Abstract—Grid-connected inverters used in High-Voltage Direct Current (HVDC) transmission systems, which have become increasingly popular worldwide in recent years, employ a variety of control methods for each frequency band, and resonances can occur between multiple control devices within a device or between multiple devices placed in close proximity. Practical impedance-based data-driven methods, rather than linear approximation models, are becoming widely used for these stability analyses. On the other hand, HVDC transmission systems require multiple domain transformations between synchronous and stationary reference frames. Therefore, in this study, both Multiple-Input Multiple-Output (MIMO) analysis using impedances on the direct-quadrature (dq) synchronous frame and Single-Input Single-Output (SISO) analysis using positive and negative sequence impedances on the stationary reference frame were performed. Consequently, the two unstable frequencies identified in the eigenvalue trajectories for the dq impedance were also identified within a single Nyquist plot for the sequence domain impedance, validating both the unstable frequencies observed in the dq domain. These frequencies suggest that weak AC systems on the generation side of the HVDC transmission system may be causing instability in the DC transmission lines and AC systems on the demand side. Additionally, the results of this study also suggest that the introduction of an advanced inverter control method, called the Virtual Synchronous Generator (VSG) control method, in the grid-connected inverter on the receiving side may mitigate the effects of having weak AC systems on the generation side.

Index Terms—High-Voltage Direct Current (HVDC) transmission, modular multilevel converter, marginally stable, virtual synchronous generator

I. INTRODUCTION

Power sources based on Renewable Energy Resources (RESs) have gained widespread popularity in interest of developing a carbon-neutral and decarbonized society. Extensive research has been conducted on the development of wind power generation systems using High-Voltage DC (HVDC) transmission systems owing to their potential economic viability if generated on a large scale.

Modular Multilevel Converters (MMCs) are often employed as converters between the DC transmission line and AC grid. However, various advanced inverter control methods have been proposed in recent years to improve AC system stability. The Virtual Synchronous Generator (VSG) control method is the one that uses storage batteries to equip grid-connected inverters with the characteristics of synchronous generators, and the resultant virtual inertia enhances AC grid stability and promotes the introduction of renewable energy sources. Therefore, there is a great possibility of the VSG control method being adopted in future HVDC systems.

However, unlike the induction generators in wind turbines or the synchronous generators in onshore AC power plants, power electronic devices such as MMCs involve high-speed, complex, and unique controls. Additionally, resonances in a wide frequency range, from several Hz to several tens of kHz, are reported within the equipment or between the equipment and grid. Therefore, there is an urgent need for accurate control and stability analysis of HVDC systems in order to improve their robustness.

The eigenvalue analysis method proposed by Hiti et al. [1] using Fourier series expansion with small-signal models is difficult to apply to modern power systems because it does not assume nonlinear elements in the range of a few Hz to several tens of kHz in power electronics equipment. Moreover, since each of the equipment in an HVDC system is provided by different vendors, inverter control specifications are generally not disclosed. To overcome these drawbacks, data-driven and impedance-based stability analysis methods have been developed recently for accurate analysis of HVDC systems.

Although various studies on impedance-based power system analysis have been conducted globally, most of them have focused primarily on the characteristics of specific equipment, such as grid-connected inverters and loads. For example, Belkhayat [2] incorporated impedance-based stability theory in DC circuits into a three-phase AC circuit in a direct-quadrature (dq) synchronous reference frame and found it to be consistent with the results of mathematical model-based eigenvalue analysis. Next, a sequence impedance-based analysis was proposed to apply the Nyquist criterion-based stability discrimination, even for 2x2 matrices representing three-phase AC impedances [3], [4], and subsequently, Rygg et
al. [5]–[7] proved that dq and sequence impedances are equivalent. In [8], an impedance-based analysis was performed in a microgrid (MG) consisting of inverter power supplies equipped with two different advanced inverter control methods (grid-forming and grid-following inverters). The applications of the impedance method have also been further developed, with methods to reduce the computational burden of numerical analysis [9], [10] and analytical results from demonstration tests [11], [12]. Amin et al. extended an inverter-based MG to an HVDC transmission system and performed the eigenvalue analysis [13]–[15]. In [16], a method was proposed to transform the entire AC system into an equivalent scalar impedance on symmetric coordinates. In [17], an impedance-based analysis of the impact of connecting inverters at different points in the existing system was performed, which can be used as a reference for future power system expansion and planning. The three-port method presented in [18], [19] can also easily represent entire complex systems such as mesh and loop systems by connecting the AC and DC impedances.

