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Control of floating wind turbines

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Abstract— **Wind energy is a clean, renewable and extremely fast growing form of electricity generation and the potential to install turbines deep offshore is only just being realised. The vast majority of commercial offshore turbines have foundations on the seabed thereby restricting the depths at which offshore farms can be installed. In an attempt to facilitate access to a potential multi-Terawatt resource, a number of floating concept wind turbines have emerged. In this tutorial paper we review the control challenges associated with the design of floating turbines and summarise recent developments in the area. Of particular interest is a fore-aft oscillation induced by attempting to regulate the generator speed to its rated value. The paper concludes with a discussion of how the control problems presented are likely to change with increasing turbine size and structural flexibility.**

I. Introduction

Increasing concerns over climate change have recently led to ambitious targets worldwide on renewable energy production [1]. With wind turbines being one of the more technologically mature options, the ability to meet these targets depends heavily upon the successful growth of the wind energy sector [2]. Part of this growth relates to making the individual turbines more efficient, leading to a trend of larger, more flexible structures [3]. This, however, comes at the cost of an increase in the visual impact, the noise produced and the real estate required for the installation of a site [4, p. 339-340]. Taking this into account, combined with the fact that some of the most powerful, sustained and low turbulent winds can be found at sea, the installation of offshore wind turbines has developed into a compelling proposition [5].

In order to fully exploit the multi-Terawatt offshore resource [2], wind turbine deployment needs to be viable in deep waters and at sites with loose sea-beds. In the former, monopiled solutions are not cost effective [6]; and in the latter, they are not possible – a realisation that has motivated the development of a number of floating wind turbine concepts, see for example [6], [5]. Despite skepticism from some quarters, the transition to floating offshore structures is a natural one, and has been made previously by the oil and gas industry with considerable success [2]. A discussion of the structural options for floating turbine concepts is beyond the scope of this paper, although an exposition can be found in [7] or [8]. We will however note that the installation and maintenance of platform-based floating turbines carries the considerable advantage over both monopile and spar-buoy type concepts that both processes can be performed onshore circumventing the need for extraordinarily expensive access vessels [6].

The designers of floating wind turbines are faced with some formidable challenges, most notably the considerable structural vibrations induced by wind and wave loads [9]. To sustain these high loads, offshore turbines tend to be heavier, and therefore more expensive, than their onshore counterparts. With capital expenditure dominating wind power plant costs [11], any efforts to reduce these material weights leads directly to a reduction in the cost of energy, thereby motivating the search for load reduction via advanced control. Aside from the necessary safety systems, the control objectives can classically be stated as achieving optimal power production whilst keeping the forces and moments that the components experience to an absolute minimum [12]. In this tutorial paper we focus on the additional challenges faced when designing a controller for power production of a floating wind turbine.

In Section II simplified wind turbine dynamics are presented to facilitate the demonstration of the control issues. The operational regions are described in Section III along with a detailed discussion of the control design problems associated with regulating generator speed to the rated value, whilst producing nameplate power. Section IV compares and contrasts the published solutions to such problems, whilst Section V gives a discussion of how the problems will scale to tomorrow's larger, more aerodynamically efficient turbine designs. The conclusions of the paper are summarised in Section VI.

II. Dynamics

The combined torque τ (Nm) generated by three blades of length R (m), pitched at β (rad), rotating at ω ($^{\text{rad}}/_{\text{s}}$) in an apparent wind of speed v ($^{\text{m}}/_{\text{s}}$) is defined:

$$\tau(\omega, v, \beta) := \frac{1}{2}\pi R^3 \rho C_Q(\omega, v, \beta) v^2,$$

where $C_Q : \mathbb{R}_+ \times \mathbb{R}_+ \times \mathbb{R} \to \mathbb{R}_+$ is the torque coefficient defined by the blade profile[1], $\mathbb{R}, \mathbb{R}_+$ the sets of real and non-negative real numbers respectively and ρ ($^{\text{kg}}/_{\text{m}^3}$) is the air density [4, p. 6].

