

Article

Impact of DC-Link Dynamics on Shaft Damping and Grid Frequency Coupling in Doubly Fed Induction Generator Wind Turbines: Mechanism Analysis and a Suppression Strategy

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Abstract

In this paper, we address shaft oscillations and grid-connected oscillation frequency coupling in doubly fed induction generators (DFIGs) under DC-link dynamics. A comprehensive DFIG shaft system model incorporating DC-link dynamics is established, and frequency coupling is analyzed. From our findings, we reached the following conclusions: (a) DC-link voltage fluctuations alter electromagnetic torque through rotor-side converter (RSC) and grid-side converter (GSC) coupling, affecting shaft dynamics; (b) DC-link dynamics compromise grid connection stability by influencing both GSC and RSC output voltages. To mitigate these effects, a DC-link dynamics suppression module is proposed. Simulations confirm that in maximum power point tracking (MPPT) mode, the module enhances electrical positive damping and improves shaft stability. In constant power mode, its stabilizing effect is comparatively limited. The suppression module effectively mitigates grid-connected frequency coupling during DC-link voltage fluctuations.

Keywords: shaft oscillation; oscillation frequency coupling; DC-link dynamics; suppression module

1. Introduction

With the advancement of wind power technology, doubly fed induction generators (DFIGs) have become the mainstream wind turbine configuration because of their cost-effectiveness, and their market penetration continues to increase [1]. However, the high penetration of wind power integration has introduced a series of grid stability challenges. Particularly notable is the sub-synchronous oscillation (SSO) phenomenon caused by the interaction between the DFIG shaft system, electrical components, and grid connection [2,3].

Practical engineering cases demonstrate that SSO can trigger large-scale wind farm disconnection incidents. For instance, in July 2015, a sub-/super-synchronous oscillation event occurred in Hami, Xinjiang, China, with active power oscillations varying between 27 Hz and 33 Hz [4]. Another notable case was the SSO incident at the UK's Hornsea wind farm on 9 August 2019, characterized by an oscillation frequency of 8.4 Hz [5]. Current research efforts primarily focus on the impact of DFIG shaft parameters, grid impedance, and controller optimization on the grid-connected stability of wind power systems [6–9]. In conventional modeling approaches for DFIG grid-connected systems, the DC-link voltage is typically assumed to be an ideal DC source, neglecting the dynamics of the DC-link [10–13]. However, in the context of DFIG shaft dynamics, DC-link voltage fluctuations affect the power output of both the rotor-side converter (RSC) and grid-side converter (GSC), thereby



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influencing the electromagnetic torque of the DFIG unit. This torque imbalance disrupts mechanical torque equilibrium, leading to rotor speed instability. If such instability exceeds safety thresholds, it can cause mechanical damage to DFIG components [14].

In the stability analysis of grid-connected DFIG systems, the results presented in Ref. [15] demonstrate that when the RSC is simplified as a constant power load, its dynamic behavior can affect system impedance through the DC-link, while the RSC and GSC become coupled via the DC-link, thereby influencing the overall system impedance. The findings presented in Ref. [16] revealed that in large-scale wind farms, the DC-link voltages of DFIG units interact with each other, and DC-link dynamics and capacitance values play critical roles in small-signal modeling at the DC-link voltage control timescale (approximately 100 ms), significantly impacting both the small-signal modeling and stability analysis of the entire system. As shown in [17], the DC-link voltage control constitutes part of the outer-loop control in voltage-source converters. Neglecting this control element would compromise the accuracy of subsynchronous oscillation analysis in DFIG-connected hybrid AC/DC grids, while frequency coupling effects would further degrade grid synchronization stability. Additionally, the phase-locked loop (PLL) and active and reactive power control loops in voltage-source converters also contribute to DC-link dynamics [18–20]. In summary, DC-link dynamics affect both the shaft dynamics of DFIGs (through RSC and GSC interactions) and the system impedance, ultimately jeopardizing grid connection security. Therefore, comprehensive electromechanical-grid modeling of DFIGs must incorporate the dynamic interactions among the DC-link, shaft system, GSC, and RSC to accurately characterize DC-link dynamics.

To mitigate the impact of DC-link dynamics on both shaft dynamics and system impedance in DFIG systems, the authors of Ref. [21] proposed an improved GSC control strategy to suppress DC-link voltage fluctuations. The authors of Ref. [22] developed a voltage-source converter controller that utilizes DC capacitance dynamics to emulate synchronous machine rotor equations, enhancing system damping through regulation of damping and inertia coefficients to suppress voltage fluctuations under power disturbances. The authors of Ref. [23] eliminated the PLL in the voltage-source converter control strategy by integrating grid synchronization with active power regulation, generating virtual power angles through DC voltage squared terms while increasing capacitance to enhance inertial support capability and reduce fluctuations during frequency deviations. The authors of Ref. [24] implemented q-axis voltage feedback in voltage-source converters to provide active damping, avoiding synchronous oscillations induced by conventional power feedforward and thereby improving weak grid stability.

Compared with existing studies, the innovations of this study are mainly reflected in three aspects:

- (1) Unlike the authors of previous studies, who treated the DC-link as an ideal voltage source or solely focused on the voltage stability of the GSC [10–13,21–24], we explicitly establish a coupling path between the torque and the DC bus through shaft system derivation. The transfer function $H_{vt}(s)$ reveals how DC-link voltage fluctuations alter the electromagnetic torque, thereby affecting the electromagnetic damping torque coefficient D_e , which directly determines the stability of shaft system oscillations.
- (2) Compared with traditional frequency-domain coupled impedance models, the model established in this study incorporates the dynamic characteristics of the DC-link [15–17], integrating the coupling effects of the RSC, GSC, and DC-link into a unified admittance term Y_{IVDC} . This admittance term is connected in parallel with the independent admittances of the RSC and GSC to form a complete impedance model of the DFIG Y_{DFIG} .

- (3) A DC-link dynamics suppression module is proposed to enhance the positive damping of the shaft system through phase modulation, which is verified via the Nyquist criterion and FFT analysis of the current at the point of common coupling.

The subsequent sections are organized as follows: In Section 2, we detail the development of the shaft model considering DC-link dynamics; in Section 3, we present the construction of the frequency-domain coupling analytical model incorporating DC-link dynamics; in Section 4, the effectiveness of the suppression strategy is analyzed and verified; in Section 5, we present the simulation validation; and in Section 6, we provide concluding remarks.

2. Modeling of DFIG Shaft System Considering DC-Link Dynamics

In DFIG systems, the DC-link provides DC voltage to both the RSC and GSC. When the DC-link voltage is subjected to disturbances, corresponding variations occur in the DFIG's electromagnetic torque and active power output. These variations affect the DFIG's electromagnetic damping, consequently impacting shaft system stability. The DFIG system configuration is illustrated in Figure 1.

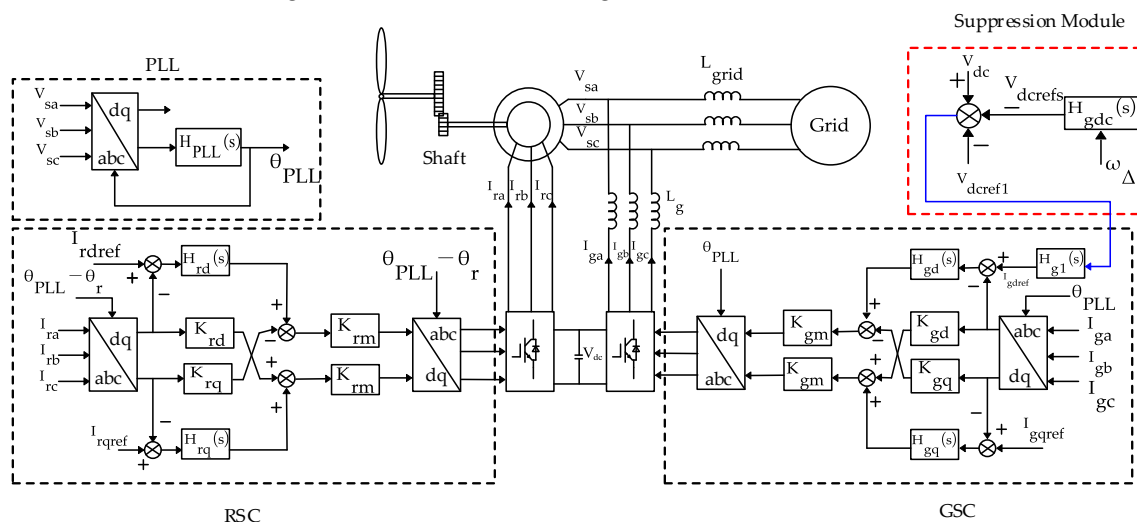


Figure 1. DFIG system diagram.

The dynamic variations of the DC-link capacitor voltage reflect power changes in both the RSC and GSC, which can be expressed using the following equation:

$$C_{dc} V_{dc} \frac{d\Delta V_{dc}}{dt} = \Delta P_{dc} = \Delta P_{gsc} - \Delta P_{rsc} \quad (1)$$

where C_{dc} is the DC-link capacitance, V_{dc} represents the steady-state DC-link voltage, and ΔP_{gsc} and ΔP_{rsc} denote the power variations at the GSC and RSC, respectively.

$$\Delta P_{rsc} = \sigma_p \omega_s \Delta T_e \quad (2)$$

where σ_p is the steady-state slip ratio, ω_s represents the synchronous angular velocity, and T_e denotes the electromagnetic torque variation.

The power response of the GSC is affected by the dynamic characteristics of its control loop. Under stator flux d-axis orientation, V_{gd} is approximately equal to the grid voltage V_g . By substituting the grid-side voltage into the GSC control loop, the corresponding expression is given as follows:

$$\Delta P_{gsc} = -V_g \left(K_{g1p} + \frac{K_{g1i}}{s} \right) \Delta V_{dc} \quad (3)$$

where K_{g1p} and K_{g1i} represent the proportional and integral gains, respectively, of the GSC's outer voltage control loop.

Substituting Equations (2) and (3) into Equation (1) yields the following:

$$C_{dc}V_{dc}\frac{d\Delta V_{dc}}{dt} = -V_g\left(K_{g1p} + \frac{K_{g1i}}{s}\right)\Delta V_{dc} - \sigma_p\omega_s\Delta T_e \quad (4)$$

Applying the Laplace transform to Equation (4) and rearranging it, the relationship between the DC-link voltage variation $\Delta V_{dc}(s)$ and electromagnetic torque variation $\Delta T_e(s)$ can be derived as follows:

$$H_{vt}(s) = \frac{\Delta V_{dc}(s)}{\Delta T_e(s)} = \frac{-\sigma_p\omega_s s}{C_{dc}V_{dc}s^2 + V_gK_{g1p}s + V_gK_{g1i}} \quad (5)$$

At the steady-state operating point, the DC-link voltage increment and electromagnetic torque increment have a fixed expression, with constant magnitude and phase characteristics. By configuring the control loop, the characteristics of the transfer function $H_{gdc}(s)/H_{vt}(s)$ can be altered to ensure that its associated electrical damping torque is positive. This process enhances shaft damping and mitigates power fluctuations caused by DC-link voltage variations, thereby reducing grid power oscillations and improving both shaft dynamics and grid stability.

To mitigate DC-link dynamics, we propose a DC-link dynamics suppression module $H_{gdc}(s)$, which integrates three functional components: phase compensation, DC blocking, and gain adjustment. The transfer function is formulated as follows:

$$H_{gdc}(s) = \frac{1 + sT_{gdc1}}{1 + sT_{gdc2}} \frac{sT_{gdc3}}{1 + sT_{gdc3}} k_{gdc} \quad (6)$$

The proposed DC-link suppression module introduces an additional transfer function into the existing DFIG control structure. The implementation complexity of this module is moderate, primarily reflected in the following aspects:

- (1) Precise parameter tuning is required based on the system operating point and natural oscillation frequency;
- (2) No additional control hardware is required;
- (3) Effective coordination with the existing RSC and GSC control loops can be achieved, avoiding the introduction of adverse interactions.

When the DC-link dynamics suppression module is incorporated, the relationship between the DFIG's electromagnetic torque variation ΔT_e and rotational speed difference variation $\Delta\omega_\Delta$ (as illustrated in Figure 2) can be expressed as follows [25].

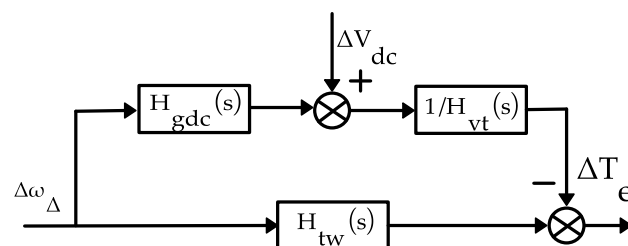


Figure 2. Block diagram of DFIG shaft transfer function under DC dynamics suppression.

$$\begin{cases} \frac{\Delta T_e}{\Delta\omega_\Delta} = H_{tw} - H_{gdc}(s)/H_{vt}(s) \\ \omega_\Delta = \omega_t - \omega_r \end{cases} \quad (7)$$

where ω_t is the turbine angular velocity and ω_r is the rotor angular velocity.

