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Failure behaviour of grid-scored foam cored composite sandwich panels for wind turbine blades subjected to realistic multiaxial loading conditions

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Failure behaviour of grid-scored foam cored composite sandwich panels for wind turbine blades subjected to realistic multiaxial loading conditions

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Abstract

The load response and failure behaviour of 'grid-scored' sandwich panels used in wind turbine blades have been investigated. This paper presents the results of a combined experimental and numerical investigation of the load response and failure behaviour of a specific grid-scored foam cored composite sandwich panel configuration subjected to multiaxial quasi-static loading conditions that are representative for realistic loading conditions present in wind turbine blades. From the experimental evidence a criterion based on fracture mechanics has been proposed for predicting the onset of fracture in the resin grid. The criterion can be applied directly in conjunction with finite element modelling based on 3D solid elements where the resin grid in situ the core is fully modelled. However, since most full-scale blade models are based on first-order shear deformation theory where the core properties normally are homogenised a strainbased failure criterion is also proposed. The input for this failure criterion is the allowable resin grid strain, which can be obtained from a simple uniaxial tension test of a grid-scored sandwich beam specimen. The predictions of the criteria have been compared with the experimental observations, and a good correlation has been found.

Keywords

Grid-scored sandwich structures, wind turbine blades, experimental characterisation, multiaxial testing, failure modelling

Introduction

Sandwich structures are extensively used for the aerofoils of modern wind turbine blades. Here, the presence of a stiff resin grid in situ the sandwich commonly occurs as the manufacturing process is based on vacuum infusion, and since the core material is scored (cut) in a grid to fit the curved geometry (see Figure 1).

The sandwich core is, in some cases, made from a compliant and lightweight polymer foam material, and here the presence of a much stiffer resin grid (including interfaces between core and resin) causes the inducement of significant local stress concentrations. These stress concentrations may lead to the inducement of cracks (local failure) in the sandwich assembly, which may cause a premature failure depending on the local loading conditions [1]. Very little research has been published on the subject of grid-scored sandwich structures. The reason for this is most likely that strength problems (e.g. failures) with grid-scored sandwich structures are most often associated with specific applications, which are typically considered as proprietary by the manufacturers. Work by Trofka [2] and, recently, by Fathi et al. [3] show that grid-scoring of polymer foam has a significant effect on the throughthickness shear properties. Generally, the strength and modulus increase while the shear strain to breakage decreases. Additionally, the local effects induced by the grid-scoring may in other cases be too localised to affect the failure response, e.g. in the case of face/core debonds or similar [4,5]. The grid-scored sandwich configuration has been studied in its more generic form which is described by the term ‘core junction’, where the core stiffness and strength properties change discontinuously.

Figure 1. Grid-scored sandwich structures used in the aerofoils of wind turbine blades.

In practice, this type of structural feature is typically associated with a change of core material, for example from a low- to a high-density polymer foam. The generic ‘core junction’ case has been the subject of significant research [6,7]. However, the generic core junction case does not accurately describe the grid-scored core case since the geometric and material description is not comparable. In particular, the overall load/stress redistribution, the locally induced stress concentrations and the characteristic decay lengths of the local effects, induced by the presence of a resin grid in the compliant

foam core, are significantly different than for the generic 'core junction' case. Accordingly, to develop a physically-based understanding of the load response and failure behaviour of the grid-scored sandwich structures, a detailed experimental and modelling study is required.

The aim of the research presented in this paper is to model the load response and failure behaviour of a specific grid-scored composite sandwich structure when subjected to quasi-static multiaxial loading conditions. The reason behind the choice of multiaxial loading conditions is that sandwich laminates/panels in modern wind turbine blades are experiencing such complex loadings during service. This is due to the complex shape of the blade cross section and the interactions caused by the joints between the aerodynamic outer shells and the internal shear web(s) in a wind turbine blade (see Figure 1). Such loading conditions cause failure modes that cannot be realised in uniaxial (coupon like) tests [8]. Thus, a dedicated multiaxial test setup has been developed and commissioned to enable failure investigation under realistic loading conditions [8]. An elaborate testing programme has been conducted using this test setup to investigate the initiation and progressive development of failure related to the resin grid in grid-scored composite sandwich structures. Based on the observations, two criteria for predicting the initiation of the failure have been proposed, and the predictive results have been compared with and confirmed by the tests.

Methodology

To facilitate testing of grid-scored composite sandwich panels subjected to multiaxial loading a substructure/component test rig that requires no dimensional scaling has been used [8], as dimensional scaling has shown to have a significant impact on the failure behaviour of composite materials [9,10]. Previous work by the authors [8] has indicated that the failure behaviour of grid-scored sandwich structures can be outlined by two multiaxial tests; one biaxial tension load case combined with a transverse bending moment, and one biaxial compression load case combined with a transverse bending moment. The compression load case was found to be the most critical since the fracture/failure of the resin grid initiated progressive failure leading to complete loss of the load-carrying capacity. For the tension load case, premature fracture/failure of the resin grid also occurred, but it did not affect the load-carrying capacity significantly. However, the failure criterion which is presented in this paper aims to predict resin (grid) fracture irrespective of the imposed loading conditions. The motivation for this is that the stiffness contribution of the infused resin is normally taken into account in the modelling of the sandwich assembly, and further that fracture of the resin grid causes a significant change (reduction) of the shear stiffness of the sandwich core [2]. This in turn will lead to a redistribution of the local stresses which may lead to material failure. Furthermore, cracks developing in situ the foam core may propagate under fatigue loading conditions causing delamination between the face sheets and the core [11,12].

The central hypothesis of the research presented is that prediction of resin grid failure based on a simple failure criterion/hypothesis can yield a conservative estimate for the

failure load of a grid-scored sandwich structure. For the purposes of the failure analysis, the local strain and stress states internally in the structure will be evaluated by a validated finite element (FE) model of the grid-scored sandwich structure since direct measurement of e.g. the straining of the resin grid in situ the foam core is not feasible.

