



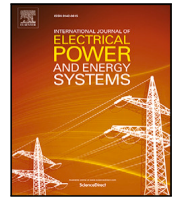
A frequency-domain study of damping and synchronizing characteristics of virtual admittance-based grid-forming converters

Downloaded from: <https://research.chalmers.se>, 2026-10-01 15:22 UTC

Citation for the original published paper (version of record):

Kamalinejad, K., Narula, A., Bongiorno, M. et al (2026). A frequency-domain study of damping and synchronizing characteristics of virtual admittance-based grid-forming converters. *International Journal of Electrical Power and Energy Systems*, 182. <http://dx.doi.org/10.1016/j.ijepes.2026.112185>

N.B. When citing this work, cite the original published paper.



A frequency-domain study of damping and synchronizing characteristics of virtual admittance-based grid-forming converters

Kavian Kamalinejad ^{a,*}, Anant Narula ^a, Massimo Bongiorno ^a, Mebtu Beza ^a,
Jan R. Svensson ^b

^a Chalmers University of Technology, Hörsalsvägen 11, 41296, Gothenburg, Sweden

^b Hitachi Energy Research, Hitachi Energy, Forskargränd 7, 722 26, Västerås, Sweden

ARTICLE INFO

Keywords:

Active phase-jump power
Damping characteristics
Frequency response
Synchronizing capability
Virtual admittance-based control

ABSTRACT

This paper investigates the damping and synchronizing characteristics that a grid-forming (GFM) converter should offer to the grid by analyzing the influence of its control parameters. With a focus on the virtual-admittance-based GFM (VA-GFM) control strategy, the analysis is based on the controller's frequency response, obtained from a linearized model of the GFM converter. Such frequency domain-based assessments have recently been recommended by system operators, as they enable the converter to be treated as a black box model, thereby simplifying the evaluation process. However, existing approaches offer limited insight into how control parameters affect damping and synchronization across varying frequency regions. The results show that GFM control parameters influence these damping and synchronization properties differently depending on the frequency range, offering valuable insights for control system design. The analytical findings are validated through detailed time-domain simulations in MATLAB/Simulink.

1. Introduction

Driven by global objectives to reduce greenhouse gas emissions, modern power systems are undergoing a transition from conventional fossil-fuel-based generation to renewable energy sources (RES), which are predominantly interfaced with the grid through voltage-source converters (VSCs) [1]. The growing penetration of such converter-based resources introduces new stability concerns, leading to the development of grid-forming (GFM) control strategies, initially conceived to replicate the dynamic behavior of synchronous generators, as a means of addressing these challenges [2]. Converters equipped with GFM functionality can establish the grid voltage, support the short-circuit power (fault level), and provide inertial support, thereby contributing to the overall system stability [3].

Moreover, similar to synchronous machines, which should desirably have a positive damping and synchronizing power [4], it is essential for GFM converters not only to exhibit an inherently damped dynamic response, but also to actively contribute to the overall damping of the system, particularly during disturbances. Additionally, GFM converters must possess strong synchronizing capabilities to maintain stable operation and remain in synchronism with the grid, ensuring reliable performance [5].

To examine the damping and synchronizing characteristics of a GFM converter, both nonlinear and linear analytical methods can be

applied [6]. Within the nonlinear category, electromagnetic transient (EMT) simulations and root-mean-square (RMS) dynamic models are the most widely used tools [7]. EMT simulations offer a detailed representation of fast converter dynamics and switching effects, while RMS models use phasor-domain representations of voltages and currents, reflecting steady-state conditions from the electrical perspective. RMS models capture slower system dynamics, such as electromechanical dynamics in synchronous generators, and preserve nonlinear system behaviors. This results in lower computational effort compared to EMT models while retaining the required dynamics. These nonlinear approaches allow the converter's response to large disturbances, control nonlinearities, and varying network conditions to be studied in depth, providing valuable insight into its overall dynamic performance.

Although nonlinear analysis can provide highly accurate results, it often requires considerable computation effort. Therefore, linearized analysis methods, applied either in the time domain or frequency domain, are commonly used to obtain quicker evaluations of system behavior [8]. Linearization enables analytical investigation of how system and control parameters affect the converter's dynamic response, such as damping and overshoot. Among these linear approaches, eigenvalue analysis of the system's state-space model is widely adopted for power systems studies. For example, the impact of virtual impedance variations or different damping controllers on the eigenvalues of GFM

* Corresponding author.

E-mail address: Kavian@chalmers.se (K. Kamalinejad).

<https://doi.org/10.1016/j.ijepes.2026.112185>

Received 24 December 2025; Received in revised form 25 April 2026; Accepted 7 September 2026

Available online 24 September 2026

0142-0615/© 2026 The Authors. Published by Elsevier Ltd. This is an open access article under the CC BY license (<http://creativecommons.org/licenses/by/4.0/>).

converters is analyzed in [9,10], while the eigenvalue-based stability of GFM converters under different short-circuit ratios (SCRs) is investigated in [11]. However, developing a complete state-space representation requires full knowledge of system parameters and control structures, which may not always be available [12]. In situations where such information is limited, frequency-domain methods, for example network frequency perturbation (NFP) analysis, Nyquist criterion, or passivity-based techniques, provide effective alternatives [13–17]. Frequency methods offer clear advantages over the aforementioned approaches. In particular, the converter can be treated as a black-box system. Small-signal perturbations are injected at selected frequencies to directly measure the output response, without requiring detailed knowledge of internal control implementations [18,19]. However, they primarily serve as tools for assessing system stability. However, such analyses typically offer limited insight beyond stability margins. In particular, they do not adequately capture system requirements across different frequency regions, nor do they clarify how control parameters affect damping and synchronization characteristics both within and across these regions. In addition, the trade-offs in controller design between damping characteristics and synchronizing performance are seldom considered. These limitations highlight the need for a more comprehensive and physically interpretable framework to understand how GFM converters meet grid requirements and how control design choices affect system behavior.

Although the standardization and grid-code requirements for GFM converters are still under development, several widely accepted characteristics are outlined in the literature. In particular, GFM converters are expected to operate as a voltage source behind an impedance, with outer control loops whose bandwidth is limited below a specified frequency [20]. This paper categorizes the frequency-domain response into three regions, as illustrated in Fig. 1. At low frequencies, typically up to 5 Hz, the GFM converter is expected to exhibit a constant-power behavior by accurately tracking its power setpoints. In this frequency range, the converter dynamics are mainly governed by the outer power-control loops, and the characteristic of a slowly varying voltage source inherent to GFM operation is realized [20]. At very low frequencies, beyond the close-to-zero but still well below the fundamental frequency, the system may become susceptible to low-frequency oscillations. Ideally, proper tuning of the power controllers results in a well-damped first-order response. In practice, insufficient damping, caused by unfavorable power controller tuning or adverse system conditions, may prevent this ideal behavior, allowing low-frequency oscillatory modes to emerge. Around the fundamental frequency in the rotating dq -frame, the converter should provide sufficient damping to minimize the amplification of DC components during transient events. These DC components originate from electrical modes associated with the resistance-to-inductance ratio of the system. Adequate damping in this frequency range is therefore essential, as DC components pose a serious risk to grid-connected equipment such as transformers and must be effectively suppressed [14]. This can be achieved by introducing transient or virtual resistive elements to provide additional damping. Beyond this, in the harmonic range, the influence of the outer loops diminishes and the GFM converter is expected to behave as a passive element. In this region, the converter should function as a constant impedance, predominantly defined by the physical filter and any implemented virtual impedance. This impedance-based behavior ensures predictable and stable interaction with the rest of the system, while enabling the converter to contribute to harmonic attenuation and overall system robustness [21].

To address the gap for providing a unified and frequency-resolved view of key dynamic behaviors, the aim of this paper is to investigate the damping and synchronizing properties of GFM converters, and to analyze how these properties manifest across different frequency regions to meet grid requirements. The focus of this work is on providing qualitative insight into how control parameters influence damping and

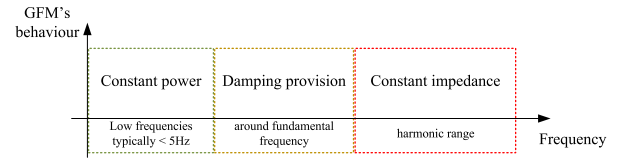


Fig. 1. Desired GFM control behavior at different frequencies.

synchronizing characteristics, and on highlighting that controller tuning inherently involves trade-offs between damping characteristics and synchronizing performance across different frequency regions, based on an analysis of the converter's intrinsic behavior independent of specific grid conditions, rather than on establishing explicit quantitative design limits. The study focuses on the influence of key control parameters within the virtual-admittance-based GFM (VA-GFM) control strategy, which is selected for its promising performance characteristics [22]. Frequency-domain analysis is employed, as it effectively represents the behavior of the converter during grid disturbances and has been recommended for compliance testing of GFM systems in recent grid codes [6, 23]. In this work, the frequency response of the exchanged active power under frequency and angle perturbations is analyzed to investigate how different control parameters influence the converter's dynamic behavior across distinct frequency ranges. Considering frequency and angle variations as system inputs also provides a more physically intuitive interpretation compared to impedance-based methods, since damping and synchronization properties are directly reflected in the power response to these perturbations. Furthermore, nonlinear time-domain simulations are conducted to validate the trends observed in the frequency domain. The main contributions of this paper are summarized as follows:

- a dedicated analysis of the VA-GFM control strategy with respect to its damping and synchronizing characteristics;
- a frequency-domain based characterization of these properties across distinct frequency regions;
- a consolidated dynamic characterization that connects small-signal frequency-response behavior with system-level damping and synchronizing performance.

The rest of the paper is organized as follows: Section 2 describes the system under study together with the controllers. The linearized equations are derived in Section 3 to obtain the frequency-domain responses and Bode plots. In Section 4, damping properties of the GFM converter are discussed based on the frequency analysis and a verification follows using time-domain simulation. Section 5 extends the analysis and verification to synchronizing characteristics and finally, conclusions are drawn in Section 6.