When the DFIG operates in maximum power point tracking (MPPT) control mode and disregards the inherent DC-link dynamics, based on mechanical and aerodynamic linearization, the transfer function H_{tw} is expressed as follows:

$$H_{tw} = -\frac{2H_t k_{opt} \omega_r}{H_r + H_t} \frac{sK_{rqp} + K_{rqi}}{(R_r + s\sigma')s + (sK_{rqp} + K_{rqi})} \quad (8)$$

where k_{opt} is a constant related to the inherent properties of the wind turbine and the gear ratio. The expression under constant power mode is as follows:

$$H_{tw} = \frac{PH_t}{\omega_r^2 (H_r + H_t)} \frac{(sK_{rqp} + K_{rqi})}{(R_r + s\sigma')s + (sK_{rqp} + K_{rqi})} \quad (9)$$

where H_t and H_r are the inertia time constants of the wind turbine and DFIG rotor, K_{rqp} and K_{rqi} are the proportional and integral gains of the rotor-side PI controller, P is the rated power, and $\sigma' = (L_r - L_m^2/L_s)/\omega_s$.

From Equation (7), the electromagnetic torque variation ΔT_e is decomposed into a damping torque component ΔT_1 (in phase with the speed difference variation $\Delta\omega_\Delta$) and a synchronous torque component ΔT_2 (in phase with the torsional angle variation $\Delta\theta$):

$$\Delta T_e = \Delta T_1 + \Delta T_2 = D_e \times \Delta\omega_\Delta + K_{e1} \times \Delta\theta \quad (10)$$

where D_e is the electromagnetic damping torque coefficient and K_{e1} is the electrical synchronous torque coefficient.

The dynamic equations of the two-mass shaft system are expressed in incremental form as follows:

$$\begin{cases} \frac{d\Delta\omega_\Delta}{dt} = -D_m \left(\frac{1}{2H_t} + \frac{1}{2H_r} \right) \cdot \Delta\omega_\Delta + \frac{1}{2H_t} \cdot \Delta T_m \\ \quad - K_m \left(\frac{1}{2H_t} + \frac{1}{2H_r} \right) \cdot \Delta\theta + \frac{1}{2H_r} \cdot \Delta T_e \\ \frac{d\Delta\theta}{dt} = \omega_s \cdot \Delta\omega_\Delta \end{cases} \quad (11)$$

where D_m is the damping coefficient of the wind turbine and rotor shaft, K_m is the stiffness coefficient, and T_m is the input mechanical torque.

From Equations (10) and (11), we obtain the following:

$$\frac{\Delta\theta}{\Delta T_m} = \frac{\frac{\omega_s}{2H_t}}{s^2 + \left(\frac{D_m}{2H_t} + \frac{D_m}{2H_r} - \frac{D_e}{2H_r} \right)s + \omega_s \left(\frac{K_m}{2H_t} + \frac{K_m}{2H_r} - \frac{K_e}{2H_r} \right)} \quad (12)$$

From Equation (12), the DFIG shaft oscillation damping coefficient is obtained as follows:

$$\zeta = \frac{D_m(H_t + H_r) - D_e H_t}{4H_t H_r \omega_n} \quad (13)$$

where ω_n is the natural oscillation frequency of the DFIG shaft system ($\omega_n = 9.14$ rad/s).

From Equation (13), the influence of D_e on the DFIG shaft oscillation damping coefficient, the phase relationship between ΔT_e and $\Delta\omega_\Delta$, and shaft stability is summarized in Table 1. In this paper, a negative electrical damping coefficient D_e corresponds to positive damping, whereas a positive D_e corresponds to negative damping.

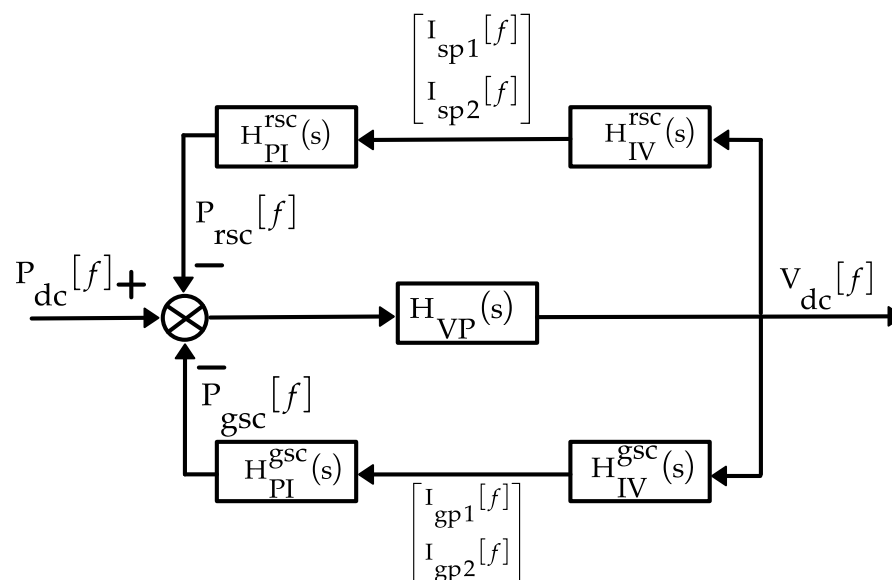
Table 1. Impact of D_e on DFIG shaft stability.

Parameter	$D_e < 0$	$D_e > 0$
Damping coefficient ζ	Increases	Decreases
Phase ($\Delta T_e / \Delta \omega_\Delta$)	$(\pi/2, 3\pi/2)$	$(-\pi/2, \pi/2)$
Electromagnetic Torque	Positive damping	Negative damping
Shaft Stability	Enhanced	Degraded

3. Frequency-Coupled Oscillation Modeling of DFIG Considering DC-Link Dynamics

3.1. Analytical Model of DFIG Under DC-Link Dynamics

As shown in Figure 1, DC-link voltage fluctuations propagate through both the outer-loop and inner-loop voltage control channels to affect the output voltage of the GSC while simultaneously influencing the RSC output voltage via the rotor-side control loop. The detailed small-signal transfer path of DC dynamics is shown in Figure 3.

**Figure 3.** Signal transmission paths of DC-link dynamics in DFIG systems.

From Equation (1), the frequency-domain transfer function H_{VP} between DC power and DC-link voltage can be derived as follows:

$$H_{VP}(s) = \frac{1}{C_{dc}sV_{dc\text{ref}}} \quad (14)$$

where $V_{dc\text{ref}} = V_{dc\text{ref}1} + V_{dc\text{ref}s} = V_{dc\text{ref}1} + \omega_{\Delta\text{ref}}H_{gdc}(s)$, $V_{dc\text{ref}}$ is the reference voltage of the DC-link, $V_{dc\text{ref}1}$ is the input reference voltage of the DC-link (a constant value), and $V_{dc\text{ref}s}$ is the compensation voltage of the DC-link. When the frequency of the DC-link voltage disturbance component is $f_p - f_l$, the d-axis current reference for the GSC at this frequency can be obtained as follows:

$$I_{gd\text{ref}}[f] = H_{g1}(s) \left(V_{dc}[f] - \omega_\Delta[f]H_{gdc}(s) \right) \quad (15)$$

where H_{g1} is the DC-link voltage outer-loop controller.

Substituting the dq-axis current components of the GSC (the frequency-domain expressions of GSC's dq-axis currents under phase-locked loop are derived in Appendix A, Equations (A1)–(A6) into the grid-side control loop yields the following:

$$\begin{cases} m_{gd} = (-I_{gd}[f] + I_{gdref}[f])H_{gd}(s) - I_{gq}[f]K_{gq} \\ m_{gq} = -I_{gq}[f]H_{gq}(s) + I_{gd}[f]K_{gd} \end{cases} \quad (16)$$

where $H_{gd}(s)$ is the inner-loop d-axis controller of the GSC, which is expressed as $H_{gd}(s) = K_{gdp} + K_{gdi}/s$, and $H_{gq}(s)$ is the inner-loop q-axis controller of the GSC, which is expressed as $H_{gq}(s) = K_{gqp} + K_{gqi}/s$, K_{gd} and K_{gq} are the dq-axis decoupling coefficients of the GSC, and I_{gd} and I_{gq} are the frequency-domain forms of the GSC dq-axis currents.

Applying the inverse Park transform to Equation (16) gives $M_a(t)$, while the GSC output voltage is determined by both the modulation coefficient K_{gm} and the DC-link voltage [11,12]:

$$V_{ga}(t) = K_{gm}V_{dc}(t)M_a(t) \quad (17)$$

where K_{gm} is the modulation coefficient of the GSC, and $V_{ga}(t)$ is the phase-a voltage of the GSC in the time domain.

Combined with the dynamic characteristics of the GSC filter inductance L_g , substituting Equations (15) and (16) into Equation (17), the relationship between the GSC current and DC-link voltage in the frequency domain is obtained as follows (this equation reveals that the GSC current perturbation is generated by the DC-link voltage through the combined effects of the inner and outer loop controllers, the decoupling terms, and the filter inductor. Furthermore, coupling exists between the positive and negative frequency components):

$$H_{IV}^{gsc}(s) = \frac{I_{gp}[f]}{V_{dc}[f]} = \begin{bmatrix} \frac{K_{gm}/2 \cdot \left\{ H_{g1}(s)H_{gd}(s)V_{dcfref} \left(1 - \frac{H_{gdc}(s)}{H_{tw}H_{vt}(s) - H_{gdc}(s)} \right) + M_{gsc1} \right\}}{V_{dcfref}K_{gm}/2 [H_{gd}(s) + H_{gq}(s) - jK_{gd} - jK_{gq}] + (s + j\omega_1)L_g} \\ - \frac{K_{gm}/2 \cdot \left\{ H_{g1}(s)H_{gd}(s)V_{dcfref} \left(1 - \frac{H_{gdc}(s)}{H_{tw}H_{vt}(s) - H_{gdc}(s)} \right) + M_{gsc1}^* \right\}}{V_{dcfref}K_{gm}/2 [H_{gd}(s) + H_{gq}(s) - jK_{gd} - jK_{gq}] + (s - j\omega_1)L_g} \end{bmatrix} \quad (18)$$

where $s = j2\pi(f_p - f_1)$, M_{gsc1} represents the fundamental-frequency voltage of the GSC, $I_{gp}[f]$ is the GSC current in the frequency domain, which is expressed as $I_{gp}[f] = [I_{gp1}[f], I_{gp2}[f]]^T$, and $*$ denotes the complex conjugate.

Similarly, the relationship between the RSC current and DC-link voltage in the frequency domain can be derived as follows:

$$H_{IV}^{rsc}(s) = \frac{I_{sp}[f]}{V_{dc}[f]} = \begin{bmatrix} -\frac{(s + j\omega_1)L_m/A_1(s) \cdot B_1(s)}{R_s + (s + j\omega_1)L_s - (s + j\omega_1)^2L_m^2/A_1(s)} \\ -\frac{(s - j\omega_1)L_m/A_2(s) \cdot B_2(s)}{R_s + (s - j\omega_1)L_s - (s - j\omega_1)^2L_m^2/A_2(s)} \end{bmatrix} \quad (19)$$

where $I_{sp}[f]$ is the stator current in the frequency domain, which is expressed as $I_{sp}[f] = [I_{sp1}[f], I_{sp2}[f]]^T$, A_1 , A_2 , B_1 , and B_2 are given as follows:

$$\begin{bmatrix} A_1(s) \\ A_2(s) \end{bmatrix} = \begin{bmatrix} \left\{ \begin{aligned} &\frac{R_r}{\sigma_p(s + j\omega_1)} + (s + j\omega_1)L_r + \\ &\frac{K_{rm}V_{dcref}K_e^2}{2\sigma_p(s + j\omega_1)} [H_{rd}(s) + H_{rq}(s) - jK_{rd} - jK_{rq}] \end{aligned} \right\} \\ \left\{ \begin{aligned} &\frac{R_r}{\sigma_{p2}(s + j\omega_1)} + (s - j\omega_1)L_r + \\ &\frac{K_{rm}V_{dcref}K_e^2}{2\sigma_{p2}(s + j\omega_1)} [H_{rd}(s) + H_{rq}(s) + jK_{rd} + jK_{rq}] \end{aligned} \right\} \end{bmatrix} \quad (20)$$

$$\begin{bmatrix} B_1(s) \\ B_2(s) \end{bmatrix} = \begin{bmatrix} \frac{1}{2} \frac{K_e K_{rm}}{\sigma_p(s + j\omega_1)} M_{rsc1} \\ \frac{1}{2} \frac{K_e K_{rm}}{\sigma_{p2}(s + j\omega_1)} M_{rsc1}^* \end{bmatrix} \quad (21)$$

In Equations (19)–(21), $H_{rd}(s)$ and $H_{rq}(s)$ are the inner-loop controllers of the RSC (subscripts d and q denote the d-axis and q-axis components of the corresponding variables, respectively), $H_{rd}(s)$ can be expressed as $H_{rd}(s) = K_{rdp} + K_{rdi}/s$, $H_{rq}(s)$ can be expressed as $H_{rq}(s) = K_{rqp} + K_{rqi}/s$, M_{rsc1} is the fundamental-frequency voltage of the RSC; K_{rd} and K_{rq} are the decoupling coefficients of the RSC, K_{rm} is the modulation coefficient, R represents the resistance (subscripts s and r correspond to the stator and rotor components of the DFIG, respectively), L is the inductance, L_m is the mutual inductance, and K_e is the turns ratio. The positive and negative sequence steady-state slip ratios σ_p and σ_{p2} in the frequency domain are given in the Appendix A, Equation (A11).

