




Article

# Full- & Reduced-Order State-Space Modeling of Wind Turbine Systems with Permanent Magnet Synchronous Generator

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**Abstract:** Full-order state-space models represent the starting point for the development of advanced control methods for wind turbine systems (WTSs). Regarding existing control-oriented WTS models, two research gaps must be noted: (i) There exists *no* full-order WTS model in form of *one* overall ordinary differential equation that considers all dynamical effects which significantly influence the electrical power output; (ii) all existing reduced-order WTS models are subject to rather arbitrary simplifications and are *not* validated against a full-order model. Therefore, in this paper, two full-order nonlinear state-space models (of 11th and 9th-order in the  $(a, b, c)$ - and  $(d, q)$ -reference frame, resp.) for variable-speed variable-pitch permanent magnet synchronous generator WTSs are derived. The full-order models cover all relevant dynamical effects with significant impact on the system’s power output, including the switching behavior of the power electronic devices. Based on the full-order models, by a step-by-step model reduction procedure, two reduced-order WTS models are deduced: A non-switching (averaging) 7th-order WTS model and a non-switching 3rd-order WTS model. Comparative simulation results reveal that all models capture the dominant system dynamics properly. The full-order models allow for a detailed analysis covering the high frequency oscillations in the instantaneous power output due to the switching in the power converters. The reduced-order models provide a time-averaged instantaneous power output (which still correctly reflects the energy produced by the WTS) and come with a drastically reduced complexity making those models appropriate for large-scale power grid controller design.

**Keywords:** Wind turbine system, wind energy conversion system, dynamic modeling, control design model, control system, operation management, switching behavior, nonlinear dynamics, model reduction, comparative simulation

## Notation

$\mathbb{N}, \mathbb{R}, \mathbb{C}$ : natural, real, complex numbers.  $\mathbf{x} := (x_1, \dots, x_n)^\top \in \mathbb{R}^n$ : column vector,  $n \in \mathbb{N}$  (where “ $\top$ ” and “:=” mean “transposed” and “is defined as”, resp.).  $\mathbf{0}_n \in \mathbb{R}^n$ : zero vector.  $\mathbf{1}_n \in \mathbb{R}^n$ : unity vector.  $\mathbf{A} \in \mathbb{R}^{n \times n}$ : (square) matrix with  $n$  rows and columns. Scalar  $\text{sat}_a^b: \mathbb{R} \rightarrow [a, b]$  and vector  $\text{sat}_{\hat{u}}: \mathbb{R}^n \rightarrow \{\mathbf{x} \in \mathbb{R}^n \mid \|\mathbf{x}\| \leq \hat{u}\}$  saturation functions, for  $b > a$  an  $\hat{u} > 0$ , given by

$$\text{sat}_a^b(x) := \begin{cases} b & , x \geq b \\ x & , a < x < b \\ a & , x \leq a. \end{cases} \quad \text{and} \quad \text{sat}_{\hat{u}}(\mathbf{x}) := \begin{cases} \hat{u} \frac{\mathbf{x}}{\|\mathbf{x}\|} & , \|\mathbf{x}\| \geq \hat{u} \\ \mathbf{x} & , \|\mathbf{x}\| < \hat{u}. \end{cases} \quad (1)$$

Transition function  $f_{\hat{a},\Delta}: \mathbb{R} \rightarrow [0, 1]$  for  $\hat{a} \in \mathbb{R}$  and  $\Delta > 0$ , given by

$$f_{\hat{a},\Delta}(x) := \begin{cases} 0 & , x \geq \hat{a} \\ \frac{-x+\hat{a}}{\Delta} & , \hat{a} - \Delta \leq x < \hat{a} \\ 1 & , x < \hat{a} - \Delta \end{cases} \quad (2)$$

$\mathcal{C}(I; Y)$  space of continuous functions mapping  $I \rightarrow Y$ . Two different reference frames (coordinate systems) will be considered (i) three-phase  $(a, b, c)$ -reference frame  $\mathbf{x}^{abc} := (x^a, x^b, x^c)^\top \in \mathbb{R}^3$  and (ii) synchronously rotating  $(d, q)$ -reference frame with  $\mathbf{x}^{dq} = (x^d, x^q)^\top \in \mathbb{R}^2$  where  $\mathbf{x}^{abc}$  and  $\mathbf{x}^{dq}$  are related by the (reduced) Clarke-Park-transformation for  $\kappa \in \{\frac{2}{3}, \sqrt{\frac{2}{3}}\}$  as follows [1, App. A.5]

$$\mathbf{x}^{dq} = \kappa \underbrace{\begin{bmatrix} \cos(\phi_p) & \cos(\phi_p - \frac{2\pi}{3}) & \cos(\phi_p - \frac{4\pi}{3}) \\ -\sin(\phi_p) & -\sin(\phi_p - \frac{2\pi}{3}) & -\sin(\phi_p - \frac{4\pi}{3}) \end{bmatrix}}_{=: T_{cp}(\phi_p) \in \mathbb{R}^{2 \times 3}} \mathbf{x}^{abc} \quad \text{with} \quad T_{cp}(\phi_p)^{-1} := \frac{2}{3\kappa} \begin{bmatrix} \cos(\phi_p) & -\sin(\phi_p) \\ \cos(\phi_p - \frac{2\pi}{3}) & -\sin(\phi_p - \frac{2\pi}{3}) \\ \cos(\phi_p - \frac{4\pi}{3}) & -\sin(\phi_p - \frac{4\pi}{3}) \end{bmatrix} \in \mathbb{R}^{3 \times 2}. \quad (3)$$

Rotation matrices (counter-clock wise by  $\frac{\pi}{2}$ ), given by

$$J := \begin{bmatrix} 0 & -1 \\ 1 & 0 \end{bmatrix} \quad \text{and} \quad J_\Sigma := \frac{1}{\sqrt{3}} \begin{bmatrix} 0 & -1 & 1 \\ 1 & 0 & -1 \\ -1 & 1 & 0 \end{bmatrix} \quad (4)$$

## 1. Introduction

Sustainable electrical energy is a major concern of modern society. Wind power represents a renewable and carbon-free energy resource which can be made available on a large scale by wind turbine systems (WTSs). During the last two decades, electricity generation by wind power experienced a vast expansion leading to a global cumulative installed generation capacity of about 486.8 GW in 2016 [2]. A WTS is a complex system, which covers multiple physical domains, as aerodynamical, mechanical and electrical subsystems. Due to this complexity, studies which have to incorporate the behavior of WTSs mostly apply model-based methods, where the starting point is the derivation of a suitable WTS model.

The history of WTS modeling goes back to the end of the 1970s, where first wind power impact studies were undertaken [3]. In the 1980s, first WTS models for fixed-speed WTSs were presented [4] and applied to large scale transient stability computer programs [5]. Since that time, there have been numerous models for induction generator (IG) WTSs [6–10], doubly fed induction generator (DFIG) WTSs [11–19] and permanent magnet synchronous generator (PMSG) WTSs [5,8,9,18,20–27]. The above-mentioned models are developed for an application in numerical simulations. In general, these models consist of a number of differential and algebraic equations with no specification of control inputs, system states, and control outputs.

Control-oriented models represent an indispensable basis for the development of advanced (state-space) control methods for WTSs [28–37]. In contrast to the above-mentioned simulation models, control-oriented models require a specification of control inputs, system states and control outputs. Moreover, a representation of the model as system of explicit differential equations is crucial. Major goal is to obtain *one* overall (vector-valued) ordinary differential equation describing the *whole* system. Thus, the available WTSs simulation models can in general not be used for the development of state-space control methods or an overall stability analysis. Due to the high system complexity and significant nonlinearities in WTSs, the conversion from a WTS simulation model to a WTS state-space model is a challenging task.

The most notable representation of control-oriented models is the state-space representation. In literature, there has been a significant number of publications addressing the derivation of full-order state-space models for WTSs. In [38], a ninth-order linear state space model of a PMSG wind turbine is derived. The model considers the mechanical subsystem, the PMSG, and the electrical power

converter. However, essential system dynamics and intrinsic nonlinearities, e.g., pitch angle or nonlinear machine dynamics and switching in the power electronics, are neglected. The authors of [28] present a seventh-order nonlinear state-space model for IG variable-speed WTSs. Again, pitch system dynamics and switching behavior of the power converters are not considered. In [39], a 16th-order nonlinear state-space model for a PMSG WTS is developed. The model describes the closed-loop WTS and thus combines the modeling of the physics and control schemes. Eight of 16 state variables are intermediate variables which are used to model the machine-side and grid-side power converter controllers. Nevertheless, crucial dynamics of the physical system, such as the DC link dynamics (interlinking energy buffer between machine and grid side), are not considered in this model. In [40], a differential-algebraic state-space model of DFIG WTSs is presented. The model is not formulated as system of explicit differential equations which hampers its application to control design. In [41], non-switching discrete-time models for DFIG and PMSG WTSs for real-time power-hardware-in-the-loop emulations are presented. The subsystem models of electrical subsystems are formulated in state-space representation. However, there is no model for the overall WTS. In [37], the authors present a model for PMSG wind turbines, which incorporates generator dynamics, power converter dynamics (including switching) and grid dynamics. However, turbine dynamics and pitch system dynamics are not considered. Generator and grid side are modeled in state-space form, but the authors do not provide an overall full-order state space model of the whole WTS.

Besides full-order models, there have been various publications addressing reduced-order models of WTSs. Reduced-order models also describe the behavior of an entire WTS, e.g., in terms of power output. However, one or more subsystems of the WTS are simplified or even neglected which leads, compared to full-order WTS models, to a reduced order of the model which might not be capable of reduplicating the dominant and crucial dynamics of the original system. In [29], a state-space model of variable-speed variable-pitch DFIG WTSs is proposed and used for model predictive control design. The electrical part is modeled as a first-order system which represents a significant (over-)simplification. In [31], a fifth-order linear state-space model for variable-speed variable-pitch WTSs is presented. The model describes the closed-loop WTS. Thus, some states do not represent physical states but controller states describing, e.g., a PI controller. This model completely neglects the electrical components of the WTS. In [34], continuous-time and discrete-time state-space models of the electrical power conversion system of a PMSG WTS are developed but the mechanical part of the WTS is not covered. In [30], a linear state-space model of WTSs is presented. The linear model describes the mechanics of the system including pitch angle dynamics. However, the electrical part of the WTS is neglected and the intrinsic nonlinearities in WTSs are not included. In [42], a linear state-space model for WTSs with focus on aerodynamics and mechanics is presented. In [32], a simplified nonlinear third-order state-space model only for the mechanics of a WTS is presented. In [33], a fifth-order nonlinear state-space model for the DFIG of a WTS is presented. However, the power converter, the pitch system dynamics and the mechanical part of the system are not considered. In [35], a second-order linear state-space model for the drive system of PMSG WTSs is developed neglecting the nonlinear behavior of real WTS.

Regarding the literature review above on control-oriented modeling of WTSs, two research gaps must be noted: (i) There exists no full-order model (in form of *one* overall ordinary differential equation) that considers all dynamical effects which significantly influence the WTS's power output and (ii) all existing reduced-order WTS models are subject to rather arbitrary simplifications of one or more subsystems. To the best of the authors' knowledge, there does *not* exist a publication that specifically (a) addresses a detailed WTS full-order state-space model derivation, (b) provides a structured and physically motivated model order reduction technique based on the derived full-order model, (c) discusses the effects of the simplifications on the validity of the reduced-order models and (d) validates the reduced-order models against the derived and underlying full-order model.

This publication tries to fill the research gaps (i) and (ii) stated above by introducing state-space models for the dynamic relation between the input wind speed and produced electrical output in variable-speed variable-pitch PMSG WTSs. The paper comes with five main contributions: (a) The

derivation of two full-order state-space models for variable-speed PMSG WTSs, which capture all dynamical effects (including the switching behavior) that significantly affect the power output of the system. For sake of comprehensibility and consistency, the models are developed step by step by recapitulating the basic physical relations of variable-speed variable-pitch PMSG WTSs; (b) The structured derivation of two reduced-order models for variable-speed variable-pitch PMSG WTSs based on a stepwise and physically motivated model reduction of the proposed full-order models from (a); (c) the motivating (simplifying) assumptions for the presented model reduction are discussed and (d) the derived full-order and reduced-order models are compared by numerical simulations and the validity of the reduced-order models (in the sense of capturing the dominant system behavior) is shown and (e) advantages and drawbacks concerning model accuracy and computational complexity of each proposed model are discussed.

The remainder of this paper is organized as follows. In Section 2, the WTS under consideration is briefly described. In Section 3, a full-order state-space model of the physics of the WTS is derived which is then formulated in the three-phase  $(a, b, c)$ - and synchronously rotating  $(d, q)$ -reference frame. In order to simulate the closed-loop behavior of the WTS model, realistic control schemes are required. Thus, in Section 4, control and operation management systems are also presented. The control systems address wind speeds between cut-in and cut-out wind speed (regime II + III) and consider practical constraints such as saturation and integral windup. In Section 5, two reduced-order models are deduced from the proposed full-order model by a physically motivated model reduction method. In Section 6, the performance of the full-order model and the reduced-order models is compared through numerical simulations. For the simulation of the closed-loop models, the control and operation management of Section 4 are applied. Finally, Section 7 provides a concluding overview and discussion of the presented models and results.

## 2. System Description

The considered system is shown in Fig. 1 and represents a state of the art WTS: a variable-speed, variable-pitch, three bladed, horizontal axis, lift turbine in up-wind position. Only a single WTS is considered neglecting aerodynamical (e.g. wake effect) or electrical interactions between multiple turbines. Regarding the type of generator (in our case PMSG), the WTS might or might not comprise a gear between turbine and generator. The generator feeds the converted power through a full-scale back-to-back converter and grid-side filter to the point of common coupling (PCC). The transformer is not explicitly modelled, but could easily be added. The grid is assumed to be symmetrical and stiff, so that the grid-side voltage source inverter (VSI) operates in grid-feeding mode.

Depending on the actual wind speed  $v_w$  (in  $\frac{m}{s}$ ), the WTS will operate in one of the four regimes of operation (see Fig. 2). For too less or too much wind (i.e.  $v_w < v_{w,cut-in}$  in regime I and  $v_{w,cut-out} \leq v_w$  in regime IV, resp.), the WTS is (usually<sup>1</sup>) at standstill or in idle mode: The turbine angular velocity is zero, i.e.  $\omega_t = 0 \frac{rad}{s}$  or the machine torque is zero, i.e.  $m_m = 0 N m$ , hence the turbine (output) power is  $p_t = 0 W$ . In regime II, the wind speed is below the nominal wind speed  $v_{w,rated}$  (in  $\frac{m}{s}$ ) but at least the (minimum) cut-in wind speed  $v_{w,cut-in}$  (in  $\frac{m}{s}$ ). Due to the time-varying nature of the wind speed  $v_w(\cdot)$ , the turbine output power will vary between zero and nominal power  $p_{t,rated}$  (in W), i.e.  $0 \leq p_t < p_{t,rated}$ . The goal is to extract as much wind power as possible, i.e. *maximum power point tracking* (MPPT) which is achieved by an underlying speed controller (see Sect. 4.1.2). In regime III, the wind speed is at least the nominal wind speed but lower than the (maximum) cut-out wind speed  $v_{w,cut-out}$  (in  $\frac{m}{s}$ ), i.e.  $v_{w,rated} \leq v_w < v_{w,cut-out}$ , and the torque  $m_m$  of the electrical machine is kept constant at its nominal value (by constant feedforward torque control) and pitch control. The nominal output power is generated, i.e.  $p_t = p_{t,rated}$  (see Sect. 4.1.3).