The impedance method has already been employed in the stability analysis of an actual HVDC transmission system that is currently in operation. At a wind farm owned by the State Grid Corporation of China, impedance-based analysis of wind turbines from several manufacturers was performed using Hardware-in-the-Loop (CHIL) real-time simulation to identify potential sub-synchronous resonances [20]. TenneT, a German transmission system operator, requires manufacturers of offshore wind turbines to perform impedance-based stability analysis using black box electromagnetic transient (EMT) models provided by HVDC equipment vendors [21].

Against the above background, this study will clarify the impact of a weak generation side AC system on the demand side AC system. Assuming that the demand side AC system is going to be connected by a large number of renewables and become more vulnerable in the future, two types of power control for its MMC were applied: a conventional active and reactive power control and VSG control, which can contribute to stabilizing the AC system. A weak system was simulated by varying the Short Circuit Ratio (SCR) of the AC grid in each terminal was calculated using $\text{SCR} = \frac{V_{\text{line}}}{Z_p} \cdot \frac{V_{\text{bus}}}{Z_s}$, where $V_{\text{line}}$ denotes the line-to-line voltage, $P_{\text{rated}}$ denotes the rated power of the VSC, and $Z_s$ represents the magnitude of $z_1$ or $z_2$ [24]. The transmission line model provided in PSCAD/EMTDC was applied to each of them.

Fig. 1 presents the topology of a symmetrical monopolar HVDC system. The monopolar HVDC system was commonly used in the early stages of research and practical applications. Although HVDC transmission systems have both bipolar and monopolar configurations, the differences between these configurations are not discussed in this study.

Terminals 1 (T1) and 2 (T2) represent the generation and demand side terminals, respectively. The electric power flows from T1 to T2, without any power storage devices involved. The generator (e.g. wind turbine) is represented by a voltage source (AC1) and a source impedance ($Z_1$) in T1, and the distribution system is represented by a voltage source (AC2) and a source impedance ($Z_2$) in T2. The T1 and T2 terminals comprise voltage source converters (VSC) MMC1 and MMC2, respectively. This study used the VSC HVDC Model Type-4, as defined in CIGRE B4.57 [23]. In both MMC1 and MMC2, the number of submodules was 76, and total cell capacitance, arm reactance, and resistance were, 2800μF, 50mH, and 1Ω, respectively. They were connected to the buses BUS1 and BUS2 through transformers Tr1 and Tr2, respectively.

The source impedances ($z_1$ and $z_2$) were assumed to be of the R-R/L type. The SCR of the AC grid in each terminal was calculated using $\text{SCR} = \frac{V_{\text{line}}}{Z_{\text{p}}}, \sqrt{\frac{V_{\text{bus}}}{Z_s}}$, where $V_{\text{line}}$ denotes the line-to-line voltage, $P_{\text{rated}}$ denotes the rated power of the VSC, and $Z_s$ represents the magnitude of $z_1$ or $z_2$ [24]. The transmission line model provided in PSCAD/EMTDC was applied to each of them.

Two types of impedances were used in the analysis: dq impedance and sequence impedance. The dq impedance was calculated in the same dq-synchronous reference frame as the generator and inverter control, while the sequence impedance was calculated from the positive and negative sequence of signals in the stationary reference frame. Both impedances were MIMO (2×2) impedances in three-phase AC systems. Although the dq MIMO impedance was easily obtained in the same dq domain as the generator and inverter control system, the analysis was slightly complicated because the unstable frequency was represented by two separate eigenvalue loci. Therefore, once the dq MIMO impedance was converted to MIMO sequence impedance, it was further converted to equivalent SISO impedance using the method adopted in [16]. This makes it possible to confirm the unstable frequency of the entire circuit with fewer calculations.

II. TARGET SYSTEM AND CONTROLS

A. HVDC Transmission System

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Fig. 1. Topology of a symmetrical monopolar HVDC.
In this study, the overhead line parameters were used for the 400km DC transmission line to simplify the simulation. The 400km DC transmission line was divided into two 200km π type sections, and the series impedance \( Z_s \) and shunt admittance \( Y_s \) were set to \( Z_s = (2.122 + j0.004) \Omega \) and \( Y_s = -j0.002 \mu \Omega \), respectively, for every 200km.