The aerodynamic torque is transferred to the electrical generator through the gearbox. For simplicity we consider an ideal gearbox with gear ratio R_g, and hence the power transfer is lossless and the high speed shaft rotates at $R_g\omega$ ($^{\text{rad}}/_{\text{s}}$).

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[1]The torque coefficient C_Q is written as a function of rotor speed and wind speed as opposed to the more commonly used tip speed ratio $\lambda := \frac{\omega R}{v}$ for notational ease when considering linearisations in Sections III and V.

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The stator side of the generator provides a torque against the motion of τ_e (Nm) and hence the power generated by the system P is defined $P := \tau_e R_g \omega$. If the (scaled) electrical and aerodynamic torques are not equal, the rotor will accelerate or decelerate:

$$I \frac{\mathrm{d}\omega}{\mathrm{d}t} = \tau(\omega, v, \beta) - R_g \tau_e - \alpha\omega,$$

where α is the viscous friction and I the moment of inertia of the rotating parts [13].

A side effect of extracting power from the wind is the excersion of a thrust force F_t (N) on the turbine defined:

$$F_t(\omega, v, \beta) := \frac{1}{2}\pi R^2 \rho C_T(\omega, v, \beta) v^2,$$

where $C_T : \mathbb{R}_+ \times \mathbb{R}_+ \times \mathbb{R} \to \mathbb{R}_+$ is the thrust coefficient, again inferred from the blade profile [4, p. 35]. The thrust force results in a motion of the rotor from both the platform movement and flexibility of the tower. The dynamics of nacelle fore-aft position z (m) is represented as the superposition of a tower bending mode and a platform tilting mode, each of which can be expressed as a second order system with associated natural frequency and damping ratio:

$$\begin{aligned} \ddot{z}_1(t) + 4\pi\zeta_1 f_1 \dot{z}_1(t) + 4\pi^2 f_1^2 z_1(t) &= F_t(\omega(t), v(t), \beta(t)), \\ \ddot{z}_2(t) + 4\pi\zeta_2 f_2 \dot{z}_2(t) + 4\pi^2 f_2^2 z_2(t) &= F_t(\omega(t), v(t), \beta(t)), \\ z(t) &= a_1 z_1(t) + a_2 z_2(t), \end{aligned}$$

where the parameters $\zeta_1, \zeta_2, f_1, f_2$ denote the damping and natural frequency of the platform (subscript 1) and tower (subscript 2) respectively, and a_1, a_2 represent the contribution of the two modes to the nacelle motion, see Figure 1.

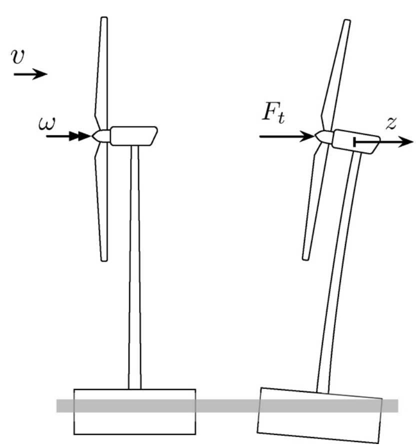

Fig. 1. Floating wind turbine configuration. The nacelle displacement is due to platform tilt and tower bending.

A motion of the nacelle forwards increases the apparent rotor wind speed, thereby affecting both the driving torque and thrust force:

$$\begin{aligned} \ddot{z}_1 + 4\pi\zeta_1 f_1 \dot{z}_1 + 4\pi^2 f_1^2 z_1 &= F_t(\omega, \bar{v} - \dot{z}, \beta), && (1a) \\ \ddot{z}_2 + 4\pi\zeta_2 f_2 \dot{z}_2 + 4\pi^2 f_2^2 z_2 &= F_t(\omega, \bar{v} - \dot{z}, \beta), && (1b) \\ z &= a_1 z_1 + a_2 z_2, && (1c) \\ I\frac{\mathrm{d}\omega}{\mathrm{d}t} + R_g \tau_e + \alpha\omega &= \tau\left(\omega, \bar{v} - \dot{z}, \beta\right), && (1d) \end{aligned}$$