<sup>1</sup> Some companies use more sophisticated control methods for high winds, e.g. see patent [43] of REpower Systems for a reduced power production above  $v_{w,cut-out}$  instead of a shut down.

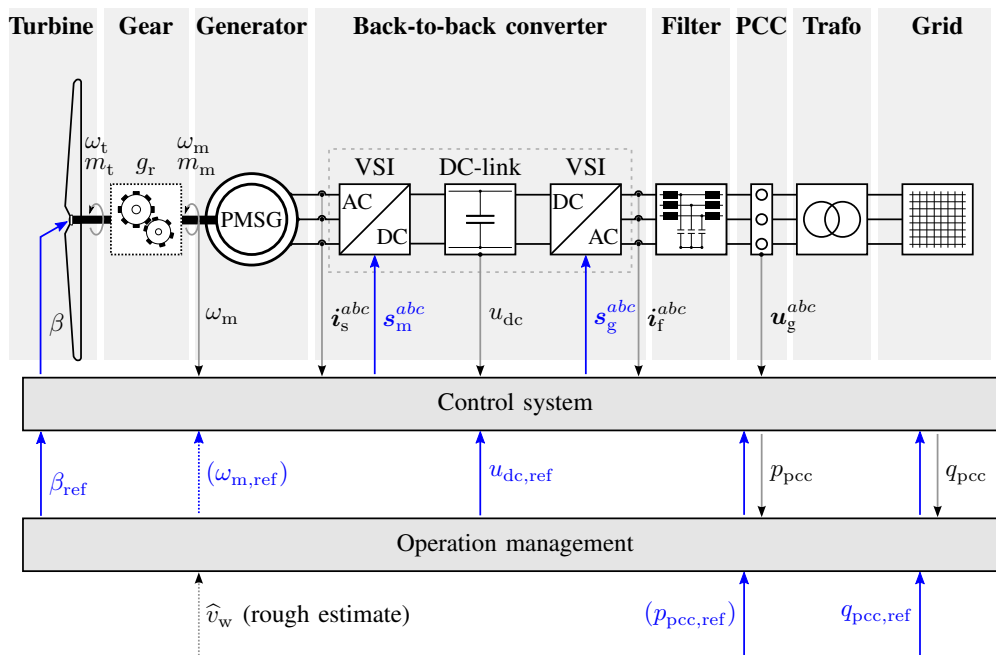


Figure 1. Overview of the core components of a WTS.

In the following sections, the different hardware components illustrated in Fig. 1 (such as turbine, gear, generator, back-to-back converter, filter, PCC) and physical quantities (e.g.  $\omega_m$ ,  $s_m^{abc}$ ,  $u_{dc}$ ) are described and introduced.

### 3. Full-Order Models

In this section, the detailed derivation of the complete physical model is presented. The modeling approach bases on the simulation model of [44]. It is extended by the pitch system dynamics, an explicit representation of the switching behavior and finally the overall reformulation as one state-space system model in the form of *one* vector-valued ordinary differential equation.

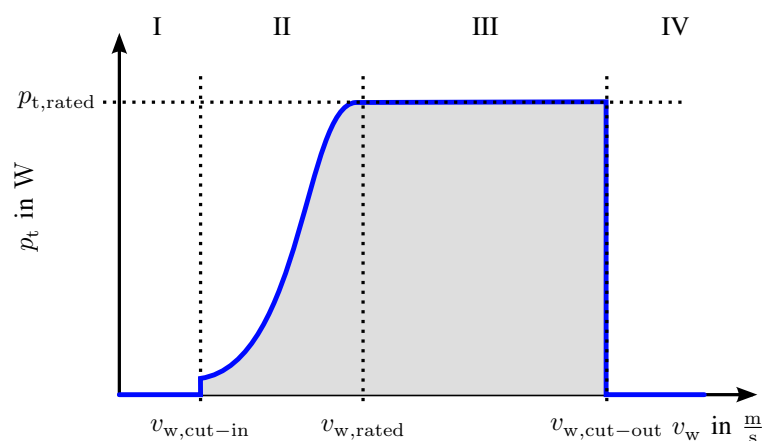


Figure 2. Operation regimes I to IV of a WTS.

### 3.1. Aerodynamics, turbine torque and drive train

Aerodynamical conversion is achieved by the three rotor blades producing a turbine torque which accelerates the drive train and generator.

#### 3.1.1. Aerodynamics

The turbine (rotor with three blades) converts part of the kinetic wind energy into rotational energy. The wind power  $p_w(v_w) := \frac{1}{2} \rho \pi r_t^2 v_w^3$  depends on air density  $\rho$  (in  $\frac{\text{kg}}{\text{m}^3}$ ), rotor radius  $r_t$  (in m), and wind speed  $v_w$  (in  $\frac{\text{m}}{\text{s}}$ ). The extractable turbine power is limited by the Betz limit  $c_{p,\text{Betz}} := 16/27 \approx 0.5926$  [45] and is given by

$$p_t(v_w, \omega_t, \beta) = c_p(v_w, \omega_t, \beta) p_w(v_w) \leq c_{p,\text{Betz}} p_w(v_w). \quad (5)$$

The power coefficient  $c_p(v_w, \omega_t, \beta) = c_p(\lambda, \beta)$  must be determined for each WTS and is a function of wind speed  $v_w$ , turbine angular velocity  $\omega_t$  (in  $\frac{\text{rad}}{\text{s}}$ ), and pitch angle  $\beta$  (in  $^\circ$ ) or of tip speed ratio  $\lambda := \lambda(v_w, \omega_t) := \frac{r_t \omega_t}{v_w}$ . Both, tip speed ratio  $\lambda$  (or  $\omega_t$ ) and pitch angle  $\beta$ , have a direct influence on the amount of power, the WTS can extract from the wind. Usually, the power coefficient  $c_p(\lambda, \beta)$  is approximated by the following function  $c_p: \mathcal{D} \rightarrow \mathbb{R}_{\geq 0}$  [46, (2.38)]

$$c_p(\lambda, \beta) := c_1 [c_2 f(\lambda, \beta) - c_3 \beta - c_4 \beta^k - c_5] e^{-c_6 f(\lambda, \beta)} \quad (6)$$

where  $\mathcal{D} := \{ (\lambda, \beta) \in \mathbb{R}_{>0} \times \mathbb{R}_{\geq 0} \mid c_p(\lambda, \beta) \geq 0 \}$ . The constants  $c_1, \dots, c_6 > 0$ , the exponent  $k \geq 0$  and the continuously differentiable function  $f: \mathcal{D} \rightarrow \mathbb{R}$  can be determined from measurements or by aerodynamic simulations. Two exemplary power coefficient approximations for two different 2 MW WTSs are as follows [47, Chap. 12]

- Power coefficient *without* pitch control system (i.e.,  $\beta = 0$ )  $c_{p,1}(\lambda, 0) := c_{p,1}(\lambda)$ :

$$c_{p,1}(\lambda) := \left[ 46.4 \cdot \left( \frac{1}{\lambda} - 0.01 \right) - 2.0 \right] e^{-15.6 \left( \frac{1}{\lambda} - 0.01 \right)}, \quad (7)$$

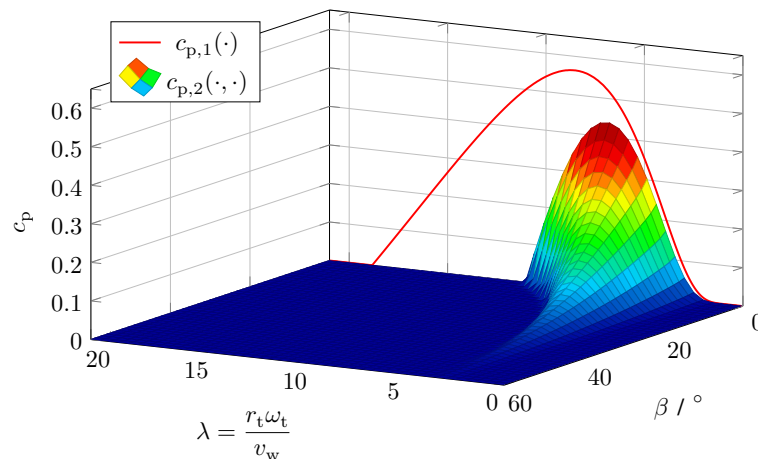
which has a global maximum at  $\lambda^* = 8.53$  with  $c_{p,1}^* := c_{p,1}(\lambda^*) = 0.564$ .

- Power coefficient *with* pitch control system (i.e.,  $\beta \geq 0$ ):

$$c_{p,2}(\lambda, \beta) := 0.73 \left[ 151 \left( \frac{1}{\lambda - 0.02\beta} - \frac{0.003}{\beta^3 + 1} \right) - 0.58\beta - 0.002\beta^{2.14} - 13.2 \right] \cdot \exp \left( -18.4 \left( \frac{1}{\lambda - 0.02\beta} - \frac{0.003}{\beta^3 + 1} \right) \right), \quad (8)$$

which, for  $\beta = \beta^* = 0$ , has its maximum at  $\lambda^* = 6.91$  with  $c_{p,2}^* := c_{p,2}(\lambda^*, \beta^*) = 0.441$ .

The graphs of  $c_{p,1}(\cdot)$  and  $c_{p,2}(\cdot, \cdot)$  are shown in Fig. 3. Both power coefficients are below the possible Betz limit of  $c_{p,\text{Betz}} = 16/27 \approx 0.59$ . The maximum value of  $c_{p,1}(\cdot)$  is larger than that of  $c_{p,2}(\cdot, \cdot)$ . This does not hold in general but is a characteristic feature of the two WTSs considered in [48, S. 9], [49], [50].



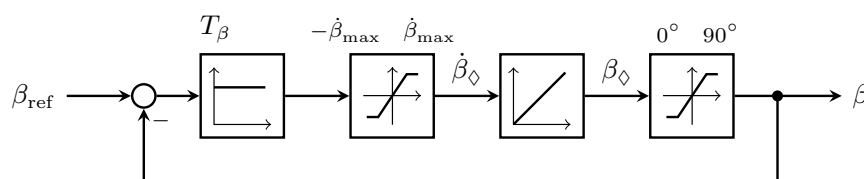
**Figure 3.** Graphs of the power coefficient approximations  $c_{p,1}(\cdot)$  and  $c_{p,2}(\cdot, \cdot)$  for a 2 MW WTS.

### 3.1.2. Pitch control system

The pitch system allows to control the pitch angle  $\beta$  (in  $^\circ$ ) to its specified reference  $\beta_{\text{ref}}$  (in  $^\circ$ ). The nonlinear dynamics of the pitch control system are approximated by<sup>2</sup>

$$\left. \begin{aligned} \frac{d}{dt} \beta_{\diamond}(t) &= \text{sat}_{-\dot{\beta}_{\max}}^{\dot{\beta}_{\max}} \left( \frac{1}{T_{\beta}} (-\beta(t) + \beta_{\text{ref}}(t)) \right), \\ \beta(t) &= \text{sat}_{0^\circ}^{90^\circ} (\beta_{\diamond}(t)), \end{aligned} \right\} \quad (9)$$

with unsaturated pitch angle  $\beta_{\diamond}$  (in  $^\circ$ ) and initial value  $\beta_{\diamond}(0) = \beta_{\diamond,0} \geq 0$  (in  $^\circ$ ), where  $\dot{\beta}_{\max} > 0$  (in  $\frac{^\circ}{s}$ ) and  $T_{\beta}$  (in s) are the maximally feasible change rate of the pitch angle and (approximated) pitch control system time constant, respectively (see Fig. 4). The underlying current, speed and position control dynamics are neglected (for details see e.g. [47, Sec. 11.2]). The overall approximated dynamics show the dynamic behavior of a first-order lag system where output and change rate of the state are saturated, respectively.



**Figure 4.** Block diagram of the approximated pitch system dynamics.

### 3.1.3. Turbine torque

The turbine converts the kinetic energy of the wind into rotational energy. If friction losses are neglected then, from turbine power  $p_t = m_t \omega_t$  with turbine torque  $m_t$  (in N m) and turbine angular velocity  $\omega_t$ , the turbine torque can directly be computed as follows

$$m_t(v_w, \lambda, \beta) \stackrel{(5),(6)}{:=} \frac{1}{2} \rho \pi r_t^3 v_w^2 \frac{c_p(\beta, \lambda)}{\lambda} = \frac{1}{2} \rho \pi r_t^2 v_w^3 \frac{c_p(v_w, \omega_t, \beta)}{\omega_t} =: m_t(v_w, \omega_t, \beta). \quad (10)$$

<sup>2</sup> A simplification of the pitch system dynamics given in [46, Sect. 2.3, 5.5].

The torque is a *nonlinear function* of pitch angle  $\beta$ , wind speed  $v_w$  and tip speed ratio  $\lambda$  or turbine angular velocity  $\omega_t$ .

**Remark 1.** The approximation (6) of the power coefficient  $c_p(\cdot, \cdot)$  does not allow for the simulation of the start-up of a WTS, since  $\lim_{\omega_t \rightarrow 0} m_t(v_w, \omega_t, \beta) = 0$  for all  $v_w > 0$  and  $\beta \geq 0$  [44]. The approximation (6) only yields physically meaningful results for  $\lambda > 0$ . At standstill, the accelerating torque would be zero.

### 3.1.4. Drive train

A gearbox transmits the mechanical turbine power via a shaft to the rotor of the generator. In modern WTSs, the turbine angular velocity  $\omega_t$  is significantly lower than the angular velocity  $\omega_m$  (in  $\frac{\text{rad}}{\text{s}}$ ) of the machine (generator). Therefore, a step-up gearbox with ratio  $g_r \gg 1$  is usually employed (exceptions are WTSs with “Direct Drive”, i.e.  $g_r = 1$ , where the generator is connected directly to the turbine rotor). For a *rigid* coupling, generator (machine) and turbine angular velocities are related by  $\omega_m = g_r \omega_t$ . Hence, denoting the machine torque by  $m_m$  (in Nm), turbine power  $p_t$  and turbine torque  $m_t$  are converted to the machine-side quantities as follows

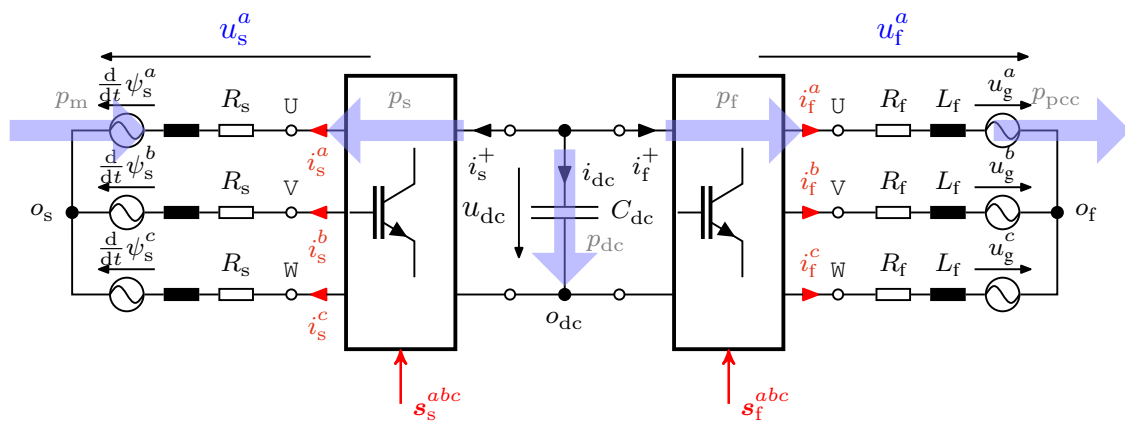
$$p_m = \omega_m m_m = g_r \omega_t \frac{m_t}{g_r} = p_t. \quad (11)$$

Moreover, the inertias  $\Theta_t$  and  $\Theta_m$  (both in  $\text{kg m}^2$ ) of turbine rotor (+hub) and machine rotor (+shaft) can be merged to the overall inertia  $\Theta := \frac{\Theta_t}{g_r^2} + \Theta_m$  of the drive train. For simplicity, turbine-side and machine-side friction and elasticity in the shaft are neglected (for more details on friction modeling & compensation, and elastic drive train modeling, see [47, Sect. 11.1.5 & Chap. 12] and [39,51]).