2. System and controller description

In this section, the structure of the system under study is presented and the adopted GFM strategy is introduced.

As can be seen from Fig. 2(a), the system consists of a VSC connected through an inductive filter and a step-up AC transformer to a grid, represented using a simplified Thévenin equivalent model.

The control structure of the VA-GFM is illustrated in Fig. 2(b). In VA-GFM, the VSC appears as a virtual machine with a virtual back electromotive force (EMF) and a virtual impedance, including the physical converter filter and a tunable virtual part [21]. Generally, VA-GFM has advantages over other GFM strategies in terms of less dependency on grid strength as it increases the electrical distance between point-of-common-coupling (PCC) and the voltage that is controlled, e_{EMF} [22]. Moreover, the virtual inductance and resistance are additional control parameters, providing more degree of freedom in controlling the system [22].

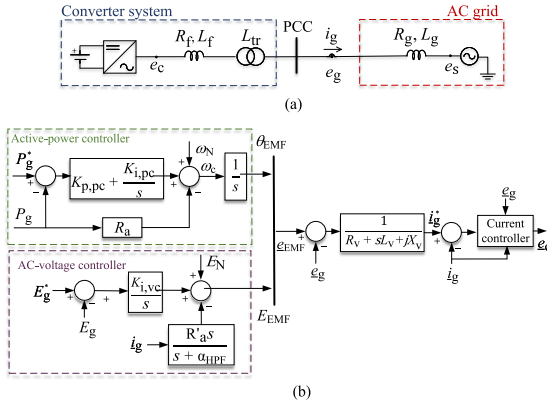


Fig. 2. (a) Structure of the system under study. (b) Control structure of VA-GFM.

The synchronization angle θ_{EMF} in VA-GFM is obtained from the active-power control (APC) loop as:

$$\theta_{EMF} = \frac{1}{s} \left[\left(K_{p,pc} + \frac{K_{i,pc}}{s} \right) (P_g^* - P_g) - R_a P_g + \omega_N \right] \quad (1)$$

where ω_N is the rated angular frequency (in rad/s) of the AC grid. Moreover, P_g and P_g^* are the injected active power and its reference, respectively. $K_{p,pc}$ and $K_{i,pc}$ denote the proportional and integral gains of the PI regulator, while R_a denotes an active damping term, which is included to improve the dynamic performance of the controller and is set equal to $K_{p,pc}$ [24]. The parameters of the controller are selected in order to obtain a first-order closed loop response from P_g^* to P_g .

The AC-voltage controller (AVC) is based on an integral term with the gain of $K_{i,vc}$, and is used to regulate the magnitude of the voltage at the PCC (E_g) to its reference value (E_g^*). In the block diagram, E_N is the rated system voltage, and R'_a is a transient damping term implemented through a high-pass filter with the bandwidth of α_{HPF} [22]. The AVC relation can be written as:

$$\underline{e}_{EMF} = E_N + \frac{K_{i,vc}}{s} (E_g^* - E_g) - \frac{G_{HPF}}{s + \alpha_{HPF}} R'_a \underline{i}_g \quad (2)$$

It is worth mentioning that underlined variables represent vectors in the converter dq -frame defined by the angle output of active-power controller (using power-invariant transformation), and capital letters denote the amplitude of the vector in this section.

In VA-GFM strategy, in addition to active-power controller and AC-voltage controller, the virtual impedance should be implemented in the control system as shown in Fig. 2(b). L_v and R_v are the virtual inductance and resistance, respectively, while $X_v = \omega_N L_v$ arises from the abc-dq transformation and represents the virtual reactance at the rated frequency. The VA block calculates the reference current i_g^* , which is the input of the current controller, as:

$$\underline{i}_g^* = \frac{\underline{e}_{EMF} - \underline{e}_g}{R_v + sL_v + jX_v} \quad (3)$$

The implemented current controller has a classical structure and is based on a PI regulator with cross-coupling cancellation and voltage feed-forward, low-pass filtered with a cut-off frequency of α_{ff} . The current controller calculates the converter-voltage reference as [25]:

$$\underline{e}_c^* = \frac{H_{ff}}{s + \alpha_{ff}} \underline{e}_g + j\omega_N L_f \underline{i}_g + \left(K_{p,cc} + \frac{K_{i,cc}}{s} \right) (\underline{i}_g^* - \underline{i}_g). \quad (4)$$

where L_f is the physical filter inductance. The terms $K_{p,cc}$ and $K_{i,cc}$ denote the proportional and integral gains of the PI regulator for the current controller, respectively.

Table 1

Circuit and base-case control parameters of studied GFM-VSC.

Parameter	Symbol	Unit
Nominal apparent power	S_N	100 MVA (1 p.u.)
Nominal RMS voltage	E_N	20 kV (1 p.u.)
Nominal frequency	f_N	50 Hz
Grid impedance (1/SCR)	L_g	1 p.u.
Active-power controller bandwidth	α_{pc}	5 Hz
Transient damping HPF bandwidth	α_{hpf}	5 Hz
Voltage controller bandwidth	α_{vc}	1 Hz
Filter resistance and inductance	R_f, L_f	0.015 p.u., 0.15 p.u.
Active damping term	R_a	0.065 p.u.
Virtual inductance	L_v	0.5 p.u.
Virtual resistance	R_v	0.25 p.u.

The main controller and system parameters are presented in Table 1.

3. Theoretical framework

This section presents the theoretical basis for analyzing the converter's frequency-domain behavior under frequency and angle variations at its point of connection to the grid. As suggested by several power system operators [19,26], the focus is on the resulting active-power response, as it represents a useful indicator for the converter's damping and synchronizing properties. In the following, the converter's input admittance is derived first. Thus, the transfer functions of active power to frequency and angle perturbations are derived.

3.1. Input admittance derivation

The input admittance defines the relationship between voltage perturbations at PCC and the induced current response, represented by the matrix below.

$$\begin{bmatrix} \Delta i_{sd}^S \\ \Delta i_{sq}^S \end{bmatrix} = \begin{bmatrix} Y_{dd} & Y_{dq} \\ Y_{qd} & Y_{qq} \end{bmatrix} \begin{bmatrix} \Delta e_d^S \\ \Delta e_q^S \end{bmatrix} \quad (5)$$

In the notation, Δ denotes perturbation quantities, and the superscript "S" indicates variables expressed a rotating dq -frame defined by the angle θ_N obtained by integrating the rated angular frequency ω_N and initialized with zero phase. The input admittance of the converter is derived in this rotating dq -frame, hereafter referred to as the system dq -frame. To perform this derivation, the controller dynamics described in Section 2 must be transformed into this frame. Using (1), the angle θ_L is defined

$$\theta_L = \theta_{EMF} - \theta_N = \frac{1}{s} [G_{pc}(P_g^* - P_g) - R_a P_g] \quad (6)$$

where θ_L represents the phase-angle difference between the emulated-voltage source and the infinite bus, and can be interpreted as the converter's load angle. Thus, any vector $\underline{y}$ defined in Section 2 can be transformed to the system dq -frame using the following relation

$$\underline{y}^S = \underline{y} e^{j\theta_L} \quad (7)$$

Using the linearized forms of (3) and (4), we obtain

$$\Delta \underline{i}_g^* = (\Delta \underline{e}_{EMF} - \Delta \underline{e}_g) \frac{\bar{Z}_v - jX_v}{\bar{Z}_v^2 + X_v^2}, \quad (8)$$

and

$$\Delta \underline{e}_c^* = H_{ff} \Delta \underline{e}_g + j\omega_N L_f \Delta \underline{i}_g + G_{cc} (\Delta \underline{i}_g^* - \Delta \underline{i}_g), \quad (9)$$

where $\bar{Z}_v = R_v + sL_v$. The linearized form of (7) results in

$$e^{j\theta_{L0}} \Delta \underline{y} = \Delta \underline{y}^S - j\omega_N \Delta \theta_L, \quad (10)$$

where the subscript “0” denotes steady-state quantities. By multiplying both sides of (9) by $e^{j\theta_{L0}}$ and utilizing (10), we obtain

$$\begin{aligned} \Delta e_{-c}^{S*} - j e_{-c0}^{S*} \Delta \theta_L &= H_{ff}(\Delta e_g^S - j e_{g0}^{S*} \Delta \theta_L) \\ &+ j \omega_N L_f (\Delta i_g^S - j i_{g0}^{S*} \Delta \theta_L) + G_{cc}(\Delta i_g^{S*} - j i_{g0}^{S*} \Delta \theta_L) \\ &- G_{cc}(\Delta i_g^S - j i_{g0}^{S*} \Delta \theta_L). \end{aligned} \quad (11)$$

Similarly, by multiplying both sides of (8) by $e^{j\theta_{L0}}$ and using (10), we obtain

$$\begin{aligned} \Delta i_g^{S*} &= j i_{g0}^{S*} \Delta \theta_L + (\Delta e_{-EMF}^S - j e_{-EMF0}^{S*} \Delta \theta_L) \frac{\bar{Z}_v - j X_v}{\bar{Z}_v^2 + X_v^2} \\ &- (\Delta e_g^S - j e_{g0}^{S*} \Delta \theta_L) \frac{\bar{Z}_v - j X_v}{\bar{Z}_v^2 + X_v^2}. \end{aligned} \quad (12)$$

