Finite element based damage assessment of composite tidal turbine blades

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Abstract. With significant interest growing in the ocean renewables sector, horizontal axis tidal current turbines are in a position to dominate the marketplace. The test devices that have been placed in operation so far have suffered from premature failures, caused by difficulties with structural strength prediction. The goal of this work is to develop methods of predicting the damage level in tidal turbines under their maximum operating tidal velocity. The analysis was conducted using the finite element software package Abaqus; shell models of three representative tidal turbine blades are produced. Different construction methods will affect the damage level in the blade and for this study models were developed with varying hydrofoil profiles. In order to determine the risk of failure, a user material subroutine (UMAT) was created. The UMAT uses the failure criteria designed by Alfred Puck to calculate the risk of fibre and inter-fibre failure in the blades. The results show that degradation of the stiffness is predicted for the operating conditions, having an effect on the overall tip deflection. The failure criteria applied via the UMAT form a useful tool for analysis of high risk regions within the blade designs investigated.

1. Introduction

One of the leading devices in the emerging ocean energy space is the horizontal axis tidal turbine. Its construction and development stem from decades of experience in the wind turbine industry. The world’s oceans contain a vast resource of predictable and consistent renewable energy. An Irish report on tidal energy has estimated a resource of 915 GWh/yr [1] for the 10-40 m water depth range around the coast of Ireland, with practical, economic, and physical limitations accounted for. Marine current turbines operate in much the same way as their wind-based counterparts. The turbines use the pressure difference caused by a blade profile generating lift and drag. The kinetic energy of the tidal flow is converted to mechanical energy by the rotating blades and to electrical energy by the generator. The critical components in the device are the turbine blades, as the design of these face unique challenges. The industry standard for the construction of tidal turbines has not yet been identified; however, fibre-reinforced composite materials are currently the favoured design choice for the blades [2]. There are a
number of advantages for their use, including corrosion resistance in a harsh environment, good specific strength and specific stiffness characteristics. In addition, construction and testing facilities can be modified from existing wind turbine operations. An analysis of possible configurations for structural designs of tidal turbine blades [3] has led to an investigation of a typical construction for a turbine blade, with spar caps and webs providing flexural stiffness and shear strength, respectively.

One of the major challenges faced by tidal turbine blade designers is due to the density of water. Comparing similarly rated wind and tidal turbines, the issue becomes apparent: a 1 MW wind turbine would require a diameter of approximately 55 m, while a similarly rated tidal turbine’s diameter would be on the order of 22 m [2]. This results in considerably higher loading on a much smaller structure, and means the tidal turbine blade will require increased stiffness and strength [4]. Talreja [5] has explained how an extensive distribution of microcracks in a composite become responsible for a deterioration in stiffness. This degradation has also been attributed to composites under complex and cyclic loading, such as the type experienced by tidal turbine blades [6]. The failure theory of Puck and Schurmann [7] was chosen for assessing the damage level of the composite laminates. This theory has a proven record for providing a phenomenological basis for the analysis of fibre reinforced composites, scoring highly in the world wide failure exercise (WWFE) of leading failure criteria [8]. The method determines failure envelopes for the initiation of fibre failure and inter-fibre failure, i.e. matrix cracking. The criteria consist of a number of stress interaction equations and uses material strengths to determine the onset of failure. Its use in finite element methods is also proven [9], and it can readily be applied to the plane stress shell element case.

In this paper, the initial design process is based on a hydrodynamic model for the preliminary design of composite tidal turbine blades [10, 11]. The study presented here uses the Puck-based failure criteria in a finite element context to evaluate the potential damage due to inter-fibre failure and fibre failure in composite tidal turbine blades. Three models were constructed from a number of airfoil profiles [12-14] with a focus on thick blade sections for the inner region of the blades, in order to maximise the blades’ second moment of area at the location of the highest bending moment. An overview of the hydrodynamic model used for the initial generation of the chord and twist distribution is provided, together with details of the blade models’ construction and the finite element procedure employed. The results were analysed with consideration of experimental work on structural testing of full scale wind turbine blades [15-18]. The intention is to use knowledge of the failure mode in actual blades under a similar (flapwise) loading situation to inform the analysis and help identify possible high-risk regions on the blades.