The DC-link power of the DFIG is related to both the GSC and RSC. From (1), the DC-link power can be rewritten as follows:

$$P_{dc} = -(v_{ra}i_{ra} + v_{rb}i_{rb} + v_{rc}i_{rc}) + (v_{ga}i_{ga} + v_{gb}i_{gb} + v_{gc}i_{gc}) \quad (22)$$

where subscripts a, b, and c denote the three-phase components, i and v represent the time-domain current and voltage, and subscripts r and g denote the RSC and GSC, respectively. Transforming (22) into the frequency domain yields the following:

$$P_{dc}[f] = \begin{pmatrix} (V_1 - sL_g I_{g1}^*) I_{gp1} + (V_1 - sL_g I_{g1}) I_{gp2} \\ \quad + I_{g1}^* V_{p1} + I_{g1} V_{p2} \\ -I_{r1}^* V_{rp1} - I_{r1} V_{rp2} - V_{r1}^* I_{rp1} - V_{r1} I_{rp2} \end{pmatrix} \quad (23)$$

where V_1 , V_{r1} , I_{g1} , and I_{r1} are the fundamental-frequency components of the grid voltage, rotor voltage, GSC output current, and rotor current. The subscript p1 denotes the frequency component at $f = f_p$, and the subscript p2 denotes the frequency component at $f = f_p - 2f_1$.

Substituting the frequency-domain equations of the DFIG's rotor and stator voltages (Appendix A, Equations (A7)–(A11)) into (23) gives the following:

$$P_{dc}[f] = H_{PV2}(s) \cdot \begin{bmatrix} V_{p1} \\ V_{p2} \end{bmatrix} + H_{PI}^{gsc}(s) \begin{bmatrix} I_{gp1} \\ I_{gp2} \end{bmatrix} + H_{PI}^{rsc}(s) \cdot \begin{bmatrix} I_{sp1} \\ I_{sp2} \end{bmatrix} \quad (24)$$

Specifically, the functional relationship between DC-link power and DC-link voltage ($H_{PV2}(s)$), the functional relationship between DC-link power and GSC current ($H_{PI}^{gsc}(s)$), and the functional relationship between DC-link power and RSC current ($H_{PI}^{rsc}(s)$) are detailed in Equations (A18)–(A20) in Appendix A.

From Equations (14), (18)–(21) and (A18)–(A22), the frequency-domain analytical model of DFIG DC-link dynamics can be derived as follows:

$$\frac{V_{dc}[f]}{P_{dc}[f]} = \frac{3/(2sC_{dc}V_{dc\text{ref}})}{1 + 3/(2sC_{dc}V_{dc\text{ref}})(H_{PI}^{gsc}(s)H_{IV}^{gsc}(s) + H_{PI}^{rsc}(s)H_{IV}^{rsc}(s))} \quad (25)$$

3.2. Frequency-Coupled Analytical Model of DFIG Under DC-Link Dynamics

Based on the signal transmission paths of DFIG DC-link dynamics, the frequency-coupled analytical model of the DFIG grid connection port under DC-link dynamics can be derived as follows:

$$Y_{IVDC}[s] = \frac{I_1[f]}{V_1[f]} = -\frac{V_{dc}[f]}{P_{dc}[f]} \left\{ \begin{array}{l} \left(H_{IV}^{gsc}(s) + H_{IV}^{rsc}(s) \right) \\ \left\{ \begin{array}{l} H_{PV2}(s) + \\ H_{PI}^{gsc}[s]Y_{gsc}[s] + \\ H_{PI}^{rsc}[s]Y_{rsc}[s] \end{array} \right\} \end{array} \right\} \quad (26)$$

When DC-link dynamics are neglected, the frequency-coupled impedances of the RSC and GSC in the DFIG remain decoupled. Therefore, when considering the effects of DC-link dynamics, the overall frequency-coupled analytical model of the DFIG system can be obtained by connecting in parallel the RSC and GSC frequency-coupled impedance models (Appendix A, Equations (A12)–(A17)) with the frequency-coupled analytical model of the DFIG grid connection port under DC-link dynamics:

$$Y_{DFIG} = Y_{gsc}[s] + Y_{rsc}[s] + Y_{IVDC}[s] \quad (27)$$

3.3. Frequency-Coupled Analytical Model of DFIG Grid Integration Considering DC-Link Dynamics

During DFIG grid integration, DC-link dynamics also affect the grid-connected system and induce corresponding frequency responses. When treating the three-phase balanced grid as a feedback loop, the ratio of the DFIG terminal voltage disturbance to current disturbance at frequency points f_p and $f_p - 2f_1$ constitutes the grid-connected frequency-coupled impedance. From Equations (27) and (28), the grid-connected impedance is derived as follows [26,27]:

$$Z_g(s) = \begin{bmatrix} Z_{gp1}(s) & 0 \\ 0 & Z_{gp2}(s) \end{bmatrix} \quad (28)$$

$$Z_{p1} = \frac{V_{p1}[f]}{I_{p1}[f]} = \frac{1 + Z_{gp2}(s) \cdot Y_{DFIG}^{22}(s)}{Y_{DFIG}^{11}(s)(1 + Z_{gp2}(s) \cdot Y_{DFIG}^{22}(s)) - Y_{DFIG}^{12}(s) \cdot Z_{gp2}(s) \cdot Y_{DFIG}^{21}(s)} \quad (29)$$

$$Z_{p2} = \frac{V_{p2}[f]}{I_{p2}[f]} = \frac{1 + Z_{gp1}(s) \cdot Y_{DFIG}^{11}(s)}{Y_{DFIG}^{22}(s)(1 + Z_{gp1}(s) \cdot Y_{DFIG}^{11}(s)) - Y_{DFIG}^{21}(s) \cdot Z_{gp1}(s) \cdot Y_{DFIG}^{12}(s)} \quad (30)$$

where the superscripts 11, 12, 21, and 22 denote the elements of the 2×2 matrix, Z_g represents the grid impedance, and Z_{p1} and Z_{p2} are the grid-connected impedances of the DFIG at frequency points f_p and $f_p - 2f_1$.

3.4. Assumptions and Limitations

The proposed model and analysis are based on the following assumptions and constraints:

- (1) Switching-cycle averaging assumption: All converters (RSC and GSC) are modeled using the switching-cycle averaged model, which neglects high-frequency switching harmonics at 10 kHz and above. This assumption is fully valid for analyzing the

low-frequency dynamics of interest in this paper, such as shaft system oscillations and subsynchronous oscillations.

- (2) Balanced three-phase grid assumption: The grid is assumed to be a balanced three-phase system, and all analyses are conducted in the synchronous dq reference frame. Unbalanced grid conditions are beyond the scope of this study.
- (3) Current limiting, PWM saturation, and anti-windup mechanisms: The model assumes ideal power switches without conduction or switching losses, and temporarily omits current limiting, PWM saturation, and anti-windup mechanisms. These nonlinear effects are only activated under large disturbances. Since this study focuses on small-signal perturbation conditions, their omission does not compromise the accuracy of the stability analysis.
- (4) Small-signal perturbation amplitude: All analyses and simulations are based on the small-signal linearization assumption, with the perturbation amplitude not exceeding 5% of the rated value. Large-disturbance conditions require nonlinear analysis methods, which may lead to deviations from the predictions made in this paper.

4. Impact of DC-Link Dynamics Suppression on DFIG Shaft Oscillation Damping and Grid-Connected Impedance

In addition to influencing the DFIG's electromagnetic torque and active power output (thereby altering the electrical damping torque coefficient associated with shaft oscillations), DC-link dynamics also impact grid stability through the control loops of both the RSC and GSC, ultimately modifying the DFIG unit's grid connection behavior.

4.1. Influence of DC-Link Dynamics Suppression on DFIG Shaft System

Based on the established DFIG shaft model, when rotor frequencies are 0.8 pu, 0.9 pu, 1.0 pu, 1.2 pu, 1.3 pu, and 1.4 pu, the phase angle of $-\Delta V_{dc}(s)/\Delta T_e(s)$ at the DFIG's natural oscillation frequency ($\omega_n = 9.14$ rad/s) is calculated. The results indicate an average required phase compensation of -150° , yielding parameter values of $T_{gdc1} = -0.408$, and $T_{gdc2} = 0.408$, with the DC-blocking constant set to $T_{gdc3} = 10$ and gain $k_{gdc} = 1.6$. The implementation and stability analysis of the suppression module in the discrete domain are presented in Appendix C.

Using the DFIG parameters listed in Table 2 and a grid inductance of 0.82 mH, the Bode plots of $\Delta T_e/\Delta\omega_\Delta$ are derived from (7) for rotor frequencies of 0.8 pu, 0.9 pu, 1.0 pu, 1.2 pu, 1.3 pu, and 1.4 pu. Figure 4 presents the results without DC-link dynamics suppression, while Figure 5 presents the effects with suppression implemented (Frequency (Hz) refers to the disturbance frequency).

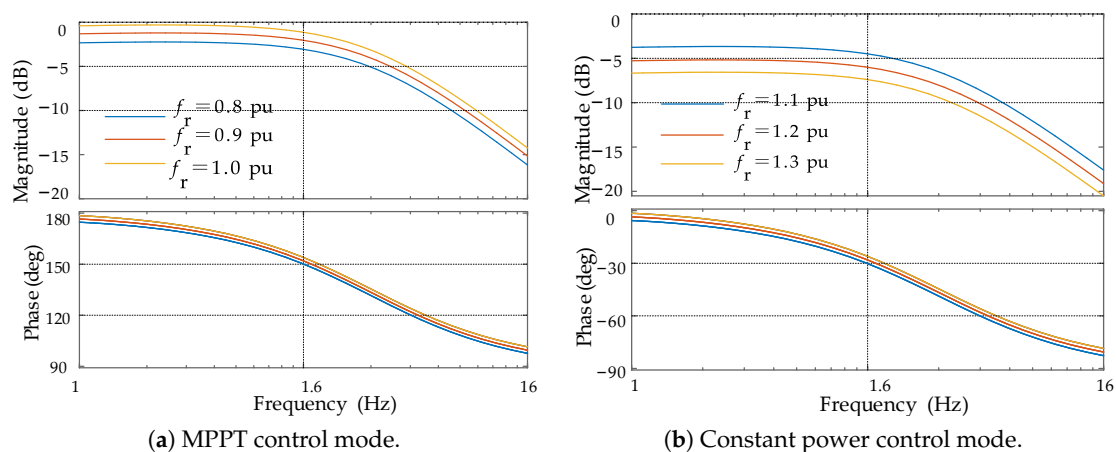
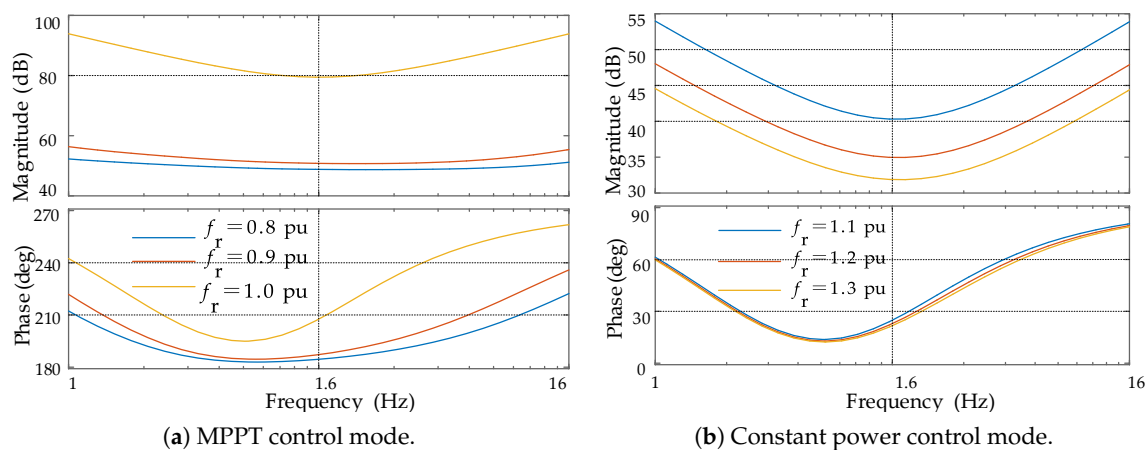


Figure 4. Bode diagrams of $\Delta T_e/\Delta\omega_\Delta$ at different wind speeds without the DC-link dynamics suppression module.

Table 2. 2MW DFIG parameters.