By substituting (12) into (11), and subsequently rearranging the resulting expression and decomposing it into its real and imaginary components, the small-signal model of the converter voltage reference in the system dq -frame is obtained as

$$\begin{aligned} \begin{bmatrix} \Delta e_{cd}^{S*} \\ \Delta e_{cq}^{S*} \end{bmatrix} &= \underbrace{\begin{bmatrix} H_{ff} - \frac{G_{cc} \bar{Z}_v}{\bar{Z}_v^2 + X_v^2} & -\frac{G_{cc} X_v}{\bar{Z}_v^2 + X_v^2} \\ \frac{G_{cc} X_v}{\bar{Z}_v^2 + X_v^2} & H_{ff} - \frac{G_{cc} \bar{Z}_v}{\bar{Z}_v^2 + X_v^2} \end{bmatrix}}_{G_{ee}} \begin{bmatrix} \Delta e_g^S \\ \Delta e_{gq}^S \end{bmatrix} \\ &+ \underbrace{\begin{bmatrix} -G_{cc} & -\omega_N L_f \\ \omega_N L_f & -G_{cc} \end{bmatrix}}_{Y_i} \begin{bmatrix} \Delta i_{gd}^S \\ \Delta i_{gq}^S \end{bmatrix} + \underbrace{\begin{bmatrix} \frac{G_{cc} \bar{Z}_v}{\bar{Z}_v^2 + X_v^2} & \frac{G_{cc} X_v}{\bar{Z}_v^2 + X_v^2} \\ -\frac{G_{cc} X_v}{\bar{Z}_v^2 + X_v^2} & \frac{G_{cc} \bar{Z}_v}{\bar{Z}_v^2 + X_v^2} \end{bmatrix}}_{Z_v} \begin{bmatrix} \Delta e_{EMFd}^S \\ \Delta e_{EMFq}^S \end{bmatrix} \\ &+ \underbrace{\begin{bmatrix} A_e & 0 \\ B_e & 0 \end{bmatrix}}_{G_{Te}} \begin{bmatrix} \Delta \theta_L \\ \Delta E_g^{S*} \end{bmatrix} \end{aligned} \quad (13)$$

The terms A_e and B_e are given by

$$\begin{aligned} A_e &= -e_{cq0}^S + H_{ff} e_{gq0}^S + i_{gd0}^S \omega_N L_f - G_{cc} i_{gq0}^S \\ &+ \frac{G_{cc} e_{EMFd0}^S \bar{Z}_v - G_{cc} e_{EMFq0}^S X_v}{\bar{Z}_v^2 + X_v^2} + \frac{-G_{cc} e_{gq0}^S \bar{Z}_v + G_{cc} e_{gd0}^S X_v}{\bar{Z}_v^2 + X_v^2}; \\ B_e &= e_{cd0}^S - H_{ff} e_{gd0}^S + i_{gq0}^S \omega_N L_f + G_{cc} i_{gd0}^S \\ &- \frac{G_{cc} e_{EMFd0}^S \bar{Z}_v + G_{cc} e_{EMFq0}^S X_v}{\bar{Z}_v^2 + X_v^2} + \frac{G_{cc} e_{gd0}^S \bar{Z}_v + G_{cc} e_{gq0}^S X_v}{\bar{Z}_v^2 + X_v^2} \end{aligned} \quad (14)$$

Furthermore, the small-signal model of the virtual back-EMF in the dq -frame can be obtained by linearizing expressions (2) and (6), and by following a procedure similar to that described above for the frame transformation of the linearized form of (2). The resulting small-signal model of the virtual back-EMF in the system dq -frame is given by

$$\begin{aligned} \begin{bmatrix} \Delta e_{EMFd}^S \\ \Delta e_{EMFq}^S \end{bmatrix} &= \underbrace{\begin{bmatrix} -(e_{EMFd0}^S + G_{HPF} R_a' i_{gq0}^S) & G_{vc} \cos \theta_{L0} \\ (e_{EMFq0}^S + G_{HPF} R_a' i_{gd0}^S) & G_{vc} \sin \theta_{L0} \end{bmatrix}}_{G_T} \begin{bmatrix} \Delta \theta_L \\ \Delta E_g^{S*} \end{bmatrix} \\ &+ G_{PVv1} \begin{bmatrix} \Delta e_{gd}^S \\ \Delta e_{gq}^S \end{bmatrix} + G_{DH} \begin{bmatrix} \Delta i_{gd}^S \\ \Delta i_{gq}^S \end{bmatrix} \end{aligned} \quad (15)$$

where

$$\begin{aligned} G_{PVv1} &= \begin{bmatrix} \frac{G_{vc} \cos \theta_{L0} e_{gd0}^R}{E_{g0}} & \frac{G_{vc} \cos \theta_{L0} e_{gq0}^R}{E_{g0}} \\ \frac{G_{vc} \sin \theta_{L0} e_{gd0}^R}{E_{g0}} & \frac{G_{vc} \sin \theta_{L0} e_{gq0}^R}{E_{g0}} \end{bmatrix}, \\ G_{DH} &= \begin{bmatrix} -G_{HPF} R_a' & 0 \\ 0 & -G_{HPF} R_a' \end{bmatrix}. \end{aligned} \quad (16)$$

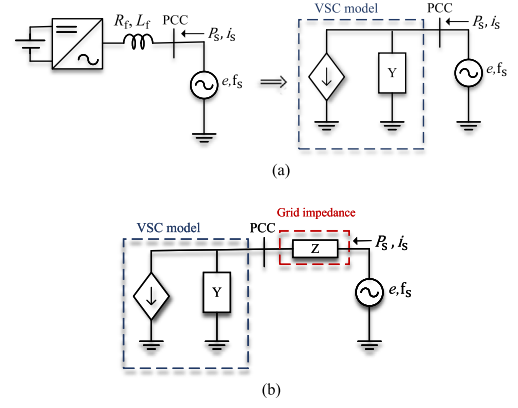


Fig. 3. Frequency perturbation implementation (a) from PCC, and (b) for the entire system.

with

$$\begin{aligned} \begin{bmatrix} \Delta \theta_L \\ \Delta E_g^{S*} \end{bmatrix} &= \begin{bmatrix} \frac{G_{pc}}{s} & 0 \\ 0 & 1 \end{bmatrix} \begin{bmatrix} \Delta P_g^{S*} \\ \Delta E_g^{S*} \end{bmatrix} \\ &+ \underbrace{\begin{bmatrix} -\frac{(G_{pc} + R_a) e_{gd0}^S}{s} & -\frac{(G_{pc} + R_a) e_{gq0}^S}{s} \\ 0 & 0 \end{bmatrix}}_{G_{PVc2}} \begin{bmatrix} \Delta i_{gd}^S \\ \Delta i_{gq}^S \end{bmatrix} \\ &+ \underbrace{\begin{bmatrix} -\frac{(G_{pc} + R_a) i_{gd0}^S}{s} & -\frac{(G_{pc} + R_a) i_{gq0}^S}{s} \\ 0 & 0 \end{bmatrix}}_{G_{PVv2}} \begin{bmatrix} \Delta e_{gd}^S \\ \Delta e_{gq}^S \end{bmatrix} \end{aligned} \quad (17)$$

where the expressions $P_g = e_{gd}^S i_{gd}^S + e_{gq}^S i_{gq}^S$, and $E_g = \sqrt{(e_{gd}^S)^2 + (e_{gq}^S)^2}$ are used for the active-power injected into the grid and PCC-voltage magnitude, respectively. Neglecting the impact of delays and using (13), (15), (17), and the linearized expression for the current dynamics in the system dq -frame, which is given by

$$\begin{bmatrix} \Delta e_{cd}^S \\ \Delta e_{cq}^S \end{bmatrix} = \begin{bmatrix} \Delta e_{gd}^S \\ \Delta e_{gq}^S \end{bmatrix} + \underbrace{\begin{bmatrix} R_f + s L_f & -\omega_N L_f \\ \omega_N L_f & R_f + s L_f \end{bmatrix}}_{Z_f} \begin{bmatrix} \Delta i_{gd}^S \\ \Delta i_{gq}^S \end{bmatrix} \quad (18)$$

the input admittance of the converter system seen from the PCC can be obtained as

$$\begin{aligned} Y_{conv} &= [Y_i + Z_v G_{DH} + Z_v G_T' G_{PVc2} + G_{Te} G_{PVc2} - Z_f]^{-1} \\ &[G_{ee} + Z_v G_{PVv1} + Z_v G_T' G_{PVv2} + G_{Te} G_{PVv2} - I] \end{aligned} \quad (19)$$

3.2. Derivation of linearized equations for frequency response analysis

In this section, the analytical linearized model of the system is derived. The accuracy of the linearized model then is verified by the detailed time-domain simulation model.

Note that the voltage e represents either e_g or e_s in Fig. 3, depending on the point where the frequency perturbation is applied.

The general active power relation is given as follows:

$$P_s = e_{sd}^S i_{sd}^S + e_{sq}^S i_{sq}^S \quad (20)$$

Linearizing (20) results in:

$$\Delta P_s = i_{sd0}^S \Delta e_d^S + e_{sd0}^S \Delta i_{sd}^S + i_{sq0}^S \Delta e_q^S + e_{sq0}^S \Delta i_{sq}^S \quad (21)$$

Furthermore, the voltage vector in the system dq -frame can be written as

$$\underline{e}^S = e_0 e^{j(\theta_s - \theta_N)} \quad (22)$$

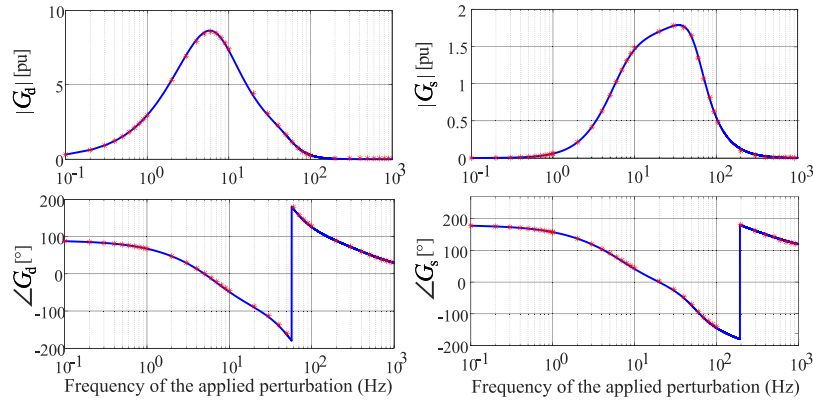


Fig. 4. Comparison between analytical (blue solid) and simulation (red stars) results for G_d (left) and G_s (right).

