Dear reviewers,

We would like to thank you for your reviews of our paper. Your suggestions did help improve it. Please find below the point-by-point reply (in blue-colored text) to your comments.

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Referee 1 (A. Barlas)

The revision of the article results in a well written and detailed scientific study of the topic. No detailed comments or modifications are considered necessary.

We would like to thank you for your appreciation of our paper.

Referee 2 (A. Riziotis)

The paper proposes and assesses an active TE flap control strategy which uses the flap hinge moment as sensor for estimating the local inflow velocity. The authors employed and enhanced an existing linear aerodynamic model for the calculation of the hinge moment of an airfoil with a moving flap that undergoes pitching and heaving motion. Using this model they developed a model based inflow estimator model which provides effective wind velocity and angle of attack seen by a blade section. Furthermore, they developed and tested a TE flap control strategy that mitigates extreme loads due to extreme turbulence. Preliminary results from the application of the method seem to be very promising. Moreover, in their text the authors discuss in detail pros and cons of the proposed strategy as it is yet at an early stage of development.

The paper is well structured and very well documented. Furthermore the work presented in the paper is complete, novel while it is substantiated that the proposed concept works well. Given all the above I recommend publication of the paper with some minor revision.

Thank you for your appreciation of the paper.

My main comment is that some further explanation is needed with regard to the way that the 2D sectional unsteady aerodynamic model has been transferred to the 3D deforming blade case. See comment in 3.2 in the attached pdf.

My comments/corrections are detailed in the supplement pdf file.

p.2 l1-2: The authors refer to the well known rule of the 3rd power (of the scaling factor). However, this rule applies only to the case of the gravity loads. In the other words if gravitational loads are neglected a geometrically upscaled blade will encounter the same stresses due to aerodynamic forces because aerodynamic loads scale with a power of 2. The additional stiffness is needed when it comes to gravity loads which scale with a power of 3.

Indeed we were referring to the square-cube law known in the wind industry. The fundamental driver is the increase in the blade mass, which increases as the 3rd power of the rotor diameter. While the aerodynamic moment increases are matched by the bending section modulus of e.g. the scaled blade, the moment due to the self-weight of the blade will not. So in response the blade will have to be made stiffer to endure the increased stress.

We also wanted to refer to other, less evident, effects that scaling has on the turbine loads. As pointed out in (Jamieson, 2018 p. 104) and (Madsen et al., 2020), the characteristic turbulent length scales present in the Earth's boundary layer are comparable to the scales of larger rotors. This makes the effect of turbulence stronger on the aerodynamic loads of wind turbines with larger rotors compared to turbines with small rotors. This also means that the effect of extreme turbulence will be larger on larger turbine rotors and hence will lead to an additional increase of the aerodynamic loads. In the reference (Chaviaropoulos et al. 2014 p. 9), a figure shows the trendline of the ratio of extreme loads to fatigue loads of the out-of-plane blade root bending moment as a function of the rotor diameter. The trendline – representing a large set of design loads from commercial projects – has a clear positive slope, indicating the effect discussed above.

Blade designs have to take both of these effects into account. Hence our mentioning of both gravitational and aerodynamic loads in this condensed sentence.

To keep the introduction brief and not get into too much detail in the fourth line of the paper, we changed this introducing sentence so that its statement is more general.

p.8 l29: I would recommend the authors to give an indication of the magnitude of other inertial loads (eg. centrifugal or Coriolis). Would they play any role? This will explain why they have been neglected.

We neglected centrifugal and Coriolis forces based on the assumption that they act in perpendicular directions to the direction what would cause a moment on the flap hinge (i.e. perpendicular to the airfoil chord and the blade span). This is only completely true for a straight blade without cone angle, prebend or deflections. In full aeroelastic calculations, we will have a small contribution of these forces that depend on the angle that the blade section has relative to the rotor plane.