2. Methods

2.1. Hydrodynamic model
The hydrodynamic model used as the preliminary design tool in this work was developed by Kennedy et al [10]. The model uses a streamtube approach and blade element momentum theory (BEMT) to balance the loads acting on a number of annular streamtube sections of the blades. The balancing procedure determines the optimum chord length for each radial blade section. A twist angle is also required, since the relative velocity of the impinging water increases along the length of the blade with increasing angular momentum. Figure 1 demonstrates the distribution of twist angle and chord length on one of the completed blades. The flap- and edgewise forces at one of the sections are also shown. These are output for each section and are applied in the finite element model for loading the blade at the maximum rated operating speed of 2.5 m/s tidal velocity.

2.2. Blade design
The three blade configurations under investigation were created using a combination of four airfoil profiles. The first 2 m of each of the three blades used one from the NACA 63-421, FFA-W3-241, and FB3500-0875 profiles (referred to as blades 1, 2, and 3 respectively hereafter). These three profiles were chosen due to their high thickness percentage (21%, 23%, and 35%, respectively), thereby
maximising the second moment of area of the blades and their ability to resist the bending moment towards the root of the blade. The outboard sections of the blade use the thinner NACA 2415 profile for better hydrodynamic efficiency and power generation. The thicker sections have drawbacks with regard to increased drag and roughness sensitivity, and they can suffer from flow separation [19]. For example, studies [19-20] have demonstrated an increased lift coefficient with the flat-back profiles; however, they also show increased base drag. Methods of offsetting this with trailing edge splitter plates have proven effective as a workaround for this issue. Since approximately 80% of the power is produced by the profiles in the outer four metres of the blade [21], the reduced lift-drag ratio has minimal effect on power production while potentially improving structural performance.

The typical construction of a wind turbine blade is used as the basis in developing the blade under consideration in this paper. The blade consists of a box spar running through its core, which acts as the main structural component of the blade. The box spar has horizontal spar caps, with a layup of $[0^\circ/\pm 45^\circ/(0^\circ)]_S$, where the outer three plies form a protective layer of GFRP over the inner unidirectional CFRP laminate. The spar caps extend from 15% of the chord length to 50%. Vertical shear webs support the spar caps and are constructed of double biased (DB) $[\pm 45^\circ]$ GFRP laminates. The root section requires additional reinforcement and a quasi-isotropic (QI) layup of $[0^\circ/0^\circ/\pm 45^\circ]_S$ CFRP is applied there. The remainder of the blade, including the leading and trailing edge sections, also consists of a quasi-isotropic layup, as the primary loading direction over the curved surfaces is difficult to predict as the blade performs. The QI layup is formed of $[0^\circ/\pm 45^\circ]_S$ GFRP. Figure 2 and Table 1 outline the layups in the various regions, their total respective thicknesses, and the details of the materials used in the finite element models. In order to compare the three blades the same laminate thicknesses and layups were used in each of the blades.

![Figure 1. Wireframe of blade showing the chord and twist distribution obtained from the hydrodynamic model.](image)

![Figure 2. (a) Plot of the thickness of the different laminates in the blade, and the distribution of maximum thickness along the blade’s span, $r$. The same thicknesses were applied to all three blades (b) diagram of the sections composing the blade, and the thickness referred to in (a).](image)
Table 1. Material properties for Silenka E-Glass MY750 epoxy (GFRP) and AS4 Carbon-Fibre 3501-6 epoxy (CFRP) laminates [22].

| Material Characteristics for a Unidirectional Layer | GFRP | CFRP |
|------------------------------------------------------|------|------|
| Longitudinal Modulus, $E_l$ (GPa)                    | 45.6 | 138  |
| Transverse Modulus, $E_t$ (GPa)                      | 16.2 | 11   |
| In-plane Shear Modulus, $G_{12}$ (GPa)               | 5.83 | 5.5  |
| Major Poisson’s Ratio, $\nu_{12}$                    | 0.278| 0.28 |
| Longitudinal Tensile Strength, $X_T$ (MPa)           | 1280 | 1500 |
| Longitudinal Compressive Strength, $X_C$ (MPa)       | 800  | 900  |
| In-plane Shear Strength, $S_{12}$ (MPa)              | 73   | 80   |
| Transverse Tensile Strength, $Y_T$ (MPa)             | 40   | 27   |
| Transverse Compressive Strength, $Y_C$ (MPa)         | 145  | 200  |
| Longitudinal Tensile Failure Strain, $\varepsilon_{1T}$ (%) | 2.807| 1.087|
| Longitudinal Compressive Failure Strain, $\varepsilon_{1C}$ (%) | 1.754| 0.652|

