Model Validation and Simulated Fatigue Load

Alleviation of SNL Smart Rotor Experiment

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In this paper an individual flap controller (IFC) design for smart rotors is presented and compared to the model identification of the Sandia National Labs Smart Rotor experiment. The controller design has been carried out using an in-house aeroservoelastic software - DU_SWAT. The root bending moment response due to a step input of the flap deflection has been linearized. Based on this linearization a Coleman transform combined with a proportionality controller has been used to eliminate the cyclic components in the root bending moment. It was shown that IFC can reduce the 1P mode similar to individual pitch control, while only modest flap deflections of below 5 degree are required.

Keywords: Aeroelasticity, Smart Rotor, Model Validation, Controller Design

I. Introduction

One of the main driving factors in wind turbine blade design are dynamic loads. This is certainly true as the ever increasing size and hence flexibility of wind turbine blades results in increasing fatigue loading. A reduction of these loads can greatly alleviate the design requirements not only on the wind turbine blades, but also on other components such as the gearbox or generator. One method to reduce the undesired fluctuating loads is the exploitation of Active Aerodynamic Devices (AAD) such as trailing edge flaps or variable camber. The combination of trailing edge flaps with...
associated sensors is considered very suitable for sophisticated control tasks because of its very high, direct control authority on the lift coefficient. During the last decade, this field of research has drawn a lot of attention. The wind energy research group of the Danish Technical University has done a lot of work on establishing an adequate aerodynamic model for unsteady aerodynamics of flapped airfoils [1–5], while Sandia National Laboratories (SNL) has done extensive research on Active Aerodynamic Load Control to determine the fatigue life increase that can be achieved for both blades and drive train [6–9]. Researchers at Delft University of Technology have focused on the control aspects and the proof of concept for a 2 m diameter rotor wind tunnel model [11], [12] and [13].

The abovementioned research shows the potential to alleviate the fatigue loads, but more experimental results are needed to validate the simulation results. The efforts of Delft University of Technology have been a first step towards such a validation. The logical following step was to test the system on a representative turbine in terms of size and frequency. For this purpose, Sandia National Laboratories has designed and built a 100kW test turbine equipped with three individually controllable flaps on every blade. To the authors’ best knowledge this wind turbine is the first turbine with this feature that is field tested. Information about the design, fabrication, integration can be found in previous AIAA Aerospace Sciences Meeting papers [14, 15].

II. Aeroservoelastic analysis tool

As the numerical model, a newly created in-house tool of Delft University of Technology was used. This tool was specifically created for aeroservoelastic analyses of wind turbines with distributed trailing edge flaps. It was developed based on the experience gained from the first generation of aeroelastic codes of Delft University of Technology [16].

The code combines a dynamic inflow model with a Blade Element Momentum (BEM) approach. The two dimensional unsteady aerodynamic model that is used in the code is an implementation of the dynamic stall model of Andersen et al. [17] with the shape integrals developed by Gaunaa [18]. The model is an extension of the Beddoes-Leishman type dynamic stall model of Hansen et al. [19]. A Prandtl tip loss correction and root flow correction was used as described in [20]. This model was embedded in Simulink to make use of the SimMechanics package included in Matlab. A multi-body approach was used to simulate the structural dynamics. In the presented analysis, 18 body elements with 3 rotational degrees of freedom were used. The rotational degrees of freedom are modelled as discrete rotational springs. Flap dynamics have been modelled as force inputs to the structural model. Modal damping has been assigned in the spring-damper elements that connect the bodies. Both the aerodynamic module and the structural module can operate with a variable number of elements or aerodynamic sections. The aerodynamic loading is interpolated on the structural elements. The structural displacements, rotations and velocities are interpolated as well and returned as input to the aerodynamic module.