Material systems

For the composite sandwich configuration investigated the face sheets consisted of stitched non-crimp Triax E-glass fabrics embedded in an epoxy matrix. The mats had a nominal density of 1200g/m^2 and consisted of 567g/m^2 0 E-glass, 301g/m^2 +45 E-glass, 301g/m^2 45 E-glass and 6g/m^2 stitching. The cross-linked PVC foam Divinycell H60 GST from DIAB was used as the baseline core material, while Divinycell H130 GST was used for reinforcement purposes in the multiaxial test specimens. The grid-scored foam configurations were based on 20mm thick sheets, which were cut in 3030mm^2 bricks with a nominal slit width of 1mm and attached to a thin carrier fabric. For the testing, both multi- and uniaxial sandwich specimens were used, as will be detailed later. Both types were manufactured using the VARTM infusion process and subsequently cured in an oven for 960min at 70°C . Two different epoxy resin systems typically used in the wind turbine blade industry were used for the investigation; Resins A and B. Resin A was the baseline resin used in this work, while Resin B with almost identical elastic properties but lower toughness was used for one test configuration.

Test specimens

The development of the ‘high-fidelity’ substructure test rig (from the establishment of specimen loading/boundary conditions to the discussion of key failure modes) is detailed in Laustsen et al. [8], but for completeness both the test specimen design and test setup are briefly summarised here. The ‘high-fidelity’ multiaxial testing rig was custom designed and built to enable replication of realistic service load conditions for composite sandwich laminates in wind turbine aerofoils. This was achieved by enabling the test rig to impose arbitrary parametric variations of three main load components consisting of a blade longitudinal in-plane load P_L , a transverse in-plane load P_T , and a transverse bending moment M , as shown in Figure 2.

Figure 2. Multiaxial loading conditions for the grid-scored sandwich structure.

Figure 3. Layout of multi-axial grid-scored composite sandwich test specimens [8].

The multi-axial test specimen and the test rig are shown in Figures 3 and 4. The test specimen design facilitates that realistic loading conditions can be realised in the gauge zone of the multi-axial test specimen. Thus, the high-fidelity test rig enables the conduction of a detailed characterisation of the load response and failure behaviour of grid-scored sandwich structures on the component/sub-structure scale rather than on the full-scale structural scale. As shown in Figure 3, the test specimen geometry has been idealised as a single-curved sandwich panel. The flat panel idealisation in the longitudinal direction of the blade corresponds well to most blade designs, while the transverse curvature in principle varies according to the location in the blade. For the present investigations, a radius of curvature of 750mm was chosen. Ideally, panels of different curvatures should be subjected

Figure 4. The multi-axial 'high-fidelity' test rig [8].

Figure 5. Layout of uniaxial tensile grid-scored sandwich beam specimens.

to the testing, but to reduce the experimental efforts an intermediate curvature between the upper and lower limits of frequently occurring curvatures was selected.

In addition to the multi-axial sandwich test specimens, a number of flat beam sandwich specimens suitable for in-plane tension tests were also prepared (see Figure 5). In order to facilitate failure to occur in the gauge zone, the specimens were reinforced with thicker face sheets in the load application regions and they were subsequently cut in a dog bone shape. Further, the grid-scored core material was placed such that a 'resin bridge' (i.e. a part of the resin grid) follows the centre line of the specimen (see Figure 5).

Figure 6. Layout of grid-scored test specimens for three-point bending tests.

Flat beam specimens for three-point bending tests were also manufactured with the purpose of investigating core failure due to transverse shear loading. These specimens were prepared as rectangular-shaped coupons as shown in Figure 6.

Experimental characterisation

All the multiaxial tests were monitored with 6mm strain gauges mounted on both sides of the specimens in the centre of the gauge zone, while the tensile specimens were monitored using a clip gauge. Digital image correlation (DIC) measurements [13,14] were performed for one of the multiaxial test specimens on the front side of the specimen to provide full-field strain data for the gauge zone of interest. The DIC test setup, a list of the hardware used, the camera settings, and the details of the image correlation and data processing are shown in Figure 7. The full-field measurements were primarily used to validate the FE model outlined in the following section.

A uniaxial servo-hydraulic 100kN (Schenck-Hydropuls PL 100) testing machine was used for the testing of the grid-scored beam specimens. For the testing of the multiaxial grid-scored specimens, a biaxial servo-hydraulic actuator setup (see Figure 4) of 400kN (longitudinal) and 63kN (transversely) was used (Schenck – Hydropuls PL 400 testing machine combined with a Hydropuls PL 63 actuator). Since specially developed test specimen geometries were used for the uniand multiaxial characterisation, the test procedures did not follow a specific test standard, but in all cases a constant cross-head displacement of 1mm/min was used. Table 1 outlines the grid-scored sandwich specimen configurations investigated and the number of specimens tested. Dependent of the location of the gridscored sandwich structure in the wind turbine blade, either multiaxial tension or compression loading will occur under ‘in service’ conditions. Near the leading edge of the wind turbine blade the sandwich panel even experiences both tension and compression loading throughout the service life of the blade. Thus, the strength was additionally tested under such alternating loading condition for one multiaxial specimen configuration.

Technique used	Stereo Image Correlation (3D with two cameras)	Displacement	
		Spatial resolution	1 mm
Subset size	40 x 40 pixel	Resolution	2 μm
Shift	40 pixel (0% overlap)	Strain	
Camera	8 bit, 2048 x 2048	Smoothing method	Averaging 3x3
	ARAMIS 4M system	Differentiation method	ARAMIS Numerical
Field of view	150 x 150 x 100 mm	Spatial resolution	3 mm
Measurement points	5000	Resolution	30 $\mu\text{m/m}$

Figure 7. DIC setup and specification of the DIC data presented in this work.