**B. Voltage and Power Controls**

Fig. 2 (a) presents DC voltage control loop in the d-axis and AC voltage control loop in q-axis for MMC1. \( V_d^* \) and \( V_q^* \) denote the reference and feedback values of the DC voltage, respectively. Similarly, \( V_d^* \) and \( V_q^* \) denote the reference and feedback values of the effective AC voltage (\( V_{ac} \)), respectively. Fig. 2 (b) depicts the conventional power control scheme used in MMC2. \( P \) and \( Q \) denote the reference and feedback values of the active power, respectively; \( Q' \) and \( Q'' \) denote the reference and feedback values of the reactive power, respectively. The outputs of the voltage and power controllers \( (i_d^* \text{ and } i_q^*) \) were limited to a maximum current reference value of 1.1pu and were fed to the current controller as commands. A non-interference control was used in conjunction with the current control, where \( o_L = 0.2 \text{pu} \). The pulse width modulation (PWM) voltage command vector \( (V_{dqc}) \), obtained from the current controller, was transmitted to the PWM controller. The superscripted "*" values, apart from \( i_d^* \text{ and } i_q^* \), were assumed to be set in the supervisory control. The transfer functions of \( V_{ds}, \ V_{ac}, \ P, \) and \( Q \) were \( H_{Vds}(s) = 6 + (20/s), \ H_{Vac}(s) = 0.1 + (20/s), \ H_P(s) = 0.25 + (5/s), \) and \( H_Q(s) = 4 + (20/s), \) respectively. The transfer functions of the current compensators for MMC1 and MMC2 were \( H_{c1}(s) = 0.65 + (100/s) \) and \( H_{c2}(s) = 0.6 + (10/s) \), respectively. Here, \( s \) denotes the Laplace’s differential operator. The value of each parameter was referenced from [25].

Numerical simulations were performed using the PSCAD/EMTDC circuit with the system configuration shown in Fig. 1 and control shown in Fig. 2. An active power load of 750 MW (0.75pu) was connected to BUS2 and the parallel power feeders from both MMCs were tested. MMC1 and MMC2 were deblocked 4s after the simulation was initiated. Both operations were launched with the output power command at \( P^* = 0 \text{pu} \). The output power command was changed to \( P^* = 0.25 \text{pu} \) and \( P^* = 0.5 \text{pu} \), respectively, after 5s and 7s into the operation. The impedance \( Z_i \) of AC in T1 was varied and its time response was measured. The SCRs of T1 were set to 0.98 (blue), 0.96 (purple), 0.94 (red), and 0.92 (green), while the SCR in T2 was set to 10. Fig. 3 (a) depicts the time responses of active and reactive powers and the time responses of DC voltages. Under the test conditions, when the SCR of T1 dipped below 0.92 (green), the DC voltage control of MMC1 collapsed, affecting the power control of T2. The time response of DC voltage exhibited significant high frequency (324Hz) and low frequency (12Hz) oscillations, while the time response of power showed low frequency (12Hz) oscillations with negligible high frequency (324 Hz) oscillations. Fig. 3 (b) shows the result of applying the discrete Fourier transform (DFT) to the time response of Fig. 3 (a) with an SCR of 0.92 for T1. In all the graphs presented in Fig. 3 (b), in addition to the 12Hz components identified in Fig. 3 (a), 24Hz, 36Hz, and 48 Hz components were also identified. These frequencies represented in the AC powers are values in dq-synchronous reference frame and correspond to 60±12Hz, 60±24Hz, 60±36Hz, and 60±48Hz in stationary reference frames with respect to the fundamental frequency of 60Hz. Similarly, 324Hz in the high-frequency band on the dq-synchronous reference frame indicates (324–60)Hz on the stationary reference frame.

**C. VSG Control (Kawasaki Topology)**

The power flow control from T1 to T2 in MMC1 assumes that the DC voltage is maintained at a constant value. However, the above tests demonstrate that a weak
AC system in T1 would prevent MMC1 from maintaining a constant DC voltage and further affect the power control of MMC2 connected to the strong AC system in T2. Therefore, we employed VSG power control in MMC2 to observe the variations in the stability of the entire HVDC system and compare them with those of conventional power control methods.