To have an idea of the contribution of these forces on the overall hinge moment, we did some simplified calculations assuming constant aligned inflow with typical wind shear (0.2 exponent). For a comparable flap section like the one shown in Fig. 5 (mass = 33.09 kg and spanwise position of 74.5 m) we found that for a wind speed of 11 m/s the centrifugal forces contributed a flap hinge moment in the order of 1.4 Nm. The Coriolis forces contributed a flap hinge moment in the order of 0.02 Nm. Compared to the average hinge moment of around -77 Nm for that wind speed, we find that the contributions due to centrifugal forces are indeed small but not necessarily negligible.

We added a sentence in Sec. 3.1.1 to mention centrifugal and other loads as part of the inertial loads and why we chose to neglect them. A more detailed hinge moment model would include the effect of these additional inertia loads as well as the effect of friction. We included the improvement of the hinge model as possible future work in the conclusions of the paper.

p.11 l.1: I suppose that the simulations correspond to pitching motion. In other words, AoA variations are due to different orientation of the airfoil and not due to heaving motion (effective AoA variation).

Yes, the simulations with varying angle of attack correspond to a pure pitching motion of the airfoil. We included a sentence to make this point clear.

p. 12 l4: As I understand it, the linearized aerodynamic model provides loads for a combined pitching and heaving motion. Then α stands for the pitching component and h stands for the heaving component.

In the case of a 3D blade pitching motion is a combination of the controlled motion due to the pitch actuators and the blade local torsion angle. Although only the above two should be attributed to α it seems that in the present work α stands for the overall effective angle of attack.
However, the effective AoA includes also a significant component due to the flapping motion which seems to be neglected. In linearized form this would be written as:
αeff=α-(dh/dt)/U

I think that for the sake of clarity the above point should be discussed. Overall, my proposal is to further discuss how the sectional unsteady aerodynamic model has been matched with the actual flow around the blade section and the local velocity triangle.

We agree that this point is not properly explained in the paper. The aerodynamic hinge moment model does include the effective angle of attack due to the flapping motion of the airfoil. If we write out the upper two rows of eqs. (5) and (6) of the paper, we obtain for eq. (5):

$$\begin{pmatrix}\dot{z}_1\\ \dot{z}_2\end{pmatrix} = \begin{bmatrix} 0 & 1 \\ -b_1 b_2 \left(\frac{V_{rel}}{b}\right)^2 & -(b_1+b_2)\cdot\left(\frac{V_{rel}}{b}\right)\end{bmatrix}\begin{bmatrix} z_1 \\ z_2\end{bmatrix} + \begin{bmatrix} 0 \\ \alpha + \dot{\alpha}\cdot\frac{b\left(\frac{1}{2}-a\right)}{V_{rel}} + \frac{\dot{h}}{V_{rel}}\end{bmatrix}.$$

If for eq. (6) we keep only the terms associated with the circulatory part of the aerodynamic hinge moment coefficient associated to α (recognizable because they contain the flap effectiveness constant ϵ_α), we obtain

$$y = \epsilon_\alpha \left[\frac{F_{12} b_1 b_2}{4}\cdot\left(\frac{V_{rel}}{b}\right)^2 \quad \frac{F_{12}(A_1 b_1 + A_2 b_2)}{2}\cdot\left(\frac{V_{rel}}{b}\right)\right]\begin{bmatrix} z_1 \\ z_2\end{bmatrix} + \frac{\epsilon_\alpha F_{12}}{4}\cdot\alpha + \frac{\epsilon_\alpha F_{12} b\left(\frac{1}{2}-a\right)}{4V_{rel}}\,\dot{\alpha} + \frac{\epsilon_\alpha F_{12}}{4V_{rel}}\,\dot{h}.$$

We note that - except for our addition of ϵ_α - these equations correspond to eqs. (8.222) and (8.221) of reference (Leishman, 2006), with the effective angle of attack defined as (eq. (8.215) of the same reference)

$$\alpha_{eff} = \alpha + \dot{\alpha}\cdot\frac{b\left(\frac{1}{2}-a\right)}{V_{rel}} + \frac{\dot{h}}{V_{rel}}.$$

So, in the model, α stands for the angle of attack due to airfoil pitch from blade torsion and from actuation of the blade pitch. The effective angle of attack is handled implicitly by the state space model.