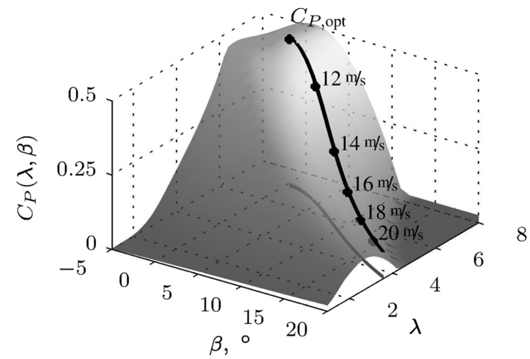

Fig. 2. The power coefficient curve and a representation of the above-rated operating strategy required to maintain $P = P_{\text{rated}}$.

where $\bar{v}$ is the mean wind speed and $\dot{z}$ is the component resulting from the motion of the nacelle.

III. Control objectives

A. Regions of operation

The aim of a wind turbine is to maximize the power produced subject to constraints on generator speed and power. As a result, the operation of the wind turbine can be divided into three regions [15]. In low wind speeds, prior to either the generator speed or the power reaching their limits, the aim is to maximize power. In order to do this, the pitch angle and electrical torque are controlled to operate the blades at their most aerodynamically efficient. In high winds, with both generator speed and power at their respective limits, the goal is to adjust the pitch to maintain these values. The produced power can be written

$$P = \frac{1}{2}\pi R^3 \rho\omega C_Q(\omega, v, \beta) v^2 = \frac{1}{2}\pi R^2 \rho C_p(\lambda, \beta) v^3,$$

where $\lambda := \omega R/v$ is the tip-speed ratio and $C_p(\lambda, \beta) := \lambda C_Q(\lambda v/R, v, \beta)$, see [4, p. 6]. Figure 2 shows how in full load the pitch can be changed to reduce aerodynamic efficiency and maintain power at its rated value. A number of transition strategies have been proposed for the interval between these regions, for an exposition see [12].

B. Control in above rated conditions

The main problem associated with the control of floating wind turbines concerns the tilt stability in full load [16]. To understand this problem, we must first consider the effect of changing wind speed on the steady state thrust. The steady state thrust curve, an example of which is shown in Figure 3, is defined, in the above rated region, as the thrust required at a given wind speed to produce rated power at constant rated generator speed. The steady state pitch varies along the operating curve to achieve constant power production:

$$\begin{aligned} \mathrm{d}P &:= \omega_r \left(\frac{\partial \tau}{\partial v}\,\delta v + \frac{\partial \tau}{\partial \beta}\,\delta\beta \right) = 0, \\ \implies \delta\beta &= -\frac{\partial \tau}{\partial v} \left(\frac{\partial \tau}{\partial \beta} \right)^{-1} \delta v, \end{aligned}$$

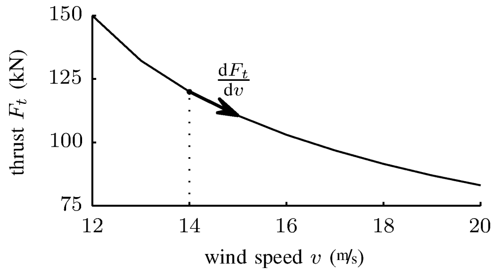

Fig. 3. Steady-state values of rotor thrust F_t as a function of wind speed v, indicating the gradient of the steady-state curve.