2.3. FE modelling

The loads on the blade were applied to the spar caps using the polynomial expression in Equation (1) for a distributed pressure load, $P$, varying along the blade span, $r$. The pressure load was defined using the thrust force distribution output from the streamtube model.

$$ P(\text{MPa}) = 4 \times 10^{-9} r^2 + 4 \times 10^{-6} r + 0.0106 \quad (1) $$

The blades were modelled from the rotor of a three-bladed tidal turbine with a theoretical output of 800 kW, a 16 m rotor diameter, and a blade length of 7.5 m. The calculated root bending moment is approximately 850 kNm, and the total thrust per blade is approximately 190 kN. The root section of an actual tidal turbine blade could be secured by a number of methods, such as a linear or circular T-bolt connection [5]. This complexity of modelling is outside the scope of this study, due to the lack of through-thickness stress components when using the plane stress shell elements. A zero-deflection boundary condition was used instead. While this is likely to be overly stiff (the pitch bearing in an actual blade for example has its own stiffness level and some small amount of deflection would be expected), it is the best method available without adding unnecessary complexity to the model. The models comprise S4R 4-noded, doubly curved, reduced integration shell elements.

Once the blade design was finalised the analysis was run using a user material subroutine (UMAT) in Abaqus. The UMAT determines the risk of failure for each element. The risk is determined by using the Puck stress interaction equations, which define a failure envelope for matrix cracking. The criteria include two equations for fibre failure (2-3) and three for inter-fibre failure (4-6), depending on which mode of IFF occurs. That is, Mode A is a tensile-shearing failure, Mode B is a compression-shearing failure, and Mode C is an extreme case of compression-shearing which results in catastrophic laminate failure. The risk of failure, or stress exposure ($f_\theta$), for each type of failure is found by calculating the left-hand side of equations (2-6). Once its value reaches one, failure has occurred. The equations used are [7],

\[
\frac{1}{\varepsilon_{1T}} \left( \varepsilon_1 + \frac{\nu_{12}}{E_1} m_s \sigma_s \right) = 1 \quad (2)
\]

\[
\sqrt{\frac{\tau_{12}}{S_{21}}} + \left( 1 - p_{s1} \right) \frac{\sigma_s}{S_{21}} + \frac{\sigma_s}{S_{21}} + \left| \sigma_1 \right| = 1 \quad (3)
\]

\[
\sqrt{\frac{\tau_{12}}{S_{21}}} + \left( p_{s1} + \frac{\sigma_s}{S_{21}} \right)^2 + \left( \frac{\sigma_s}{S_{21}} + \frac{\sigma_s}{S_{21}} \right) + \left| \sigma_1 \right| = 1 \quad (4)
\]

\[
\left[ \frac{\tau_{12}}{2(1 + p_{s1})} \right]^2 + \left( \frac{\sigma_s}{Y_C} \right)^2 + \left( \frac{\sigma_s}{Y_C} \right) + \left| \sigma_1 \right| = 1 \quad (5)
\]

\[
\left[ \frac{\tau_{12}}{2(1 + p_{s1})} \right]^2 + \left( \frac{\sigma_s}{Y_C} \right)^2 + \left( \frac{\sigma_s}{Y_C} \right) + \left| \sigma_1 \right| = 1 \quad (6)
\]
These equations contain the terms for the material properties given in Table 1. In addition, several other parameters are required: $m_{\sigma f}$ is the mean stress magnification factor for the fibres in the transverse direction, $\nu_{fzz}$ is the Poisson's ratio of the fibres, $p_{1\parallel}^{(+)}$, $p_{1\parallel}^{(-)}$, $p_{1\perp}^{(+)}$ and $p_{1\perp}^{(-)}$ are parameters that control the shape of the failure envelope around the zero transverse stress point, and $\sigma_{1D}$ is used to decrease the resistance to inter-fibre failure due to early fibre breaks (≈1.1$\sigma_f$).