The variation of the voltage vector during a frequency perturbation can be obtained by linearizing (22) as

$$\Delta \underline{e}^S = j e_0 e^{j(\theta_{s0} - \theta_{N0})} \Delta \theta_s; \quad \Delta \theta_s = \frac{\Delta \omega_s}{s} \quad (23)$$

Note that $\theta_{s0} = \theta_{N0}$ is assumed before a frequency perturbation, $\Delta \omega_s$ is applied in (23). Using (23) and (5), the dq -components of the current variation can be obtained as follows:

$$\Delta i_{sd}^S = \frac{e_0}{s} Y_{dq} \Delta \omega_s, \quad \Delta i_{sq}^S = \frac{e_0}{s} Y_{qk} \Delta \omega_s \quad (24)$$

Utilizing (21), (23) and (24), the variation in the active power can be obtained as

$$\Delta P_s = Y_{dq} \frac{e_0^2}{s} \Delta \omega_s + i_{sq0} \frac{e_0}{s} \Delta \omega_s \quad (25)$$

Finally, the transfer function from the applied frequency perturbation to the active-power variation can be obtained as follows:

$$G_d = \frac{\Delta P_s}{\Delta \omega_s} = Y_{dq} \frac{e_0^2}{s} + i_{sq0} \frac{e_0}{s} \quad (26)$$

Using (26), the applied angle perturbation can also be correlated with variations in active power as follows:

$$G_s = \frac{\Delta P_s}{\Delta \theta_s} = Y_{dq} e_0^2 + i_{sq0} e_0 \quad (27)$$

It can be seen that the transfer functions from variation in frequency and angle to active-power change are dependent on only one component of the input admittance matrix (Y_{dq}) as well as the voltage and current steady-state values. The input admittance of the converter with VA-GFM strategy has been derived, which will be used to obtain the frequency-domain responses in the rest of the paper. While the transfer function G_d gives a valuable insight to the damping characteristics, the response from G_s is used to investigate synchronizing characteristics of the VSC in this paper. To validate the accuracy of the analytical model, the derived transfer functions G_d and G_s are compared with simulation results using the parameter values listed in Table 1. In the EMT simulation model, a small perturbation in the PCC frequency or phase angle is applied as the input and swept across the frequency range, while the resulting output power magnitude and phase relative to the input are measured. As shown in Fig. 4, the analytical and simulation results exhibit close agreement, confirming the correctness of the linearized model.

4. Analysis of damping characteristics

In this section, frequency response of transfer function G_d obtained in (26)¹ is analyzed. Different values of the converter's control parameters are selected to analyze their impact on damping characteristics.

¹ These are also referred in the literature as Network Frequency Perturbation (NFP) plots [26,27].

The control parameters investigated include active damping (R_a), transient damping (R'_a), and virtual impedance (L_v and R_v), owing to their direct influence on the system's damping characteristics. Subsequently, frequency response for the entire system is obtained, to demonstrate that the damping behavior provided by the GFM converter remains consistent at the system level. Finally, the analyses are validated by time-domain simulation results.

4.1. Resonances under damping investigation

Recent research indicates that GFM-VSCs commonly exhibit two typical resonances in their output power, occurring at low and mid frequencies [28]. This is primarily due to the use of low-pass filters in the power measurement and the inherently slow dynamics of the outer control loops to suppress high-frequency components. As a result, the low- and mid-frequency resonances, referred to as sub-synchronous and synchronous resonances respectively, become more prominent. In this paper, synchronous resonance refers to resonance phenomena occurring in the vicinity of the nominal grid frequency, i.e., around the synchronous frequency ($f_N = 50$ Hz in this study), whereas sub-synchronous resonance refers to resonance phenomena occurring at frequencies below the synchronous frequency ($f < f_N$), typically in the low-frequency range around the bandwidth of the active-power controller. In addition, high-frequency resonances are generally mitigated to some extent by the damping provided by system filters and parasitic resistances and are therefore not the main focus of this paper, which centers on the resonances that are more consequential at the system level. Therefore, this paper focuses on the analysis of synchronous and sub-synchronous resonances. Synchronous resonance is caused by the virtual and physical impedances of the GFM system [29], which has two conjugate poles at

$$\rho_{1,2} = -\frac{R}{L} \pm j\omega_N \quad (28)$$

where R and L denote the resistance and inductance between the internal voltage of the VSC system ($\underline{e}_{EMF}$) and either the PCC or the grid voltage. In (28), the imaginary part corresponds to the synchronous frequency in the rotating system dq frame which corresponds to the DC component in the current response of the system in the stationary frame. On the other hand, the real part of the poles is determined by the R/L ratio. This resonance requires damping provision as discussed in Section 1 using Fig. 1, and is highlighted with yellow dashed lines in Fig. 5. Sub-synchronous resonance, on the other hand, is primarily associated with the second-order dynamics of the APC, arising from either design deficiencies or system disturbances. It generally appears just beyond the APC bandwidth, where the GFM's power-constant behavior starts to degrade. This behavior is highlighted with a green dashed line in Fig. 5. In Fig. 5, the frequency response of G_d is shown, including an approximate estimation of its amplitude and phase-transition around

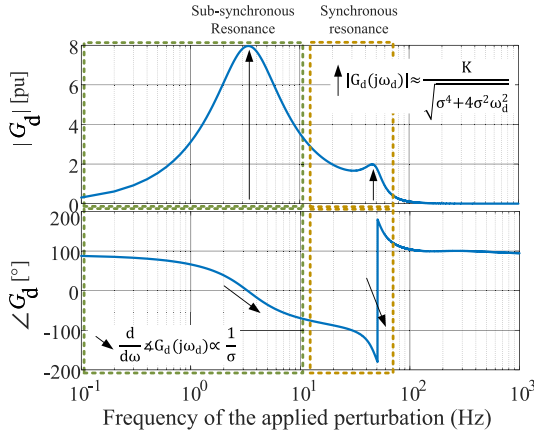


Fig. 5. Synchronous and sub-synchronous resonances in the Bode plot of G_d , representing either the converter or the overall system.

the resonances to be used for later discussion. In the estimates, σ represents the real part of the conjugate poles, ω_d represents the resonance frequency and K is a numerator constant.

4.2. Damping characteristics analysis

To investigate the damping characteristics of the GFM converter for the aforementioned resonances using the Bode plots of G_d , the impact of the two resonance modes on the gain and phase characteristics of the Bode plot in Fig. 5 should be understood. As such, a rapid phase transition and high gain in the Bode plots are indicative of undesirable damping properties, as they imply a small real part of the pole which is contrary to system operators recommendations [26]. Based on this, when assessing the impact of a controller parameter on damping, the preferred scenario is the one in which the Bode plot of the converter (or the overall system) does not exhibit excessive gain or abrupt phase-shift in the frequency of interest where the resonance resides. This behavior reflects the GFM converter's contribution to damping across different frequency regions.

4.3. Impact of control parameters on damping characteristics

The Bode plots obtained solely for the converter, excluding the grid and evaluated at the PCC terminal, are presented in Fig. 6. It can be seen from Fig. 6(a) that the active-damping term (R_a) mainly impacts sub-synchronous frequencies in the dq frame, particularly around the loop bandwidth of the active-power controller, where it is implemented, and enhances the damping in this region. It is also observed that its impact is not confined to the constant power region, where the implementation is designed to yield a first order response to active power reference changes, but extends, albeit weakly here but generally based on the loop bandwidth of APC, into the damping provision region. This indicates that the term can also slightly influence synchronous resonance damping.

The transient damping term (R'_a) is mostly active after its high-pass filter cut-off frequency ($\omega_{HPF} = 5$ Hz in this case). Therefore, it enhances the damping characteristics around the synchronous frequency (50 Hz in the dq -frame) by reducing the gain and smoothing the phase variation within the damping provision region discussed in Section 1, as shown in Fig. 6(b).

Moreover, as presented in Fig. 6(c), a higher value of the virtual resistance (R_v) effectively reduces the magnitude and smooths the phase transition at the synchronous frequency. However, it slightly reduces the magnitude and sharpens the phase shift at sub-synchronous frequencies, which is undesirable. It should be noted that the virtual resistance cannot be set arbitrarily high to damp synchronous resonance.

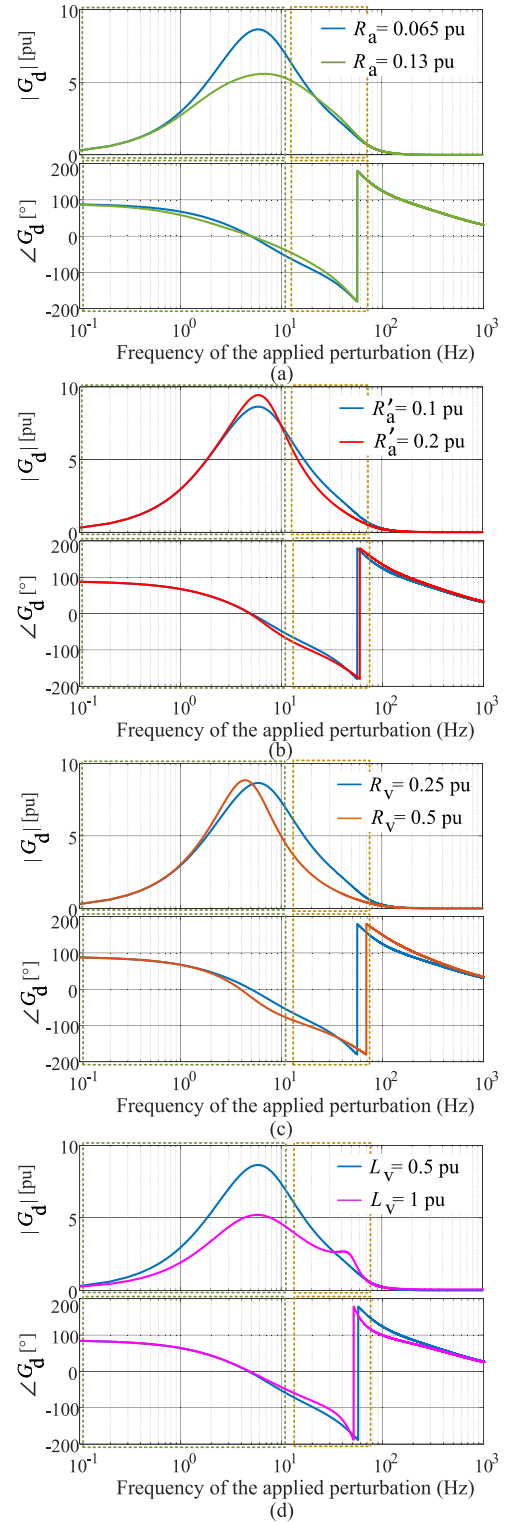


Fig. 6. Bode plots (G_d) of the VSC. (a) impact of active damping term (R_a). (b) impact of transient damping term (R'_a). (c) impact of virtual resistance (R_v). (d) impact of virtual inductance (L_v).