Fig. 4 illustrates the Kawasaki topology of VSG control [22]. \( \omega_i \) and \( \omega_c \) denote the reference and feedback values of the angular velocity of the AC voltage. \( \omega_i \) was referenced from the supervisory control, whereas \( \omega_c \) was calculated using the phase-locked loop in the controller. The transfer function of the phase-locked loop (PLL) was 
\[ H_{PLL,sq}(s) = \frac{1}{1+(s^2+1.016s+20+125/s)} \]
The P-F controller (red-framed portion) implemented the virtual swing equation and droop characteristic of \( \omega_c \) corresponding to \( P \); its transfer function was denoted as 
\[ H_{P-F}(s) \]
The output of the P-F controller is the phase angle (\( \delta \)). The Q-V controller (blue-framed portion) implements the droop characteristic of \( V_{ac} \) corresponding to \( Q \) and acts as the automatic voltage regulator (AVR). The transfer function of the droop characteristic considering the measurement delay was denoted as \( H_{QV}(s) \), and the transfer function of the AVR was denoted as \( H_{AVR}(s) \). The output of the Q-V controller is the electromotive force \( (E_v) \). The impedance model (green-framed portion) outputs the current command values (\( i_q^* \) and \( i_d^* \)). The P-F controller, the Q-V controller, and the impedance model are represented by

\[
\delta = \omega_i - \frac{(P^* - P)H_{P-F}(s)}{s} - \omega_c + \omega_r
\]

\[
|E_v| = H_{QV}(s)\left((Q^* - Q)H_{QV}(s) - V_{ac}\right)
\]

\[
\Gamma = \begin{bmatrix}
\frac{r}{r^2 + x^2} & \frac{x}{r^2 + x^2} \\
\frac{-x}{r^2 + x^2} & \frac{r}{r^2 + x^2}
\end{bmatrix}
\]

The terms \( r \) and \( x \) denote the virtual resistance and reactance set in the software, respectively. The AC voltage and electromotive force vectors in the stationary reference (system) frame \( (V_{ac} \text{ and } E_v) \) were transformed into the dq-synchronous reference (converter) frame as

\[
V_{ac} = \begin{bmatrix} v_d \cr v_q \end{bmatrix} \quad \text{and} \quad E_v = \begin{bmatrix} e_d \cr e_q \end{bmatrix}
\]

respectively. In the VSG control, a non-interference control was not included. In this study, \( \omega_i = 2\pi 60 \text{rad/s} \). The transfer functions of \( H_{P-F}(s) \), \( H_{QV}(s) \), and \( H_{AVR}(s) \) were \( H_{P-F}(s) = 0.05/(1+0.12s) \), \( H_{QV}(s) = 0.05/(1+0.15s) \), and \( H_{AVR}(s) = 10+80/s \), respectively. The transfer function of the current compensator was \( H_{LC}(s) = 0.1+2/s \) in the VSG controller. The virtual impedances were set as \( r=0.2pu \) and \( x=0.4pu \), respectively.

III. ALGORITHM OF SYSTEM IMPEDANCE

A. Analysis on the Dq Synchronous Reference Frame

The detailed derivation of the algorithm used in this study is provided in [7]. In the impedance analysis, the general power system was split between the source and load subsystems. Fig. 6 illustrates Thévenin and Norton equivalents and their block diagrams, respectively. The impedances of the source and load subsystems are \( Z_s(s) \) and \( Y_L(s) = Z_L(s)^I(s) \). If the system is a DC power system, it can be analyzed using the Nyquist criterion in the minor-loop gain, which is defined as \( L(s) = Z_L(s)Y_L(s) \). While in the three-phase AC system, the impedance is generally converted from the stationary reference frame to the dq-synchronous reference frame, neglecting the zero-sequence component. Then, the minor-loop gain, given by \( L(s) = Z_L(s)Y_L(s) \), is a \( 2 \times 2 \) matrix in the dq-
synchronous reference frame, and subsequently, the generalized Nyquist criterion (GNC) can be applied to $L(s)$, similar to the DC system. This indicates that the system stability can be assessed by the condition that the loci of all eigenvalues of the minor-loop gain $L(s)$ ($\lambda_1(s)$ and $\lambda_2(s)$), which satisfy (4), do not encircle the point $(-1, j0)$.