Parameter	Value	Parameter	Value
H_t	4.200 pu	K_m	0.430 pu
H_r	1 pu	ω_s	100π rad/s
Rated Voltage	690 V	DC-link Voltage	1200 V
Rated Power	2 MW	Pole Pair	2
f_1	50 Hz	Turns Ratio K_e	0.340
Stator Resistance R_s	0.012 pu	Rotor Resistance R_r	0.013 pu
Stator Inductance L_s	3.589 pu	Rotor Inductance L_r	3.589 pu
Mutual Inductance L_m	3.469 pu	DC Bus Capacitor C_{dc}	2.160 pu
GSC Inductance L_g	0.861 pu	K_{gd}	0.707
K_{gq}	0.618	K_{gm}	8.76×10^{-4}
K_{gdp}	3.092	K_{gqp}	7.029
K_{gdi}	2.651	K_{gqi}	8.435
K_{g1p}	11.490	K_{g1i}	0.197
k_{pPLL}	0.318	k_{iPLL}	0.010
K_{rd}	0.243	K_{rm}	8.76×10^{-4}
K_{rdp}	3.977	K_{rqp}	3.977
K_{rdi}	5.062	K_{rqi}	4.969
K_{rq}	0.910	k_{opt}	0.57

**Figure 5.** Bode diagrams of $\Delta T_e / \Delta \omega_\Delta$ at different wind speeds with the DC-link dynamics suppression module.

From Figures 4 and 5, the following observations can be made:

- (1) In MPPT control mode without the DC-link dynamics suppression module, the phase angle range of $\Delta T_e / \Delta \omega_\Delta$ is approximately 100° – 180° , with electrical damping coefficients D_e of -0.6524 , -0.7340 , and -0.8147 at different rotor frequencies. With the suppression module, the phase angle range shifts to 180° – 260° and D_e becomes -0.6728 , -0.8452 , and -2.7263 . All points lie in the second and third quadrants of the phasor diagram where $D_e < 0$, indicating positive damping for the shaft system. As rotor frequency increases, D_e increases (D_e becomes more negative), enhancing shaft stability.
- (2) In constant power control mode without suppression, the phase angle range is -90° – 0° , with D_e values of 0.5532 , 0.4648 , and 0.3961 . With suppression, the range changes to 10° – 80° , and D_e becomes 4.7579 , 2.6063 , and 1.8416 . All points are in the first and fourth quadrants where $D_e > 0$, providing negative damping that weakens shaft stability under disturbances. Under constant power control, the RSC regulates the active power reference to a fixed value rather than tracking the MPPT curve. Consequently, the electromagnetic torque T_e becomes insensitive to the angular velocity deviation

ω_{Δ} . The shaft system control adjusts the rotor current to actively counteract the speed deviation, which weakens the electromechanical coupling effect. In engineering applications, when operating in constant power mode, the wind turbine activates pitch angle control. This control mechanism essentially provides strong mechanical damping for shaft torsional oscillations. Consequently, the negative impact of the suppression module (if kept active) on shaft stability is significantly mitigated by the pitch control.

- (3) For the DFIG in MPPT mode, the suppression module significantly reduces D_e (i.e., makes it more negative), thereby increasing positive damping from the electrical system and improving shaft stability.

4.2. Impact of DC-Link Dynamics Without the Suppression Module on DFIG Grid Impedance

When the DC-link dynamics suppression module is disabled (parameters unchanged), the frequency coupling characteristics of DFIG considering DC-link dynamics (27) at rotor frequencies of 0.8 pu, 1.0 pu, and 1.2 pu are shown in Figure 6. The grid-impedance characteristics (29) are presented in Figure 7.

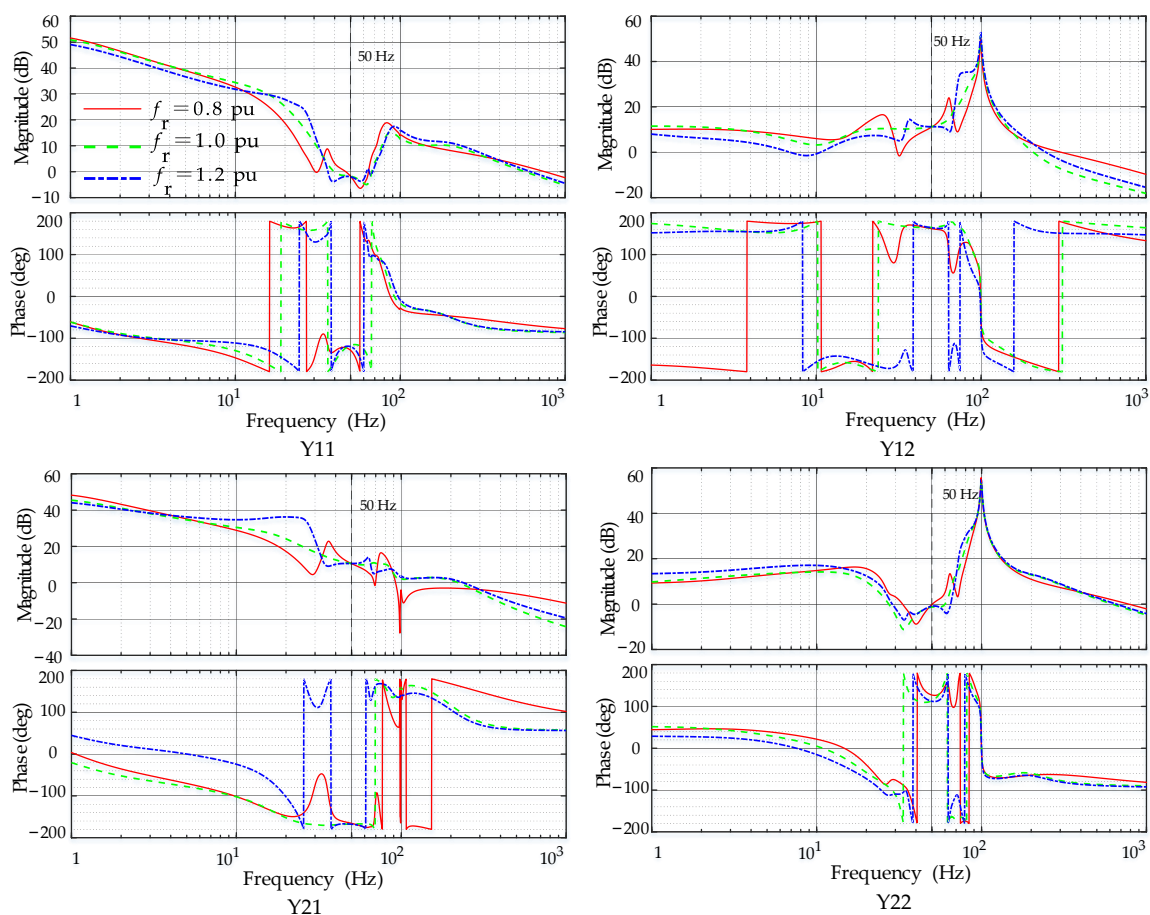


Figure 6. Frequency coupling characteristics of DFIG with DC-link dynamics (without the suppression module).

In Figure 6, it can be observed that, near the 50 Hz fundamental frequency, the amplitude ratios between diagonal components Y_{11} , Y_{22} and off-diagonal components Y_{12} , Y_{21} approach unity, indicating significant frequency coupling. As rotor frequency increases, frequency coupling characteristics change substantially below 50 Hz but remain relatively stable above 50 Hz.

In Figure 7, it can be observed that the grid impedance exhibits positive damping when $Z_{p1} > 0$ and negative damping when $Z_{p1} < 0$. The zero-crossing frequencies of Z_{p1} (where $Z_{p1} = 0$) are 2 Hz, 1.5 Hz, and 2.5 Hz at increasing rotor frequencies. Under MPPT control, this finding indicates reduced negative damping; under constant power control, it implies increased negative damping. At different rotor frequencies, DFIG grid impedance has a greater impact below 50 Hz and less influence above 50 Hz.

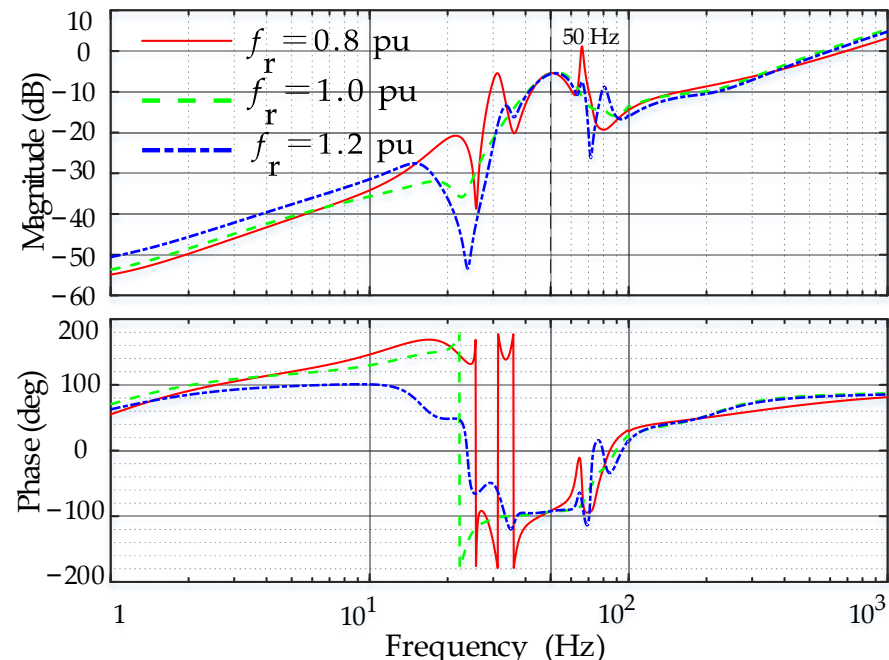


Figure 7. Grid-impedance characteristics of DFIG with DC-link dynamics (without the suppression module).

According to the generalized Nyquist stability criterion [26,27],

$$\mathbf{L} \equiv \mathbf{I} + \mathbf{Y}_{\text{DFIG}} \cdot \mathbf{Z}_g = 0 \quad (31)$$

The Nyquist plot of the DFIG, with DC-link dynamics included and the DC-link dynamics suppression module disabled, is shown in Figure 8. It is observed that all Nyquist curves encircle the $(-1, j0)$ point, with intersection frequencies at 18 Hz and 82 Hz, 20 Hz and 80 Hz, and 17 Hz and 83 Hz, respectively. This finding indicates oscillatory instability during grid synchronization, with oscillation frequencies of 18/82 Hz, 20/80 Hz, and 17/83 Hz at the grid connection point.

Based on the loop gain matrix $\mathbf{L}_1(s) = \mathbf{Y}_{\text{DFIG}} \cdot \mathbf{Z}_g$, the gain margin (GM) and phase margin (PM) of the system are calculated. Without the suppression module, when the rotor frequency is 0.8 pu, $\text{GM} = -5.1$ dB and $\text{PM} = 18^\circ$; at a rotor frequency of 1.0 pu, $\text{GM} = -1.6$ dB and $\text{PM} = 12^\circ$; and at 1.2 pu, $\text{GM} = -8.9$ dB and $\text{PM} = 16^\circ$. The GM of the system is less than 0, and the PM is relatively small, indicating that the system may experience oscillations due to multi-modal coupling or nonlinear factors.

4.3. Impact of the DC-Link Dynamics Suppression Module on Grid Impedance

With the DC-link dynamics suppression module activated (parameters unchanged), the frequency coupling characteristics (27) and grid-impedance characteristics (29) of the DFIG at rotor frequencies of 0.8 pu, 1.0 pu, and 1.2 pu are shown in Figures 9 and 10.

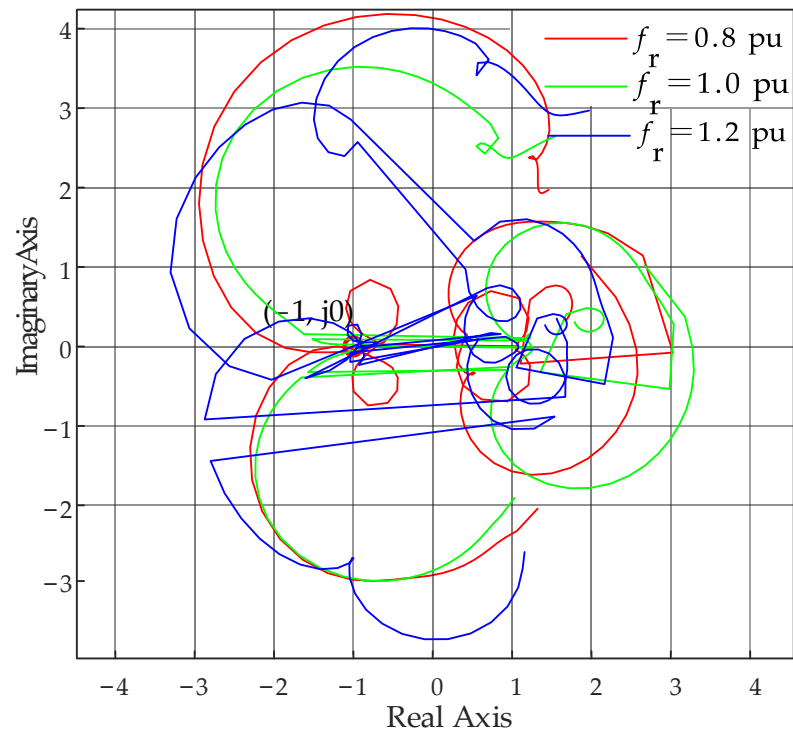


Figure 8. Nyquist plot of DFIG with DC-link dynamics (without a suppression module).