Increasing the resistance strengthens the coupling between active and reactive power dynamics, as the controllers are designed under the assumption of a highly inductive system [21]. This can negatively affect the power-constant region, as already indicated by the slight impact observed here. Conversely, Fig. 6(d) illustrates that an increased virtual

inductance (L_v) degrades damping at the synchronous frequency, as it amplifies the magnitude and sharpens the phase transition. Nevertheless, it mitigates sub-synchronous amplification and smooths the phase shift, leading to improved damping. The differing impact of L_v and R_v at synchronous frequency are readily observed from (28). It is worth noting that, as will be shown in the next section, higher values of L_v have an adverse impact on synchronizing characteristics. Therefore, when designing a system that is not strong from a synchronizing perspective, it should be carefully considered whether damping subsynchronous oscillations using virtual inductance may jeopardize transient stability. On the other hand, as a general guideline, in a system that is weakly damped, priority should be given to tuning parameters that have a beneficial impact on resonances in the desired frequency region. Therefore, the analysis conducted in this paper demonstrates that a trade off is required.

To evaluate the damping contribution of the GFM converter to the overall system, the Bode plots for the entire system are obtained, where the grid is represented with the short circuit ratio (SCR) of 1 pu. As shown in Fig. 7, both the synchronous and sub-synchronous resonances are clearly shown. From Fig. 7(a), it can be observed that the active damping term (R_a) has a positive impact on sub-synchronous oscillations (around 3 Hz in this case-study), as can also be interpreted from Fig. 6(a). In addition, its impact in the synchronous frequency range is minimal as expected, although it does introduce a slight detrimental effect. On the other hand, the transient damping term (R'_a) improves the damping of synchronous oscillation whereas its impact in the sub-synchronous frequency range is minimal, as illustrated in Fig. 7(b). Then, the impact of R_v is shown in Fig. 7(c). It is evident that larger value of R_v increases damping in synchronous frequencies while it can have a slight undesirable impact on sub-synchronous frequencies. Fig. 7(d), on the other hand, reveals the detrimental impact of higher value of L_v at synchronous frequency, although this can enhance damping at sub-synchronous frequencies.

To verify the correctness of the conducted analysis, a phase jump in the grid voltage is considered as an example case for the time-domain simulation to trigger both sub-synchronous and synchronous frequency oscillations, allowing the corresponding damping characteristics to be observed in the active power response, as shown in Fig. 8. Fig. 8 has two oscillation frequencies, namely, the higher frequency which is the synchronous resonance and the lower (slow oscillation) being the sub-synchronous resonance. From the results, it is evident that increased R_a enhances the damping of sub-synchronous oscillations, while having minimal, though slightly adverse, impact on synchronous oscillations. On the other hand, increased R'_a enhances damping of synchronous oscillation without affecting sub-synchronous oscillation. The impact of R_v to improve damping for synchronous oscillation as well as the contradicting impact of increased L_v in deteriorating the synchronous oscillation and enhancing the subsynchronous oscillation are demonstrated, verifying the analytical findings. In addition to the phase-jump disturbance, voltage dip disturbances are applied in Fig. 9 as representative grid-side events. This provides a more realistic condition and serves as an additional validation of the analytical results. It can be observed that, regardless of the type of disturbance applied to the system, the impact of the parameters on the damping of subsynchronous oscillations and synchronous oscillations (zoomed boxes) remains consistent with the conducted analysis. This behavior is confirmed when a 40% voltage dip occurs at the zero operating point, followed by a step change in the active power reference and a subsequent 60% voltage dip.

5. Analysis of synchronizing characteristics

Unlike damping characteristics, which are determined solely through linearized dynamic analysis, the synchronizing characteristics of a GFM converter depend on both static constraints and dynamic response. Therefore, in this section, the synchronizing characteristics of the GFM are analyzed using frequency-domain response in conjunction with a quasi-steady-state approach based on power-angle ($P-\delta$) curves.

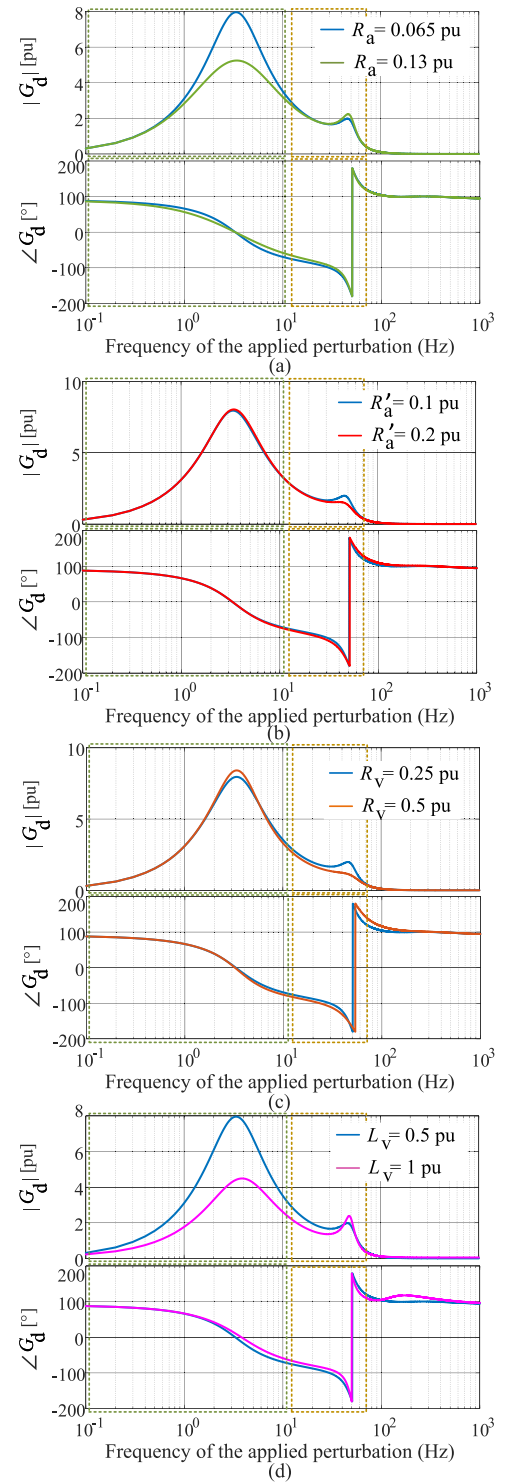


Fig. 7. Bode plots (G_d) of the entire system. (a) impact of active damping term (R_a). (b) impact of transient damping term (R'_a). (c) impact of virtual resistance (R_v). (d) impact of virtual inductance (L_v).

5.1. Criteria determining synchronizing performance

In this section, the criteria used to characterize the synchronizing performance of a GFM converter are defined. Subsequently, the influence of controller parameters on these characteristics is examined through frequency-domain analysis and $P-\delta$ curves.

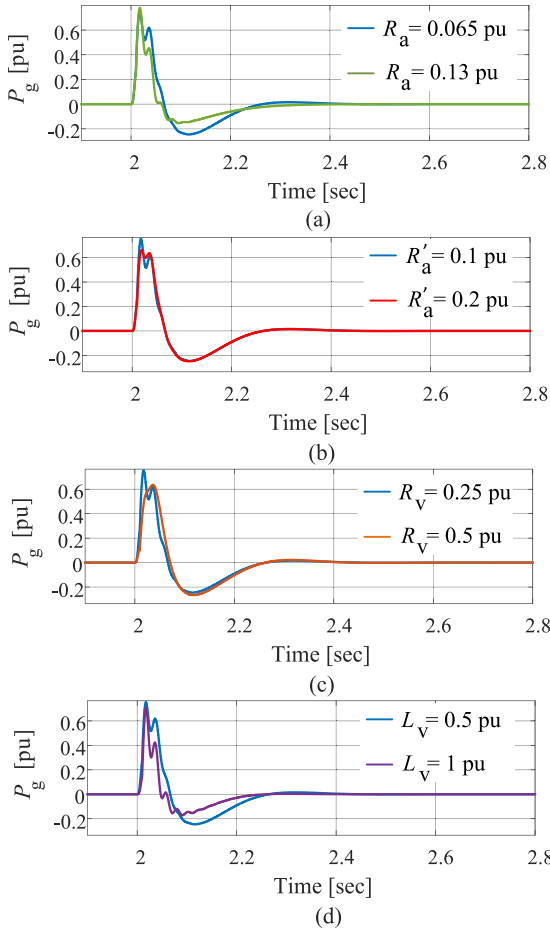


Fig. 8. Time-domain simulation results for a phase jump disturbance. (a) impact of active damping term (R_a). (b) impact of transient damping term (R'_a). (c) impact of virtual resistance (R_v). (d) impact of virtual inductance (L_v).