### 3.2. Electrical system in three-phase ( $a, b, c$ )-reference frame

In Fig. 5, the (simplified) electrical network of a WTS with PMSG or IG is depicted. The machine-side network (left) shows the stator windings with stator phase voltages  $\mathbf{u}_s^{abc} = (u_s^a, u_s^b, u_s^c)^\top$  (each in V), stator phase current  $\mathbf{i}_s^{abc} = (i_s^a, i_s^b, i_s^c)^\top$  (each in A), stator phase resistance  $R_s$  (in  $\Omega$ ), and stator flux linkage  $\boldsymbol{\psi}_s^{abc} = (\psi_s^a, \psi_s^b, \psi_s^c)^\top$  (each in Wb). The grid-side network (right) comprises filter and grid with filter phase voltages  $\mathbf{u}_f^{abc} = (u_f^a, u_f^b, u_f^c)^\top$  (each in V), filter phase currents  $\mathbf{i}_f^{abc} = (i_f^a, i_f^b, i_f^c)^\top$  (each in A), filter resistance  $R_f$  (in  $\Omega$ ), filter inductance  $L_f$  (in H) and the grid phase voltages  $\mathbf{u}_g^{abc} = (u_g^a, u_g^b, u_g^c)^\top$  (each in V). The (stepped-down) grid voltage  $\mathbf{u}_g^{abc}$  is measured at the PCC. The transmission ratio of the transformer (not shown in Fig. 5) is not explicitly modeled. Machine-side and grid-side converters exchange the stator power  $p_s$  (in W) and the filter power  $p_f$  (in W) via the DC-link. In the continuous operation of the WTS, the DC-link capacitance  $C_{dc}$  (in F) on average is *not* charged or discharged and no DC-link power,  $p_{dc}$  (in W), is exchanged within the circuit and the DC-link voltage  $u_{dc}$  remains (almost) constant. The (active) power  $p_{pcc}$  (in W) is fed into the grid at the PCC.





**Figure 5.** Electrical network of overall WTS: PMSG (left), back-to-back converter (middle) sharing a common DC-link, grid-side filter, point of common coupling (PCC) and balanced grid (right, neglecting the transformer).

### 3.2.1. Machine-side dynamics (electrical machine/generator and drive train)

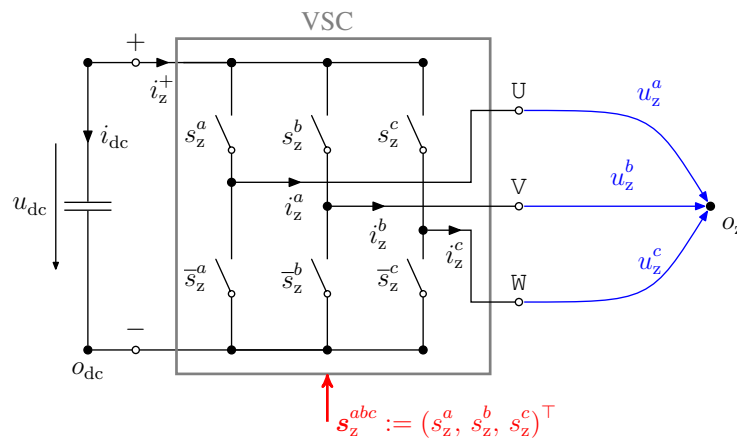
For an isotropic PMSG, Kirchhoff's laws (see the electrical circuit in Fig. 5) and Newton mechanics yield the following fifth-order dynamic system (for details see [47, Chap. 14])

$$\left. \begin{aligned} \frac{d}{dt} \mathbf{i}_s^{abc}(t) &= (\mathbf{L}_s^{abc})^{-1} \left[ \mathbf{u}_s^{abc}(t) - R_s \mathbf{i}_s^{abc}(t) + n_p \omega_m(t) \widehat{\boldsymbol{\psi}}_{pm} \begin{pmatrix} \sin(n_p \phi_m(t)) \\ \sin(n_p \phi_m(t) - \frac{2}{3}\pi) \\ \sin(n_p \phi_m(t) - \frac{4}{3}\pi) \end{pmatrix} \right] \\ \frac{d}{dt} \omega_m(t) &= \frac{1}{\Theta} \left[ \frac{m_t(v_w(t), \omega_m(t)/g_r, \beta(t))}{g_r} + \underbrace{n_p \mathbf{i}_s^{abc}(t)^\top \mathbf{J}_\Sigma \boldsymbol{\psi}_{pm}^{abc}(\phi_m(t))}_{=: m_m(\mathbf{i}_s^{abc}(t), \phi_m(t))} \right] \\ \frac{d}{dt} \phi_m(t) &= \omega_m(t) \end{aligned} \right\} \quad (12)$$

with initial values  $\mathbf{i}_s^{abc}(0) = \mathbf{i}_{s,0}^{abc}$ ,  $\omega_m(0) = \omega_{m,0}$  and  $\phi_m(0) = \phi_{m,0}$ , and where

$$\mathbf{L}_s^{abc} := \begin{bmatrix} L_{s,m} + L_{s,\sigma} & -\frac{L_{s,m}}{2} & -\frac{L_{s,m}}{2} \\ -\frac{L_{s,m}}{2} & L_{s,m} + L_{s,\sigma} & -\frac{L_{s,m}}{2} \\ -\frac{L_{s,m}}{2} & -\frac{L_{s,m}}{2} & L_{s,m} + L_{s,\sigma} \end{bmatrix} \quad \text{and} \quad \boldsymbol{\psi}_{pm}^{abc}(\phi_m) := \widehat{\boldsymbol{\psi}}_{pm} \begin{pmatrix} \cos(n_p \phi_m) \\ \cos(n_p \phi_m - \frac{2}{3}\pi) \\ \cos(n_p \phi_m - \frac{4}{3}\pi) \end{pmatrix} \quad (13)$$

are inductance matrix  $\mathbf{L}_s^{abc} = (\mathbf{L}_s^{abc})^\top > 0$  (with mutual inductance  $L_{s,m}$  and leakage inductance  $L_{s,\sigma}$  such that  $L_s^{abc} > 0$ ; both in H) and permanent magnet flux linkage vector  $\boldsymbol{\psi}_{pm}^{abc}$  (each in Wb), respectively;  $\mathbf{J}_\Sigma$  is the rotation matrix as in (4). The dynamics in (12) incorporate electrical stator dynamics (the first three states) with stator flux linkage  $\boldsymbol{\psi}_s^{abc}(\mathbf{i}_s^{abc}, \phi_m) = \mathbf{L}_s^{abc} \mathbf{i}_s^{abc} + \boldsymbol{\psi}_{pm}^{abc}(\phi_m)$  and the rotatory (mechanical) dynamics of the generator. The stator currents  $\mathbf{i}_s^{abc}$  and the mechanical angular velocity  $\omega_m$  (and/or the mechanical angle  $\phi_m$ ) are measured and available for feedback control.



**Figure 6.** Electrical circuit of voltage source converter (VSC) with ideal switches and DC-link ( $z \in \{s, f\}$ ).

### 3.2.2. Power electronics and DC-link dynamics (back-to-back converter)

Although multi-level converters for the regulation of wind power plants are likely to be used in the future [52], the still widely used two-level back-to-back converter will be considered in this section. Each of the two voltage source converters (VSCs) of the back-to-back converter can be modeled as illustrated in Fig. 6 by ideal switches (dynamics of the semi-conductors and free-wheeling diodes are neglected).

The stator-side converter feeds the electrical machine (generator) with the phase voltages  $\mathbf{u}_s^{abc}$ , while the filter-side converter applies the phase voltages  $\mathbf{u}_f^{abc}$  to the line filter. The output voltages of the converters depend on the DC-link voltage  $u_{dc}$  and are generated by adequate modulation, i.e. the application of an adequate sequence of the switching vectors  $\mathbf{s}_s^{abc} = (s_s^a, s_s^b, s_s^c)^\top$  and  $\mathbf{s}_f^{abc} = (s_f^a, s_f^b, s_f^c)^\top$ . For the considered two-level VSCs, there exist eight possible switching vectors  $(s_z^{abc})^\top \in \mathbb{S}_8 := \{000, 100, \dots, 111\}$  for  $z \in \{s, f\}$  (see Fig. 6). Moreover, for balanced<sup>3</sup> voltages, the stator voltages and filter voltages are given by

$$\mathbf{u}_z^{abc}(\mathbf{s}_z^{abc}, u_{dc}) = u_{dc} \frac{1}{3} \underbrace{\begin{bmatrix} 2 & -1 & -1 \\ -1 & 2 & -1 \\ -1 & -1 & 2 \end{bmatrix}}_{=: T_{vsc}} \mathbf{s}_z^{abc} \implies \|\mathbf{u}_z^{abc}(\mathbf{s}_z^{abc}, u_{dc})\| \leq \hat{u} := \frac{2}{3} u_{dc} \quad (14)$$

for  $z \in \{s, f\}$ . Due to the limited DC-link voltage, each VSC can generate only a constrained phase voltage amplitude. The shared DC-link capacitor  $C_{dc}$  is charged or discharged via the DC-link current  $i_{dc} = -i_m^+ - i_f^+$  (in A) which depends on the machine-side and grid-side currents, respectively (see Fig. 5). The DC-link dynamics are given by

$$\frac{d}{dt} u_{dc}(t) = \frac{1}{C_{dc}} i_{dc}(t) = \frac{1}{C_{dc}} \left( \underbrace{-\mathbf{i}_s^{abc}(t)^\top \mathbf{s}_s^{abc}(t)}_{=: i_s^+(t)} - \underbrace{\mathbf{i}_f^{abc}(t)^\top \mathbf{s}_f^{abc}(t)}_{=: i_f^+(t)} \right), \quad (15)$$

<sup>3</sup> I.e.  $u_s^a(t) + u_s^b(t) + u_s^c(t) = u_f^a(t) + u_f^b(t) + u_f^c(t) = 0$  holds  $\forall t \geq 0$ .

with initial value  $u_{dc}(0) = u_{dc}^0 > 0$  (in V).

The state-of-the-art modulation technique is the *space vector modulation* (SVM) which can reproduce *average phase voltage amplitudes* up to  $\bar{u} := u_{dc}/\sqrt{3}$ . The classical *pulse width modulation* (PWM; without over-modulation) can reproduce average phase voltage amplitudes up to  $\bar{u} := u_{dc}/2$  (see [53, S. 658–720] and [54, S. 132–136]). Consider a feasible stator reference phase voltage vector  $\mathbf{u}_{s,ref}^{abc}(\cdot) \in \mathcal{C}(\mathbb{R}_{\geq 0}; [-\bar{u}, \bar{u}]^3)$  and a carrier signal  $c_{\wedge}(\cdot) \in \mathcal{C}(\mathbb{R}_{\geq 0}; [-1, 1])$  (e.g. a sawtooth or triangular carrier signal with period  $T_{sw} = 1/f_{sw}$  (in s) inversely proportional to the switching frequency  $f_{sw}$  (in Hz)). Then, PWM generates its pulse pattern by a simple and instantaneous comparison of normalized reference phase voltages and carrier signal. More precisely, the actual switching signal vector for PWM is obtained by

$$\mathbf{s}_z^{abc}(\mathbf{u}_{z,ref}^{abc}, u_{dc}, t) := \underbrace{\begin{pmatrix} \sigma\left(\frac{u_{z,ref}^a}{u_{dc}/2} - c_{\wedge}(t)\right) \\ \sigma\left(\frac{u_{z,ref}^b}{u_{dc}/2} - c_{\wedge}(t)\right) \\ \sigma\left(\frac{u_{z,ref}^c}{u_{dc}/2} - c_{\wedge}(t)\right) \end{pmatrix}}_{=: \sigma\left(\frac{\mathbf{u}_{z,ref}^{abc}}{u_{dc}/2} - \mathbf{1}_3 c_{\wedge}(t)\right)} \in \mathbb{S}_8 \quad (16)$$

where  $\sigma(\cdot)$  is the Heaviside (step) function. For SVM, the reference voltage vector  $\mathbf{u}_{z,ref}^{abc} = (u_{z,ref}^a, u_{z,ref}^b, u_{z,ref}^c)^\top$  in (16) must be replaced [55, p. 267–271] by the expression

$$\mathbf{u}_{z,SVM,ref}^{abc} = \left[ \mathbf{u}_{z,ref}^{abc} - \frac{\mathbf{max}(\mathbf{u}_{z,ref}^{abc}) + \mathbf{min}(\mathbf{u}_{z,ref}^{abc})}{2} \right] \quad (17)$$

where  $\mathbf{u}_{z,ref}^{abc}$  is the (original) reference vector from the control system and  $\mathbf{max}(\boldsymbol{\zeta}) := \max(\boldsymbol{\zeta})\mathbf{1}_3$  and  $\mathbf{min}(\boldsymbol{\zeta}) := \min(\boldsymbol{\zeta})\mathbf{1}_3$  return the minimal and maximal entries of the vector  $\boldsymbol{\zeta} \in \mathbb{R}^3$ , respectively. The reference voltages are normalized with respect to  $u_{dc}/2$ . For each phase  $p \in \{a, b, c\}$  and  $z \in \{s, f\}$ , the phase switching signal is high, i.e.  $s_z^p(t) = 1$ , when the normalized reference phase voltage is larger than or equal to the carrier signal, i.e.  $u_{z,ref}^p(t) \geq c_{\wedge}(t)$ ; whereas the switching signal is low, i.e.  $s_z^p(t) = 0$ , when the normalized reference is smaller than the carrier signal, i.e.  $u_{z,ref}^p(t) < c_{\wedge}(t)$ .

Due to the finite (eight) number of switching vectors, not all reference voltage vectors can be generated instantaneously. The converter exhibits some delay which is inversely proportional to the switching frequency  $f_{sw} \gg 1$  Hz [56, p. 525–526]. On average, this delay can be quantified by the *inverter delay time*  $T_{avg}$  (in s) which is required to produce the *average output phase voltage vector* defined by [47, Chapter 14]

$$\bar{\mathbf{u}}_z^{abc}(t) := \frac{1}{T_{avg}} \int_{t-T_{avg}}^t \mathbf{u}_z^{abc}(\tau) d\tau \approx \mathbf{u}_{z,ref}^{abc}(t - T_{avg}) \quad (18)$$

for all  $t \geq T_{avg}$ . The delay varies within the interval  $T_{avg} \in \left[\frac{1}{2f_{sw}}, \frac{3}{2f_{sw}}\right]$  [57] and depends on switching frequency  $f_{sw}$ , the selected modulation scheme (e.g. PWM or SVM) and its implementation<sup>4</sup> (e.g. on FPGA, DSP or micro-processor).