where $\mathrm{d}P$ denotes the total derivative of P, see [17], and the partial derivatives are evaluated along the equilibrium trajectory. The variation of pitch to maintain rated power yields a thrust curve with the gradient $\frac{\mathrm{d}F_t}{\mathrm{d}v}$ derived as:

$$\begin{aligned} \mathrm{d}F_t &:= \frac{\partial F_t}{\partial v}\,\delta v + \frac{\partial F_t}{\partial \beta}\,\delta\beta, \\ &= \left(\frac{\partial F_t}{\partial v} - \frac{\partial F_t}{\partial \beta}\frac{\partial \tau}{\partial v}\left(\frac{\partial \tau}{\partial \beta}\right)^{-1}\right)\delta v, \\ \implies \frac{\mathrm{d}F_t}{\mathrm{d}v} &= \frac{\partial F_t}{\partial v} - \frac{\partial F_t}{\partial \beta}\frac{\partial \tau}{\partial v}\left(\frac{\partial \tau}{\partial \beta}\right)^{-1}. \end{aligned} \tag{2}$$

It is clear from Figure 3 that $\frac{\mathrm{d}F_t}{\mathrm{d}v} < 0$ for all v above rated, a condition that is necessarily true for all conventional pitch-to-feather wind turbines [4].

In normal operation at above rated wind speed, the turbine nacelle will move forwards and backwards. When the turbine is moving forwards, the rotor sees a slightly higher relative wind speed and, if the generator speed controller is faster than the motion, the blades are pitched to prevent the generator speed from growing. As $\frac{\mathrm{d}F_t}{\mathrm{d}v} < 0$, this reduces the rotor thrust, thereby causing the nacelle to move further forwards. The converse is true when the nacelle is moving backwards. This problem is known in control circles within the wind turbine community as the '*negative damping problem*' [16].

The problem can be viewed analytically by considering a linearised model of (1), with an example pole-zero map shown in Figure 4. For commercial reasons, the parameters are not specific to a given turbine or floating foundation, rather certain quantities such as natural frequencies are selected to be in the vicinity of existing published works [14], [8] and efficiency tables from [10]. It is well-known in control theory that the closed-loop poles of a system migrate towards the open-loop zeros as the feedback gain is increased. On the basis of the pole-zero configuration in Figure 4 it becomes clear that as the feedback gain is increased the nacelle fore-aft oscillation becomes less damped, whilst the generator speed tracking improves. In the case where the zeros are in the right half plane, which for the model visualized in Figure 4 is true only for the tower zeros, the frequencies provide bandwidth limits on the pitch to generator speed loop [18]. In this example the control system bandwidth must be smaller than the frequency of the

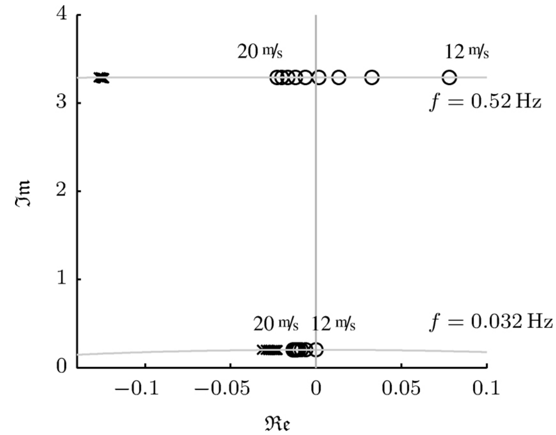

Fig. 4. Poles and zeros in the dynamics from pitch to generator speed as a function of wind speed due to the tower bending mode (top) and the platform tilt mode (bottom). Only the upper half of the complex plane is shown.

tower to avoid instability.

The phenomenon of negative damping is in no way unique to floating turbines. However, in turbines with fixed foundations, it is less of a problem because the lowest frequency eigenmode is that of the first tower fore-aft mode. The generator speed controller can be designed to have a low gain at or above this frequency with a higher gain for lower frequencies. This implies that the generator speed can not reject disturbances at or above the lowest fore-aft mode. Proportional-integral (PI) controllers are commonplace with a variety of gain-scheduling schemes proposed to address the parameter varying system nature [12]. In other words, the speed controller is designed to ensure tracking of signals lower than the tower fore-aft natural frequency. This results in a generator speed response with a maximum over-shoot of less than the generator speed limit in most operational cases [4, p. 213]. With floating turbines, the lowest natural frequency is that of the platform tilt resonance and this is typically an order of magnitude lower than that of the tower [5]. If one were to simply detune the generator speed controller such that the generator speed only tracks signals lower than the platform natural frequency, the generator would regularly exceed its over-speed limit in normal operation. This is illustrated in Figure 5 for the example turbine in this paper. A more advanced solution is therefore necessary.