If inter-fibre failure (IFF) occurs, the UMAT degrades the transverse and shear stiffnesses using the following equations [23], [24],

\[
E_{\perp} = \frac{1}{1 - \eta \epsilon_E} \left( 1 + G (f_E(\text{IFF}) - 1) \right) E_{\perp s} \tag{7}
\]

\[
G_{\perp} = \frac{1 - \eta \eta_E}{1 + G (f_E(\text{IFF}) - 1) \left( 1 + G (f_E(\text{IFF}) - 1) \right)} G_{\perp s} \tag{8}
\]

\[
G_{\perp} = \left( \frac{f_E^{(\tau_{z1})} + C(\sigma_E)(f_E - f_E^{(\tau_{z1})})}{1 - f_{\text{thr}}^{(\tau_{z1})}} \right) x \left( G_{\perp s} - G_{\perp} \right) \tag{9}
\]

\[
E_{\perp} = \left( \frac{f_E^{(\tau_{z1})} + C(\sigma_E)(f_E - f_E^{(\tau_{z1})})}{1 - f_{\text{thr}}^{(\tau_{z1})}} \right) x \left( E_{\perp s} - E_{\perp} \right) \tag{10}
\]

where $E_{\perp s}$, $G_{\perp s}$ are the secant moduli at IFF-initiation. $\eta$, $\epsilon$ and $\xi$ are parameters that need to be calibrated by experiments [23], $f_E(\text{IFF})$ is the stress exposure at IFF. $G_{\parallel}$ and $E_{\parallel}$ are the shear and secant moduli, while $G_{\parallel s}$ is the shear modulus at IFF initiation with only shear stress applied. $f_E$ is the stress exposure for normal and shear stresses, and $f_{\text{thr}}$ is the threshold stress exposure below which no damage occurs. $n$ and $C$ are degradation response control factors for equations (9) and (10).

Figure 3 displays the results of validation of the plane stress shell element UMAT against experimental data from the WWFE. The model shows good agreement with the results for both carbon and glass fibre material properties. The stress exposure values can be used as indicators for the risk of damage in the laminates throughout the blade, and the level of degradation to the transverse and shear moduli is also calculated. The UMAT has been calibrated here against the experiment data of Puck [24] for GFRP and CFRP.

![Figure 3](image)

**Figure 3.** (a) Validation of the model for (0°/90°) GFRP laminate for applied $\sigma_x$ uniaxial tension (b) validation of the model for a (0°/±45°/90°) CFRP laminate for applied $\sigma_y$ uniaxial tension [7].

### 3. Results and discussion

Some results of the finite element analyses are illustrated in figures 4 to 9. Before the analysis commenced a mesh sensitivity study was performed; it was found there was no significant difference in the results calculated between the current mesh density (approx. 26,000) and a much finer mesh of 200,000+ elements. Hence, the lower density mesh was chosen.

Figure 6 displays the stress response of the spar caps for the three blades analysed. The plots display the values from the outermost 0° carbon fibre ply. Since the outer three layers form a glass...
fibre protective layer, these are the results for the highest loaded carbon fibre layer. The longitudinal failure strain for carbon fibre is approximately 60% lower than for glass fibres and so is more critical when considering possible failures. It is clear that the increased blade thickness due to the flat-back profile (Blade 3) is beneficial to the structural response of the blade. The stresses in the first 2 m of Blade 3 are considerably lower, almost 50% of the other two blades. A number of peaks can be seen in the resulting plots, due to the decreasing laminate thicknesses.

In conjunction with the lower predicted stresses, Figure 4 demonstrates the improved predicted stiffness of the thicker blade. Blades 1 and 2 display total flap-wise tip deflection of approximately 9% and 7.5% of the total blade length, respectively, while Blade 3 outperforms them with only a 4.5% deflection. It should be noted, however, that although the profiles for blades 1 and 2 had very similar maximum thickness values, they show a notable difference in tip deflection and stress distribution. Profile geometry should, therefore, also be considered when analysing the results. An example of where this is important is around the 0.3 mark on the normalised rotor radius. Blades 2 and 3 both exhibit a significant discontinuity in stress, which is less prominent in Blade 1 (see Fig. 6). However, Blade 1 consists of two NACA profiles, which is not the case with the other two blades, and may imply the difference in geometry between these two profiles is less severe.