Finite element analyses

As explained in the previous section, strain measurements were only conducted on the outer surfaces of the specimens, and hence no direct measures of the critical strains inside the resin grid were obtained. To convert the experimentally measured surface strains into values that can be compared to the interior strains predicted numerically, a detailed FE model of the multiaxial grid-scored test specimen was developed using the general purpose FE code MSC Nastran, version 2011.1 (see Figure 8).

The FE model of the specimen (global model) was combined with a submodel (or local model as indicated by the dashed region) of the gauge zone where the resin grid was modelled in situ the foam core. This sub-structured modelling approach

[illegible]

Figure 8. FE models (global and local) of the multiaxial grid-scored sandwich specimen.

was necessary to reduce the computation time of the global FE model. Thus, the resin grid was not modelled in the global model but instead taken into account by homogenising the properties of the foam core and the resin grid by adopting the simple ‘rule of mixtures’ homogenisation procedure proposed in Thomsen and Larsen [15]. The elastic properties used for the computations are shown in Table 2. As indicated, the FE models were based on the assumption of linear elastic material behaviour, as this is assumed to be sufficient to predict the load response until failure initiation. The GFRP and the foam were modelled as orthotropic materials, whereas the resins (A and B) were assumed to be isotropic. The elastic properties were obtained mainly from testing. However, the Young’s moduli of the foam materials were achieved by linear scaling by their density and the full stiffness characterisation reported in Taher et al. [16].

Since large displacements and rotations generally occur for composite wind turbine blade structures, the global model was based on a geometrically nonlinear formulation. The predicted displacement boundary conditions (dashed lines) were subsequently imposed on the local FE model, which was based on a geometrically linear formulation. The global FE model was discretised by 17,000 eight-node solid elements in MSC Nastran, version 2011.1 where the elements for the face sheet utilised an enhanced assumed strain formulation (solid shell) to stabilise and circumvent locking effects due to high aspect ratios of the elements. The local FE model was discretised using 45,500 eight-node solid elements. For both models

Table 2. Elastic properties of the materials used in the FE model.

Material	Elastic properties
GFRP	$E_{11}^{1/4}$ 28,500 MPa, $E_{22}^{1/4}$ 15,000 MPa, $E_{33}^{1/4}$ 15,000 MPa, $G_{12}^{1/4}$ 7500 MPa, $G_{23}^{1/4}$ 4861 MPa, $G_{31}^{1/4}$ 4861 MPa, $\nu_{12}^{1/4}$ 0.416, $\nu_{23}^{1/4}$ 0.3, $\nu_{31}^{1/4}$ 0.3
Resins A and B	$E^{1/4}$ 3000 MPa, $\nu^{1/4}$ 0.3
Foam (H60)	$E_{11}^{1/4}$ 32 MPa, $E_{22}^{1/4}$ 32 MPa, $E_{33}^{1/4}$ 70 MPa, $G_{12}^{1/4}$ 19 MPa, $G_{23}^{1/4}$ 19 MPa, $G_{31}^{1/4}$ 19 MPa, $\nu_{12}^{1/4}$ 0.3, $\nu_{23}^{1/4}$ 0.3, $\nu_{31}^{1/4}$ 0.3
Foam (H130)	$E_{11}^{1/4}$ 79 MPa, $E_{22}^{1/4}$ 79 MPa, $E_{33}^{1/4}$ 175 MPa, $G_{12}^{1/4}$ 50 MPa, $G_{23}^{1/4}$ 50 MPa, $G_{31}^{1/4}$ 50 MPa, $\nu_{12}^{1/4}$ 0.3, $\nu_{23}^{1/4}$ 0.3, $\nu_{31}^{1/4}$ 0.3
Homogenised grid-scored H60 core	$E_{11}^{1/4}$ 132 MPa, $E_{22}^{1/4}$ 132 MPa, $E_{33}^{1/4}$ 262 MPa, $G_{12}^{1/4}$ 20 MPa, $G_{23}^{1/4}$ 57 MPa, $G_{31}^{1/4}$ 57 MPa, $\nu_{12}^{1/4}$ 0.3, $\nu_{23}^{1/4}$ 0.3, $\nu_{31}^{1/4}$ 0.3
Homogenised grid-scored H130 core	$E_{11}^{1/4}$ 179 MPa, $E_{22}^{1/4}$ 179 MPa, $E_{33}^{1/4}$ 360 MPa, $G_{12}^{1/4}$ 53 MPa, $G_{23}^{1/4}$ 88 MPa, $G_{31}^{1/4}$ 88 MPa, $\nu_{12}^{1/4}$ 0.3, $\nu_{23}^{1/4}$ 0.3, $\nu_{31}^{1/4}$ 0.3

GFRP: glass fibre reinforced polymer.

symmetry conditions as indicated in Figure 8 were imposed to reduce the computation time.

Results

Since the objective of the present work is to develop the experimental data necessary for describing the onset and propagation of failure in the resin grid, results similar to the ones presented in Laustsen et al. [8] have been generated for the purposes of this work with additional attention on the load response and failure behaviour. In all of the multiaxial cases (see Table 1), the load response was monitored by strain gauges in the centre of the specimen on the front (convex) and rear (concave) surfaces. To validate the FE model predictions full-field strain measurements were conducted by DIC for the ‘Multiaxial compression, II’ case. As shown in Figure 9, the model predictions of the front side of the specimen model (both local and global, see Figure 8) were compared to the DIC measurements. Since the FE models were based on the adoption of symmetry conditions, the strain predictions are only shown for a quarter of the gauge zone. Further, to avoid erroneous strain computations across the ply-drop boundaries around the gauge zone (see Figures 3 and 8) of the specimen in the DIC processing, only the surface area inside the gauge zone of the multiaxial test specimen was considered for these results. The predicted and measured strain fields are shown in Figure 9, respectively. First of all it should be noticed how the influence of the resin grid has been captured by the local FE

model, while this is not the case for the global FE model as expected. The difference in the predictions is obviously caused by the homogenisation of the core