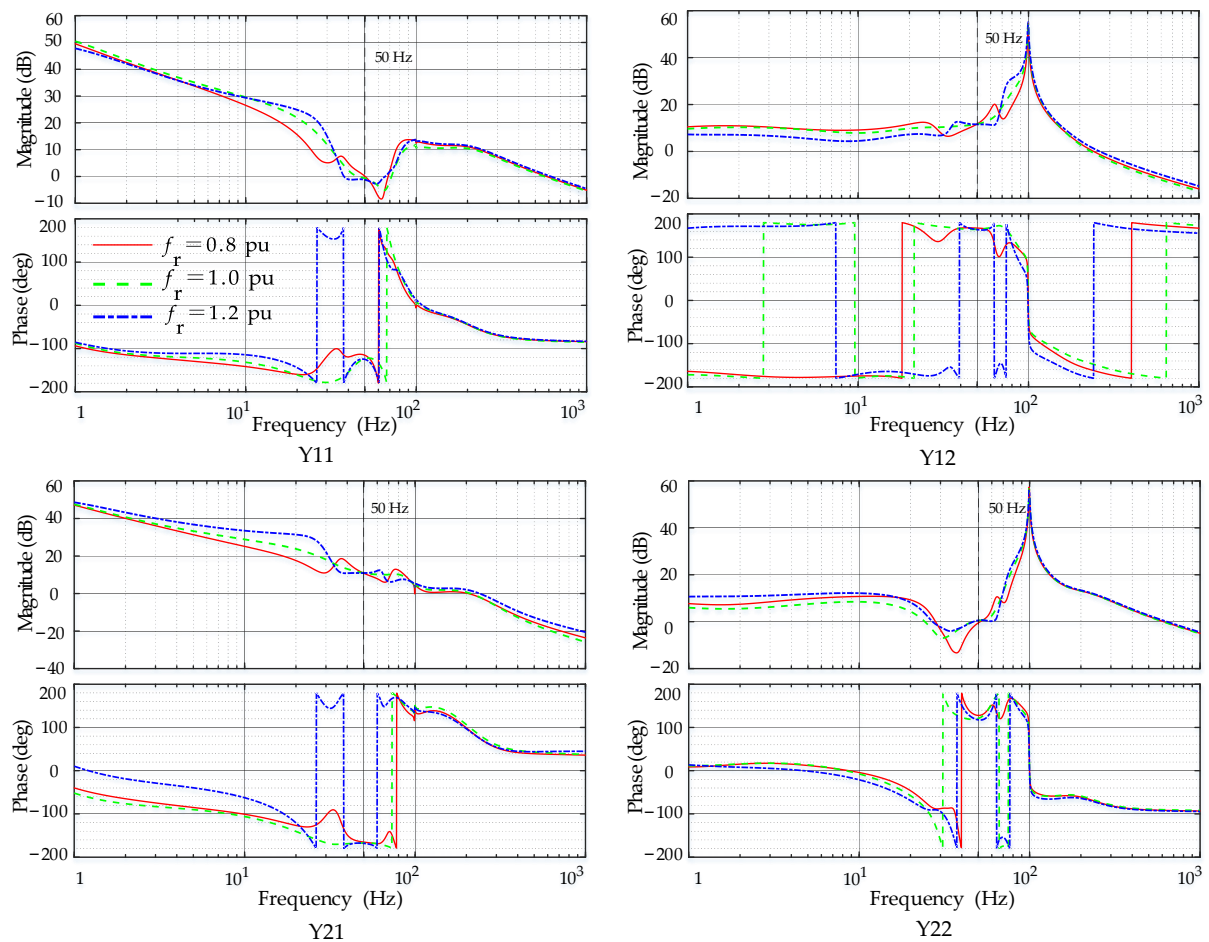


Figure 9. Frequency coupling characteristics of the DFIG under DC-link dynamics (with the suppression module).

In Figure 9, it can be observed that with the DC-link dynamics suppression module activated, the amplitude fluctuations of Y_{11} , Y_{22} , Y_{12} , Y_{21} are significantly reduced. As the rotor frequency increases, the frequencies where $Z_{p1} = 0$ in Figure 10 (DFIG grid impedance with suppression) are 1.4 Hz, 1.0 Hz, and 2.0 Hz, indicating a moderate reduction in the degree of negative damping in the grid impedance after implementing the suppression module.

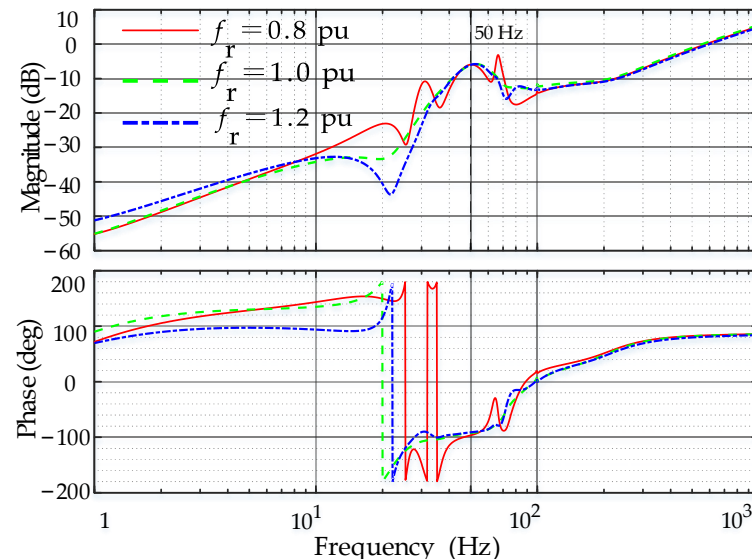


Figure 10. Grid-impedance characteristics of the DFIG under DC-link dynamics (with the suppression module).

The Nyquist plot of the DFIG with the DC-link dynamics suppression module activated is shown in Figure 11. As observed in Figure 11, neither Nyquist curve of the interconnected system encircles the $(-1, j0)$ point, demonstrating that the system satisfies small-signal stability requirements.

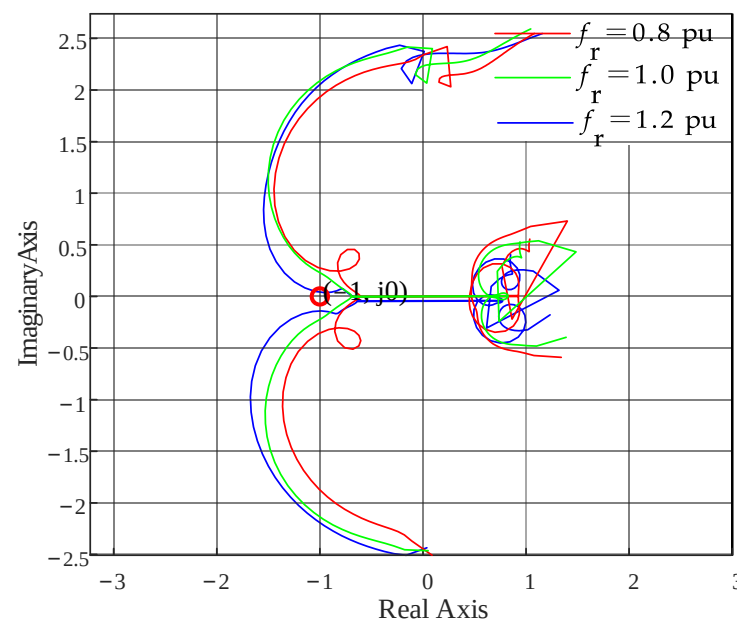


Figure 11. Nyquist plot of the DFIG under DC-link dynamics (with the suppression module).

After incorporating the suppression module, the GM and PM of the system are calculated. At a rotor frequency of 0.8 pu, GM = 8.5 dB and PM = 42°; at 1.0 pu, GM = 7.2 dB

and $PM = 35^\circ$; and at 1.2 pu, $GM = 8.1$ dB and $PM = 40^\circ$. Compared to the case without the suppression module, the stability margins have been significantly improved.

5. Simulation Verification

A 2 MW DFIG simulation model is established on the software-in-the-loop real-time simulation platform shown in Figure 12 (which includes a computer, an oscilloscope, and RT-LAB); the simulation time step is set to 1 μ s. The system sampling time is 100 μ s. In the simulation experiments, a step disturbance with an amplitude of 5% of the rated DC voltage (60 V) is superimposed on the DC bus voltage reference, and this identical disturbance is applied across all simulation experimental cases. Based on the DFIG wind power system parameters listed in Table 2 (grid inductance: 0.82 mH, rotor frequency: 0.8 pu), the results presented in Figure 13 demonstrate the impact of the DC-link dynamics suppression module on the DFIG rotor speed.

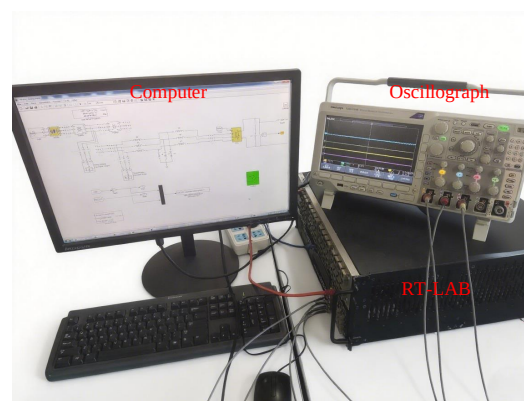


Figure 12. Real-time simulation platform.

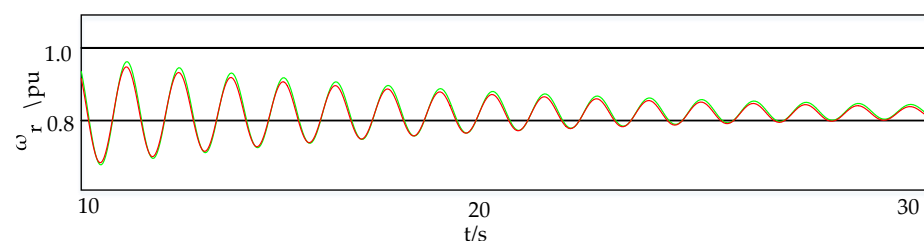


Figure 13. Rotor speed waveforms under DC-link voltage fluctuations. Red—with the suppression module; green—without the suppression module.

As seen in Figure 13, with the DC-link dynamics suppression module activated, the rotor speed fluctuations of the DFIG are significantly reduced, and shaft oscillations diminish. These results align with the shaft stability analysis presented in Section 4.1.

At a rotor frequency of 0.8 pu, after the DFIG system with the suppression module reaches steady-state operation, the following waveforms at the grid connection point are captured and shown in Figure 14: the three-phase voltage at the grid connection point (V_{ab} , V_{bc} , V_{ca}), three-phase currents (I_{abc}), and DC-link voltage (V_{dc}). The corresponding FFT analysis results are presented in Figure 15.

The FFT analysis results presented in Figure 15 reveal minor harmonic components in the grid connection point current, signifying a stable state. This finding confirms that the DC dynamics suppression link effectively mitigates frequency coupling oscillations.

At a rotor frequency of 0.8 pu, the following waveforms at the grid connection point of the DFIG machine-side system under steady-state operation without the DC-link dynamics suppression module are shown in Figure 16. The corresponding FFT analysis results are presented in Figure 17.

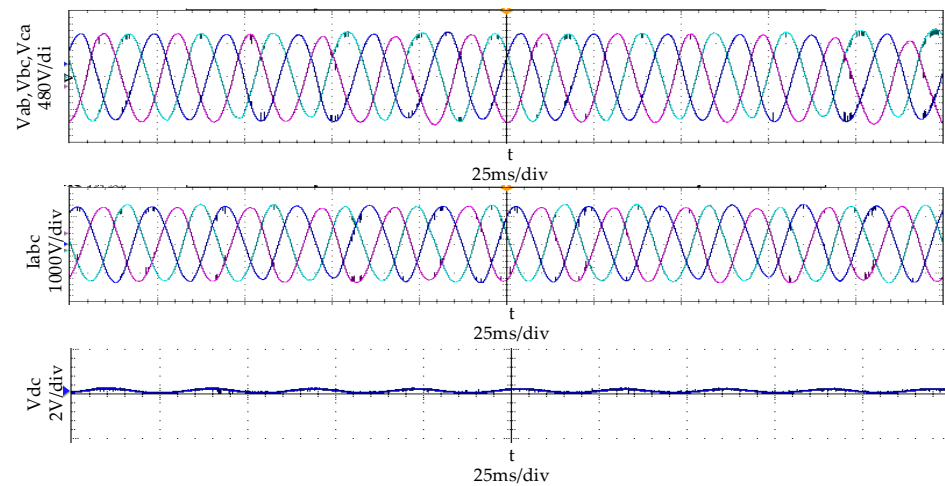


Figure 14. Grid-connected waveforms of the DFIG (with the suppression module).