### 3.2.3. Grid-side dynamics (filter, PCC and grid)

To induce sinusoidal phase currents to the power grid, a line filter must be used to filter out the switching behavior of the VSC. A simple *RL-filter* (in each phase) with filter inductance  $L_f$  (in H)

<sup>4</sup> For most modern implementations, the reference voltages are sampled with the switching frequency  $f_{sw}$  at the maximum (or minimum) of the carrier signal  $c_{\wedge}(\cdot)$  and held constant over the period  $T_{sw} = 1/f_{sw}$ , i.e. “*symmetrical sampling*” [47, Chapter 14], [55, Chapter 3.6].

and filter resistance  $R_f$  (in  $\Omega$ ) is considered. The grid-side converter generates the (filter) voltages  $\mathbf{u}_f^{abc}$  which are applied to the filter and, due to the inductance  $L_f$ , lead to (approximately) sinusoidal filter phase currents  $\mathbf{i}_f^{abc}$ . In the filter resistance  $R_f$ , the copper losses  $R_f \|\mathbf{i}_f^{abc}\|^2$  (in W) are dissipated and converted into heat. The grid-side electrical network with grid voltages  $\mathbf{u}_g^{abc}$  is shown in Fig. 5. According to Kirchoff's voltage law, the grid-side dynamics are given by

$$\frac{d}{dt} \mathbf{i}_f^{abc}(t) = \frac{1}{L_f} \left[ \mathbf{u}_f^{abc}(t) - R_f \mathbf{i}_f^{abc}(t) - \hat{\mathbf{u}}_g(t) \underbrace{\begin{pmatrix} \cos(\phi_g(t)) \\ \cos(\phi_g(t) - \frac{2\pi}{3}) \\ \cos(\phi_g(t) - \frac{4\pi}{3}) \end{pmatrix}}_{=: \mathbf{u}_g^{abc}(t)} \right], \quad (19)$$

with  $\mathbf{i}_f^{abc}(0) = \mathbf{i}_{f,0}^{abc} \in \mathbb{R}^3$ . The grid voltages  $\mathbf{u}_g^{abc}(\cdot)$  depend on a (possibly time-varying) amplitude  $\hat{u}_g(\cdot) \geq 0$  V and on a time-varying grid angle  $\phi_g(\cdot) := \int_0^\cdot \omega_g(\tau) d\tau + \phi_{g,0}$  (in rad). The angular grid frequency<sup>5</sup>  $\omega_g$  (in  $\frac{\text{rad}}{\text{s}}$ ) might also vary over time and the initial phase angle  $\phi_{g,0}$  is usually unknown (a phase-locked loop is employed to detect  $\phi_{g,0}$  and  $\omega_g$  [44]). The grid voltages  $\mathbf{u}_g^{abc}$  are measured before (or after) the transformer. The transformer steps up the voltage to a higher voltage level at the PCC (for example, to the medium voltage level of the power grid).

**Remark 2.** In WTSs, also LCL-filters are used instead of RL-filters. The design of an LCL-filter allows for smaller inductances. Thus, an LCL-filter can be made smaller than an RL-filter. A detailed discussion of the LCL-filter design can be found in [1, Kap. 11]. The control of grid-side power converters connected to the grid via LCL-filters is discussed in e.g. [59,60].

### 3.2.4. Power output

The WTS outputs the active and reactive instantaneous powers  $p_{\text{pcc}}$  (in W) and  $q_{\text{pcc}}$  (in var) at the PCC which, for  $\mathbf{J}_\Sigma$  as in (4) and  $\mathbf{u}_g^{abc}(t)$  as in (19), are respectively given by

$$p_{\text{pcc}}(\mathbf{i}_f^{abc}, t) = \mathbf{u}_g^{abc}(t)^\top \mathbf{i}_f^{abc} \quad \text{and} \quad q_{\text{pcc}}(\mathbf{i}_f^{abc}, t) = \mathbf{u}_g^{abc}(t)^\top \mathbf{J}_\Sigma \mathbf{i}_f^{abc}. \quad (20)$$

### 3.2.5. Overall dynamics in nonlinear state-space representation

The overall model is of 11th order and considers switching. For state vector

$$\mathbf{x} := \left( \underbrace{(x_1, x_2, x_3)}_{=: \mathbf{x}_{1-3}^\top}, x_4, x_5, x_6, \underbrace{(x_7, x_8, x_9)}_{=: \mathbf{x}_{7-9}^\top}, x_{10}, x_{11} \right)^\top := \left( (\mathbf{i}_s^{abc})^\top, \omega_m, \phi_m, u_{\text{dc}}, (\mathbf{i}_f^{abc})^\top, \phi_g, \beta_\diamond \right)^\top \in \mathbb{R}^{11},$$

and control input vector

$$\mathbf{u} := \left( \underbrace{(u_1, u_2, u_3)}_{=: \mathbf{u}_{1-3}^\top}, \underbrace{(u_4, u_5, u_6)}_{=: \mathbf{u}_{4-6}^\top}, u_7 \right)^\top := \left( (\mathbf{u}_{s,\text{ref}}^{abc})^\top, (\mathbf{u}_{f,\text{ref}}^{abc})^\top, \beta_{\text{ref}} \right)^\top \in \mathbb{R}^7,$$

<sup>5</sup> In Europe, the grid frequency  $f_g$  (hence,  $\omega_g = 2\pi f_g$ ) must remain within the frequency band 50 Hz  $\pm$  0.5 Hz to ensure grid stability (see [58, pp. 13,20,27]).

the overall system dynamics with output  $\mathbf{y}$  are given by the following nonlinear ordinary differential equation

$$\frac{d}{dt} \mathbf{x} = \underbrace{\begin{pmatrix} (L_s^{abc})^{-1} \left[ x_6 T_{vsc} \sigma \left( \frac{u_{1-3}}{x_6/2} - \mathbf{1}_3 c_\wedge(t) \right) - R_s x_{1-3} + n_p x_4 \hat{\psi}_{pm} \begin{pmatrix} \sin(n_p x_5) \\ \sin(n_p x_5 - \frac{2}{3}\pi) \\ \sin(n_p x_5 - \frac{4}{3}\pi) \end{pmatrix} \right] \\ \frac{1}{\sigma} \left[ \rho \pi r_f^2 v_w(t) \frac{c_p \left( r_t x_4 / (s r v_w(t)), \text{sat}_{0}^{90^\circ}(x_{11}) \right)}{2x_4} + n_p \hat{\psi}_{pm} x_{1-3}^\top J_\Sigma \begin{pmatrix} \cos(n_p x_5) \\ \cos(n_p x_5 - \frac{2}{3}\pi) \\ \cos(n_p x_5 - \frac{4}{3}\pi) \end{pmatrix} \right] \\ x_4 \\ \frac{1}{C_{dc}} \left[ -x_{1-3}^\top \sigma \left( \frac{u_{1-3}}{x_6/2} - \mathbf{1}_3 c_\wedge(t) \right) - x_{7-9}^\top \sigma \left( \frac{u_{4-6}}{x_6/2} - \mathbf{1}_3 c_\wedge(t) \right) \right] \\ \frac{1}{L_f} \left[ x_6 T_{vsc} \sigma \left( \frac{u_{4-6}}{x_6/2} - \mathbf{1}_3 c_\wedge(t) \right) - R_f x_{7-9} - \hat{u}_g(t) \begin{pmatrix} \cos(x_{10}) \\ \cos(x_{10} - \frac{2\pi}{3}) \\ \cos(x_{10} - \frac{4\pi}{3}) \end{pmatrix} \right] \\ \omega_g(t) \\ \text{sat}_{-\beta_{max}}^{\beta_{max}} \left( \frac{1}{T_\beta} (-\text{sat}_{0}^{90^\circ}(x_{11}) + u_7) \right) \end{pmatrix}_{=: f_{abc}(\mathbf{x}, \mathbf{u}, t) \in \mathbb{R}^{11}} \quad (21)$$

$$\mathbf{y} = \underbrace{\hat{u}_g(t) \begin{pmatrix} \cos(x_{10}) \\ \cos(x_{10} - \frac{2\pi}{3}) \\ \cos(x_{10} - \frac{4\pi}{3}) \end{pmatrix}^\top}_{=: h_{abc}(\mathbf{x}, t) \in \mathbb{R}^2} \begin{bmatrix} I_3 \\ J_\Sigma \end{bmatrix} x_{7-9} = \begin{pmatrix} p_{pcc}(i_f^{abc}, t) \\ q_{pcc}(i_f^{abc}, t) \end{pmatrix}$$

with initial values

$$\mathbf{x}(0) = ((i_{s,0}^{abc})^\top, \omega_{m,0}, \phi_{m,0}, u_{dc,0}, (i_{f,0}^{abc})^\top, \phi_{g,0}, \beta_{\diamond,0})^\top. \quad (22)$$

Note that, for brevity and clarity, the argument  $t$  is only shown for (purely time-varying) disturbances like wind  $v_w(\cdot)$ , carrier signal  $c_\wedge(\cdot)$  of modulator, grid amplitude  $\hat{u}_g(\cdot)$  and grid angular frequency  $\omega_g(\cdot)$ .

### 3.3. Electrical system in (simplified) synchronously rotating $(d, q)$ -reference frame

Due to the star-connection of machine (stator) windings and grid-side network, the sums of stator and filter currents are zero for all time, i.e.  $i_s^a(t) + i_s^b(t) + i_s^c(t) = i_f^a(t) + i_f^b(t) + i_f^c(t) = 0$  for all  $t \geq 0$ , respectively. Applying the Clarke-Park transformation (with transformation angle  $\phi_p = n_p \phi_m$  and  $\phi_p = \phi_g$  on machine and grid side, resp.) to the machine-side and grid-side entries in (21) yields a representation of the respective models in the simplified synchronously rotating  $(d, q)$ -reference frame. To detect the grid voltage angle  $\phi_g$  used in Clarke-Park transformation  $T_{cp}(\phi_g)$ , a phase-locked loop is usually used [1]. For details see [44] and [47].

### 3.3.1. Machine-side dynamics: Electrical machine (generator) and drive train

For permanent magnet flux linkage orientation, i.e.  $\phi_p(t) = n_p \phi_m(t)$ , the PMSG dynamics simplify. The simplified PMSG dynamics in the  $(d, q)$ -reference frame are given by [44]

$$\left. \begin{aligned} \frac{d}{dt} \mathbf{i}_s^{dq}(t) &= L_s^{-1} \left[ \mathbf{u}_s^{dq}(t) - R_s \mathbf{i}_s^{dq}(t) - n_p \omega_m(t) \mathbf{J} \left( L_s \mathbf{i}_s^{dq}(t) + \underbrace{\begin{pmatrix} \frac{3}{2} \kappa \hat{\psi}_{pm} \\ 0 \end{pmatrix}}_{=: \psi_{pm}^{dq}} \right) \right] \\ \frac{d}{dt} \omega_m(t) &= \frac{1}{\Theta} \left[ \frac{m_t(v_w(t), \beta(t), \omega_m(t))}{g_r} + \underbrace{n_p \frac{2}{3\kappa^2} \mathbf{i}_s^{dq}(t)^\top \mathbf{J} \psi_{pm}^{dq}}_{=: m_m(\mathbf{i}_s^{dq}(t))} \right] \\ \frac{d}{dt} \phi_m(t) &= \omega_m(t) \end{aligned} \right\} \quad (23)$$

with initial values  $\mathbf{i}_s^{dq}(0) = \mathbf{T}_{cp}(n_p \phi_m(0)) \mathbf{i}_{s,0}^{abc}$ ,  $\omega_m(0) = \omega_{m,0}$  and  $\phi_m(0) = \phi_{m,0}$ , and overall stator inductance  $L_s := \frac{3}{2} L_{s,m} + L_{s,\sigma}$ . Note that, for the considered isotropic PMSG, the machine torque is independent of the  $d$ -current component and simplifies to [47, p. 534]

$$m_m(\mathbf{i}_s^{dq}) = m_m(\mathbf{i}_s^q) = n_p \frac{2}{3\kappa^2} \frac{3}{2} \kappa \hat{\psi}_{pm} i_s^q = \frac{n_p}{\kappa} \hat{\psi}_{pm} i_s^q \implies i_{s,\text{ref}}^q = \frac{\kappa}{n_p \hat{\psi}_{pm}} m_{m,\text{ref}} \text{ and } i_{s,\text{ref}}^d = 0, \quad (24)$$

which allows to compute the required reference currents  $i_{s,\text{ref}}^d = 0$  (to reduce copper losses) and  $i_{s,\text{ref}}^q$  for given reference torque  $m_{m,\text{ref}}$  (in N m).

### 3.3.2. Power electronics and DC-link dynamics (back-to-back converter)

Note that, for all  $\phi_p \in \mathbb{R}$ , it holds

$$\mathbf{T}_{cp}(\phi_p)^{-\top} \mathbf{T}_{cp}(\phi_p)^{-1} = \frac{2}{3\kappa^2} \mathbf{I}_2 \ \& \ \mathbf{T}_{cp}(\phi_p) \mathbf{T}_{vsc} = \mathbf{T}_{cp}(\phi_p). \quad (25)$$

Hence, the output voltages of the VSCs in the  $(d, q)$ -reference frame are given by  $\forall \phi_p \in \{n_p \phi_m, \phi_g\} \ \forall z \in \{s, f\}$ :

$$\begin{aligned} \mathbf{u}_z^{dq} &:= \mathbf{T}_{cp}(\phi_p) \mathbf{u}_z^{abc}(s_z^{abc}, u_{dc}) \\ &\stackrel{(14),(25)}{=} u_{dc} \mathbf{T}_{cp}(\phi_p) \mathbf{s}_z^{abc} \end{aligned} \quad (26)$$

$$\stackrel{(16)}{=} u_{dc} \mathbf{T}_{cp}(\phi_p) \sigma \left( \frac{\mathbf{T}_{cp}(\phi_p)^{-1} \mathbf{u}_{z,\text{ref}}^{dq}}{u_{dc}/2} - \mathbf{1}_3 c_\wedge(t) \right), \quad (27)$$

i.e.,  $\mathbf{u}_z^{dq} = \mathbf{u}_z^{dq}(\mathbf{u}_{z,\text{ref}}^{dq}, u_{dc}, \phi_p)$ . This allows to derive the DC-link dynamics as follows

$$\begin{aligned} \frac{d}{dt} u_{dc}(t) &\stackrel{(15)}{=} \frac{1}{C_{dc}} \left( -\mathbf{i}_s^{dq}(t)^\top \mathbf{T}_{cp}(n_p \phi_m(t))^{-\top} \mathbf{s}_s^{abc}(t) - \mathbf{i}_f^{dq}(t)^\top \mathbf{T}_{cp}(\phi_g(t))^{-\top} \mathbf{s}_f^{abc}(t) \right), \\ &\stackrel{(26),(25)}{=} \frac{1}{C_{dc} u_{dc}} \left( \underbrace{-\frac{2}{3\kappa^2} \mathbf{i}_s^{dq}(t)^\top \mathbf{u}_s^{dq}(t)}_{=: p_s(t)} - \underbrace{\frac{2}{3\kappa^2} \mathbf{i}_f^{dq}(t)^\top \mathbf{u}_f^{dq}(t)}_{=: p_f(t)} \right) \\ &\stackrel{(27)}{=} \frac{2}{3\kappa^2 C_{dc}} \left( -\mathbf{i}_s^{dq}(t)^\top \mathbf{T}_{cp}(n_p \phi_m) \cdot \sigma \left( \frac{\mathbf{T}_{cp}(n_p \phi_m)^{-1} \mathbf{u}_{s,\text{ref}}^{dq}}{u_{dc}/2} - \mathbf{1}_3 c_\wedge(t) \right) \right. \\ &\quad \left. - \mathbf{i}_f^{dq}(t)^\top \mathbf{T}_{cp}(\phi_g) \cdot \sigma \left( \frac{\mathbf{T}_{cp}(\phi_g)^{-1} \mathbf{u}_{f,\text{ref}}^{dq}}{u_{dc}/2} - \mathbf{1}_3 c_\wedge(t) \right) \right). \end{aligned} \quad (28)$$

### 3.3.3. Grid-side dynamics (filter, PCC, and grid)

The grid-side dynamics also simplify. For grid voltage orientation, i.e.  $\phi_p = \phi_g$ , the grid-side system in the  $(d, q)$ -reference frame is given by [44]

$$\frac{d}{dt} \mathbf{i}_f^{dq}(t) = \frac{1}{L_f} \left[ \mathbf{u}_f^{dq}(t) - R_f \mathbf{i}_f^{dq}(t) - \omega_g(t) L_f \mathbf{J} \mathbf{i}_f^{dq}(t) - \underbrace{\begin{pmatrix} \frac{3}{2} \kappa \hat{u}_g(t) \\ 0 \end{pmatrix}}_{=: \mathbf{u}_g^{dq}(t)} \right], \quad (29)$$

with initial value  $\mathbf{i}_f^{dq}(0) = T_{cp}(\phi_g(0)) \mathbf{i}_{f,0}^{abc} \in \mathbb{R}^2$ .

