA, 2006.
9. Feih S, Shercliff HR. The validation of boundary conditions for finite element analyses. In: *NAFEMS Advanced Workbook of Examples and Case Studies (Vol. 2) [Chapter 5]*, NAFEMS, February 2004.
10. Tong L. An assessment of failure criteria to predict the strength of adhesively bonded double lap joints. *J Reinf Plas Comp* 1997;16:698–715.
11. Orifici AC, Herszberg I, Thomson RS. An analysis methodology for failure in postbuckling skin-stiffener interfaces. In: *Proceedings of the 14th International Conference on Composite Structures*, Melbourne, Australia, 14–16 November 2007.