In our aeroelastic calculations, the hinge model was implemented as follows: The aerodynamic hinge model uses as an input the vector $u = (\alpha \quad \dot{\alpha} \quad \ddot{\alpha} \quad \delta \quad \dot{\delta} \quad \ddot{\delta} \quad \dot{h} \quad \ddot{h})$ and the relative wind speed. All of the input quantities are calculated by our aeroelastic software and fed directly to our model (implemented within the controller dll in QBlade and within a Simulink framework with FAST). There is no direct feedback of the hinge model to the aeroelastic software except in the form of a flap deflection demanded by the proposed control strategy. As explained at the beginning of Sec. 3.1.2, we calculated the unsteady lift and pitch coefficients of the blade sections via the implementation of ATEFlap model in QBlade.

QBlade uses the lifting line free vortex wake aerodynamic model to calculate the aerodynamic loads. As with the BEM model used in FAST, the blade is segmented into several elements for which the aerodynamic inflow information is calculated separately. In particular, one angle of attack and relative velocity is used for every blade element. A similar approach is used for the hinge moment model. The quantities of the input vectors for each hinge moment model were chosen to be the ones at the center of the flapped blade section. These quantities are assumed to be representative for the whole blade section. In FAST, we discretized the blade into 46 aerodynamic and 57 structural nodes. We placed the nodes so that the relevant ones lied at the center position of the flapped blade sections. QBlade interpolates the aerodynamic and structural quantities linearly between blade elements. For this study, we used a blade discretized into 25 aerodynamic elements in a sinusoidal manner and 20 structural elements in linear manner. The entries for the input vector at the center position were obtained by interpolating between neighboring blade elements.

If the QBlade aerodynamic discretization would have been linear, one blade element in our simulation would have corresponded to 4% of the rotor length. In comparison, one 3 m flapped blade section corresponded to about 3.3% of the rotor length. So, our assumption of using one set of effective input quantities per flapped blade section leads to a similar error if we compare it to the error we would introduce by discretizing our blade into 25 individual, linearly separated, elements.

We included a new Section (Sec. 4.1) to explain how we implemented the hinge model and observer into our aeroelastic simulations and also discuss how the implementation could be improved. We also mention explicitly in Sec. 3.1.2 that the aerodynamic hinge moment model includes the effective angle of attack obtained from a flapping blade motion. We kept the last part short because we already mention at the beginning of Sec. 3.1.2 the reference from which we took the model. The interested reader can look up the reference to see the model in a less compact form.

p. 25 fig. 12: In this particular case failure of the controller to reduce the ultimate load is combined with an increase in the local peak load. Very fair from the authors' side that they present this case. However, could this case imply that depending on the selection of the seed one could come up with a higher ultimate load? In other words, I understand that this is yet a proof of concept study. However, shouldn't be tested for several different seeds before someone concludes that it is always effective.

We tried to show the simulations when the controller performed well and when it didn't in our critical discussion of the controller performance. Indeed, the simulation depicted in the Figures 12e to 12h is one of the cases where the controller did not perform well. To minimize the risk that our controller was doing particularly well only for one turbulent wind seed, we did 6 repetitions with different turbulence seeds for each wind speed bin. Our ultimate loads metric was then taken as the average of the six highest values of all simulations (Secs. 5.2 and 5.3). This is the same metric used for many design load calculations and certifications of wind turbines.

Figure 9 shows an overview of the extreme values of M_Y^{BR} for all simulations of all wind speed bins. We can see in this figure that the controller manages to systematically reduce all the extrema of this sensor independently of the wind speed bin or the turbulence seed. A similar distribution is expected for the other two considered sensors: M_{XY}^{BR} and D_X^{T2T}.

The peak shown in Fig.12e was not one of the 6 highest peaks of M_Y^{BR} that were taken for the calculation of the ultimate load. So even if the controller did not perform well in this particular case, it managed to reduce the ultimate load in a statistical manner according to our metric.

This conclusion of course only holds true for these particular sensors and this particular DLC group. In our outlook we mention the inclusion of more DLC groups and more load sensors as part of future work to better evaluate the performance of the controller strategy.