Figure 4. Deflection of the blades under max operating load, 2.5 m/s tidal velocity.  
Figure 5. Longitudinal ply-orientated stresses through the compression-side spar cap thickness.  
Figure 6. Longitudinal stress plot from the spar caps of the three blades.

A combination of failure modes is possible, with the faces of the blade in tension and compression. Due to the position of the neutral bending axis of the blade, some of the thick laminates near the root experience tension on one side and compression on the other side of the same laminate (Figure 5). The possibility for complex failure interaction becomes large when the compressive side has a relatively large value for Mode C stress exposure. The results are presented for the maximum operating load case, so low values of stress exposure for all modes are expected. Flood conditions are expected to be
the driving factor for the failure of the blades, especially in cases where control systems fail or are slow to react and the turbines operate beyond their rated velocity.

Degradation is applied in the UMAT via a number of damage variables (valued from 0 to 1, where 1 corresponds to failure). The two damage parameters affected by equations (7) to (10) are for the transverse stiffness and shear stiffness, and are applied to the unidirectional material data before the stiffness matrix is calculated for each element. Contour plots of these variables for the three blades are shown outlined in figures 7 to 9. The changes in shear and transverse material stiffness applied in the UMAT are too low in magnitude and too localised to have a significant effect on tip deflection, increasing only from 691.3 mm for blade 1 in an elastic analysis to 693.1 mm in the non-linear UMAT analysis.

Elements at a risk of IFF in Mode A are visible on both sides of the blade due to the position of the neutral bending axis. The grey area in figures 7 and 8 near the root indicates that IFF has occurred on the tension side in the outer plies and, hence, the next design iteration should involve further strengthening the laminates in this region. From the maximum stress exposure in the contour plots it is apparent there is less chance of a compressive form of IFF occurring, with maximum values of 0.79, 0.54, and 0.42 for Mode B in the three blades. However, a higher risk of Mode C is visible from the plots, with 0.77 and 0.40 in blades 2 and 3 and 1.08 in Blade 1 implying failure. Since Mode C is a more catastrophic type of failure, care should be taken with the design. Figure 9 shows that a small amount of shear degradation has occurred throughout the blade’s box-spar region, which presumably accounts for the change in overall tip deflection of the blade.

Figure 7. Contour plots of the stress exposure level for Blade 1 for Mode A (top), Mode B (middle), and Mode C (bottom).

Figure 8. Contour plots of the stress exposure level for Blade 2 for Mode A (top), Mode B (middle), and Mode C (bottom).

4. Conclusions

Key results from this study showed that thicker blade sections provide a beneficial structural response, and help to reduce the risk of inter-fibre failure damage near the blade roots. The outermost highest loaded plies were predicted to have a high risk of IFF. Further work will investigate solid models for sections of the blade where through-thickness stresses are likely. The possibility of more computationally efficient modelling methods will be explored for such a model.
The CFRP and GFRP results both demonstrated a close match to the experimental data. However, issues arise with the triaxial nature of the stresses in the thick sections of the blade, especially around geometric discontinuities such as the root attachments. This is a limit to this type of shell based analysis. When used as a failure criteria, the UMAT proves to be a powerful tool for understanding the risks of inter-fibre failure for a fully loaded blade, and even modelling the reduction in stiffness in the regions that do suffer damage. Blade failures in some of the first test devices [25, 26] indicate the importance of developing better predictive techniques for assessing the damage occurring in large composites-based structures. The UMAT has added advantages in that it is not specific for turbine blade applications, but can be applied to the analysis of any glass or carbon fibre reinforced composite structure, where the plane stress assumption is valid.

Figure 9. (Clockwise) Contour plots of the stress exposure level for Blade 3 for Mode A (top-left), Mode B (top-right), and Mode C (bottom-left). Shear damage variable for the box-spar section of the blade (bottom-right).

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