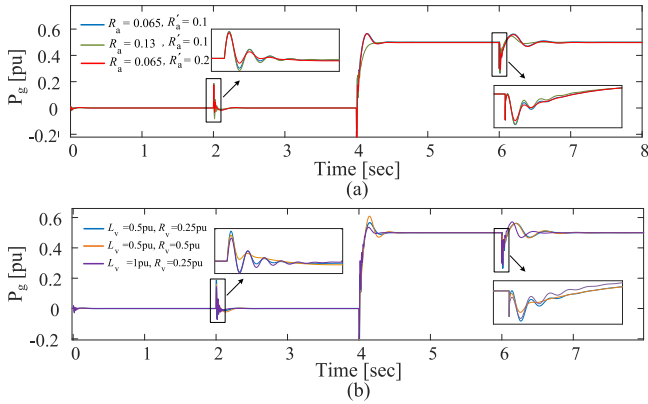


Fig. 9. Time-domain simulation results for voltage dips of 0.4 pu and 0.6 pu at 2 s and 6 s, occurring at zero and non-zero operating points, respectively. (a) impact of active damping term (R_a) and transient damping term (R'_a). (b) impact of virtual resistance (R_v) and virtual inductance (L_v).

The synchronizing performance of the GFM converter is assessed based on three key criteria in this paper:

- Existence of a stable steady-state operating point after a disturbance;
- Ability to avoid exceeding the critical angle;

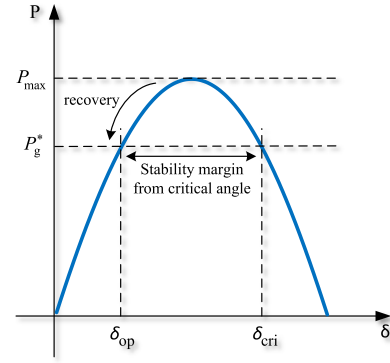


Fig. 10. $P-\delta$ curve with key characteristics.

- Speed of response following a disturbance.

Achieving a stable post-disturbance steady state depends primarily on the converter's ability to supply sufficient active power. This is constrained by the terminal voltages and the system impedance, which together define the maximum transferable power (P_{max}). The shape of the $P-\delta$ curve, representing the relationship between the active power and the load angle, helps to determine the converter's operational limits, as shown in Fig. 10. If the required power exceeds P_{max} , as indicated by the $P-\delta$ curve, the converter will fail to reach to a steady-state operating point and loss of synchronism occurs [4].

Avoiding the load angle exceeding the critical angle (δ_{cri}) during transient, on the other hand, is not only related to the shape of the $P-\delta$ curve, but also closely tied to the dynamic response of the controller. Large overshoots in the load angle, influenced by the controller's dynamics and damping characteristics, can push the system beyond the critical angle, resulting in loss of synchrony as well [30].

Finally, in addition to the two criteria outlined earlier that determine system stability, when stability is achievable, the speed of response of the converter following a phase-jump disturbance and how fast is the recovery defines the concept of synchronizing strength. This characterizes how closely the converter remains coupled to the grid during the transient. Also, from a practical perspective, this property is valuable because it reduces reliance on large energy storage systems; by limiting prolonged active power imbalances, the converter requires less stored energy to stay synchronized with the grid.

5.2. Insights into synchronizing characteristics from frequency-domain analysis

To evaluate the synchronization characteristics of the GFM converter, the Bode plot G_s of the converter is presented in Fig. 11(a). In addition, the corresponding time-domain active-power response to a phase jump is illustrated in Fig. 11(b). Here, the objective is to link the time-domain synchronizing characteristics with the corresponding frequency response (G_s). In Fig. 11(b), the solid blue curve shows the active-power response to a phase jump. Immediately after the disturbance, the active-power shows an instantaneous change, typically referred to as the instantaneous phase-jump power. This is followed by a change in the opposite direction, where the power reaches its maximum deviation, referred to as the active phase-jump power. The magnitude of this deviation is governed by the $P-\delta$ relationship and is influenced by parameters such as virtual impedance, as will be explained later. Afterward, the active power begins to return to its pre-disturbance value. This recovery is driven by the presence of the APC, which forces the system to follow the active-power reference. The system returns to this value at a specific speed of recovery, and during this process, a low-frequency (sub-synchronous) oscillation may be observed, originating from the system's low-frequency resonance

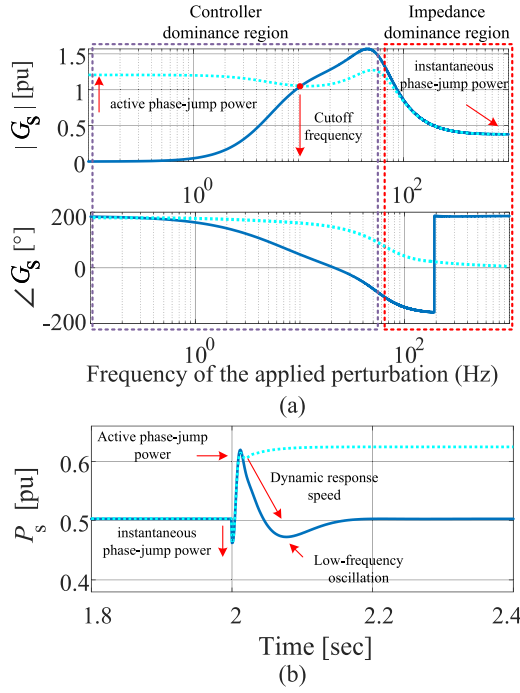


Fig. 11. (a) Bode plots of the GFM converter transfer function G_s with APC (solid blue) and without APC (dotted light blue). (b) corresponding time-domain responses to a phase jump of -0.1 rad applied at 2 s.

pole, mainly associated with APC dynamics. For comparison, the dotted light-blue curve illustrates the active power response with the APC deactivated, achieved by setting $G_{pc} = 0$. Under this condition (see Fig. 2(b)), θ_{EMF} is obtained as the integral of ω_N . In this case, it is evident that after a phase-jump disturbance, the active power no longer follows its reference but settles near a new operating point. This operating point is determined by the $P-\delta$ relationship and is nearly equal to the active phase-jump power.

Turning to the frequency response in Fig. 11(a), two key observations can be made. Firstly, two distinct frequency regions can be identified in the Bode plot. Below the APC loop bandwidth, the magnitude remains close to zero in the solid blue curve, indicating a constant power behavior of the GFM. In contrast, the dotted blue curve, representing the case without APC, shows a specific non-zero value in this range. In addition, there is a region near or below the fundamental frequency but above the APC loop bandwidth, where damping provision occurs, as discussed in Section 1. In this analysis, these two regions are grouped together, representing the combined influence of APC action and transient damping, both of which significantly affect the converter's dynamic response. These regions are highlighted in the figure with a purple dashed outline. The second region appears at higher frequencies, where the effects of virtual impedance and filter impedance dominate, and the previously mentioned controller parameters have no significant influence. This is in line with the constant impedance behavior within the harmonic range and is highlighted with a red dashed line. Thus, the former region is denoted as controller dominance region, and the latter one is the impedance dominance region.

Secondly, by examining the frequency response, the transient behavior of the GFM converter during a phase jump disturbance can be interpreted. From this, information can be obtained about the three criteria defined earlier for assessing the synchronizing characteristics of the GFM converter. Let us first consider how the active phase-jump power can be determined. Following an instantaneous change in active power caused by the phase jump, the system approaches a

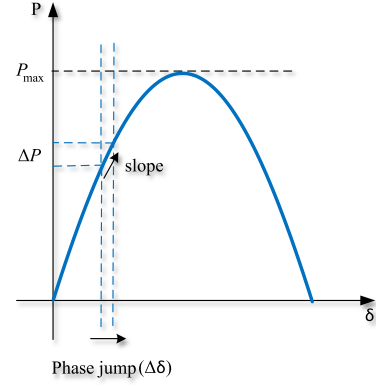


Fig. 12. Illustration of the relation between active phase-jump power and the synchronizing power coefficient.

new operating point. This new operating point can be identified at zero frequency ($s = 0$) in the dotted blue curve, which represents the steady-state value after the disturbance. In the time-domain results, for example, the active power reaches approximately 0.62 pu following a phase jump of -0.1 rad. This value can be calculated using the relationship $P = -G \cdot \Delta\delta$, where G is the magnitude (gain) of the Bode plot at zero frequency, and $\Delta\delta$ is the phase jump in rad. The negative sign accounts for the converter's phase being close to 180° , indicating an inverting behavior. However, due to the active-power controller's role to restore the system to its pre-disturbance operating point (constant power region), this would be a transient power injection or absorption. Based on the definition provided in the grid code, this transient injection or absorption of active power from a grid-forming plant to the total system as a result of changes in the phase angle between the internal voltage source of the grid-forming plant and the grid entry point or user system entry point is called active phase-jump power, and is an inherent capability of a grid-forming plant that starts to respond naturally, within less than 5 ms [26]. This power provides valuable insight into the shape of the $P-\delta$ curve, as shown in Fig. 12. It can be observed that, for a small phase jump, the change in active power corresponds directly to the definition of the synchronizing power coefficient, given by $\Delta P / \Delta\delta$, which also represents the slope of the $P-\delta$ curve. In general, a higher synchronizing power coefficient, and thus a steeper slope, implies a larger margin to P_{max} and to the critical angle, which are the first and second criteria defined earlier.

In cases where the APC cannot be deactivated and the GFM is treated as a black box, the active phase-jump power can still be inferred. This is because it primarily depends on the steady-state impedance between the PCC and the back-EMF voltage source (R_v and X_v). When APC remains active and direct information on the active phase-jump power is unavailable, the impedance-dominant region can indicate the shape of the $P-\delta$ curve, allowing static constraints to be analyzed, as will be discussed later.

Moreover, at high frequencies ($s \rightarrow \infty$), the instantaneous phase-jump power is reflected, which is the instantaneous reaction of the converter's active power to the phase jump. It can be shown from (27) that this value depends on the initial value of the current's q component (i_{sq0}) in the dq -frame rotating with the nominal frequency. This power, however, is not the focus of this paper, as it is not directly linked to the synchronizing characteristics.