### 3.3.4. Power output

In the  $(d, q)$ -reference frame, active and reactive instantaneous powers  $p_{pcc}$  (in W) and  $q_{pcc}$  (in var) at the PCC simplify to (cf. [44] and [47, Chap. 14])

$$\begin{aligned} p_{pcc}(\mathbf{i}_f^{dq}, t) &= \frac{2}{3\kappa^2} \mathbf{u}_g^{dq}(t)^\top \mathbf{i}_f^{dq} \stackrel{(29)}{=} \frac{1}{\kappa} \hat{u}_g(t) i_f^d \quad \text{and} \\ q_{pcc}(\mathbf{i}_f^{dq}, t) &= \frac{2}{3\kappa^2} \mathbf{u}_g^{dq}(t)^\top \mathbf{J} \mathbf{i}_f^{dq} \stackrel{(29)}{=} -\frac{1}{\kappa} \hat{u}_g(t) i_f^q, \end{aligned} \quad (30)$$

with  $\mathbf{u}_g^{dq}$  and  $\hat{u}_g$  as in (29) and  $\kappa \in \{\frac{2}{3}, \sqrt{\frac{2}{3}}\}$ .

### 3.3.5. Overall dynamics in nonlinear state-space representation

In the  $(d, q)$ -reference frame, the overall model is of 9th order. Note that switching is still considered. For (reduced) state vector

$$\mathbf{x} := \left( \underbrace{(x_1, x_2)}_{=: \mathbf{x}_{1-2}^\top}, x_3, x_4, x_5, \underbrace{(x_6, x_7)}_{=: \mathbf{x}_{6-7}^\top}, x_8, x_9 \right)^\top := \left( (\mathbf{i}_s^{dq})^\top, \omega_m, \phi_m, u_{dc}, (\mathbf{i}_f^{dq})^\top, \phi_g, \beta_\diamond \right)^\top \in \mathbb{R}^9, \quad (31)$$

and (reduced) control input

$$\mathbf{u} := \left( \underbrace{(u_1, u_2)}_{=: \mathbf{u}_{1-2}^\top}, \underbrace{(u_3, u_4)}_{=: \mathbf{u}_{3-4}^\top}, u_5 \right)^\top := \left( (\mathbf{u}_{s,\text{ref}}^{dq})^\top, (\mathbf{u}_{f,\text{ref}}^{dq})^\top, \beta_{\text{ref}} \right)^\top \in \mathbb{R}^5, \quad (32)$$

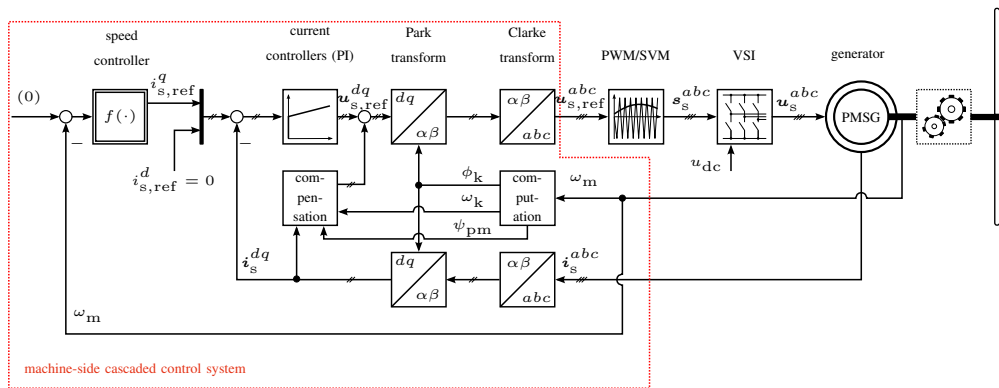


Figure 7. Machine-side cascaded control system (Regime II).

the overall (but reduced) nonlinear system dynamics with output are given by the following ninth-order ordinary differential equation

$$\begin{aligned}
 \frac{d}{dt} \mathbf{x} &= \underbrace{\begin{pmatrix} \frac{1}{L_s} \left[ x_5 T_{cp}(n_p x_4) \sigma \left( \frac{T_{cp}(n_p x_4)^{-1} u_{1-2}}{x_5/2} - \mathbf{1}_3 c_{\wedge}(t) \right) - R_s x_{1-2} - n_p x_3 J \left( L_s x_{1-2} + \begin{pmatrix} \frac{3}{2} \kappa \hat{\psi}_{pm} \\ 0 \end{pmatrix} \right) \right] \\ \frac{1}{\Theta} \left[ \varrho \pi r_f^2 \hat{v}_w(t)^3 \frac{c_p(r_f x_3 / (g r v_w(t)), \text{sat}_{0^\circ}^{90^\circ}(x_9))}{2x_3} + \frac{n_p}{\kappa} \hat{\psi}_{pm} x_2 \right] \\ x_3 \\ \frac{2}{3\kappa^2 c_{dc}} \left[ -x_{1-2}^\top T_{cp}(n_p x_4) \cdot \sigma \left( \frac{T_{cp}(n_p x_4)^{-1} u_{1-2}}{x_5/2} - \mathbf{1}_3 c_{\wedge}(t) \right) - x_{6-7}^\top T_{cp}(x_{10}) \cdot \sigma \left( \frac{T_{cp}(x_{10})^{-1} u_{3-4}}{x_5/2} - \mathbf{1}_3 c_{\wedge}(t) \right) \right] \\ \frac{1}{L_f} \left[ x_5 T_{cp}(x_{10}) \sigma \left( \frac{T_{cp}(x_{10})^{-1} u_{3-4}}{x_5/2} - \mathbf{1}_3 c_{\wedge}(t) \right) - R_f x_{6-7} - \omega_g(t) L_f J x_{6-7} - \begin{pmatrix} \frac{3}{2} \kappa \hat{u}_g(t) \\ 0 \end{pmatrix} \right] \\ \omega_g(t) \\ \text{sat}_{-\beta_{max}}^{\beta_{max}} \left( \frac{1}{T_\beta} (-\text{sat}_{0^\circ}^{90^\circ}(x_9) + u_5) \right) \end{pmatrix}}_{=: f_{dq}(\mathbf{x}, \mathbf{u}, t) \in \mathbb{R}^9} \quad (33) \\
 \mathbf{y} &= \underbrace{\frac{1}{\kappa} \begin{pmatrix} \hat{u}_g(t) \\ 0 \end{pmatrix}^\top}_{=: h_{dq}(\mathbf{x}, t) \in \mathbb{R}^2} \begin{bmatrix} I_2 \\ J \end{bmatrix} x_{6-7} = \begin{pmatrix} p_{pcc}(i_f^{dq}, t) \\ q_{pcc}(i_f^{dq}, t) \end{pmatrix}
 \end{aligned}$$

with initial value

$$\mathbf{x}(0) = ((T_{cp}(n_p \phi_m(0)) i_{s,0}^{abc})^\top, \omega_{m,0}, \phi_{m,0}, u_{dc,0}, (T_{cp}(\phi_g(0)) i_{f,0}^{abc})^\top, \phi_{g,0}, \beta_{\Delta,0})^\top.$$

Again, the argument  $t$  is only shown for wind  $v_w(\cdot)$ , modulator carrier signal  $c_{\wedge}(\cdot)$ , grid voltage amplitude  $\hat{u}_g(\cdot)$  and grid angular frequency  $\omega_g(\cdot)$ . Note that, in view of the star connection on machine and grid side, the models (21) and (33) are equivalent.

## 4. Control systems and operation management

### 4.1. Controllers

In this section, the individual controllers of the cascaded control system are described. In Section 6, the controllers will be used for the numerical simulation of the WTS models. The overall control system is shown in Fig. 7 for the machine side and in Fig. 8 for the grid side.



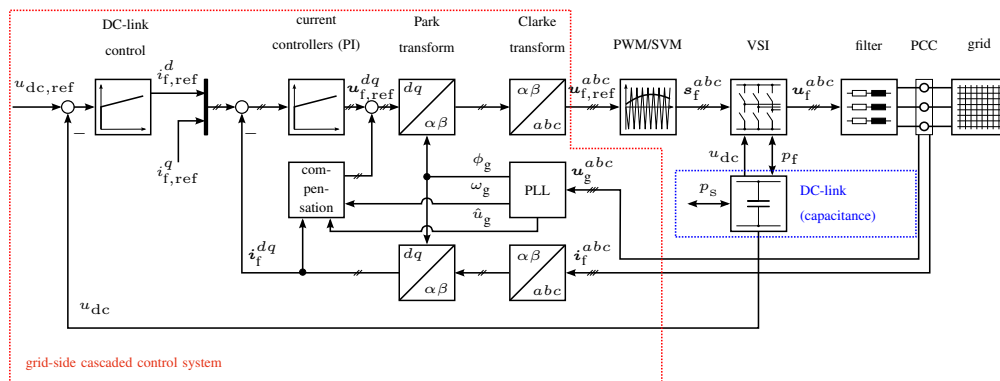


Figure 8. Grid-side cascaded control system.

#### 4.1.1. Machine-side and grid-side current controllers ( $z \in \{s, f\}$ )

The current closed-loop systems on machine/stator ( $z = s$ ) and grid/filter ( $z = f$ ) side consist of two PI controllers (usually implemented in the  $(d, q)$ -reference frame), two disturbance compensation feedforward controllers, and the respective current dynamics as presented above. The applied control action consists of two parts, for  $z \in \{s, f\}$ , as follows

$$\mathbf{u}_{z,\text{ref}}^{dq}(t) = \underbrace{\mathbf{u}_{z,\text{pi}}^{dq}(t)}_{\text{PI controller output}} + \underbrace{\mathbf{u}_{z,\text{comp}}^{dq}(t)}_{\text{disturbance compensation}}. \quad (34)$$

Details can be found in e.g. [61, Sec. 7.1.1] or [44] (with similar notation as here). Hence, the voltage reference  $\mathbf{u}_{z,\text{ref}}^{dq} = (u_{z,\text{ref}}^d, u_{z,\text{ref}}^q)^\top$  – the control input to the VSC – is the sum of the disturbance compensation  $\mathbf{u}_{z,\text{comp}}^{dq} = (u_{z,\text{comp}}^d, u_{z,\text{comp}}^q)^\top$  and the output  $\mathbf{u}_{z,\text{pi}}^{dq} = (u_{z,\text{pi}}^d, u_{z,\text{pi}}^q)^\top$  of the PI controller(s). The goal of the disturbance compensation is to obtain (almost) *decoupled* current dynamics for controller design in the  $(d, q)$ -reference frame. Therefore, depending on the application, the coupling terms [44,62] are, for  $z \in \{s, f\}$ , given by

$$\mathbf{u}_{z,\text{dist}}^{dq}(t) := \begin{cases} -n_p \omega_m(t) \mathbf{J} (L_s \dot{\mathbf{i}}_s^{dq}(t) + \boldsymbol{\psi}_{\text{pm}}^{dq}), & \text{for PMSGs as in (23)} \\ -\omega_g(t) L_f \mathbf{J} \dot{\mathbf{i}}_f^{dq}(t) - \mathbf{u}_g^{dq}(t), & \text{for RL-filter \& grid as in (29),} \end{cases} \quad (35)$$

which can be (roughly) compensated for by introducing the following feedforward control action  $\mathbf{u}_{z,\text{comp}}^{dq} = -\mathbf{u}_{z,\text{dist}}^{dq}$ .

It is well known that PI(D) controllers in presence of input saturation may exhibit integral windup (in particular for large initial errors) leading to large overshoots and/or oscillations in the closed-loop system response (see, e.g., [63,64]). Due to the limited DC-link voltage  $u_{\text{dc}}$  (in V), the output of the VSC is constrained by the saturation level

$$\hat{u}(u_{\text{dc}}) \in \left[ \frac{u_{\text{dc}}}{2}, \frac{2u_{\text{dc}}}{3} \right] \quad (36)$$

(in V) which depends on the employed modulation strategy (such as pulse-width modulation (PWM) or space-vector modulation (SVM) with or without over-modulation [53, Sec. 8.4]).

Due to the input saturation, a simple but effective anti-windup strategy<sup>6</sup> (similar to *conditional integration*) is implemented. To do so, the transition function  $f_{\tilde{u}, \Delta_{\tilde{z}}}(\cdot)$  as in (2) is combined with the two-input two-output PI controller

$$\left. \begin{aligned} \frac{d}{dt} \tilde{\zeta}_z^{dq}(t) &= f_{\tilde{u}, \Delta_{\tilde{z}}}(\|\mathbf{u}_{z, \text{ref}}^{dq}(t)\|) \mathbf{e}_z^{dq}(t), \\ \mathbf{u}_{z, \text{pi}}^{dq}(t) &= k_{z, \text{p}} \mathbf{e}_z^{dq}(t) + k_{z, \text{i}} \tilde{\zeta}_z^{dq}(t), \end{aligned} \right\} \quad (37)$$

where  $\tilde{\zeta}_z^{dq}(0) = \tilde{\zeta}_{z,0}^{dq} \in \mathbb{R}^2$  is the initial value,  $\tilde{\zeta}_z^{dq} = (\tilde{\zeta}_z^d, \tilde{\zeta}_z^q)^\top$  is the integrator output vector of the PI controllers, and  $\mathbf{e}_z^{dq} = (e_z^d, e_z^q)^\top = \mathbf{i}_{z, \text{ref}}^{dq} - \mathbf{i}_z^{dq}$  is the current tracking error,  $\Delta_{\tilde{z}}$  is the transition interval during which anti-windup already becomes active [47, Sect. 14.4] ( $z \in \{s, f\}$ ). The parameters  $k_{z, \text{p}}$  and  $k_{z, \text{i}}$  are the proportional and integral controller gain, respectively. The controller gains can be tuned, e.g., according to the "Magnitude Optimum criterion" (i.e.,  $k_{z, \text{p}} = L_z / (2T_{\text{avg}})$  and  $k_{z, \text{i}} = R_z / (2T_{\text{avg}})$ ; see [44,65]) or any other convenient/preferred tuning rule.

#### 4.1.2. Speed controller (Regime II)

For wind speeds below the nominal wind speed, maximum power point tracking is the desired control objective (cf. Fig. 2). The following nonlinear controller, given by

$$m_{m, \text{ref}}(t) = -\text{sat}_0^{\bar{m}_m} \left[ k_p^* \omega_m(t)^2 \right] \text{ where } k_p^* := \frac{\rho \pi r_t^5 c_p(\lambda^*, \beta^*)}{2g_t^3 (\lambda^*)^3}, \quad (38)$$

achieves maximum power point tracking even without wind speed measurement<sup>7</sup>. The controller (38) requires knowledge of the optimal tip speed ratio  $\lambda^*$  and optimal pitch angle  $\beta^* \geq 0$  for the turbine to extract the maximum available wind power. Moreover, its output is saturated by  $\bar{m}_m$  (e.g. by the nominal/rated machine torque). In [66], for a constant wind speed  $v_w > 0$ , it has been shown that the speed closed-loop system (neglecting the underlying current closed-loop system and the pitch control system, i.e.  $m_{m, \text{ref}} = m_m$  and  $\beta = \beta^*$ ) is stable and the optimal tip speed ratio  $\lambda^*$  (or the optimal speed  $\omega_m^* = \lambda^* \frac{v_w}{r_t}$ ) is reached asymptotically.