IV. Control of tilt oscillations

A. Passive solutions

Typically in structural design, one way to reduce an oscillation is to include a tuned mass damper and indeed these have been considered and are implemented in some onshore wind turbines [20]. However, tuned resonators are at their most effective for sharp peaks whilst the fore-aft resonant peak is broad and so they are shown to have only limited benefit [20]. It may also be an option to add hydrodynamic damping to the motion of the platform in the water although this may represent a significant cost.

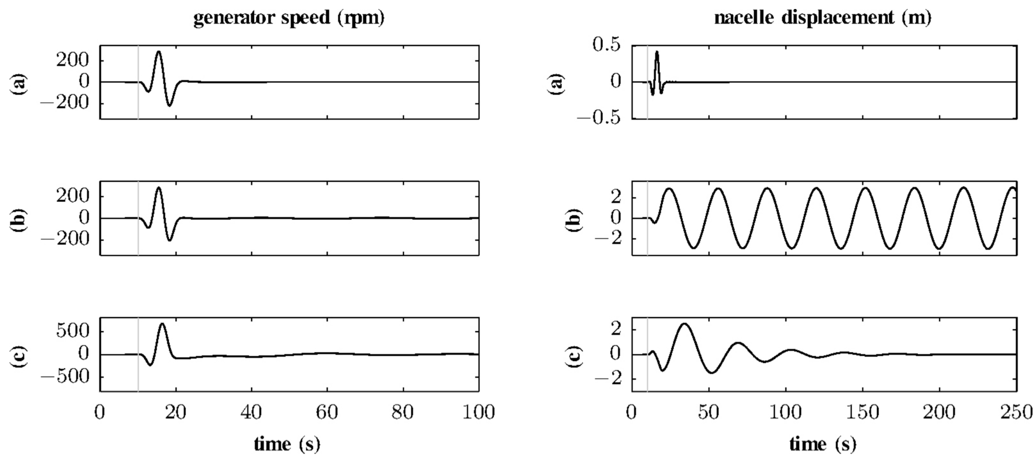

Fig. 5. Generator speed (left) and nacelle displacement (right) in response to an IEC class III extreme operating gust initiated at $t = 10\,\mathrm{s}$ ($\bar{v} = 12\,\mathrm{m/s}$). Cases: (a) on-shore turbine, conventional controller, (b) floating turbine, conventional controller. (c) floating turbine, reduced bandwidth controller. Note the different scales. The bandwidth of the conventional controller is $f_{\mathrm{bw}} = 0.22\,\mathrm{Hz}$ whilst that of the reduced bandwidth controller is $f_{\mathrm{bw}} = 0.02\,\mathrm{Hz}$.

B. Active solutions

Having briefly detailed passive solutions, we now turn our attention to active alternatives using blade pitch actuators as the control input. We constrain our review to collective blade pitch strategies as the problem concerns the thrust on the turbine as opposed to its out of plane moments. Benefits from individual blade pitch algorithms are typically seen in side-side tower motions and blade loads and are similar for onshore and offshore turbines, although the interested reader can discover more in [9].

As discussed in Section III-B, poor generator speed tracking results in frequent shutdowns resulting from the generator speed reaching its limit. One way to circumvent this problem is to detune the existing PI controller on generator speed such that the bandwidth is lower than the natural frequency of the platform; and also reduce the rated generator speed, see [22]. This means that larger deviations of generator speed can be tolerated without shutdown, although a reduction in the power produced and an increase in its standard deviation is implied and therefore this solution is neither economically viable nor appreciated by the generating board.