The aerodynamic and structural model were especially created for this code. The input of the wind field has been significantly remodelled compared to previous codes of Delft University of Technology [16] to decrease calculation time. The wind file generation is done in Turbsim, however
the data is pre-processed such that only part of the interpolation needs to be done during the execution of the code. The torque and pitch controller are implementations of [21], analogue to previous aeroelastic codes of Delft University of Technology. For the modelled turbine, the pitch controller has been omitted. The model has been benchmarked against aeroelastic analyses of the 5 MW reference turbine in GH Bladed and FAST and found to be in good agreement with them in terms of tip deflection, eigenfrequencies, power production, root bending moment and generator power [22].

III. Experimental set-up

The flapped rotor designed and built by Sandia National Laboratories was tested on the SNL turbine located on the USDA-ARS site in Bushland, Texas, USA. As shown in Figure 1, the test turbine is a three-bladed, fixed-pitch, upwind Micon 65/13 turbine with modifications to the brakes, gearbox, generator, and blades. The generator is rated at 115 kW and operates at 1200 rpm while the rotor turns at a nominal 55 rpm (the standard Micon 65/13 rotates at 45 rpm).

The surrounding terrain is essentially flat and characteristic of the Great Plains of the USA. Upwind of the turbine is a meteorological tower instrumented with cup anemometers at hub height (23 meters), rotor top, rotor bottom, and 2 m from ground level. In addition to the cup anemometers, a wind vane and ATI sonic anemometer are installed at hub height. The anemometry is located approximately 30.7 m upwind of the turbine, or roughly 1.7 rotor diameters in front of the turbine.

As detailed in previous AIAA papers [14, 15], the blades are modified versions of the CX-100 design. These 9 m blades are internally instrumented with accelerometers, fiber optic strain, fiber optic temperature, and metal foil strain gages. The active flaps extend 20% of chord and roughly 20% of blade span (starting at 7.029 m span and extending 1.83 m or 6 ft). Before the rotor was installed, each blade was characterized with three ground tests: 1) blade pulls in flap and edge with blade cantilevered to test stand, 2) modal test with blade cantilevered to test stand, and 3) modal test with blade suspended in approximate free-free boundary condition. After the rotor was installed, the flaps were actuated with various motions to excite structural dynamics while the rotor was parked. These motions consisted of sinusoidal motion at discrete frequencies, sinusoidal motion with logarithmic sweep of frequency, and step motions between 0 degrees flap and ± 5, 10, 15, and 20 degrees flap. Finally, these flap actuations were repeated while the rotor was turning and producing power.

All of these flap excitations were open-loop, meaning that there was no feedback measurement of a blade sensor driving the flap motion. In the following sections, a closed-loop flap control strategy is proposed and simulated; however there is no closed-loop experimental data available for comparison yet.

IV. Individual Flap Control

Besides power regulation, controllers can be extended to also alleviate fatigue loads on rotor components. For this purpose the basic torque controllers can be expanded by a set of additional controllers such as individual pitch controllers, which allow reducing vibrations that occur at low
frequencies. The undesired effect of the interaction between the basic and additional controllers can be circumvented by using multi-input-multi-output controllers. This type of controller is based on turbine linearization around a certain operation point. Current practice is to decouple the system, using the Coleman transformations (sometimes labelled as the park, d-q, or Multi-Blade transformations), in the yaw and tilt direction [23, 24]. This decoupling will make it possible to design simple single-input single-output controllers (simple integral controllers are typically used).

In this paper we will use a similar approach, but instead of constructing an IPC, the flap deflection will be the control input, rendering the system an Individual Flap Controller (IFC). However, there are two main distinctions. Instead of having only one actuator on every blade we now can have multiple flaps which leads again to a Multiple-Input Multiple-Output (MIMO) controller design problem. Moreover, flaps typically have a higher actuation bandwidth and therefore they can also be used to mitigate higher periodic loads and they can be used to add structural damping to some of the system modes.