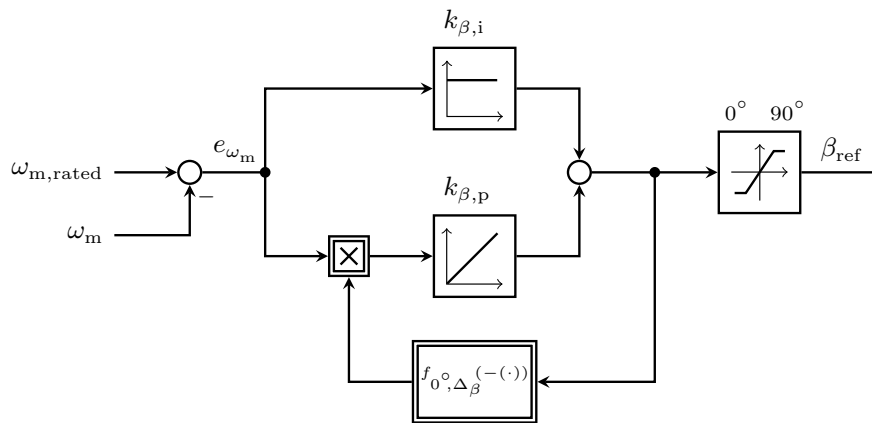
#### 4.1.3. Torque controller and pitch reference controller (Regime III)

When the wind speed exceeds the nominal wind speed of the turbine, i.e.  $v_w > v_{w, \text{rated}}$ , the WTS operates in regime III where the rotor/machine speed is controlled indirectly by the pitch control system and the machine-side torque control system outputs the nominal generator torque (cf. Fig. 2). The pitch control system allows to reduce the turbine torque (independently of the wind). To reduce mechanical stress, a smooth (continuous) transition between regime II and regime III is crucial which can be established by introducing an outer pitch reference controller cascade which adjusts the pitch angle reference  $\beta_{\text{ref}}$  appropriately. Here, an output-saturated PI controller with anti-windup is proposed. Its nonlinear dynamics are given by

$$\left. \begin{aligned} \frac{d}{dt} \tilde{\zeta}_\beta(t) &= f_{0^\circ, \Delta_{\tilde{\zeta}_\beta}} \left( -k_{\beta, \text{p}} \overbrace{(\omega_{m, \text{rated}} - \omega_m(t))}^{=: e_{\omega_m}(t)} - k_{\beta, \text{i}} \tilde{\zeta}_\beta(t) \right) e_{\omega_m}(t), \\ \beta_{\text{ref}}(t) &= \text{sat}_{0^\circ}^{90^\circ} [k_{\beta, \text{p}} e_{\omega_m}(t) + k_{\beta, \text{i}} \tilde{\zeta}_\beta(t)] \end{aligned} \right\} \quad (39)$$

<sup>6</sup> There exists a variety of anti-windup strategies. For this paper, the simple and popular conditional integration technique was chosen. Any other approach seems also feasible.

<sup>7</sup> This implies a perfectly working torque control. If this assumption does not hold, this might impact WTS efficiency and power production [67].



**Figure 9.** Nonlinear pitch reference PI controller.

with initial value  $\xi_{\beta}(0) = \xi_{\beta,0}$ . The dynamics depend on speed error  $e_{\omega} := \omega_{m,rated} - \omega_m$ , integrator state  $\xi_{\beta}$  with anti-windup transition function  $f_{0^{\circ}, \Delta_{\xi_{\beta}}}(\cdot)$  as in (2) (with  $\hat{a} = 0^{\circ}$  and  $\Delta = \Delta_{\xi_{\beta}} > 0$ ), proportional gain  $k_{\beta,p} > 0$  and integrator gain  $k_{\beta,i} > 0$ . Note that the output  $\beta_{ref}$  of the PI controller is saturated to the interval  $[0^{\circ}, 90^{\circ}]$ . Figure 9 shows the block diagram of the nonlinear PI controller implementation.

#### 4.1.4. DC-link voltage controller and reactive power feedforward controller

On the grid side, active and reactive power can be fed into the grid. The active power is indirectly controlled by the DC-link voltage controller via the  $d$ -component of the grid-side currents, whereas the reactive power is controlled by a simple feedforward controller via the  $q$ -component of the filter current. DC-link voltage control is a non-trivial task, since the DC-link system dynamics might exhibit a non-minimum phase behavior. Therefore, some care must be exercised during controller tuning leading to a rather conservative design (for more details see [44,68,69]). In most cases, a PI controller is implemented for DC-link voltage control. Such a PI controller with anti-windup is given by

$$\left. \begin{aligned} \frac{d}{dt} \xi_{dc}(t) &= f_{i, \Delta_{\xi_{dc}}}(\|\hat{i}_{f,ref}^{dq}(t)\|) \underbrace{(u_{dc,ref}(t) - u_{dc}(t))}_{=: e_{dc}(t)}, \\ \hat{i}_{f,ref}^d(t) &= k_{dc,p} e_{dc}(t) + k_{dc,i} \xi_{dc}(t), \end{aligned} \right\} \quad (40)$$

where  $\xi_{dc}(0) = \xi_{dc,0} \in \mathbb{R}$  is the initial value and  $f_{i, \Delta_{\xi_{dc}}}(\cdot)$  is as in (2),  $\hat{i}$  is the maximally admissible filter current amplitude (e.g. nominal current) and  $\Delta_{\xi_{dc}}$  is the transition interval for anti-windup. For given reactive power reference  $q_{pcc,ref}$  (provided e.g. by the grid operator), the grid-side reference  $q$ -current

$$\hat{i}_{f,ref}^q(t) = -\kappa \frac{q_{pcc,ref}(t)}{\hat{u}_g(t)} \quad (41)$$

can be obtained by rearranging (30) and can be used to feedforward control the desired reactive power.

#### 4.2. Operation management

The operation management is the high-level control system of the WTS (see Fig. 1). Based on the measured wind speed  $\hat{v}_w$  (rough estimate of the actually incoming wind speed  $v_w$ ), it aligns the rotor perpendicular to the wind direction (i.e. yawing; not considered in this paper) and it commands the transitions between the four operation regimes. For example, it triggers startup (transition between regime I  $\rightarrow$  regime II or regime IV  $\rightarrow$  regime III) or shutdown (transition between regime II  $\rightarrow$  regime I or regime III  $\rightarrow$  regime IV). Moreover, the operation management is the link between single WTSs and the wind park management system or the system operator. It receives reference values for reactive (in

the future, also active) power and propagates them to the underlying control system. Additionally, it also might provide the DC-link voltage reference  $u_{dc,ref}$  for the back-to-back converter or decides whether to perform an emergency shutdown to protect the WTS.

## 5. Reduced-Order Models

The presented WTS models in Section 3 consider all relevant dynamic effects and switching of the power electronic devices. The dynamic models are of eleventh and ninth order for the  $(a, b, c)$ - and the  $(d, q)$ -reference frame, respectively, and involve a high complexity with respect to computation and control design. Hence, for many studies concerning large-scale power systems [5,6,14,23] or high-level controller design of renewable energy systems for contributing in frequency and voltage stability [19], these detailed models are not a viable option. Thus, in this section, two models with reduced complexity are developed. In contrast to existing low-complexity WTS models [23,27,31,42,70], the model reduction in this paper follows a systematic step by step procedure with well-founded simplification assumptions.

### 5.1. Non-switching model (nsm) or averaging model

In a first simplification step, the explicit switching of the power electronics is neglected. The voltages applied on machine ( $z = s$ ) and grid side ( $z = f$ ) by the power electronics can be assumed to be equal to the averaged but saturated output phase voltage vector  $\mathbf{u}_z^{abc}(t) = \mathbf{sat}_{\hat{u}}(\bar{\mathbf{u}}_z^{abc}(t)) \approx \mathbf{sat}_{\hat{u}}(\mathbf{u}_{z,ref}^{abc}(t - T_{avg}))$  with  $\hat{u} = \hat{u}(u_{dc})$  as in (36) (see also (14) and (18) in Section 3.2.2 for a definition of the saturation function  $\mathbf{sat}_{\hat{u}}(\cdot)$ ). Moreover, in comparison to the other dynamics of the WTS, the time constant  $T_{avg}$  is negligible (as of in the range of microseconds) finally leading to the simplification  $\mathbf{u}_z^{abc}(t) = \mathbf{sat}_{\hat{u}}(\mathbf{u}_{z,ref}^{abc}(t))$  which can be written as  $\mathbf{u}_z^{dq}(t) = \mathbf{sat}_{\hat{u}}(\mathbf{u}_{z,ref}^{dq}(t))$  in the  $(d, q)$ -reference frame (neglecting cross-coupling terms due to the Park transformation [47, p. 521]). Consequently, the power electronics apply instantaneously the requested but possibly saturated voltages. Now, the resultant *non-switching model* can be introduced. Its state and control input vector are

$$\mathbf{x} := \left( \underbrace{(x_1, x_2)}_{=: \mathbf{x}_{1-2}^\top}, x_3, x_4, x_5, \underbrace{(x_6, x_7)}_{=: \mathbf{x}_{6-7}^\top}, x_8, x_9 \right)^\top := \left( (\mathbf{i}_s^{dq})^\top, \omega_m, u_{dc}, (\mathbf{i}_f^{dq})^\top, \beta_\diamond \right)^\top \in \mathbb{R}^7, \quad (42)$$

and

$$\mathbf{u} := \left( \underbrace{(u_1, u_2)}_{=: \mathbf{u}_{1-2}^\top}, \underbrace{(u_3, u_4)}_{=: \mathbf{u}_{3-4}^\top}, u_5 \right)^\top := \left( (\mathbf{u}_{s,ref}^{dq})^\top, (\mathbf{u}_{f,ref}^{dq})^\top, \beta_{ref} \right)^\top \in \mathbb{R}^5, \quad (43)$$

respectively. Its nonlinear dynamics with output are given by

$$\left. \begin{aligned} \frac{d}{dt} \mathbf{x} &= \underbrace{\begin{pmatrix} L_s^{-1} \left[ \text{sat}_{\hat{u}(x_4)}(\mathbf{u}_{1-2}) - R_s \mathbf{x}_{1-2}(t) - n_p x_3 J \left( L_s \mathbf{x}_{1-2} + \begin{pmatrix} \frac{3}{2} \kappa \hat{\psi}_{pm} \\ 0 \end{pmatrix} \right) \right] \\ \frac{1}{\Theta} \left[ \rho \pi r_f^2 v_w(t) \frac{3 c_p (r_t x_3 / (g_r v_w(t)), \text{sat}_{0^\circ}^{90^\circ}(x_9))}{2 x_3} + \frac{n_p}{\kappa} \hat{\psi}_{pm} x_2 \right] \\ \frac{2}{3 \kappa^2 C_{dc}} x_4 \left[ -\mathbf{x}_{1-2}^\top \text{sat}_{\hat{u}(x_4)}(\mathbf{u}_{1-2}) - \mathbf{x}_{6-7}^\top \text{sat}_{\hat{u}(x_4)}(\mathbf{u}_{3-4}) \right] \\ \frac{1}{L_f} \left[ \text{sat}_{\hat{u}(x_4)}(\mathbf{u}_{3-4}) - R_f \mathbf{x}_{6-7} - \omega_g(t) L_f J \mathbf{x}_{6-7} - \begin{pmatrix} \frac{3}{2} \kappa \hat{u}_g(t) \\ 0 \end{pmatrix} \right] \\ \text{sat}_{-\hat{\beta}_{max}}^{\hat{\beta}_{max}} \left( \frac{1}{T_\beta} (-\text{sat}_{0^\circ}^{90^\circ}(x_9) + u_5) \right) \end{pmatrix}}_{=: \mathbf{f}_{nsm}(\mathbf{x}, \mathbf{u}, t) \in \mathbb{R}^7} \\ \mathbf{y} &= \underbrace{\frac{1}{\kappa} \begin{pmatrix} \hat{u}_g(t) \\ 0 \end{pmatrix}^\top}_{=: \mathbf{h}_{nsm}(\mathbf{x}, t) \in \mathbb{R}^2} \begin{bmatrix} I_2 \\ J \end{bmatrix} \mathbf{x}_{6-7} = \begin{pmatrix} p_{pcc}(i_f^{dq}, t) \\ q_{pcc}(i_f^{dq}, t) \end{pmatrix}. \end{aligned} \right\} \quad (44)$$

As before, the argument  $t$  is only shown for disturbance signals such as wind  $v_w(\cdot)$  and grid voltage amplitude  $\hat{u}_g(\cdot)$ . Note that the (transformation) angles  $\phi_m$  and  $\phi_g$  are not needed for this WTS model. Compared to the detailed model (33) in the  $(d, q)$ -reference frame, the non-switching model (44) is of seventh order and does not cover switching. As will be shown in Sect. 6, the simulation time and computational complexity of the non-switching model (44) reduces by several orders of magnitude compared to the simulation time of the detailed model (33) (in particular due to the neglected switching).

**Remark 3.** *The average voltage dynamics could also be considered and are often approximated by a saturated first order-lag system of the following form [47, Chap. 14]*

$$\forall z \in \{\mathbf{s}, \mathbf{f}\}: \quad \frac{d}{dt} \mathbf{u}_z^{dq} = \frac{1}{T_{avg}} \text{sat}_{\hat{u}}(-\mathbf{u}_z^{dq} + \mathbf{u}_{z,ref}^{dq})$$

with initial value  $\mathbf{u}_z^{dq}(0) = \mathbf{0}_2$ .

### 5.2. Reduced-order model (rom)

In the next simplification step, no switching will be considered and only the dominant dynamics in (44) are identified. Hence, those dynamics will be neglected, which do not significantly contribute to the electrical power output of the WTS. In physical systems, the dynamics are related to the intrinsic energy storages in the system [71]. Considering the non-switching model (44) at its nominal (steady state) working point  $p_t = 2$  MW (cf. [44] and Tab. 2), the following stored energies can be computed:  $\frac{1}{2} \Theta \omega_m^2 \approx 23\,960$  kJ,  $\frac{1}{2} C_{dc} u_{dc}^2 \approx 349.92$  kJ,  $\frac{1}{2} \frac{2}{3 \kappa^2} L_s^q (i_s^q)^2 \approx 2.1$  kJ<sup>8</sup>, and  $\frac{1}{2} \frac{2}{3 \kappa^2} L_f \|i_f^{dq}\|^2 \approx 4.39$  kJ, which gives the following energy relations for all wind speeds satisfying  $v_w \geq v_{w,cut-in}$ :

$$\frac{1}{2} \Theta \omega_m^2 \gg \frac{1}{2} C_{dc} u_{dc}^2 \gg \frac{1}{2} \frac{2}{3 \kappa^2} L_f \|i_f^{dq}\|^2 \approx \frac{1}{2} \frac{2}{3 \kappa^2} L_s^q (i_s^q)^2.$$

<sup>8</sup> The factor  $\frac{1}{2} \frac{2}{3 \kappa^2}$  is due to the Clarke transformation factor  $\kappa \in \{\frac{2}{3}, \sqrt{\frac{2}{3}}\}$  which scales electrical power and energy by  $\frac{2}{3 \kappa^2}$  [47, p. 510].