A number of proposed solutions exploit information about the tower fore-aft motion. In the simplest form, this purely involves appending the pitch demand from the generator speed controller with a gain multiplied by the nacelle fore-aft velocity, an approach sometimes referred to as '*parallel compensation*', [20] see Figure 6. To analyse this strategy, consider the inner transfer function, labeled $G(s)$ on Figure 6, and how it evolves with increasing velocity feedback gain, k_t, see Figure 7.

From Figure 7 it is clear that an increasing gain moves the platform and nacelle poles away from their respective zeros. This leads to poorer generator speed tracking in the frequency region of the fore-aft motions, but an increased damping of nacelle oscillation.

Specific versions of this solution are discussed in a number of sources under a variety of guises. In [16], the gain k_t is chosen such that the generator speed loop does not respond at all to deviations resulting from motion of the nacelle, whilst [22] discusses the option to have both a higher and lower gain to change the priority of generator speed tracking and nacelle velocity damping.

Another way to design such a controller would be to consider the single input two output system mapping collective blade pitch angle to generator speed and nacelle

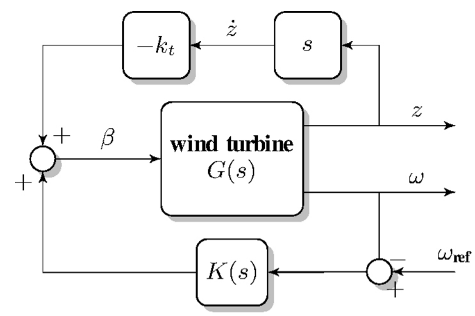

Fig. 6. Parallel modification to generator speed controller

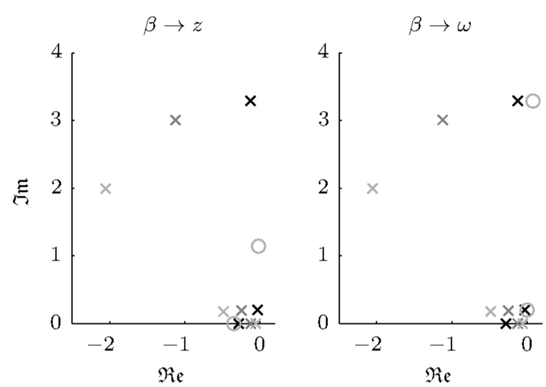

Fig. 7. Pole zero maps showing the effect of velocity feedback on the open-loop system ($k_t = 0$ (black), $k_t = 5$ (gray), $k_t = 10$ (light gray)). Left figure: pitch to nacelle displacement. Right figure: pitch to generator speed.

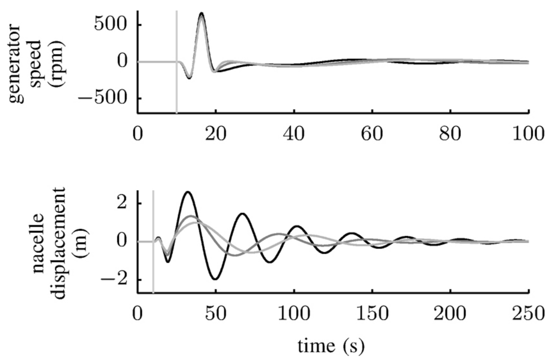

Fig. 8. Generator speed (top) and nacelle displacement (bottom) in response to an IEC class III extreme operating gust initiated at $t = 10\,\mathrm{s}$ ($\bar{v} = 12\,\mathrm{m/s}$). Velocity feedback with $k_t = 0$ (black), $k_t = 5$ (gray) and $k_t = 10$ (light gray).