Quantities that are obtained from aeroelastic simulations or measurements are either taken with respect to the rotating reference frame of the rotor or with respect to the fixed reference frame of the tower and nacelle. The Coleman transform [25] converts these quantities from a rotating reference frame to a fixed one, circumventing the periodicity of the system. This transform is given by:

\[ P^{-1} = \begin{bmatrix} \frac{1}{3} & \frac{1}{3} & \frac{1}{3} \\ \frac{2}{3} \sin \psi_1 & \frac{2}{3} \sin \psi_2 & \frac{2}{3} \sin \psi_3 \\ \frac{1}{3} \cos \psi_1 & \frac{1}{3} \cos \psi_2 & \frac{2}{3} \cos \psi_3 \end{bmatrix} \]  

(1)

with \( \psi \) as the azimuth angle of the blades. The linearized time-invariant system (LTI) is an integrator controller combined with a gain. Figure 2 shows the overall control system configuration.
V. Model validation

As a first step the system has been linearized. This has been done by adding a perturbation introduced by flap deflections to the steady state of the system. Figure 3 shows the Bode plots of the linearized system. The two upper plots represent the magnitude and phase of the out-of-plane deflection, whereas the lower two plots correspond to the magnitude and the phase of the in-plane deflection. Periodic components are filtered out. The first resonance in the uppermost plot of Figure 3 occurs at 4.52 Hz for the out-of-plane displacement. This corresponds very well with the measurements of Sandia that obtained a first frequency at 4.4 Hz. One can see that the elevation is relatively wide and aerodynamically strongly damped. Next to this elevation, there is a peak, at 5.65 Hz. This peak originates from in-plane motion as can be seen in the third plot of Figure 3. In contrast to the out-of-plane deflection, where plunge-type motions are strongly aerodynamically damped, this type of damping does not occur for in-plane deflections. Consequently the peak is very high, even compared to the out-of-plane deflections. It should be stressed that modal damping was used in the simulations and that the damping parameters can only be seen as an estimation of the damping of the real turbine.

Figure 4 shows the spectral density of the out-of-plane accelerations due to a frequency sweep of the flaps between 0.1 Hz and 6 Hz. A wide resonance peak can be seen for the first eigenmode at 4.4 Hz, while a sharp, second resonance is found between 18 Hz and 19 Hz. This compares very well with the numerically obtained eigenfrequencies, which the in-house code predicts to be 11 Hz for the second eigenfrequency out-of-plane and 18 Hz for the second eigenfrequency in-plane. It is expected that the low damped second in-plane mode is captured by the accelerometers of the SNL rotor.

In a second step the accelerations of the numerical model are compared to the ones of the SNL test turbine as shown in Figure 5. The first observation is that the power spectral density of the test turbine is a lot smoother compared to the numerical simulation without control. This can be attributed to a simulation time of 100 second. It is assumed that the PSD will smoothen out when
Fig. 3 Bode plot of numerical system: Magnitude (1st plot) and phase (2nd plot) of out-of-plane and Magnitude (3rd plot) and phase (4th plot) of in-plane tip deflection as a result of flap deflection

the simulation is run for a longer time interval. The second observation is that the numerically obtained curve corresponds very well with the power spectral density of the experiment. The experimental curve peaks at a similar amplitude and frequency as the numerically obtained curve without controller. Between 1Hz and 10 Hz, the simulation and the experiment provide excellent agreement in terms of power spectral density. Above 10 Hz, the experiment is only excited by the turbulent wind field; flaps are only used below those 10 Hz. Therefore the power spectral density of the tip acceleration drops an order of magnitude compared to the numerical model, which includes also higher frequencies of flap excitation.

At higher frequencies both power spectral densities decrease towards values of -30dB/Hz or -40 dB/Hz. The numerical simulation shows a more pronounced second resonance frequency around 12 Hz. The response at the second eigenfrequency is about 15dB/Hz lower than the one of the first
Fig. 4 Acceleration response to periodic flap excitation of SNL rotor: Frequency steps from 0.1 Hz (t = 600s) to 6 Hz (t = 1100 s), red corresponds to a high acceleration amplitude, blue to low accelerations.

eigenfrequency, rendering the first eigenfrequency most critical to the fatigue loading.