Figure 9. Global and local FE model predictions vs. DIC measurements for the ‘Multiaxial compression, II’ case at $P_L/460$ kN.

properties (foam and resin grid) which provided material input data for the global FE model. Thus, the best match of the measured longitudinal and transverse strain components can be observed for the local FE model, and hence the predictions of this model will be used for the further comparisons. From Figure 9 it is seen that the local FE predictions qualitatively fit the experimentally obtained strain map distributions rather well. Discrepancies are however seen on the strain magnitudes due to the unavoidable compromise made between sufficient spatial resolution and acceptable noise for the DIC measurements. Based on this it was concluded that the FE models can be used to provide estimates of the nominal strain and stress distributions in situ the grid-scored composite

sandwich structure. It was further concluded that the imposed assumptions of symmetry provided a satisfactory representation of the realised strain fields. Thus, the local FE model facilitated a closer

Figure 10. Longitudinal normal strain distribution, ϵ_x , predicted by the local FE model for the ‘Multiaxial compression, II’ case at $P_L^{1/4}$ 60 kN.

investigation of the internal strain distribution of the grid-scored sandwich specimen (see Figure 10).

It should be noticed that the resin grid causes local bending of the face sheet due to the mismatch in longitudinal straining of the foam core and the resin grid, resulting in different through-thickness normal straining (z -direction) in the foam and the resin grid. The reason for this is seen on the strain distribution in the transversely oriented (y -direction) resin grid, which only exhibits high strain values very close to the face sheet interface. Further, it is important to notice that the strain distribution in the resin grid parallel with the x -axis is almost uniform and with values similar to the strains in the face sheets, while the resin grid oriented transversely only exhibits half of that strain magnitude.

Failure behaviour observed in multiaxial tensile tests

For the multiaxial tensile test cases the longitudinal load component, P_L , was found to exert the primary influence on failure/fracture of the resin grid. As shown in Table 1, two different multiaxial tension cases were tested. In Figure 11, the load response of the tension test ‘Multiaxial tension/compression’, which subsequently was tested in compression, is shown.

Figure 11. Load response observed for the ‘Multiaxial tension/compression’ load case when loaded in tension.

The measured strains in the x (longitudinal) and y (transverse) directions are compared to the FE model predictions (local model) at $P_L \approx 100\text{kN}$. As shown, the response characteristics are captured reasonably well although discrepancies can be seen. The discrepancies are believed to be caused by small misalignments in the test setup. The misalignment issue was decided to be of minor significance, since the strain predictions in the resin grid did not show significant sensitivity to improvement of this. Although not visible from the load response curves shown in Figure 11, a significant number of cracks occurred in the longitudinal resin bridge at a load level of $P_L \approx 90\text{kN}$, and hence the test was stopped at $P_L \approx 100\text{kN}$. The principal strain predictions in the longitudinal resin bridges were computed to 6360me for $P_L \approx 90\text{kN}$. The failure behaviour observed was exactly the same for the tension case ‘Multiaxial tension’ (Table 1). Post mortem investigations of the fractured longitudinal bridges were performed by microscopy (see Figure 12). A section of the specimen was cut out where white spots appeared on the front side indicating fracture and debonding between the face sheet and the core/resin grid. The damaged longitudinal (x-direction) bridge was then subsequently inspected in the through-thickness plane of the sandwich (z-direction). As shown in Figure 12, through-thickness resin cracks were discovered and observed to correspond with the white spots that were visible through the face sheets.

The location of the induced cracks did not appear to be influenced by the macroscopic geometrical stress raisers internally in the sandwich structure, like e.g. the corners in the resin grid. To elucidate this failure behaviour a computed tomography (CT) scan of a resin bridge (constituting a part of the resin grid) and the adjacent core interface was made, as shown in Figure 13. As shown, the nominal slit width for the resin bridge does not solely define the amount of resin in the cross section. Resin-rich domains exist on both sides of the resin bridge where resin

Figure 12. Post mortem inspection cracks in the longitudinal resin bridge.

has penetrated into the foam cells, which have been opened in the grid-scoring (machining) process. Consequently, a very rough and notched surface of the resin bridge has been formed, and it is observed that small edge cracks have been initiated from the ‘open’ foam cells.

Since the objective of the present work is to relate fracture onset (crack initiation) to the nominal defined resin grid the detailed topography of the core/resin grid interface shown in Figure 13, which displays a stochastic nature, has not been taken into account in the numerical modelling. The modelling of the nominal resin grid alone requires a substantial amount of degrees of freedom for the local model, and hence detailed modelling of the interface between the foam and the resin grid would increase the computation time to an unacceptable level. Thus, the stochastic distribution of the locations where cracks may occur cannot be directly taken into account.

An important observation from the tensile tests was that the transverse load component, P_T , did not seem to have a significant influence on the fracture onset in the longitudinal resin grid. Thus, it is reasonable to assume that the strain component

perpendicular to the resin grid does not influence the crack initiation and fracture behaviour significantly. This conclusion is supported by the fact that the resin bridge is very thin and supported by the compliant core material. Thus, it appears reasonable to assume a state of plane stress through the width of the resin bridges. In the tensile tests, the crack initiation did not interact with any other possible failure modes of the sandwich assembly. This agrees well with the fact that the load capacity is defined primarily by the face sheets for such load case, which means that fracture in situ the core will not influence the ultimate tensile strength of

Figure 13. CT scan of the resin bridge in grid-scored sandwich structure.

the sandwich assembly. In order to investigate the sensitivity of the structure to subsequent compression loading the tension test shown in Figure 11 was stopped at $P_L \frac{1}{4} 100 \text{ kN}$. Hereafter the specimen, which now contained a significant number of resin cracks, was subjected to compression loading as will be discussed in the following section.