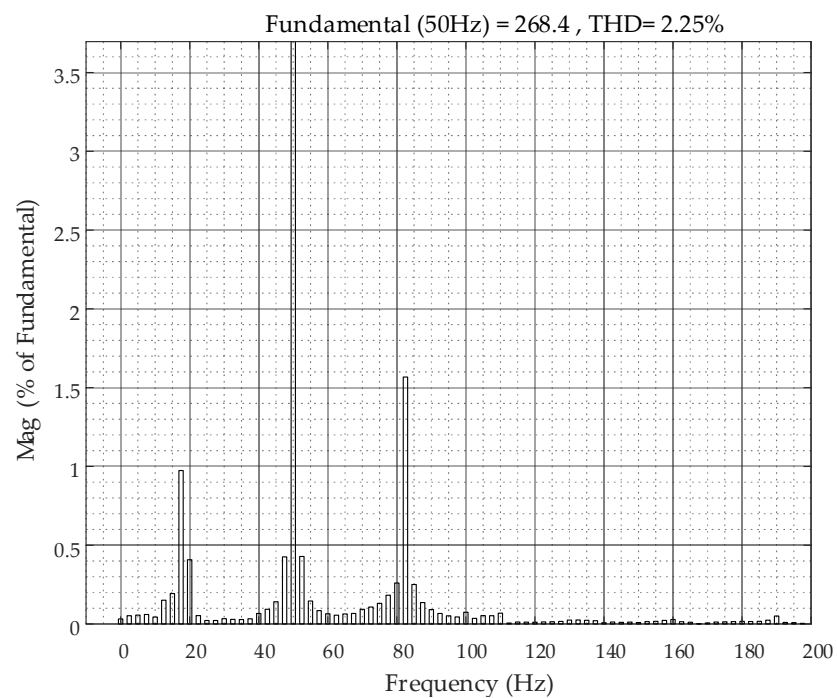


Figure 15. FFT analysis of the three-phase currents at the grid connection point (with the suppression module).

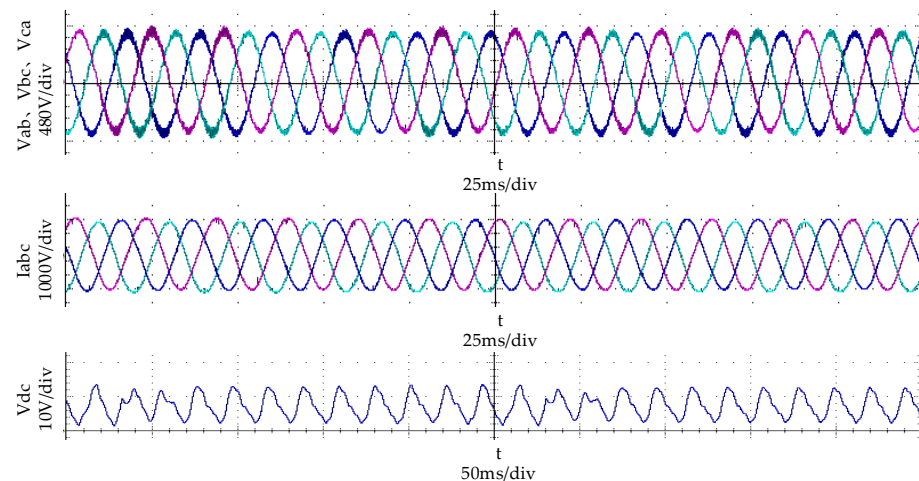


Figure 16. Grid-connected waveforms of the DFIG (without the suppression module).

In Figure 17, the FFT analysis results of the three-phase currents at the grid connection point (without the suppression module) reveal distinct harmonic components at approximately 18/19 Hz and 82/83 Hz, alongside the fundamental 50 Hz component. These frequencies correspond to sub-synchronous and super-synchronous oscillations, which are symmetrically distributed around the fundamental frequency. The presence of these components leads to a high total harmonic distortion (THD) level (the value is 19.95% higher than the THD obtained with the suppression module), indicating severe distortion of the grid current. This distortion is a direct manifestation of the frequency-coupling effect induced by DC-link dynamics. The high THD confirms that the DFIG system without suppression is prone to oscillatory instability, which can compromise power quality and grid reliability. This phenomenon aligns with the generalized Nyquist criterion results presented in Figure 8. The encirclement of the critical point $(-1, j0)$ confirms system instability at the 18/82 Hz band.

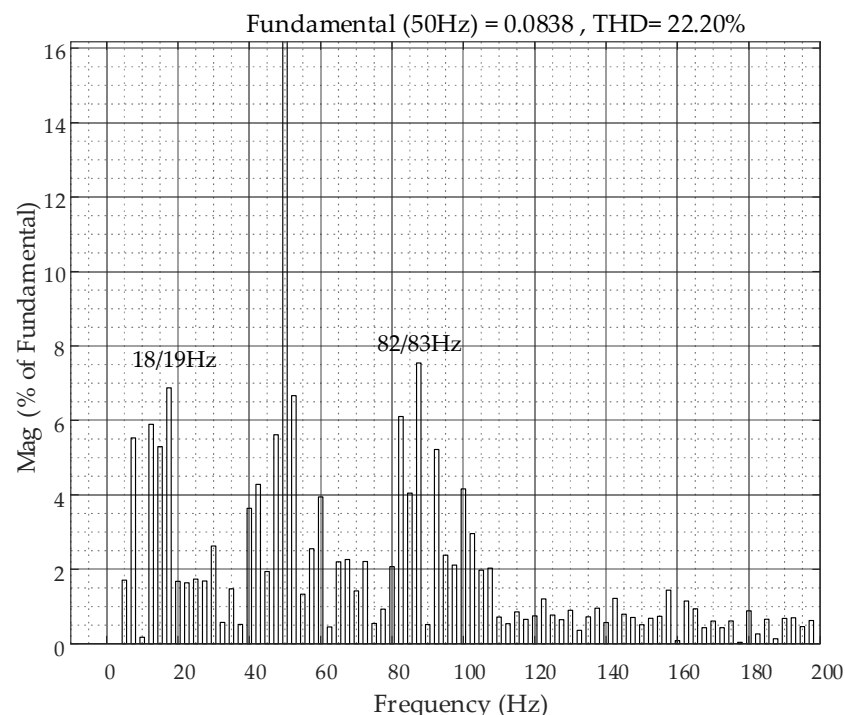


Figure 17. FFT analysis of the three-phase currents at the grid connection point (without the suppression module).

To demonstrate the effectiveness of the DC-link dynamics suppression module, the following observations can be made from Figure A2 (Appendix B): when the rotor frequency is 1.2 pu, the FFT analysis results show that after applying the DC dynamics suppression module, the harmonic component of the grid-connected current increases by 1.53% compared to the case at a rotor frequency of 0.8 pu. However, the system remains stable, which is consistent with the Nyquist analysis results presented in Figure 11. Furthermore, as shown in Figure A4 (Appendix B), at a rotor frequency of 0.8 pu and a grid inductance of 1.64 mH, the FFT analysis results indicate that after applying the suppression module, the harmonic component of the grid-connected current increases by 3.79% compared to the case at a rotor frequency of 0.8 pu, and the system still maintains satisfactory stability.

When the PLL control parameter is reduced to 0.5 times its nominal value (bandwidth: 21 Hz), the Nyquist curve in Figure A5 does not encircle the $(-1, j0)$ point. The system achieves a GM of 10.5 dB and a PM of 45° , both of which are improved over the nominal condition, confirming that the small-signal stability requirements are satisfied. Under the

same parameter setting, Figure A7 shows that the harmonic content of the grid-connected current is 5.89%, indicating stable system operation.

Conversely, when the parameter is increased to 2 times the nominal value (bandwidth: 82 Hz), the Nyquist curve in Figure A5 encircles the $(-1, j0)$ point, with a unit-circle crossing at 23/77 Hz. The GM drops to -3.5 dB and the PM to -35° , which may induce system oscillations. Furthermore, the harmonic content rises to 10.12% (an increase of 7.87% relative to the nominal condition), and the FFT analysis in Figure A9 detects the 23/77 Hz component. Therefore, increasing the PLL control bandwidth adversely affects the grid-connected operation of the DFIG.

6. Conclusions

Based on our investigation into the mechanisms of shaft oscillation and grid-connected frequency coupling in DFIGs under DC-link dynamics, we draw the following conclusions:

- (1) DC-link voltage fluctuations affect electromagnetic torque through interactions between the RSC and GSC, altering shaft dynamic characteristics. Simultaneously, they compromise system stability by propagating through control loops to the grid-connected system.
- (2) The DC-link dynamics suppression module proposed in this paper can significantly enhance electrical positive damping in MPPT mode. By optimizing the phase relationship of the electromagnetic torque response, this module effectively mitigates speed fluctuations and improves shaft stability. However, in constant power mode, its stabilizing effect is limited and may even introduce negative damping.
- (3) Frequency-domain modeling and the generalized Nyquist criterion demonstrate that the suppression module reduces the negative damping region of grid impedance, avoids encirclement of the critical point $(-1, j0)$ in Nyquist plots, eliminates sub/super-synchronous oscillations (e.g., 18/82 Hz), and enhances grid connection robustness. Simulation results validate model accuracy and confirm the engineering applicability of the proposed suppression strategy.

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Appendix A

Based on the small-signal modeling approach [11,12], the frequency-domain expressions for the voltage and current of the DFIG's GSC are given as follows:

$$V_{ga}[f] = \begin{cases} V_{g1}/2, f = f_1 \\ V_{gp1}[f], f = f_p \\ V_{gp2}[f], f = f_p - 2f_1 \end{cases} \quad (A1)$$

$$I_{ga}[f] = \begin{cases} I_{g1}/2, f = f_1 \\ I_{gp1}[f], f = f_p \\ I_{gp2}[f], f = f_p - 2f_1 \end{cases} \quad (A2)$$

where $V_{gp1}[f] = V_{gp1}e^{\pm j\varphi_{vp}}/2$, and other components can be derived analogously.

Applying the Park transformation to (A1) and (A2), the frequency-domain expression of the PLL is obtained through the harmonic linearization method:

$$\cos(\theta_{\text{PLL}})[f] = \begin{cases} 1, f = f_1 \\ \pm 1/2 [G(s)V_{p1}[f] - G(s)V_{p2}[f]], f = f_p \\ \mp 1/2 [G(s)V_{p1}[f] - G(s)V_{p2}[f]], f = f_p - 2f_1 \end{cases} \quad (\text{A3})$$

where

$$G(s) = \frac{H_{\text{PLL}}(s)}{1 + V_1 H_{\text{PLL}}(s)} \quad (\text{A4})$$

$$H_{\text{PLL}}(s) = \frac{k_{\text{pPLL}} + k_{\text{iPLL}}/s}{s} \quad (\text{A5})$$

Based on the frequency-domain expression of the grid-side PLL in the DFIG, the current expressions in the dq-frame for the GSC can be derived as follows:

$$\begin{aligned} I_{\text{gd}}[f] &= \begin{cases} I_{\text{g1}} \cos \varphi_{\text{g1}} & f = \text{dc} \\ \begin{cases} I_{\text{gp1}}[f] + I_{\text{gp2}}[f] + \\ I_{\text{g1}} \sin \varphi_{\text{g1}} [-jG(s)V_{p1}[f] + jG(s)V_{p2}[f]] \end{cases} & f = f_p - f_1 \end{cases} \\ I_{\text{gq}}[f] &= \begin{cases} I_{\text{g1}} \sin \varphi_{\text{g1}} & f = \text{dc} \\ \begin{cases} -jI_{\text{gp1}}[f] + jI_{\text{gp2}}[f] + \\ I_{\text{g1}} \cos \varphi_{\text{g1}} [jG(s)V_{p1}[f] - jG(s)V_{p2}[f]] \end{cases} & f = f_p - f_1 \end{cases} \end{aligned} \quad (\text{A6})$$

By performing a frequency transformation on the DFIG voltage and flux equations in the dq-frame, the rotor voltage expressions at frequencies f_p and $f_p - 2f_1$ are, respectively, given as follows:

$$V_{\text{sp1}}[f] = -R_s I_{\text{sp1}}[f] - sL_s I_{\text{sp1}}[f] - sL_m I_{\text{rp1}}[f] \quad (\text{A7})$$

$$\frac{V_{\text{rp1}}[f]}{\sigma_p(s)} = -\frac{R_r I_{\text{rp1}}[f]}{\sigma_p(s)} - sL_r I_{\text{rp1}}[f] - sL_m I_{\text{sp1}}[f] \quad (\text{A8})$$

$$V_{\text{sp2}}[f] = -R_s I_{\text{sp2}}[f] - (s - j2\omega_1)L_s I_{\text{sp2}}[f] - (s - j2\omega_1)L_m I_{\text{rp2}}[f] \quad (\text{A9})$$

$$\frac{V_{\text{rp2}}[f]}{\sigma_{p2}(s)} = -\frac{R_r I_{\text{rp2}}[f]}{\sigma_{p2}(s)} - (s - j2\omega_1)L_r I_{\text{rp2}}[f] - (s - j2\omega_1)L_m I_{\text{sp2}}[f] \quad (\text{A10})$$

where

$$\begin{cases} \sigma_p(s) = \frac{s - j2\pi f_r}{s} \\ \sigma_{p2}(s) = \frac{s - j2\omega_1 + j2\pi f_r}{s - j2\omega_1} \end{cases} \quad (\text{A11})$$