Significant differences in the behaviour of the plots can be observed for frequencies below 1 Hz. While the experimentally obtained signal is perturbed by measurement noise from the sensors and shows significant low frequency loading, the numerical simulation does not reproduce this low frequency response. These differences in the experiment have multiple sources. First of all, one should mention low frequency turbulence. Other sources are directly connected to the type of accelerometer used. It is operating at low frequencies down to 0 Hz picking up gravity and centripetal accelerations, which appear as increased 1P and nearly-static accelerations, respectively. Both curves in Figure 5 show strong resonance for 1P at 0.9 Hz. A comparison of amplitudes is however difficult, as the amplitude shift might be attributed to the low frequency response of the sensor. The amplitudes for 2P and 3P of the experiment and the simulation are comparing a lot better being -6.4dB/Hz and -3.3dB/Hz for the SNL test rotor, while the numerical simulation results are -4.5dB/Hz and -3.4dB/Hz for 2P and 3P respectively.

The Individual Flap Control (IFC) controller has been designed to eliminate the periodic components (1P) of the rotor and tuned such that it also reduces the resonance associated to the first flapwise bending frequency. It was chosen to control the periodic fatigue loading with the root bending moment as control input. Figure 6 shows the PSD for uncontrolled and IFC controlled rotor simulations with a turbulent wind input. It can be seen that for the uncontrolled case the periodic root bending moment are dominant compared to all other moments. Upon implementing the controller, these periodic vibrations are strongly diminished. The PSD around 0.92Hz is therefore strongly reduced. As the controller was tuned such that it also reduces the first resonance frequency of 4.52 Hz, a slight reduction of the moment amplitude for the flapwise mode can be observed. This can be seen back both in Figure 5 and the left hand side of Figure 6. The conclusion that can to be drawn at this point is that individual flap control can replace individual pitch control albeit not collective pitch control for power regulation. For the current controller, where all control surfaces are fed the same signal, tuning allows load reduction of 1P without having to pay a penalty at higher frequencies. In a next step the potential of distributed control needs to be evaluated such that both
the 1P component in the vibration spectrum and the resonance at the first eigenfrequency can be significantly reduced.

The right hand side of Figure 5 shows that most control action is taken at 0.9Hz. The flap deflections at higher frequencies are more than a factor 10 lower than for 1P. This explains the relatively small impact on the PSD for the first and second eigenfrequency. At the same time it advocates using multiple control objectives as flaps can operate at significantly higher frequencies than IPC.

Figure 7 shows yet another time the potential of IFC compared to IPC. The flap deflection signals on the right hand side are clearly dominated by frequencies of 0.9 Hz. The vibration at this damping frequency are significantly reduced. It is worth to notice that the amplitude of the required flap deflection is in the order of 5 degree. This is very desirable as a high deflection would lead to flow separation, thereby effectively causing an inversion on the desired effect on the lift coefficient. At 5 degrees, flow separation is limited and the gain in lift coefficient can be assumed to be close to linear. It also demonstrates that IFC can efficiently take over the tasks of IPC certainly as only a limited range of flap deflection angles is needed. Moving from IPC to IFC reduces the actuation rate requirements on the pitch bearings and actuators, possibly leading to a less maintenance prone and cheaper system. At the same time 1P and high frequency loads can be suppressed, reducing the overall fatigue damage.

**Conclusion**

A model verification has been successfully completed. The response of the test turbine strongly resembles the numerically evaluated system, especially for out-of-plane motions. In-plane resonance is more difficult to capture as it depends on structural damping, a parameter that is difficult to estimate. It was shown that Individual Flap Control can be used analogue to Individual Pitch Control, however to exploit all the benefits of distributed flaps, a controller needs to be established that
Fig. 6 Numerically obtained power spectral density of root bending moment for turbulent wind speed of blades 1, 2 and 3

individually steers the flaps in order to alleviate fatigue loads from 1P and suppress the resonance at the first eigenfrequency.

While for the presented control logic, all flaps of a blade are deflected with the same command, in a next step the controller should be expanded to independently steerable flaps. This should be done both in the numerical model to predict the load alleviation capacity of a more advanced control scheme, as well as for the test turbine.

References
Fig. 7 Simulation time history of tip deflection for turbulent wind input