Failure behaviour observed in multiaxial compression tests

For the compression load cases the failure behaviour was found to be dependent mainly on the imposed transverse bending moment, M . Due to the fact that the transverse moment acted in combination with a biaxial in-plane compression load a dramatic failure event was observed, which involved a catastrophic collapse immediately after the occurrence of the first failure initiation event. Two different failure initiation phenomena were observed in the multiaxial compression tests. For the cases where the transverse

bending moment, M , was $M/40.004P_T$ (referred to as ‘Multiaxial compression, I’ in Table 1), the grid-scored specimen failed due to parasitic effects in the test specimen. In Figure 14, the load response obtained from the ‘Multiaxial tension/compression’ test is shown, whereas the load response for ‘Multiaxial compression, I’ has been omitted. The failure behaviour and load responses of the two compression tests were very similar although the ‘Multiaxial tension/compression’ specimen had developed cracks in the longitudinal resin bridges due to the fact that it had been subjected to prior tensile loading, whereas the ‘Multiaxial compression, I’ case was conducted using a virgin

Figure 14. Load response obtained for the ‘Multiaxial tension/compression’ case when loaded in compression.

specimen. The small offset at $P_L \approx 0$ kN occurred due to a small preload was initially applied.

As shown in Figure 15, failure occurred due to localised bending of the face sheets in the vicinity of the ply-drop adjacent to the gauge zone. Although the failure event was a result of parasitic effects in the sandwich specimen, additional efforts were not made to improve the specimen design to avoid this. Since the occurrence of ply-drops is very common in wind turbine blades and other composite sandwich structures the recorded failure behaviour is instead assumed to indicate a loading configuration where failure initiation triggered by fracture of the resin grid will not occur.

For the compression ‘Multiaxial compression, II’ case (see Table 1) the transverse bending moment, M , reached a sufficiently high magnitude so that failure initiation related to fracture in the transverse resin grid occurred.

The fracture event caused failure of the front face sheet leading to a complete failure of the sandwich structure. In Figure 16, the load response of the compression test is shown and compared to the FE model predictions (for $P_L \approx 100$ kN). A close correspondence between the predicted and measured strain responses is observed. To further support the experimental investigations, DIC measurements were conducted (see

Figure 17), on the front side of the specimen in a similar load configuration as shown in Figure 16.

From Figure 17, it is observed that resin failure in the transverse grid (observed from the rear side video recordings) caused face sheet wrinkling due to a redistribution of the in-plane compression loads from the rear side of the sandwich panel to the front side. The principal strain in the transverse grid was predicted to approximately $6430\mu\epsilon$ by the FE analysis. It should be further noted that the corresponding compressive strain in the longitudinal resin bridges was $9800\mu\epsilon$, which provides an indication of the resistance of the resin grid to compression loading. Thus, the most likely failure sequence for the ‘Multiaxial compression,

served for the 'Multiaxial compression, A' case where failure in the 'resin bridges' did not occur.

Figure15.

Figure 16. Load response observed for one of the three 'Multiaxial compression, B' cases.

Figure 17. Failure event recorded by DIC on the front side of the specimen and video recording from the rear side at $P_L/4110$ kN. The subsequent out-of-plane face sheet displacement fields within the circular area are shown at three different stages.

II' case was that resin grid fracture caused local load redistributions, which again led to increased compressive loading of the face sheets in the transverse (or y-) direction, which then caused a nonlinear load-strain response for the rear face sheet. This resulted in a redistribution of the compressive loading from the rear to the front face sheet, which

subsequently buckled and collapsed in a wrinkling instability mode. The outlined sequence is supported by Figure 16, where significant straining of the rear face sheet can be observed after the resin grid fracture event. The wavy buckling pattern of the front face sheet is clearly visible from the DIC out-of-plane displacement plot in Figure 17.

It is important to notice that the failure event occurred very close to the ply-drop boundary, and it is possible that interactions between the local stress concentrations induced in the vicinity of the ply-drop, and the fracture of the resin grid may have contributed to the failure of the sandwich specimen.

Uniaxial tensile test results

As shown in Table 1, three tensile test configurations were investigated. The main motivation for conducting the tensile tests was that the strain magnitude in the resin grid could directly be obtained by a simple strain measurement of the face sheet, and hence not requiring any FE modelling efforts. However, the disadvantage was that the fracture/crack initiation had to be detected visually and audibly since it did not influence the load response curve.

As reported in Laustsen et al. [17], efforts were made by the authors to facilitate systematic recording of the fracture initiation in the resin grid by acoustic emission (AE) [18]. From Laustsen et al. [17] it was found that events of high energy could be measured and related to the onset of fracture. This is also consistent with the fact that the resin grid fracture events investigated in this work were audible to the human ear. Thus, an AE system with an appropriate number of sensors could offer an objective recording of the strain to failure. However, since such a system was not available, a video recording was used in this work to systematically detect cracks. In Table 3, the failure strain values obtained from the tensile tests are shown. From this it is seen that the different core materials affected the failure strain significantly. For the tensile test conducted on test specimens with PVC H60 core and Resin A the failure strain was found to be slightly higher than observed for the multiaxial loading cases. Significant higher failure strain values were observed for the H130 foam core specimen configurations. A possible explanation for this is the slightly smoother and thinner core/resin interface that occurs due to the smaller average cell size of the higher density foam. As shown in Figure 18, this influenced the size of the edge cracks in the resin bridges and the width of the core/resin domain.

Table 3. Failure strains obtained from tension tests.