This comparison of the energy contents shows that, the energies stored in the inductances of the generator and filter are rather small compared to the kinetic energy and the DC-link energy. In other words, in the reduced representation, the electrical dynamics of generator and filter will not be modeled explicitly anymore. The electrical dynamics of the currents, their current control loops with underlying VSCs are neglected. This implies that (i) the actual currents can be considered to equal their respective reference currents and (ii) the actual torque equals its reference value, i.e.

$$\mathbf{i}_s^{dq} = \mathbf{i}_{s,\text{ref}}^{dq}, \quad \mathbf{i}_f^{dq} = \mathbf{i}_{f,\text{ref}}^{dq} \quad \text{and} \quad m_m = m_{m,\text{ref}} \quad \text{in (33) or (44), resp.} \quad (45)$$

Therefore, the reduced-order model has only a three-dimensional state vector

$$\mathbf{x} := (x_1, x_2, x_3)^\top = (\omega_m, u_{dc}, \beta_\diamond)^\top \in \mathbb{R}^3 \quad (46)$$

comprising angular velocity  $\omega_m$ , DC-link voltage  $u_{dc}$  and pitch angle  $\beta_\diamond$ . The control input vector

$$\mathbf{u} := (u_1, \underbrace{(u_2, u_3)}_{=: \mathbf{u}_{2,3}^\top}, u_4)^\top = (m_{m,\text{ref}}, (\mathbf{i}_{f,\text{ref}}^{dq})^\top, \beta_{\text{ref}})^\top \in \mathbb{R}^4 \quad (47)$$

of the reduced-order model consists of the reference signals above and the reference pitch angle  $\beta_{\text{ref}}$ . Note that the four reference values come from the speed controller (38), the DC-link voltage controller (40), the reactive power (feedforward) controller (41) and the pitch controller (39), respectively, as introduced in Section 4.1.

Although the generator and filter dynamics are negligible, the resistive losses on machine and grid side should still be considered, since those scale with the squared current magnitude, i.e.

$$p_{R_s} := \frac{2}{3\kappa^2} R_s \|\mathbf{i}_s^{dq}\|^2 = \frac{2}{3\kappa^2} R_s \|\mathbf{i}_{s,\text{ref}}^{dq}\|^2 \stackrel{(24)}{=} \frac{2R_s}{3n_p^2 \psi_{pm}^2} m_{m,\text{ref}}^2 \quad (48)$$

$$p_{R_f} := \frac{2}{3\kappa^2} R_f \|\mathbf{i}_f^{dq}\|^2 = \frac{2}{3\kappa^2} R_f \|\mathbf{i}_{f,\text{ref}}^{dq}\|^2, \quad (49)$$

respectively. Note that the stator losses can be expressed by a nonlinear function of the reference torque instead of the stator currents. The copper losses in (49) will be included into the DC-link dynamics (15) as follows

$$\begin{aligned} \frac{d}{dt} u_{dc} &\stackrel{(44)}{=} \frac{2}{3\kappa^2 C_{dc} u_{dc}} \left[ -(\mathbf{i}_s^{dq})^\top \mathbf{sat}_{\hat{u}}(\mathbf{u}_{s,\text{ref}}^{dq}) - (\mathbf{i}_f^{dq})^\top \mathbf{sat}_{\hat{u}}(\mathbf{u}_{f,\text{ref}}^{dq}) \right] \\ &\stackrel{(28)}{\approx} \frac{2}{3\kappa^2 C_{dc} u_{dc}} \left[ -(\mathbf{i}_s^{dq})^\top \mathbf{u}_s^{dq} - (\mathbf{i}_f^{dq})^\top \mathbf{u}_f^{dq} \right] \\ &\stackrel{(23),(29)}{=} \frac{2}{3\kappa^2 C_{dc} u_{dc}} \left[ -(\mathbf{i}_s^{dq})^\top (R_s \mathbf{i}_s^{dq} + n_p \omega_m \mathbf{J} \psi_{pm}^{dq}) - (\mathbf{i}_f^{dq})^\top (R_f \mathbf{i}_f^{dq} + \begin{pmatrix} \frac{3}{2} \kappa \hat{u}_g(t) \\ 0 \end{pmatrix}) \right] \\ &= \frac{2}{3\kappa^2 C_{dc} u_{dc}} \left[ - (R_s \|\mathbf{i}_s^{dq}\|^2 + \underbrace{n_p \omega_m (\mathbf{i}_s^{dq})^\top \mathbf{J} \psi_{pm}^{dq}}_{\stackrel{(23)}{=} \frac{3\kappa^2}{2} \omega_m m_m}) - (R_f \|\mathbf{i}_f^{dq}\|^2 + \frac{3}{2} \kappa \hat{u}_g(t) i_f^d) \right], \quad (50) \end{aligned}$$

where, in the second to last step, (23) and (29) were solved for  $\mathbf{u}_s^{dq}$  and  $\mathbf{u}_f^{dq}$ , respectively, and the results were inserted into (50) while  $(\mathbf{i}_s^{dq})^\top \mathbf{J} \mathbf{i}_s^{dq} = 0$ ,  $(\mathbf{i}_f^{dq})^\top \mathbf{J} \mathbf{i}_f^{dq} = 0$  and the derivative terms with  $\frac{d}{dt} \mathbf{i}_s^{dq}$  and  $\frac{d}{dt} \mathbf{i}_f^{dq}$  were neglected (recall motivation above).

Finally, by recalling state vector (46) and control input vector (47), and invoking (45), (50) and (49), the nonlinear state-space representation of the reduced-order model with output is obtained as

$$\frac{d}{dt} \mathbf{x} = \underbrace{\begin{pmatrix} \frac{1}{\Theta} \left[ \varrho \pi r_t^2 v_w(t)^3 \cdot \frac{c_p(r_t x_1 / (g_r v_w(t)), \text{sat}_{0^\circ}^{90^\circ}(x_3))}{2x_1} + u_1 \right] \\ \frac{1}{C_{dc} x_2} \left[ - \underbrace{x_1 u_1}_{\stackrel{(11)}{=} p_m = p_t < 0} - \frac{2R_s}{3n_p^2 \varphi_{pm}^2} u_1^2 - \underbrace{\frac{1}{\kappa} \hat{u}_g(t) u_2}_{\stackrel{(30)}{=} p_{pcc}(i_t^d, t)} - \frac{2}{3\kappa^2} R_f \|\mathbf{u}_{2-3}\|^2 \right] \\ \text{sat}_{-\beta_{max}}^{\beta_{max}} \left( \frac{1}{I_\beta} (-\text{sat}_{0^\circ}^{90^\circ}(x_3) + u_4) \right) \end{pmatrix}}_{=: f_{rom}(\mathbf{x}, \mathbf{u}, t) \in \mathbb{R}^3} \quad (51)$$

$$\mathbf{y} = \underbrace{\frac{1}{\kappa} \begin{pmatrix} \hat{u}_g(t) \\ 0 \end{pmatrix}^\top \begin{bmatrix} \mathbf{I}_2 \\ \mathbf{J} \end{bmatrix} \mathbf{u}_{2-3}}_{=: h_{rom}(\mathbf{u}, t) \in \mathbb{R}^2} \stackrel{(30),(45)}{=} \begin{pmatrix} p_{pcc}(i_{t,ref}^d, t) \\ q_{pcc}(i_{t,ref}^d, t) \end{pmatrix}$$

with initial values  $\mathbf{x}(0) = (\omega_{m,0}, u_{dc,0}, \beta_{\diamond,0})^\top$ . Note that via the DC-link dynamics the mechanical power  $p_m = p_t = \omega_m m_{m,ref} < 0$  (minus the copper losses) is transferred to the PCC, whereas the active power  $p_{pcc}$  is induced. Again, only wind speed  $v_w(\cdot)$  and grid voltage amplitude  $\hat{u}_g(\cdot)$  are considered as time-varying disturbance signals. The reduced-order model (51) is of third order. Hence, compared to (44), six state variables are additionally eliminated, which reduces the computation and simulation time further (see Sect. 6). The controllers (38), (40), (41) and (39) presented in Sect. 4.1 can still be used as controllers of the reduced-order model(s).

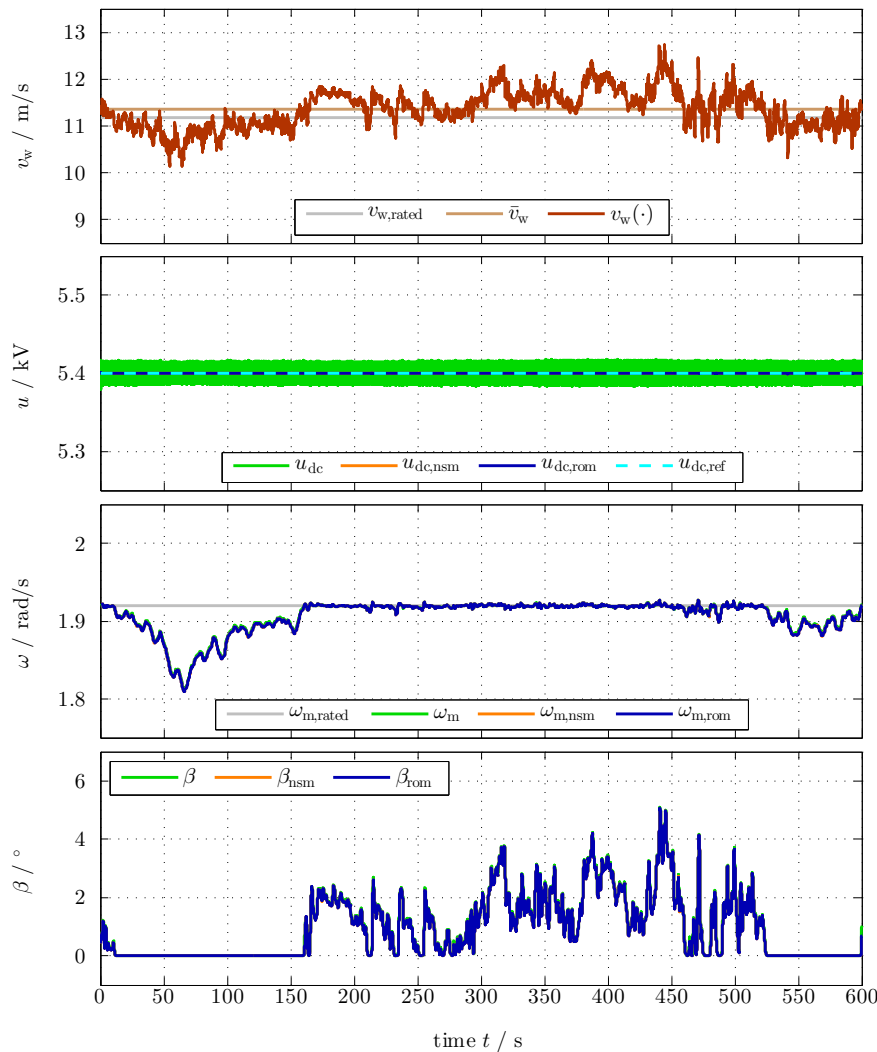
## 6. Implementation and Comparative Simulations

In this section, the full-order model (33) from Sect. 3.3.5, the non-switching model (44) from Sect. 5.1 and the reduced-order model (51) from Sect. 5.2 were implemented in Matlab/Simulink and Modelica, respectively (for details see Tab. 1). Note that, due to the equivalence of the full-order models (21) and (33), only the model in the synchronously rotating reference frame is implemented.

The comparative simulation results of all three implementations are shown in Fig. 10–12. The results will be discussed in the following. The implementations were performed exactly based on the derived state space models described in the respective sections. The implementation parameters are listed in Tab. 2. The wind data used was measured at the FINO1 research platform (54° 00' 53, 5" N, 06° 35' 15, 5" E) on the 23rd of September 2009 between 8:10 - 08:20 am (with a time resolution of 10 Hz). In all Fig. 10–12, the wind speed profile is shown in the upper most subplot. It is varying around the nominal wind speed (under- and overshooting) leading to an operation of the WTS in regime II and regime III. In order to achieve a fair comparison, all models are implemented with identical controllers/tuning for pitch angle, DC-link voltage and angular velocity (see Section 4 and Tab. 2). The closed-loop systems were fed by the identical disturbance signals such as wind speed  $v_w$  and reactive power reference  $q_{pcc,ref}$ . Since the reduced-order model (51) does not consider current dynamics, no current controllers were implemented.

The comparative simulation results of all implementations are split into three plots:

- Fig. 10 compares DC-link voltage  $u_{dc}$ , mechanical angular velocity  $\omega_m$  and pitch angle  $\beta$  of all three models;
- Fig. 11 compares turbine  $p_t$ , active  $p_{pcc}$  and reactive  $q_{pcc}$  power (at the PCC) and the produced energy  $E$  of all three models; and

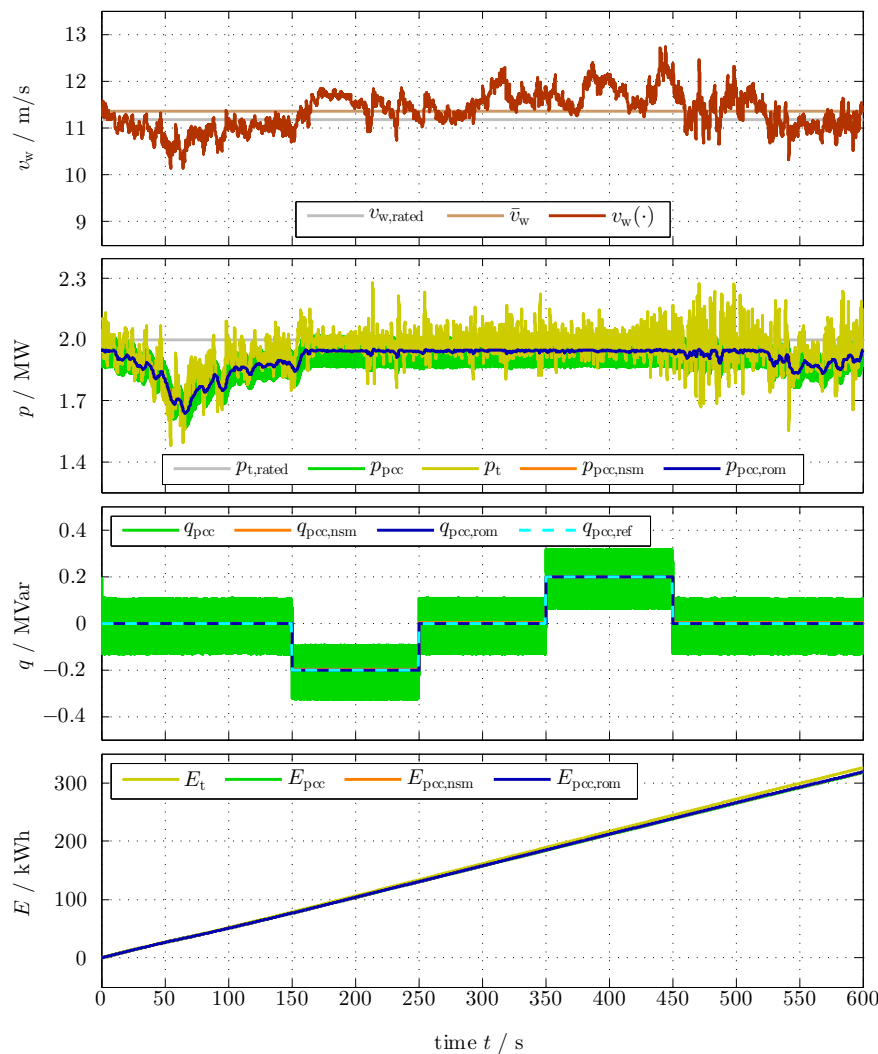


**Figure 10.** Comparison of DC-link voltages, mechanical angular velocities, and pitch angles.

- Fig. 12 compares machine torque  $m_m$  of all three models and currents  $i_s^d$ ,  $i_f^d$ , &  $i_f^q$  of full-order model and non-switching model.