Finally, The controller-dominant region reflects the low-frequency oscillation and the expected overshoot magnitude in case of voltage dip for instance, features that, in addition to the shape of the $P-\delta$ curve, determine the risk of exceeding the critical angle. However, since an overshoot is closely related to the damping characteristics, it is more effectively observed in the Bode plots presented in Section 4, as these plots reveal both the oscillation frequency and the damping, which

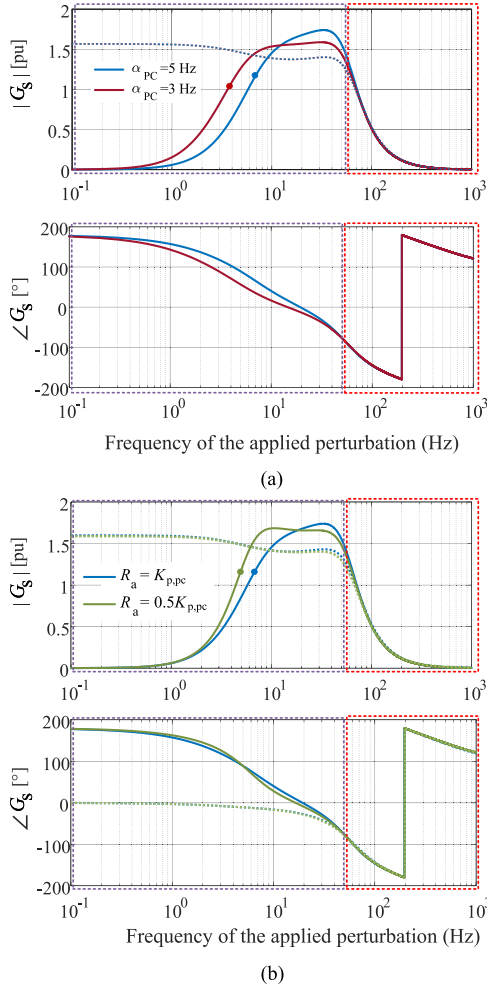


Fig. 13. Impact of APC parameters on Bode plots (G_s) of GFM converter, where solid curves include the APC effect, dotted curves exclude it, and circles indicate cutoff frequencies. (a) APC bandwidth (α_{PC}) impact. (b) active damping term (R_a) impact.

together determine the extent of overshoot. Furthermore, this region significantly influences the dynamic response speed of the system, shaping how quickly it restores to the pre-disturbance operating point (third criterion). Based on the concept of synchronizing strength, the faster a system returns to its pre-disturbance state, the greater its synchronizing strength. By examining the high-pass filter shape of the solid blue Bode plot in Fig. 11(a) within the controller-dominance region, it can be concluded that a larger cutoff frequency, defined here as the frequency at which the magnitude decreases by 3 dB from its peak, leads to a faster response to the pre-disturbance operating point, and hence a better synchronizing capability. Based on the discussion in this section, the impact of parameters on synchronizing characteristics will be discussed in the following.

5.3. Controller parameters impact on synchronizing characteristics

In this part, impact of virtual inductance, virtual resistance, and APC parameters on the frequency response and hence on the synchronizing characteristics will be described.

Fig. 13(a) and (b) show the effect of APC bandwidth and the active damping term on the Bode plots, respectively. As expected, changes in the APC parameters only impact the controller dominance region, leaving the active phase-jump power and impedance dominance region unaffected. Regarding the high-pass filter-like characteristics in the

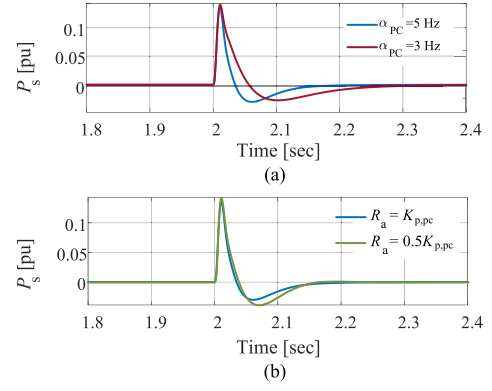


Fig. 14. Time-domain simulation result of active power for phase jump of -0.1 pu at 2 s. (a) impact of active-power controller bandwidth (α_{PC}). (b) impact of active damping term (R_a).

controller dominance region, where a higher cutoff frequency corresponds to a faster response, a smaller APC bandwidth lowers the cutoff frequency and thus slows the dynamic response. With respect to the active damping term, its influence on the cutoff frequency is less than that of the APC bandwidth, but it alters the low-frequency oscillation dynamics. As discussed in Section 4, this term is introduced to dampen the low frequency oscillations. Therefore, insufficient R_a can increase overshoot noticeably. These behaviors are further illustrated by the time-domain simulation results in Fig. 14(a) and (b). A higher APC bandwidth ($\alpha_{PC} = 5$ Hz) enables faster convergence to the steady-state operating point, thereby improving synchronizing strength. Conversely, a smaller R_a amplifies low-frequency oscillations, which is undesirable from a synchronization standpoint.

Fig. 15(a) and (b) show that varying the virtual inductance or resistance influences both frequency regions. Increasing L_v accelerates the response and reduces low-frequency oscillation amplification, while increasing R_v mainly slows the recovery speed; both also affect the impedance-dominant region and thus the active phase-jump power. As explained earlier, the value of the active phase-jump power mainly depends on the steady-state impedance; however, this value is unavailable in Bode plots when APC is active. In such cases, the impedance-dominant region still contains information about the impedance dynamics ($R_v + sL_v$). Therefore, active phase-jump power can comparatively be analyzed in this region. It can be shown from Fig. 15(a) and (b) that increasing virtual inductance or resistance decreases the bode plots magnitude in the impedance dominance region, meaning decreasing active phase-jump power, also indicated with reduced peaks in Fig. 16(a) and (b) following the phase jump. This can also be explained based on $P - \delta$ curves shown in Fig. 17, where the same phase jump results in different new operating points and corresponding active power with different values of L_v and R_v , in agreement with the time-domain simulations. Therefore, a lower active phase-jump power indicates that $P_{\max}$ is reduced, and from the shape of the $P - \delta$ curves, it can be seen that typically the margin between possible operating points and critical angles decreases, both of which make achieving synchronizing stability more challenging.

While the above interpretation holds when only the GFM is under consideration, it is important to note that, when the entire system is considered and the AVC regulates the PCC voltage to 1 pu, neither R_v nor L_v influence $P_{\max}$. In this case, unlike in Fig. 15, $P_{\max}$ is determined solely by the grid impedance. This is because the AVC maintains the PCC voltage magnitude at a fixed value, such that the steady-state power transfer is governed by the classical power-angle relationship between two stiff voltage sources connected through the grid impedance. Consequently, the virtual impedance parameters mainly affect the internal converter voltage, but do not alter the steady-state

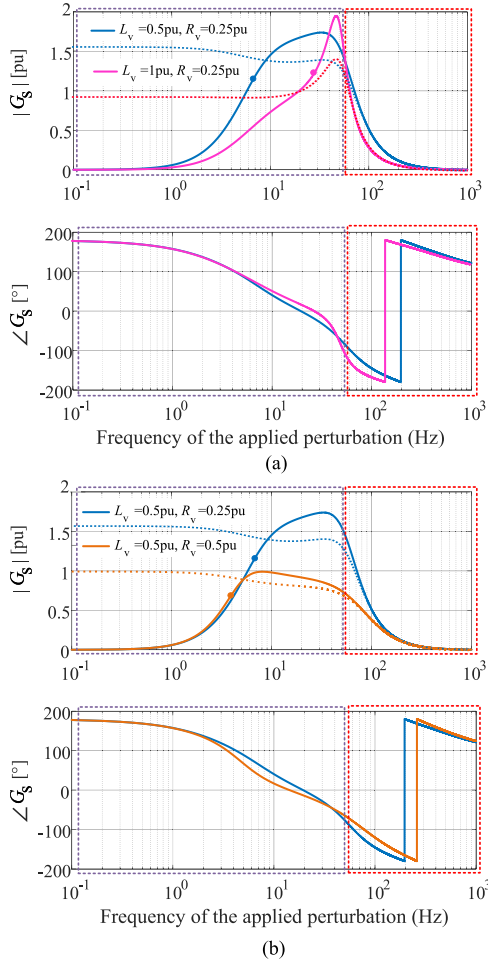


Fig. 15. Impact of virtual impedance on Bode plots (G_s) of GFM converter, where solid curves include the APC effect, dotted curves exclude it, and circles indicate cutoff frequencies. (a) virtual inductance (L_v) impact. (b) virtual resistance (R_v) impact.

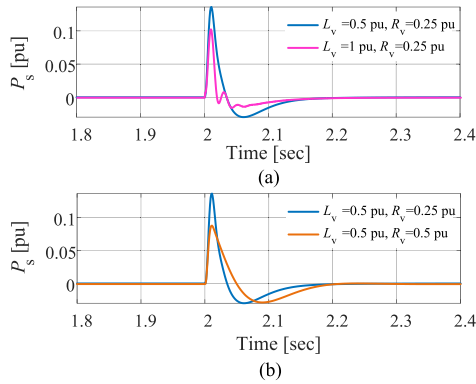


Fig. 16. Time-domain simulation result of active power for phase jump of -0.1 pu at 2 s. (a) impact of virtual inductance (L_v). (b) impact of virtual resistance (R_v).

voltage magnitude at the PCC or the maximum transferable power, which becomes grid-limited rather than converter-limited. However, the critical angle shifts closer to the feasible operating points when virtual inductance and resistance increase, as shown in Fig. 18. This indicates that, although increasing R_v or L_v does not significantly change the P - δ curves compared to the previous case, it still exerts a slight

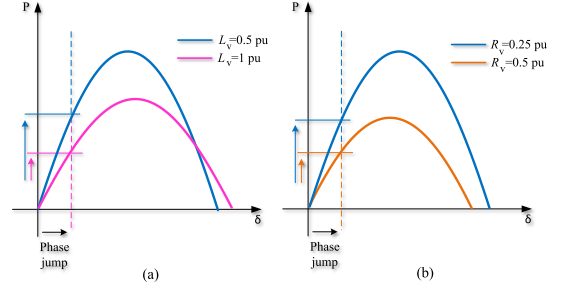


Fig. 17. Active phase-jump power analysis based on P - δ curves. (a) impact of virtual inductance (L_v). (b) impact of virtual resistance (R_v).