Test configuration	Failure strain (mm/m)		Failure mode
	Average	Standard deviation	
Uniaxial tension, H60	8443	1948	Longitudinal resin bridge fracture
Uniaxial tension, H130	13,120	492	Longitudinal resin bridge fracture
Uniaxial tension, Resin B	5194	973	Longitudinal resin bridge fracture

For the tension test with the less tough Resin B epoxy system a significantly lower failure strain was observed compared to the baseline Resin A system (see Table 3). The different fracture behaviour will be addressed later, but it is important to notice that the failure strains observed for both resin systems in situ the gridscored sandwich specimens were much lower than the failure strains in the range of 6–8% that are typically claimed by the resin manufacturers. Thus, both epoxy systems exhibited a distinctly brittle behaviour (rather than ductile) when tested in-situ the grid-scored sandwich configuration. In addition to the tensile tests ('Uniaxial tension, H60' – see Table 1), a test to investigate the load response and failure behaviour for transverse shear loading was conducted. This was achieved by conducting a three-point bending test on a grid-scored sandwich beam with H60 PVC foam core ('Transverse shear' – Table 1). To convert the failure load into a principal (tensile) failure strain a nonlinear FE modelling was conducted. As before, the FE analysis was conducted using the commercial FE code MSC Nastran, version 2011.1. A geometrically nonlinear FE model using 5300 eight-node solid elements was used to provide a converged solution. Similar to the multiaxial investigations the constituent materials were assumed linear elastic, and hence the material properties listed in Table 2 were used. A characteristic feature of the transverse shear test was that the fracture of the resin grid occurred very close to the ultimate failure load of the specimen. Thus, although not directly traceable from the load response, a more straightforward observation/ identification of the fracture event could be achieved. A representative load response obtained for a transverse shear test is shown in Figure 19. The fracture event could be identified by an audible cracking sound for the shear tests. Furthermore, the load–displacement curves revealed a transition from a linear response to a nonlinear response part, and this transition was similar to the tension tests accompanied by a cracking sound. Subsequent investigation of the induced cracks in the resin grid showed that all the cracks were oriented in 45 direction indicating through-thickness shear stresses of significant magnitude. The average failure load (for resin grid fracture) obtained from the shear test is shown in Table 4 along with the corresponding principal (tensile) failure strain of the core/resin. From the FE analysis of the transverse shear test, the principal (tensile) failure strain in the resin grid was estimated. The estimate of the 'principal' (tensile) strain to failure deduced from the transverse shear tests corresponds well with the tensile failure strain experimentally obtained from the tensile tests (see Table 3).

Figure 18. CT scans of the resin grid infused with PVC H60 and PVC H130 on each side, respectively.

Failure modelling and discussion

Based on the experimental results, criteria for fracture of the resin grid can be proposed. The proposed failure criteria are based on the following considerations:

1. An apparent ductile to brittle transition occurs for both Resins A and B when infused into the foam core, albeit Resin A was tougher than Resin B in bulk form. The failure strain for both resins are in the range of 6–8%, while fracture occurred around 0.5–0.8% (local resin grid) strain levels for all tested gridscored sandwich specimens. This suggests that a failure criterion based on fracture mechanics and not material yielding would be suitable for the purpose.
2. Christensen [19] suggests that the principal stress component is compared with the tensile strength of brittle materials. However, this implies that a uniaxial tension tests must be conducted in order to obtain the apparent bulk strength of the material. Strain is preferred to stress if a point stress/strain failure criterion is selected. First of all this is directly comparable with the ultimate strain value obtained from the uniaxial tensile tests. Secondly, a strain based criterion can be directly used in models where homogenised elastic properties of the core/resin grid are being used as input for the modelling.

Figure 19. Representative load response of the three point bending test.

3. Plane stress is a reasonable assumption for the resin grid (as argued previously), i.e. the transverse straining (in 3-direction, see Figure 20) of the resin grid is only caused by the Poisson's ratio effect. Thus, the transverse stresses are assumed to be negligible when suggesting a model for the resin grid failure behaviour. From this it follows that the maximum positive (tensile) principal strain component should be computed in the 1–2 plane of the resin grid (Figure 20) and subsequently used for the evaluation of resin grid failure.
4. The fracture onset (crack initiation) of the epoxy resin is dependent on the topography of the interfaces between the resin grid and the core material.
5. Assuming that pre-existing cracks/defects in the resin grid cause crack propagation leading to a complete fracture of the resin implies that an energy release approach (fracture mechanics) should be taken. The observed fracture event in situ the grid-score of a sandwich structure can be compared to tunnelling cracks in constrained layers [20], i.e. crack growth in a brittle layer between two tough substrates (see Figure 21).

Table 4. Failure load obtained from transverse shear test and the corresponding principal strain of the resin grid (termed 'principal failure strain' obtained from the geometrically nonlinear FE analysis).

Test configuration	Failure load (N)		Principal failure strain (mm/m)
	Average load	Standard deviation	
Transverse shear	2291	0.353	8610

Figure 20. Coordinate system for the resin grid and transformation to principal strains.

Although it can be argued that the tunnelling crack model appears to be very idealised in the context of the resin grid fracture phenomenon, which is the object of this investigation, it appears as an attractive approach. Thus, for cases where stresses and strains induced in the resin grid can be predicted, the tunnelling crack model can be adopted since the failure prediction can be performed based on the critical energy release rate for the resin material. However, it is questionable if the assumption of ‘tough adjacent substrates’ is actually valid for the grid-scored composite sandwich structures investigated in this work, as the fracture toughness of PVC foam [21] is comparable to the fracture toughness of epoxy resin. However, since the crack propagation observed in the test is similar to a tunnelling crack, and for the purposes of proposing a simple model, this criterion has been investigated. The energy release rate for tunnel crack nucleation is given by [22]

$$G \approx \frac{1}{4} \frac{\sigma^2}{E} \quad (1)$$

where G is the energy release rate, σ is the applied (far field) stress, a is the crack length and E is the modulus of elasticity. As explained in Ho and Suo [20] and shown in Figure 21, the energy release rate for the crack growth reaches a steady state value, G_{ss} , given by

$$G_{ss} \approx \frac{4h}{E} \quad (2)$$

where h is the width of the resin bridge.