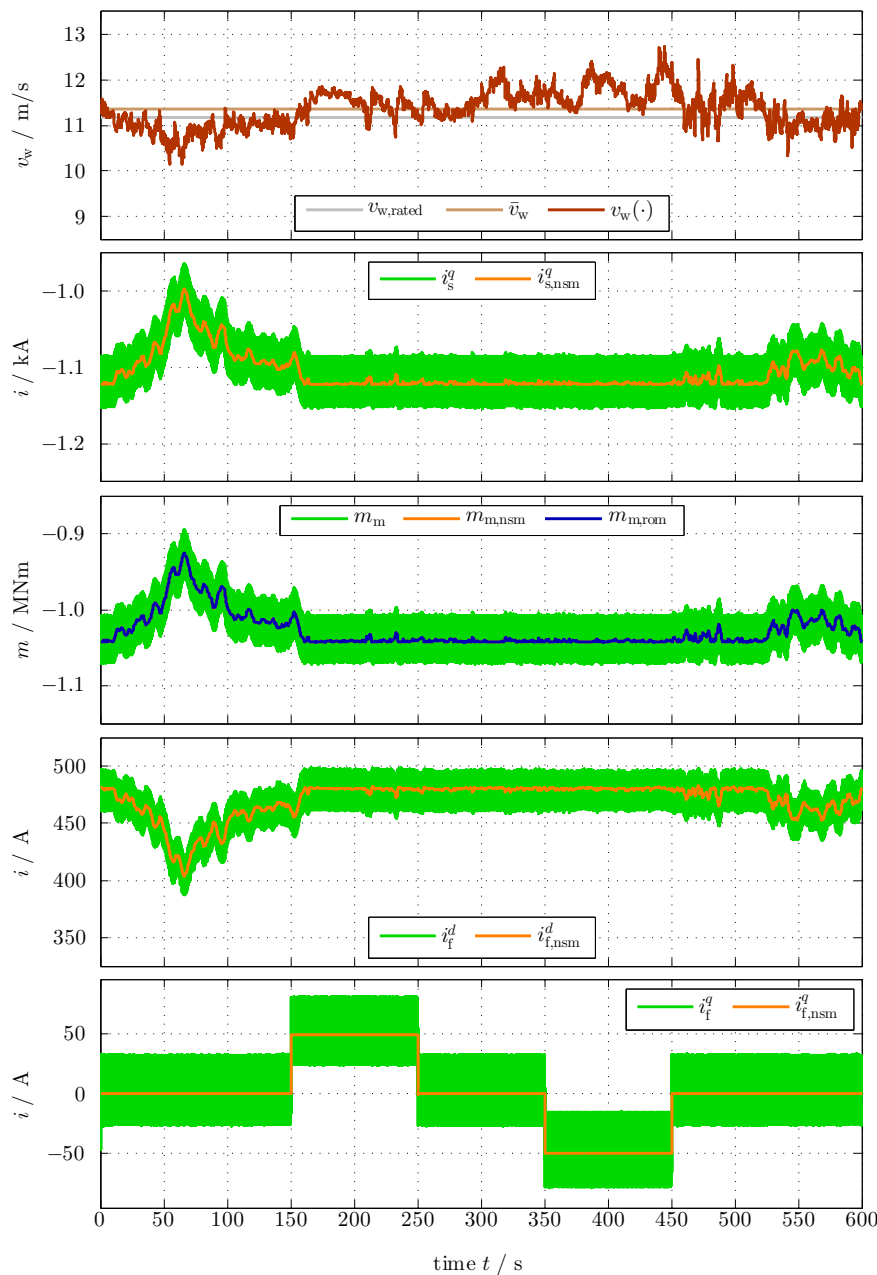
Quantities of non-switching and reduced-order model are indicated by the additional subscript “nsm” and “rom”, respectively. The quantities of the full-order model come without additional subscript. The upper subplot in Fig. 10 shows the wind speed  $v_w$ , its mean value  $\bar{v}_w$  and the nominal wind speed  $v_{w,rated}$  of the WTS. In the major part of the simulation (mainly around  $t = 150...530s$ ), the wind speed is significantly higher than the nominal wind speed of the turbine. Thus, the averaged wind speed is slightly above the nominal wind speed. In the second subplot, the DC-link voltages are depicted:  $u_{dc,nsm}$  (of non-switching model) and  $u_{dc,rom}$  (of reduced-order model) coincide and track their reference value of 5.4 kV nicely. The voltage  $u_{dc}$  (of the full-order model) also tracks its reference value but due to the modeled switching behavior of the power converters it is not as smooth as the other DC-link voltages, where the switching is not considered in the models. The third subplot shows the angular velocity of the shaft. The velocities of all three models are almost identical and do only exceed the nominal mechanical velocity  $\omega_{m,rated}$  for short periods. Hence, pitch and speed control system are working properly. The pitch angles of all three implementations are shown in the last subplot. The three pitch controllers work – as expected – very similarly and are only active when the wind speed exceeds its nominal value. The first subplot of Fig. 11 shows again the wind





**Figure 11.** Comparison of active and reactive powers, and produced energy.

speed. The second subplot depicts nominal turbine power  $p_{t,rated}$ , turbine power  $p_t$  and active power  $p_{pcc}$  at the PCC of all models. Within the time interval [150 s, 530 s), where the wind speed is (almost always) higher than the nominal wind speed of the turbine, the WTS is operated in regime III and nominal power is fed into the grid. Within the intervals [0 s, 150 s) and [530 s, 600 s), the power output undergoes strong fluctuations. The WTS is operated in regime II and follows the rapid variations of the wind power  $p_w \propto v_w^3$ . The principle behavior of all models is similar for both operating regimes. However, the switching behavior becomes only obvious in the active power of the full-order model. The active powers of the non-switching and reduced-order models represent the mean (averaged) output power of the full-order model. The reactive powers at the PCC are shown in the third subplot. The reference reactive power is followed almost instantaneously by all models (independently of being capacitive or inductive reactive power), which underpins the capability of WTSs to contribute to voltage stability of the grid. The switching behavior is again only visible for the full-order model. The fourth subplot illustrates the produced energies of turbine  $E_t := \int p_t dt$  and induced energy  $E_{pcc} := \int p_{pcc} dt$  at the PCC (as integrals of powers over time). Due to the (copper) losses in generator and filter, the turbine energy  $E_t$  is slightly higher than the electrical energies at the PCC of the three models. The trajectories of  $E_{pcc}$ ,  $E_{pcc,nsm}$  and  $E_{pcc,rom}$  are almost not distinguishable. Hence, the produced energies of the three models do match as well.



**Figure 12.** Comparison of machine torques, and machine-side and filter-side currents (if available in model).

In the first subplot of Fig. 12, again the wind speed is plotted. The second subplot shows the  $q$ -component of the stator current  $i_s^q$  for the full-order model and the non-switching model. The reduced-order model does not consider current dynamics. The non-switching model current represents the mean (average) current of the full-order model. In the third subplot, the generator torque is depicted. Its dynamics are similar to those of stator current or (negative) filter current (proportional to active power at the PCC). In regime III, the generator torque is almost constant at its nominal value (see interval [150 s, 530 s]). The switching can only be observed in the torque of the full-order model, since it is proportional to the stator current which is directly affected by the switching in the converter. The torques of non-switching and reduced-order model are not distinguishable. The fourth and fifth subplots show the  $d$ - and  $q$ -components of the filter currents which are proportional to active power and reactive power at the PCC, respectively. In both subplots, the non-switching model gives again

**Table 1.** Simulation environments and durations.

	full-order model (33)	non-switching model (44)	reduced-order model (51)
order	9	7	3
OS	Windows 10 (Education 64-bit)	Windows 10 (Home 64-bit)	Windows 10 (Home 64-bit)
CPU	Intel Xenon E5-1650 v3 (3.50 GHz, 12 CPUs)	Intel Core i7-3630QM (2.40 GHz, 4 CPUs)	Intel Core i7-3630QM (2.40 GHz, 4 CPUs)
RAM	131 072 MB	8 000 MB	8 000 MB
Software	Matlab Simulink (R2013b 64-bit)	OpenModelica (v1.11.0 64-bit)	OpenModelica (v1.11.0 64-bit)
Step size	$4 \cdot 10^{-6}$ s	$2 \cdot 10^{-4}$ s	$2 \cdot 10^{-4}$ s
Solver	Runge-Kutta (ode4)	Runge-Kutta (ode4)	Runge-Kutta (ode4)
Duration	2:58 h (10 680 s)	0:03 h (170 s)	0:02 h (119 s)

the averaged currents of the full-order model. As the currents dynamics are not simulated for the reduced-order model, these currents are not shown.

Table 1 compares the simulation environments and the duration of the simulations of all three implementations. All simulations were performed on the same operating system, only the versions differ. The full-order model was implemented in Matlab/Simulink R2013b 64-bit, and the non-switching and reduced-order model in OpenModelica v1.11.0. All simulations were run with the same fixed-step solver (Runge-Kutta (ode4)). The computational power available for the Matlab/Simulink implementation of the full-order model is significantly higher than the computational power used for the simulation of the non-switching and reduced-order model. The computer with the Intel Xeon CPUs has three times more cores than the computer with the Intel i7 CPUs. Moreover, the frequency of each core is higher. The RAM memory available for the the full-order model is more than 15 times larger than the RAM memory for the other models. However, this does not necessarily have direct impact on the simulation duration. The simulation of the full-order model needs almost 60 times longer (with 2:58 h) than the non-switching model (with 00:03 h) in order to compute active and reactive power over the scenario duration of 00:10 h = 600 s (see e.g. Fig. 10). This is due to the higher complexity of the full-order model and due to the smaller simulation step size ( $4 \cdot 10^{-6}$  s) required to simulate the switching behavior of the converters correctly. Neglecting the switching behavior allows the use of larger simulation step sizes ( $2 \cdot 10^{-4}$  s). So, the simulation durations of the non-switching and reduced-order model are smaller than 3 min (170 s) and 2 min (119 s), respectively. The simulations of these two models without switching of the power electronics could even be further accelerated by using variable-step solvers.

## 7. Conclusion

Existing control-oriented models of wind turbine systems (WTS) are subject to rather poorly motivated simplifications. In this paper, a physically motivated 11th-order state-space model in the three-phase ( $a, b, c$ )-reference frame for the whole variable-speed variable-pitch WTS with permanent magnet synchronous generator (PMSG) and its overall control and operation management system are presented. The derived full-order model considers all relevant effects (including the switching behavior of the power converters) which significantly affect the active and reactive power output of the system. By transforming the 11th-order model to its equivalent representation in the synchronously rotating ( $d, q$ )-reference frame, a 9th-order state-space model is obtained. Based on the 9th-order model, by invoking a structured and physically motivated model reduction procedure, a non-switching 7th-order model and a simple 3rd-order model are finally derived. The reduced-order models do still

capture the dominant system dynamics and cover system losses of the considered WTS. Moreover, the reduced-order state-space models come with a significant reduction of the computation/simulation time and control design complexity. The validity of the reduced-order models against their underlying full-order model is shown by numerical simulations using a realistic wind speed profile (covering regime II + III): Full-order and reduced-order models do produce plausible and (very) comparable results. Due to the modeled switching in the power converters, the full-order model exhibits realistic high frequency oscillations in the instantaneous power output; whereas the non-switching 7th-order model and the reduced 3rd-order model can only provide a time-averaged instantaneous power output. Nevertheless, both reduced-order models can correctly reflect the (average) energy production of the considered WTS. Regarding the power output, the simulations suggest no significant difference between the non-switching 7th-order model and the 3rd-order model. Thus, for dynamical power flow-based studies or controller design analysis in large-scale power systems, the proposed and validated 3rd-order model can be used to produce sufficiently exact simulation results and allows for a reasonably fast computation.

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**Table 2.** List of system parameters used for simulations.

Description	Symbol	Value
<i>Turbine &amp; gear (direct drive)</i>		
Air density	$\rho$	$1.293 \frac{\text{kg}}{\text{m}^3}$
Turbine radius	$r_t$	40 m
Turbine inertia	$\Theta_t$	$8.6 \cdot 10^6 \text{ kg m}^2$
Power coefficient	$c_{p,2}(\cdot, \cdot)$	as in (8)
Maximal change rate of pitch angle	$\dot{\beta}_{\max}$	$8 \frac{^\circ}{\text{s}}$
Pitch control time constant	$T_\beta$	0.5 s
Gear ratio	$g_r$	1
<i>Permanent magnet synchronous generator (isotropic)</i>		
Number of pole pairs	$n_p$	48
Stator resistance	$R_s$	$0.01 \Omega$
Stator inductance(s)	$L_s^d = L_s^q$	3.0 mH
flux linkage of permanent magnet	$\psi_{\text{pm}}$	12.9 V s
Generator inertia	$\Theta_m$	$1.3 \cdot 10^6 \text{ kg m}^2$
<i>Back-to-back converter</i>		
DC-link capacitance	$C_{\text{dc}}$	2.4 mF
Switching frequency	$f_{\text{sw}}$	2.5 kHz
Delay	$T_{\text{delay}} = \frac{1}{f_{\text{sw}}}$	0.4 ms
<i>Filter &amp; grid voltage</i>		
Filter resistance	$R_f$	$0.1 \Omega$
Filter inductance	$L_f$	6 mH
Grid angular frequency	$\omega_g = 2\pi f_g$	$100\pi \frac{\text{rad}}{\text{s}}$
Grid voltage amplitude	$\hat{u}_g$	2.7 kV
Grid voltage initial angle	$\alpha_0$	0 rad
<i>Controller parameters</i>		
PI current controller (37) (grid-side)	$k_{f,p}$	7.5 $\Omega$
	$k_{f,i}$	125 $\Omega \text{ s}$
	$\Delta \xi_f$	$1 \cdot 10^{-3} \text{ V}$
	$\hat{u}$	$\frac{u_{\text{dc}}}{\sqrt{3}}$
PI current controller (37) (machine-side)	$k_{s,p}$	3.75 $\Omega$
	$k_{s,i}$	12.5 $\Omega \text{ s}$
	$\Delta \xi_s$	$1 \cdot 10^{-3} \text{ V}$
	$\hat{u}$	$\frac{u_{\text{dc}}}{\sqrt{3}}$
Speed controller (38)	$k_p^*$	282.80 $\frac{\text{kNm}}{\text{s}^2}$
	$m_{m,\text{rated}}$	1.0419 MN m
DC-link voltage PI controller (40)	$k_{\text{dc},p}$	$-0.576 \frac{\text{A}}{\text{V}}$
	$k_{\text{dc},i}$	$-18.33 \frac{\text{A s}}{\text{V}}$
Phased-locked loop PI controller as in [44]	$V_{r,\text{pll}}$	20000 $\frac{1}{\text{s}}$
	$T_{n,\text{pll}}$	0.2 ms
Pitch angle reference PI controller (39)	$k_{\beta,p}$	$-400.2 \frac{^\circ \text{ s}}{\text{rad}}$
	$k_{\beta,i}$	$-100.1 \frac{^\circ \text{ s}^2}{\text{rad}}$
	$\Delta \xi_\beta$	$1 \cdot 10^{-3} \text{ }^\circ$
	$\omega_{m,\text{rated}}$	1.9195 $\frac{\text{rad}}{\text{s}}$