(A12)–(A17) present the frequency-coupled impedance analytical model for the DFIG's RSC and GSC when DC-link dynamics are neglected:

$$\begin{bmatrix} E_{11} \\ E_{12} \\ E_{21} \\ E_{22} \end{bmatrix} = \begin{bmatrix} -\left(\frac{R_r}{\sigma_p(s)K_e} + \frac{sL_r}{K_e}\right) - \frac{K_{rm}V_{\text{dcref}}K_e}{2\sigma_p} (H_{\text{rd}}(s) + H_{\text{rq}}(s) - jK_{\text{rd}} - jK_{\text{rq}}) \\ -\frac{K_{rm}V_{\text{dcref}}K_e}{2\sigma_p} (H_{\text{rd}}(s) - H_{\text{rq}}(s) + jK_{\text{rd}} - jK_{\text{rq}}) \\ -\frac{K_{rm}V_{\text{dcref}}K_e}{2\sigma_{p2}(s)} (H_{\text{rd}}(s) - H_{\text{rq}}(s) + jK_{\text{rd}} - jK_{\text{rq}}) \\ -\frac{R_r}{\sigma_{p2}(s)K_e} - \frac{(s - j2\omega_1)L_r}{K_e} - \frac{K_{rm}V_{\text{dcref}}K_e}{2\sigma_{p2}(s)} (H_{\text{rd}}(s) + H_{\text{rq}}(s) + jK_{\text{rd}} - jK_{\text{rq}}) \end{bmatrix} \quad (\text{A12})$$

$$\begin{bmatrix} F_{11} \\ F_{12} \\ F_{21} \\ F_{22} \end{bmatrix} = \begin{bmatrix} -\frac{K_{rm}V_{dcref}K_e}{2\sigma_p}G(s) \begin{pmatrix} (H_{rd}(s)/2 + H_{rq}(s)/2 - jK_{rd}/2 - jK_{rq}/2)I_{r1} \\ -(H_{rd}(s)/2 - H_{rq}(s)/2)I_{r1}^* + M_{rsc1} \end{pmatrix} \\ \frac{K_{rm}V_{dcref}K_e}{2\sigma_p}G(s) \begin{pmatrix} (H_{rd}(s)/2 + H_{rq}(s)/2 - jK_{rd}/2 - jK_{rq}/2)I_{r1} \\ -(H_{rd}(s)/2 - H_{rq}(s)/2)I_{r1}^* + M_{rsc1} \end{pmatrix} \\ \frac{K_{rm}V_{dcref}K_e}{2\sigma_{p2}(s)}G(s) \begin{pmatrix} (H_{rd}(s)/2 + H_{rq}(s)/2 + jK_{rd}/2 + jK_{rq}/2)I_{r1}^* \\ -(H_{rd}(s)/2 - H_{rq}(s)/2)I_{r1} + M_{rsc1}^* \end{pmatrix} \\ -\frac{K_{rm}V_{dcref}K_e}{2\sigma_{p2}(s)}G(s) \begin{pmatrix} (H_{rd}(s)/2 + H_{rq}(s)/2 + jK_{rd}/2 + jK_{rq}/2)I_{r1}^* \\ -(H_{rd}(s)/2 - H_{rq}(s)/2)I_{r1} + M_{rsc1}^* \end{pmatrix} \end{bmatrix} \quad (A13)$$

$$Y_{rsc}(s) = \left\{ I - \frac{L_m}{K_e} \begin{bmatrix} s & 0 \\ 0 & s - j2\omega_1 \end{bmatrix} [E(s)]^{-1} [F(s)] \right\} \left\{ \begin{bmatrix} R_s + sL_s & 0 \\ 0 & R_s + (s - j2\omega_1)L_s \end{bmatrix} - \frac{L_m}{K_e} \begin{bmatrix} s & 0 \\ 0 & s - j2\omega_1 \end{bmatrix} [E(s)]^{-1} \begin{bmatrix} sL_m & 0 \\ 0 & (s - j2\omega_1)L_m \end{bmatrix} \right\}^{-1} \quad (A14)$$

$$Y_{gsc}[s] = \begin{bmatrix} Y_{gsc}^{11} \\ Y_{gsc}^{12} \\ Y_{gsc}^{21} \\ Y_{gsc}^{22} \end{bmatrix} = \begin{bmatrix} \frac{1 - 1/2K_{gm}V_{dcref}G(s-j\omega_1) \left((H_g^1(s) - 1/2jK_{gd} - 1/2jK_{gq})I_{g1} - H_g^2(s)I_{g1}^* + M_{g1} \right)}{K_{gm}V_{dcref} \left(H_g^1(s) - 1/2jK_{gd} - 1/2jK_{gq} \right) + sL_g} \\ \frac{1/2K_{gm}V_{dcref}G(s-j\omega_1) \left((H_g^1(s) + 1/2jK_{gd} + 1/2jK_{gq})I_{g1} - H_g^2(s)I_{g1}^* + M_{g1} \right)}{K_{gm}V_{dcref} \left(H_g^1(s) - 1/2jK_{gd} - 1/2jK_{gq} \right) + sL_g} \\ \frac{1/2K_{gm}V_{dcref}G(s-j\omega_1) \left((H_g^1(s) + 1/2jK_{gd} + 1/2jK_{gq})I_{g1}^* - H_g^2(s)I_{g1} + M_{g1}^* \right)}{K_{gm}V_{dcref} \left(H_g^1(s) + 1/2jK_{gd} + 1/2jK_{gq} \right) + (s-j2\omega_1)L_g} \\ \frac{1 - 1/2K_{gm}V_{dcref}G(s-j\omega_1) \left((H_g^1(s) + 1/2jK_{gd} + 1/2jK_{gq})I_{g1}^* - H_g^2(s)I_{g1} + M_{g1}^* \right)}{K_{gm}V_{dcref} \left(H_g^1(s) + 1/2jK_{gd} + 1/2jK_{gq} \right) + (s-j2\omega_1)L_g} \end{bmatrix} \quad (A15)$$

$$H_g^1(s) = \frac{H_{gd}(s) + H_{gq}(s)}{2} \quad (A16)$$

$$H_g^2(s) = \frac{H_{gd}(s) - H_{gq}(s)}{2} \quad (A17)$$

$$H_{PV2}(s) = 3/2 [I_{g1}^* + D; I_{g1} + C] \quad (A18)$$

$$H_{PI}^{gsc}(s) = 3/2 \begin{bmatrix} -V_1 - sL_f I_{g1}^* \\ -V_1 - sL_f I_{g1} \end{bmatrix}^T \quad (A19)$$

$$H_{PI}^{rsc}(s) = \begin{bmatrix} \frac{3}{2} \begin{bmatrix} (s + j\omega_1 - j\omega_r)L_m I_{r1}^* / K_e \\ -[R_s + (s + j\omega_1)L_s]D \end{bmatrix} \\ \frac{3}{2} \begin{bmatrix} (s - j\omega_1 + j\omega_r)L_m I_{r1} / K_e \\ -[R_s + (s - j\omega_1)L_s]C \end{bmatrix} \end{bmatrix}^T \quad (A20)$$

To simplify Equation (A18) and Equation (A20), C and D are given as follows:

$$C = \frac{V_{r1} \cdot K_e + [R_r + (s - j\omega_1 + j\omega_r)L_r] \cdot I_{r1} / K_e}{(s - j\omega_1)L_m} \quad (A21)$$

$$D = \frac{V_{r1}^* \cdot K_e + [R_r + (s + j\omega_1 - j\omega_r)L_r] \cdot I_{r1}^* / K_e}{(s + j\omega_1)L_m} \quad (A22)$$

Appendix B

At a rotor frequency of 1.2 pu, after the DFIG system with the suppression module reaches steady-state operation, the waveforms at the grid connection point shown in Figure A1 are captured (the three-phase voltage at the grid connection point (V_{ab} , V_{bc} , V_{ca}),

three-phase currents (I_{abc}), and DC-link voltage (V_{dc}). The corresponding FFT analysis results are presented in Figure A2.

At a rotor frequency of 0.8 pu (grid inductance: 1.64 mH), after the DFIG system with the suppression module reaches steady-state operation, the following waveforms at the grid connection point are captured, as shown in Figure A3: the three-phase voltage at the grid connection point (V_{ab} , V_{bc} , V_{ca}), three-phase currents (I_{abc}), and DC-link voltage (V_{dc}). The corresponding FFT analysis results are presented in Figure A4.

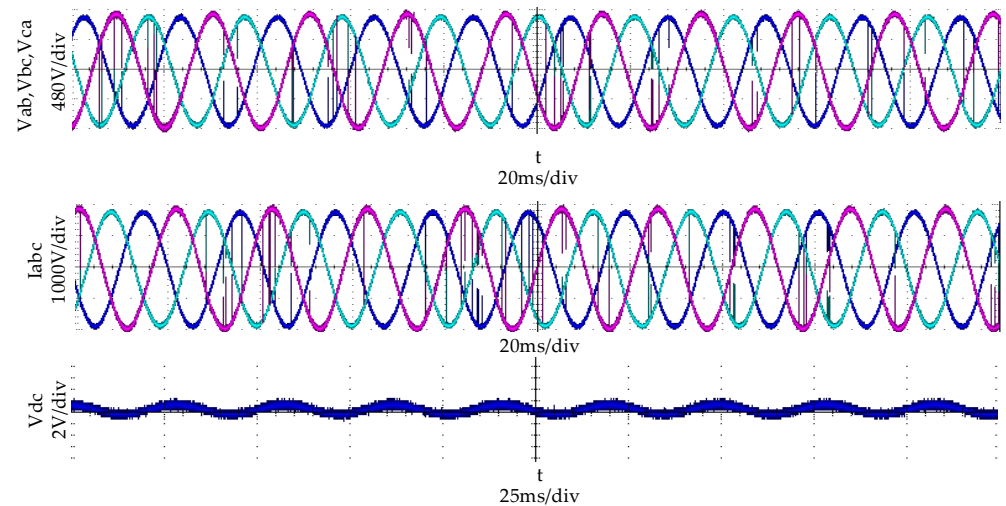


Figure A1. Grid-connected waveforms of the DFIG (with the suppression module, rotor frequency is 1.2 pu).

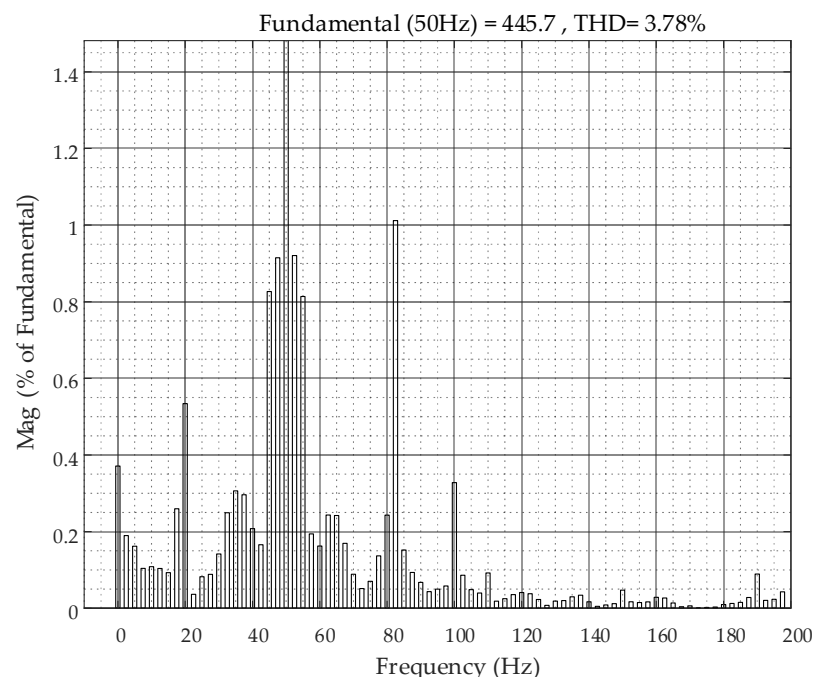


Figure A2. FFT analysis of the three-phase currents at the grid connection point (with the suppression module, rotor frequency is 1.2 pu).

Using the PLL control parameters in Table 2, adjusting [kpPLL kiPLL] to 0.5 and 2 times their original values changes the corresponding PLL control bandwidth to 21 Hz and 82 Hz from 40 Hz. Keeping the rotor frequency at 0.8 pu and the remaining parameters unchanged, the Nyquist analysis with the DC-link suppression module included is presented in Figure A5.