This steady-state phenomenon occurs after the initial crack of size a has propagated to the two adjacent substrates and reached a certain length. Thus, the energy release rate becomes independent of the initial crack and can serve as a conservative prediction of crack propagation. The steady-state energy release rate has to be

Figure 21. Tunnelling crack in constrained layer [20].

compared with the critical energy release rate, r_c , for the resin material, and hence the criterion for resin fracture in the grid-scored sandwich structure becomes

$$\frac{p_1 h}{4r_c E} = 1 \quad (3)$$

Here, the critical energy release rate is obtained from a fracture toughness test, while p_1 is the largest positive principal stress component computed by the FE (local) model (see Figure 8). The parameter, h , should be chosen as the nominal resin grid width but as shown in Figure 13, the actual thickness of the resin bridge was in some cases measured to be three times as high due to the highly variable foam/resin interface topography. Thus, the fracture criterion proposed by equation (3) is very sensitive to the choice/selection of a correct resin grid width. For epoxy resins of the type used in this study, the critical energy release rate typically display values in the range 0.1–0.3N/mm [23]. Hence, it is equally important to use the correct value of the fracture toughness in equation (3). Since the present study intends to provide a conservative and easy to use criterion for resin grid failure, no elaborate efforts have been made to accurately determine the critical resin bridge width, h , or the critical energy release rate, r_c . Based on this the failure indices $\delta_{\text{failure index}} = \frac{1}{4} \frac{p_1^2 h}{r_c E}$ of the different test cases shown in Table 5 have been computed based on the trends of the parameters that govern the failure results. Thus, all failure indices should ideally equate to 1 if the failure criterion was capable of predicting failure correctly.

As shown in Table 5, a reasonable correlation is observed between the failure loads and the proposed fracture criterion (as expressed by the failure index values) for the multiaxial tension and compression cases. Thus, a failure index of 1.1 was predicted for both these cases at the experimentally observed failure loads, which means that the failure predictions are slightly conservative. For the uniaxial tension load cases the correlation is found less good. High failure indices of the uniaxial tension tests of H60

(failure index 1.8) and H130 (failure index 1.6) and of the transverse shear test (failure index 1.9) were obtained as seen in Table 5. This means that the failure criterion predicts that failure should occur at much lower loads than the failure loads observed experimentally, thus providing overly conservative predictions with the assumed input parameters for the criterion. Considering equation (3), it is seen that the failure criterion is very sensitive to the estimated tensile principle stress p as it appears in the nominator raised to the power of 2. The prediction of p is significantly influenced by both the local geometry and the input material parameters in the vicinity of the area where the largest stresses are predicted. Thus, the predicted peak value of p is associated with some uncertainty. In addition, the critical energy release rate, r , which appears in the denominator of equation (3) is also a parameter which is associated with significant uncertainty. For the ‘Uniaxial tension, Resin B’ (less tough than Resin A) case a lower critical energy release rate (0.1N/mm rather than 0.3N/mm) was used to fit the results. As shown, a failure index of 0.8 was obtained at the experimentally

Table 5. Failure indices computed by the fracture mechanics fracture criterion together with the (local) FE model.

Test configuration	Failure load	Principal stress	Material and geometrical parameters	Failure index
Multiaxial tension	P_L 90 kN	21.0 MPa	r ¼ 0.3 N/mm, h ¼ 3 mm, E ¼ 3.0 GPa	1.1
Multiaxial compression, II	P_L 110 kN	21.2 MPa	r ¼ 0.3 N/mm, h ¼ 3 mm, E ¼ 3.0 GPa	1.1
Uniaxial tension, H60	10,777 N	27.8 MPa	r ¼ 0.3 N/mm, h ¼ 3 mm, E ¼ 3.0 GPa	1.8
Uniaxial tension, H130	17,975 N	44.6 MPa	r ¼ 0.3 N/mm, h ¼ 1 mm, E ¼ 3.0 GPa	1.6
Uniaxial tension, Resin B	6276 N	10.3 MPa	r ¼ 0.1 N/mm, h ¼ 3 mm, E ¼ 3.0 GPa	0.8
Transverse shear	2291 N	28.4 MPa	r ¼ 0.3 N/mm, h ¼ 3 mm, E ¼ 3.0 GPa	1.9

observed failure load. This is rather close to 1, albeit nonconservative. Based on the above observations, it is reasonable to suggest that proper characterisation of the critical energy release rate of the two adopted resin systems is very important to obtain accurate predictions of the failure loads. Considering the apparent consistent under-prediction of the failure load observed for the uniaxial tests (failure indices 1.8, 1.6 and 1.9), a possible explanation could be that the critical peak stresses only appear in a very limited volume of the sandwich structure compared to the multiaxial tests where the critical peak stresses occurs over a much larger volume of material, and where a much better correlation between observed and predicted failure loads was observed (failure index 1.1). Overall,

the results displayed in Table 5 suggest that the tunnelling crack criterion defined by equation (3) can be useful to provide an estimate of the sensitivity to resin fracture of grid-scored foam cored sandwich structures subjected to realistic multiaxial loading conditions. It is important to mention the uncertainties of adopting such a simplified fracture model. Besides the fact that proper assessment of the parameters τ and h is required, interaction effects between pre-existing edge cracks in the core/ resin grid assembly are likely to influence the fracture behaviour. Also considerations related to the time dependency of the material properties, i.e. viscoelastic effects of the polymeric foam and resins, have not been included, but these are deemed to be of lesser importance for the problem considered.