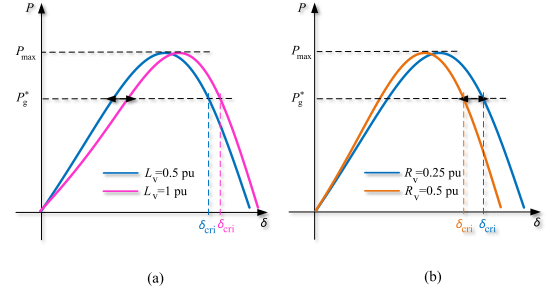


Fig. 18. P - δ curves with AVC. (a) impact of virtual inductance (L_v). (b) impact of virtual resistance (R_v).

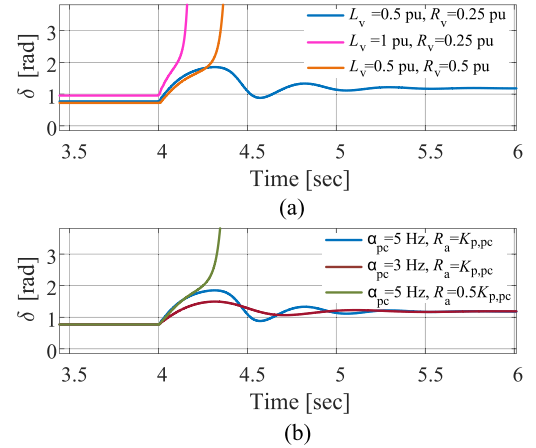


Fig. 19. Time-domain simulation result of load angle (δ) for 40% Voltage dip and 0.55 pu active power injection. (a) impact of virtual inductance and resistance. (b) impact of APC parameters.

negative effect. Therefore, in both scenarios, higher values of virtual inductance or resistance can be detrimental from a quasi-steady-state perspective.

To validate the analysis, as an example, a 40% voltage dip is applied on the grid side as a realistic disturbance that can lead to instability in cases where the control parameters are not properly tuned, thereby allowing the synchronizing capability of the grid-forming converter to be examined. As shown in Fig. 19(a), higher values of virtual inductance and resistance lead to a loss of synchronizing stability. Moreover, Fig. 19(b) illustrates a small active damping term can also cause loss of synchronization during the dip. A slower APC, on the other hand, does not result in instability but changes the system's response.

6. Conclusion

In this paper, the impact of control parameters for a virtual-admittance based grid-forming control strategy on damping and synchronizing characteristics of GFM converter has been investigated. In particular, the study focuses on how the converter's frequency response should comply with grid requirements and how it is shaped and modified by different parameters. These insights are crucial for guiding the systematic design of GFM control systems and for setting control parameters to ensure both robust damping behavior and reliable synchronization performance. Based on the analysis of the Bode plots obtained from the linearized model, it has been shown that virtual impedance and different damping terms in active-power controller and AC-voltage controller can affect synchronous and sub-synchronous oscillations differently. Therefore, the analysis provides design awareness by evaluating their impact over the entire frequency range, beyond the specific region for which they are primarily intended. Furthermore, the influence of parameters on synchronization properties has been extensively studied. It has been demonstrated that some parameters, such as active damping term and APC bandwidth, affecting the transient response influence the rate at which a system converges to its operating point following a disturbance. Additionally, these parameters can impact the overshoot, which may compromise the system's synchronizing stability. On the other hand, virtual inductance and resistance, aside from their effects on dynamic response, play a significant role in determining static stability; incorporating this analysis into the evaluation of damping characteristics enhances the understanding of virtual impedance selection and highlights the trade-off between damping and synchronizing performance. The validity of these findings has been confirmed through time-domain simulation results.

CRedit authorship contribution statement

Kavian Kamalinejad: Writing – original draft, Validation, Methodology, Conceptualization. **Anant Narula:** Writing – original draft, Supervision, Resources. **Massimo Bongiorno:** Supervision, Conceptualization. **Mebtu Beza:** Validation, Supervision. **Jan R. Svensson:** Supervision.

Declaration of competing interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

Data availability

Data will be made available on request.

References

- [1] Matevosyan J, et al. Grid-forming inverters: Are they the key for high renewable penetration? *IEEE Power Energy Mag* 2019;17(6):89–98.
- [2] Rocabert J, Luna A, Blaabjerg F, Rodriguez P. Control of power converters in AC microgrids. *IEEE Trans Power Electron* 2012;27(11):4734–49.
- [3] Huang S, et al. Predefined-time observer-based sliding mode control for fast frequency support in heterogeneous power system through grid-forming converter. *IEEE Trans Power Syst* 2025;40(6):5116–29.
- [4] Kundur P. Introduction to the power system stability problem. In: *Power system stability and control*. New York: McGraw-Hill; 1994, p. 377–462.
- [5] Rosso R, Wang X, Liserre M, Lu X, Engelken S. Grid-forming converters: Control approaches, grid-synchronization, and future trends—A review. *IEEE Open J Ind Appl* 2021;2:93–109.
- [6] National Grid ESO. Great Britain grid forming best practice guide. Technical report, 2023.
- [7] Favuzza S, Musca R, Zizzo G, Sa'ed JA. Comparative modeling and analysis of EMT and phasor RMS grid-forming converters under different power system dynamics. *IEEE Trans Ind Appl* 2024;60(2):3613–24.
- [8] July A, Savaghebi M, Li G, Saeedifard M, Cutululis NA. Small-signal stability analysis of inertia-based grid-forming MMC interfaced with a grid of varying short-circuit ratio. In: *Proc IEEE energy converters congr expo europe (ECCE europe)*. 2025, p. 1–5.
- [9] Aragon DA, Unamuno E, Ceballos S, Barrena JA. Comparative small-signal evaluation of advanced grid-forming control techniques. *Electr Power Syst Res* 2022;211:108154.
- [10] Mateu-Barriados E, et al. Power oscillation damping controllers for grid-forming power converters in modern power systems. *IEEE Trans Power Syst* 2025;40(6):5130–43.
- [11] Gao X, Zhou D, Anvari-Moghaddam A, Blaabjerg F. Stability analysis of grid-following and grid-forming converters based on state-space modelling. *IEEE Trans Ind Appl* 2024;60(3):4910–20.
- [12] Wang X, Blaabjerg F. Harmonic stability in power electronic-based power systems: Concept, modeling, and analysis. *IEEE Trans Smart Grid* 2019;10(3):2858–70.
- [13] Mohammed N, Udawatte H, Zhou W, Hill DJ, Bahrani B. Grid-forming inverters: A comparative study of different control strategies in frequency and time domains. *IEEE Open J Ind Electron Soc* 2024;5:185–214.
- [14] Zhao F, Wang X, Zhu T. Low-frequency passivity-based analysis and damping of power-synchronization controlled grid-forming inverter. *IEEE J Emerg Sel Top Power Electron* 2023;11(2):1542–54.
- [15] Avdiaj E, Lee D, Sakinci ÖC, Beerten J. Passivity-based design methodology for current-controlled grid-forming MMCs. *IEEE J Emerg Sel Top Ind Electron* 2025;6(4):1366–77. <http://dx.doi.org/10.1109/JESTIE.2025.3585455>.
- [16] Kamalinejad K, Narula A, Bongiorno M, Beza M, Svensson JR. Impact of control parameters on angular stability of grid-forming converters using virtual-admittance based control. In: *Proc energy converters congr expo eur (ECCE europe)*. 2025, p. 1–6.
- [17] Zhao L, Wang X, Jin Z. Exploring damping effect of inner control loops for grid-forming VSCs. *IEEE Open J Power Electron* 2025;6:1595–608. <http://dx.doi.org/10.1109/OJPEL.2025.3614708>.
- [18] ENTSO-E. High penetration of power electronic interfaced power sources (HPOPEIPS). Technical report, 2017.
- [19] Gamesa Siemens. GC0137 SGRE guide to producing NFP plots and interpreting them. Technical report, 2022.
- [20] National Energy System Operator. The grid code. Technical report, 2024.
- [21] Imgart P, Narula A, Bongiorno M, et al. Decoupled PQ grid-forming control with tunable converter frequency behaviour. 2025, TechRxiv.
- [22] Narula A, Bongiorno M, Beza M. Comparison of grid-forming converter control strategies. In: *Proc IEEE energy converters congr expo. ECCE*, 2021, p. 361–8.
- [23] National Grid ESO. Minimum specification required for provision of GB grid forming (GBGF) capability. Technical report, 2021.
- [24] Narula A, Imgart P, Bongiorno M, Beza M, Svensson JR, Hasler J-P. Voltage-based current limitation strategy to preserve grid-forming properties under severe grid disturbances. *IEEE Open J Power Electron* 2023;4:176–88.
- [25] Zamani AR, Ghaderi MH, Hamzeh M. Dynamic response and filtering capability improvement of $\alpha\beta$ -frame cascaded delayed signal cancellation based PLL. *IEEE Trans Energy Convers* 2022;37(2):1156–63.
- [26] Lewis EA. Enstore's updated guide for GB grid forming converters (v-004). National Grid ESO; 2021.
- [27] Yu M, Roscoe AJ, Dyśko A, et al. Instantaneous penetration level limits of non-synchronous devices in the British power system. *IET Renew Power Gener* 2017;11(8):1211–7.
- [28] Zhao F, Zhu T, Li Z, Wang X. Low-frequency resonances in grid-forming converters: Causes and damping control. *IEEE Trans Power Electron* 2024;39(11):14430–47.
- [29] Zhang L, Nee H-P, Harnefors L. Analysis of stability limitations of a VSC-HVDC link using power-synchronization control. *IEEE Trans Power Syst* 2011;26(3):1326–37.
- [30] Pan D, Wang X, Liu F, Shi R. Transient stability of voltage-source converters with grid-forming control: A design-oriented study. *IEEE J Emerg Sel Top Power Electron* 2020;8(2):1019–33.