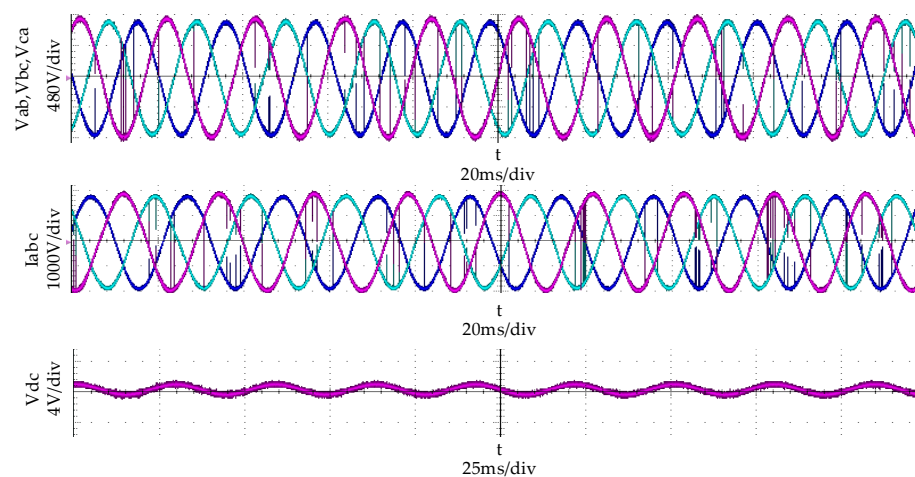


Figure A3. Grid-connected waveforms of the DFIG (with the suppression module, grid inductance: 1.64 mH).

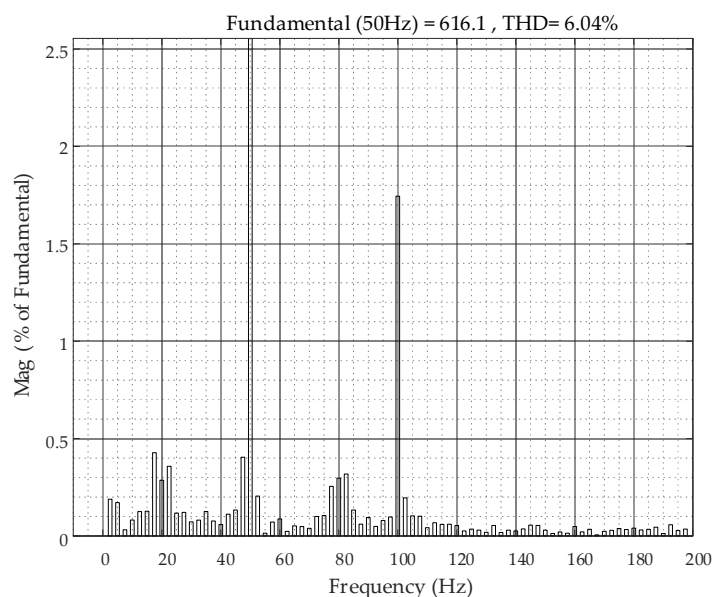


Figure A4. FFT analysis of the three-phase currents at the grid connection point (with the suppression module, grid inductance: 1.64 mH).

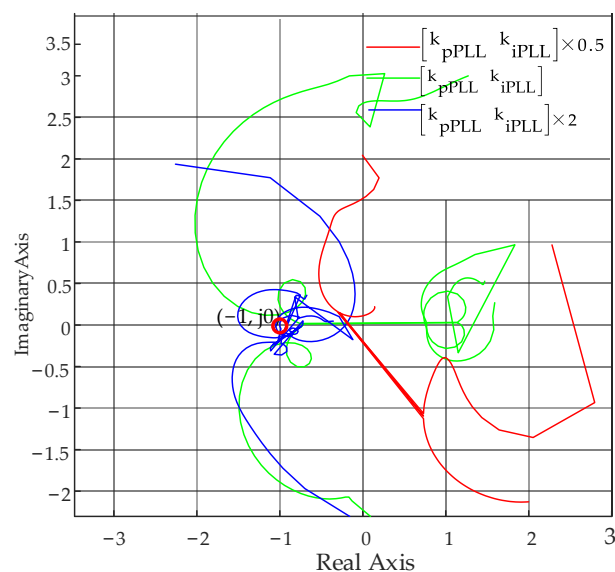


Figure A5. Nyquist plot of the DFIG considering the PLL parameters (with the suppression module).

At a rotor frequency of 0.8 pu ($[kpPLL \text{ kiPLL}] \times 0.5$), after the DFIG system with the suppression module reaches steady-state operation, the following waveforms at the grid connection point are captured and shown in Figure A6: the three-phase voltage at the grid connection point (V_{ab} , V_{bc} , V_{ca}), three-phase currents (I_{abc}), and DC-link voltage (V_{dc}). The corresponding FFT analysis results are presented in Figure A7.

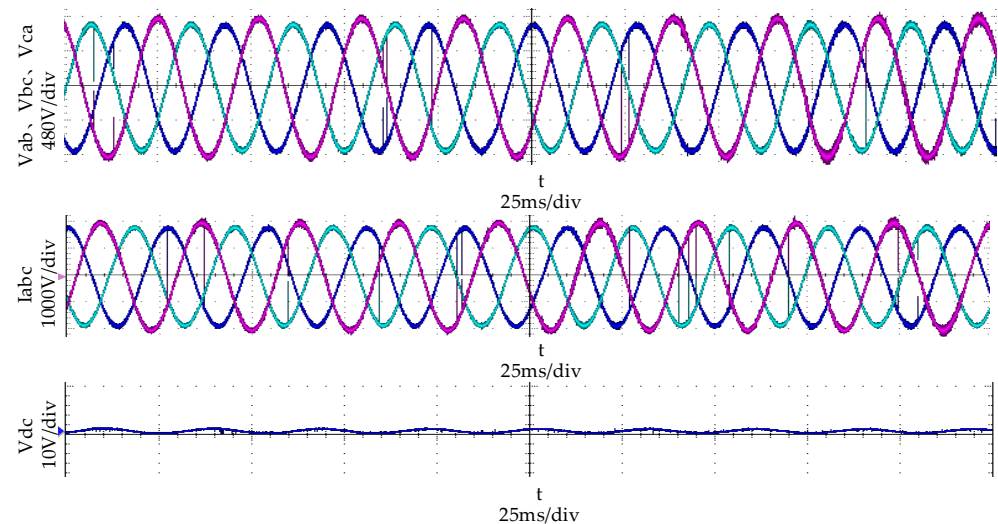


Figure A6. Grid-connected waveforms of the DFIG under $[kpPLL \text{ kiPLL}] \times 0.5$ (with the suppression module).

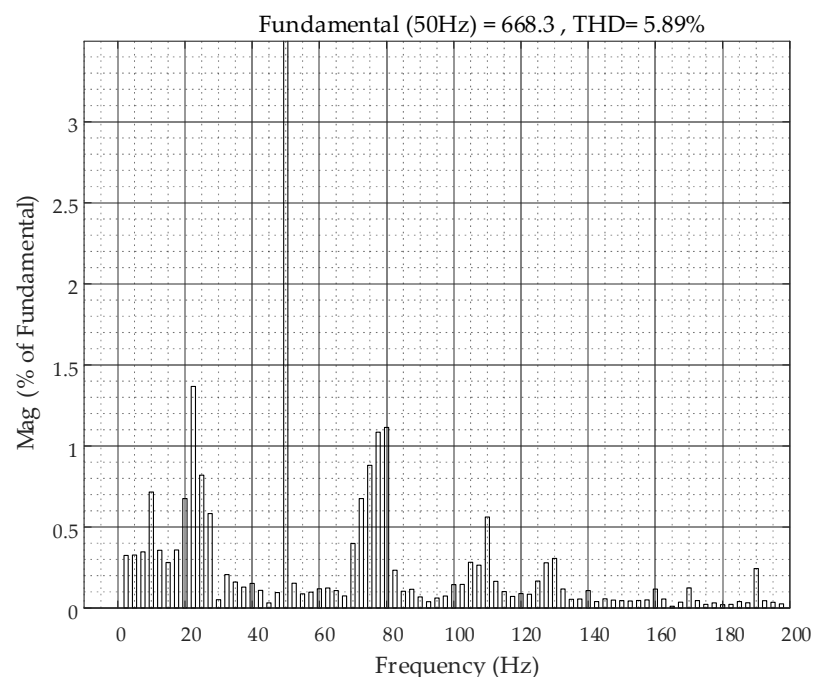


Figure A7. FFT analysis of the three-phase currents at the grid connection point (with the suppression module, $[kpPLL \text{ kiPLL}] \times 0.5$).

At a rotor frequency of 0.8 pu ($[kpPLL, \text{ kiPLL}] \times 2$), after the DFIG system with the suppression module reaches steady-state operation, the following waveforms at the grid connection point are captured and shown in Figure A8: the three-phase voltage at the grid connection point (V_{ab} , V_{bc} , V_{ca}), three-phase currents (I_{abc}), and DC-link voltage (V_{dc}). The corresponding FFT analysis results are presented in Figure A9.

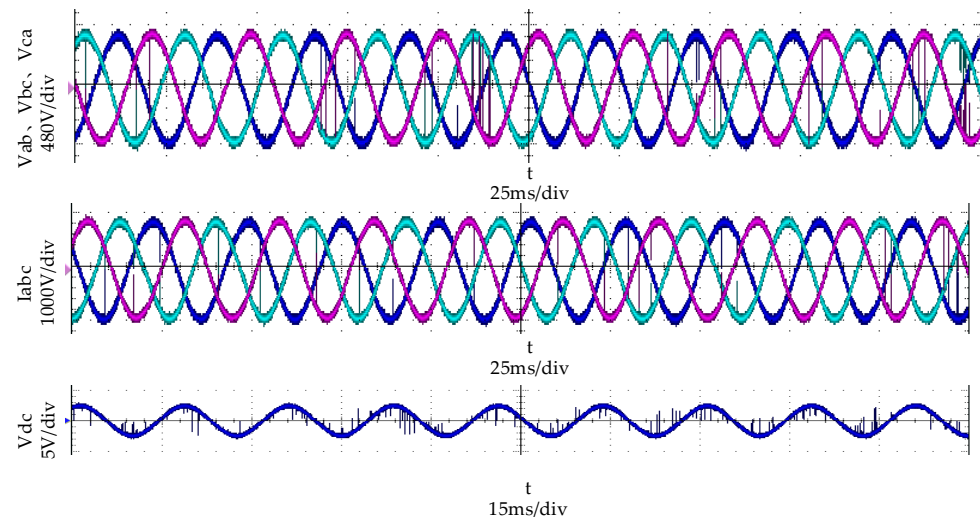


Figure A8. Grid-connected waveforms of the DFIG under $[kpPLL \ kiPLL] \times 2$ (with the suppression module).

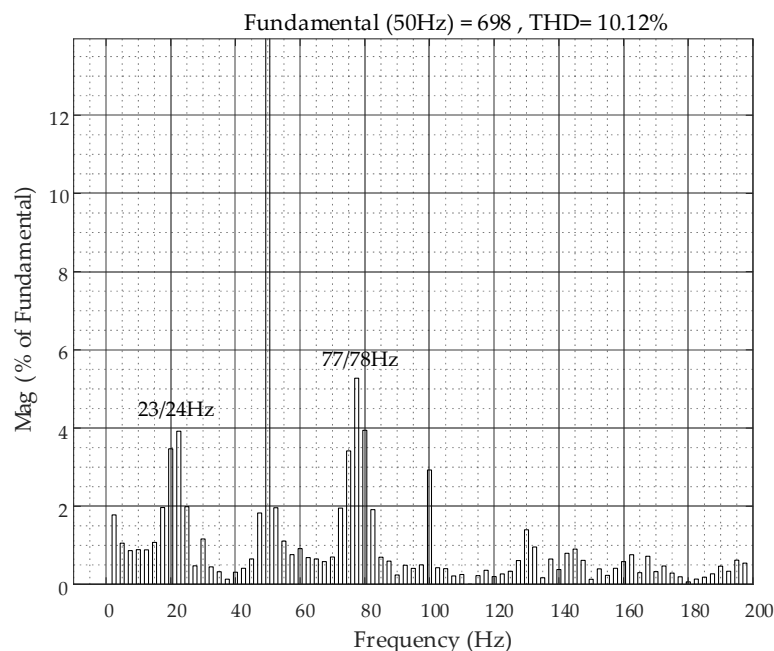


Figure A9. FFT analysis of the three-phase currents at the grid connection point (with the suppression module, $[kpPLL \ kiPLL] \times 2$).

Appendix C

With a sampling time of $T_s = 100 \mu s$, the DC-link suppression module in this paper is discretized using the bilinear transformation. The transformation formula is given by the following:

$$s = \frac{2}{T_s} \frac{z-1}{z+1} = 20000 \frac{z-1}{z+1} \quad (A23)$$

This yields the zero-pole form:

$$H_{gdc}(z) = -1.6 \frac{(z-1)(z-1.0002)}{(z-0.999755)(z-0.99999)} \quad (A24)$$

From Equation (A24), the discrete-domain poles are 0.999755 and 0.999990. Since both lie within the unit circle, the controller itself is asymptotically stable. The discrete-domain zeros are 1.000245 and 1. The zero at 1.000245 is located in the right half-plane (RHP),

introducing a non-minimum phase characteristic. However, since the controller bandwidth is primarily in the low-frequency range, this characteristic will not affect the high-frequency dynamic performance. The zero at 1 is introduced to achieve DC blocking, ensuring that the suppression module does not compromise the steady-state regulation accuracy of the DC-link voltage.

In summary, the condition $T_{gdc1} < 0$ introduces an RHP zero. This phenomenon is acceptable and does not compromise system stability. The compensator is causal and stable and can be readily implemented in the discrete-time domain.

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