As explained previously, most full-scale composite blade FE models (or FE models for other large scale composite structures) are based on the use of equivalent single layered FE elements which are usually limited to include so-called first-order shear deformation assumptions. For such FE models the detailed representation of the resin grid becomes too computational exhaustive, and homogenisation of the core/resin grid properties is adopted instead as described in section 'Finite element analyses'. Thus, prediction of failure by equation (3) is not feasible since stresses and strains must be evaluated in the resin bridges. Instead the following (point) maximum strain failure criterion is proposed

$$\frac{\epsilon_p}{\epsilon_{ult,t}} \leq 1 \quad (4)$$

where ϵ_p is the largest positive principal strain component in the 1–2 plane (see Figure 20), and $\epsilon_{ult,t}$ is the allowable elongation before break of the used epoxy resin.

The tensile strain to failure, $\epsilon_{ult,t}$, must be obtained by either a uniaxial tension or 3-point bending test of a grid-scored sandwich specimen as discussed previously. As shown from the results presented in section 'Uniaxial tensile test results', a good correlation was found between the observed failure strains for the different uniaxial test configurations. Thus, only one of the tests are required for deriving $\epsilon_{ult,t}$. However, as shown by the results presented in this paper it is very important that the grid-scored sandwich beam specimens used (being either for tensile or transverse shear testing) are made from the same material combination and geometrical layout as the grid-scored sandwich substructure that is being investigated. Thus, the 'Uniaxial tension, H60' failure strain ($\epsilon_{ult,t} = 1/8443 \text{ mm/m}$) has been used.

It should be emphasized here that FE models that are based on first-order shear deformation theory neglect the existence of through-thickness strains, which may contribute to the principal failure strain. However, as shown in Table 6, the model predictions of the FE shell models (global models) correspond well to the predictions of the 3D solid element FE models (local models). From this it is concluded that failure predictions based on a FE shell formulation (in this case discretised by four-node shell

elements using MSC Nastran, version 2011.1) will provide reasonably accurate results for flat or moderately curved grid-scored sandwich structures. Thus, the failure indices have only been computed from the FE shell model results (failure index $\frac{1}{4} \frac{p}{p_{ult,t}}$).

It should be noticed that failure was actually not predicted to occur for the multiaxial grid-scored sandwich cases (failure indices are 0.8 and 0.9, respectively – Table 6). The explanation for this is that an average failure strain was used in the calculations leading to the results shown in Table 6. If the maximum principle strain criterion given by equation (4) were to be used in a design context a design limit for the allowable strain should therefore be established from statistical considerations to improve the prediction accuracy (as discussed previously).

From the above discussions it appears reasonable that the choice of either criterion (3) or (4) for the prediction of resin grid-score failure in foam cored sandwich structures would have to be based on an assessment of the available resources. If large-scale composite sandwich structures are to be assessed with respect to resin grid failure the maximum principal strain criterion given by equation (4) would be favourable, as this would be associated with significantly less computational

Table 6. Predictions of resin-grid principal strains at failure based on from a solid and shell FE models of the multiaxial grid-scored sandwich specimen.

Test configuration	Failure load	Failure strain (mm/m)		Failure index
		FE solid model, " $p_{,1}$ "	FE shell model, " $p_{,1}$ "	
Multiaxial tension	$P_{L\frac{1}{4}}$ 90 kN	6360	6660	0.8
Multiaxial compression, II	$P_{L\frac{1}{4}}$ 110 kN	6430	7710	0.9
Transverse shear	2291 N	8610	9890	1.2

modelling efforts. However, if needed, failure assessment of the detailed grid-scored design can be obtained by using the tunnelling crack criterion given by equation (3), the prerequisite for this being that local 3D FE solid models will be developed. Further, detailed experimental characterisation of the effective resin width, h , and the critical energy release rate, r_c , would be needed, as both parameters influence the failure predictions significantly. A unique determination of especially h will be difficult and requires substantial characterisation efforts, and therefore the tunnelling crack criterion is more useful in identifying the parameters governing the resin grid failure phenomenon rather than as a practical tool for failure prediction. Irrespectively, the two different failure criteria defined by equations (3) or (4) must in any case be used in combination with an appropriate failure criterion for the polymeric foam core material (see e.g. [24,25]), such that foam core failure can be predicted for material combinations where resin bridge fracture will not occur.

Conclusions

The load response and failure behaviour of grid-scored foam cored sandwich structures under realistic multiaxial loading conditions have been investigated. Highfidelity tests on substructural elements representative for composite wind turbine aerofoils have been conducted and supported by uniaxial sandwich beam specimen tests. The experimental evidence together with FE model predictions have been used to propose two simple failure criteria for the onset of fracture in the resin grid in the foam cored sandwich structure. The first criterion relies on a fracture mechanics approach, where the resin bridge is considered as a brittle layer constrained between two tough substrates. The crack initiation and propagation problem therefore resembles the so-called ‘tunnelling crack’ problem, and a classic solution for this has been adopted for the purposes of the present investigation. The tunnelling crack criterion was implemented in a 3D solid FE model of the grid-scored foam cored sandwich structure considered. The tunnelling crack criterion mainly serves to outline the governing parameters for the resin grid failure phenomenon since thorough experimental characterisation of the effective resin grid width and the critical energy release rate of the epoxy resin would be needed as input. Especially, the effective resin grid width is difficult to characterise and can vary with a factor of three. In addition to the tunnelling crack criterion, a much simpler maximum principal strain (point wise) criterion has been proposed. This criterion can be implemented in the context of first-order shear deformation theory based FE shell models, where homogenised properties also are input for the polymer foam/ resin grid assembly. The maximum principal strain criterion requires input that can be obtained from a simple uniaxial tension test conducted on a grid-scored sandwich beam/panel element, where the influence on the fracture strength of the resin– core interface and the chosen resin system are implicitly taken into account. The advantage of the proposed maximum principal strain criterion is that it is significantly less computationally expensive to use than the tunnelling crack criterion, and that it requires much simpler and easy to obtain experimental material data input. With this in mind, and acknowledging that there appear to be no marked difference between the accuracy of the strength estimates provided by the two criteria, the general conclusion is that the maximum principal strain criterion would be more useful for engineering design purposes.

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