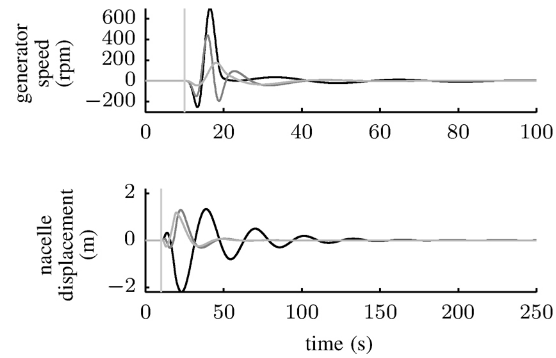

Fig. 9. Generator speed (top) and nacelle displacement (bottom) in response to an IEC class III extreme operating gust initiated at $t = 10\,\mathrm{s}$ ($\bar{v} = 12\,\mathrm{m/s}$). LQR with control weights: $R = 10^{12}$ (black), $R = 10^3$ (gray) and $R = 10^0$ (light gray).

velocity. Clearly, a single control input cannot independently control two outputs. Using a squaring down approach [19], the control objective can be made to design a controller to minimize the output $\tilde{y} := c_1\omega + c_2\dot{z}$ where $c_1, c_2 \in \mathbb{R}$ are weighting constants. Notice that $\{c_1, c_2\}$ can now be selected such that the loop $\beta \mapsto \tilde{y}$ has zeros far into the left half plane (i.e. minimum-phase), this does not imply that the performance constraints on generator speed tracking can be circumvented. The loop $\beta \mapsto \omega$ still has the same zeros, and these still define the restrictions on the closed-loop [19].

Published solutions taking the parallel compensation form vary with regards to how the nacelle velocity is obtained. In [22] an accelerometer is fitted to the nacelle and the signal integrated whilst in [14] the velocity is inferred from information about the platform tilt angle. A range of filters for dealing with these real sensors have been proposed, see for example [16].

In order to give an indication of the performance restrictions implied by the sensors, we consider a full state feedback solution in the form of a linear quadratic regulator with integral action. The cost matrices Q and R were selected to reflect both the scale of the signals and the relative importance of a lack of deviations in their respective values. Notice that we now have a tall transfer function, and that we cannot hope to control each of the measured outputs independently at all times [19, pp. 796–800]. The LQR design essentially results in a form of soft-sharing control [19, pp. 796–800]. An interesting observation is that the MIMO system does not have any transmission zeros, but that this does not imply an absence of performance limitations. Non-minimum phase zeros still lie between the pitch and generator speed, thus irrespective of information from extra sensors, the performance limitations due to that zero still remain [18], [19]. That is not to say that extra information cannot give improved performance, it simply says that the bandwidth of the generator speed response is still limited by the frequency of the right half plane zeros. The closed-loop responses of the LQR solution are shown in Figure 9, with the conclusion being that increased measurement will only lead to a marginal increase in performance. This suggests that an additional control degree of freedom may be required, in the form of an additional actuator.

V. Future turbines

From the analysis of the linearised dynamics in Section III-B, it is clear that the position of the platform zeros defines the difficulty of the problem and the limit of the efficacy of the solution. In order to consider quantitatively what affects the position of these zeros, and thereby examine how the control problem is likely to evolve, we consider a simplified model linearised about $\omega = \omega_r$, $\dot{z} = 0$, $z = \bar{z}$, $\beta = \bar{\beta}$, where $\bar{\cdot}$ denotes the equilibrium value, assuming a stiff tower for brevity:

$$\ddot{\delta z}_1 + 4\pi\zeta_1 f_1 \dot{\delta z}_1 + 4\pi^2 f_1^2 \delta z_1 = \frac{\partial F_t}{\partial \omega}\,\delta\omega - \frac{\partial F_t}{\partial v}\,a_1 \dot{\delta z}_1 + \frac{\partial F_t}{\partial \beta}\,\delta\beta,$$

$$I\dot{\delta\omega} + \alpha\delta\omega = \frac{\partial \tau}{\partial \omega}\,\delta\omega + \frac{\partial \tau}{\partial v}\,a_1\dot{\delta z}_1 + \frac{\partial \tau}{\partial \beta}\,\delta\beta,$$

where $\delta z_1, \delta\omega, \delta\beta$ represent the deviation of z_1, ω, β from their equilibrium values, respectively. By taking Laplace transforms, substituting for δz_1 and using (2), the following transfer function from pitch to generator speed can be obtained[2]:

$$\frac{\Omega(s}{B\ \ s} \quad \frac{\frac{\partial\tau}{\partial\beta}s^2 \quad \frac{\partial\tau}{\partial\beta}\left(\ \ \pi\zeta_1 f_1 \quad a_1\frac{\mathrm{d}F_t}{\mathrm{d}v}\right)s \quad \pi^2 f_1^2\frac{\partial\tau}{\partial\beta}}{\left(s^2 \quad \left(\ \ \pi\zeta_1 f_1 \quad a_1\frac{\partial F_t}{\partial v}\right)s \quad \pi^2 f_1^2\right)\left(Is \quad \alpha - \frac{\partial\tau}{\partial\omega}\right) - s\frac{\partial\tau}{\partial v}\frac{\partial F_t}{\partial\omega}},$$

where Ω and B denote the Laplace transforms of $\delta\omega$ and $\delta\beta$ respectively.

Typically, it is fair to assume that:

$$16\pi^2 f_1^2 \gg \left(4\pi\zeta_1 f_1 + a_1\frac{\mathrm{d}F_t}{\mathrm{d}v}\right)^2, \tag{3}$$

and this is reflected by the complex nature of the platform zeros and the fact that their frequency is almost exactly the

[2]The algebraic manipulation is omitted due to page restrictions.

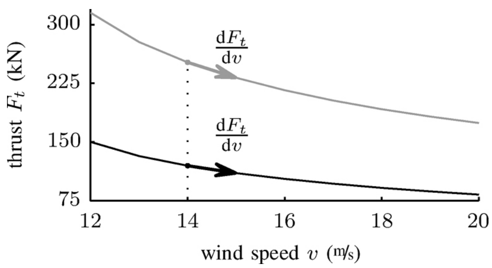

Fig. 10. Figure showing the effect on the thrust curve of increasing rotor size and power to maintain a constant rated wind speed. Light grey is the larger turbine with the greater nameplate power.

platform natural frequency, see Figure 4. In this case, the zeros of the transfer function can be well approximated by

$$z \approx -\frac{1}{2}\left(4\pi\zeta_1 f_1 + a_1 \frac{\mathrm{d}F_t}{\mathrm{d}v}\right) \pm j2\pi f_1.$$

Should these zeros be in the right half plane, they provide hard limits on the bandwidth of the full load controller. Even in the left half plane, their position determines the amount of fore-aft oscillation that must be tolerated in order to achieve a given performance level of generator speed tracking. Clearly, increasing the damping ζ_1 lessens the problem whilst increasing the gradient of the steady state thrust curve makes it worse.

From Figure 3, it can be seen that the curve is less steep at higher wind speeds and therefore the problem is typically most pronounced in just above rated condtions. We now consider how the problem will evolve for the next generation of wind turbines, by considering a simplified relationship between C_Q and C_T via the axial induction factor [4]. Figure 10 shows the effect on the thrust curve of increasing rotor size and power to maintain a constant rated wind speed.

- Increased rotor size. Larger rotors typically lead to a better investment because they can extract more power for the same tower height [3], but this will make the problem more severe because the magnitude of the gradient $\frac{\mathrm{d}F_t}{\mathrm{d}v}$ increases, see Figure 10, thereby increasing the real part of the platform zeros.
- Taller towers. Taller towers tend to give the turbine access to less turbulent air and a smoother resource due to the shape of the boundary layer [3]. However, these taller towers are likely to lead to lower natural frequencies of both tower and platform. This creates a more restrictive problem because it is these natural frequencies that define the bandwidth limitations on the generator speed controller.

VI. Conclusions

In this tutorial paper, we have described the some of the control issues associated with designing a floating wind turbine. Solutions from the literature have been discussed and their relative efficacy demonstrated on a simple numerical example. These solutions all relate to using the collective blade pitch angle as the control input. Additional control inputs may prove advantageous, a promising option being active mass dampers. Currently, they are not considered due to their cost and need for infeasible mechanical travel [14]. In the final section of the paper some insight is given into how the problem is likely to scale to the next generation of larger, taller and more efficient wind turbine.

Acknowledgements

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