$$\det(I + L(s)) = \prod_{i}(1 + \lambda_i(s)) \quad (4)$$

Hence, it is possible to convert the impedance of a 2×2 matrix to an equivalent scalar impedance [16]. Here, the MIMO dq impedance was converted to MIMO sequence impedance using equation (2), which was then converted to the equivalent SISO sequence impedance. The subscripts of the matrix represent the source/load subsystem as before, and the superscripts “pp”, “pn”, “np”, and “nn” represent the (1,1), (1,2), (2,1), and (2,2) components of a matrix, respectively. Equations (6) and (7) represent the positive and negative sequence impedances of the entire circuit, and a notable advantage of the method is that these values are scalars.

$$Z^p(s) = Z^p_s + Z^p_{IL} - \left(\frac{Z^m_s + Z^m_{IL}}{Z^m_s + Z^m_{IL}}\right)(Z^p_s + Z^p_{IL}) \quad (6)$$

$$Z^n(s) = Z^n_s + Z^n_{IL} - \left(\frac{Z^m_s + Z^m_{IL}}{Z^m_s + Z^m_{IL}}\right)(Z^n_s + Z^n_{IL}) \quad (7)$$

Splitting $Z^p(s)$ between source and load impedances ($Z^p(s) = Z^p_s + Z^p_{IL}$) as expressed in (8) and (9),

$$Z^p_s = \left(\frac{Z^m_s + Z^m_{IL}}{Z^m_s + Z^m_{IL}}\right) Z^p_{IL} \quad (8)$$

$$Z^p_{IL} = \left(\frac{Z^m_s + Z^m_{IL}}{Z^m_s + Z^m_{IL}}\right) Z^p_s \quad (9)$$

In each of (6)–(9), the interference terms were aggregated in the numerator of the fraction in the final term. The loop gains of positive and negative sequences ($\lambda^p(s)$, $\lambda^n(s)$), which were also scalars, were calculated using (10) and (11).

$$\lambda^p(s) = Z^p_s(s)/Z^p_{IL}(s) \quad (10)$$

$$\lambda^n(s) = Z^n_s(s)/Z^n_{IL}(s) \quad (11)$$

It is much easier to analyze the system by using these scalar eigenvalues than to analyze it using the two eigenvalues, including the interphase interference expressed in (4).

IV. IMPEDANCE-BASED STABILITY ANALYSIS

In this study, a series voltage perturbation was used in the simulation, and the impedance was calculated from the measured voltage and current. Table I lists the testcases (#1–2) used to measure these values.

<table>
<thead>
<tr>
<th>Case</th>
<th>Perturbation Point</th>
<th>SCR of T1</th>
</tr>
</thead>
<tbody>
<tr>
<td>#1</td>
<td>BUS1</td>
<td>0.92</td>
</tr>
<tr>
<td>#2</td>
<td>BUS1</td>
<td>0.94</td>
</tr>
</tbody>
</table>

Fig. 7 to Fig. 12 show the results of the impedance-based analyses for the system using the PQ control for MMC2 in T2.
Fig. 7. dq MIMO impedances of sub-circuits: (a) Output impedance of source-side ($Z_s^n(s)$) and (b) Output impedance of load-side ($Z_l^n(s)$).

Fig. 8. Bode plots of the eigenvalues of the MIMO minor-loop gain: (a) Bode plots of $\lambda_1$ (the right is an enlargement of the left) and (b) Bode plots of $\lambda_2$ (the right is an enlargement of the left).

Fig. 9. Nyquist plots of the eigenvalues of the MIMO minor-loop gain: (a) Eigenvalue loci of $\lambda_1$ and (b) Eigenvalue loci of $\lambda_2$.

Fig. 10. Equivalent SISO impedance of sub-circuits: (a) Source-side impedance ($Z_s^n$) and (b) Load-side impedance ($Z_l^n$).
visually. Both the low-frequency unstable eigenvalues are shown in Fig. 12. The right is an enlargement of the left figure. The eigenvalues should be used to determine the stability (Fig. 11 and Fig. 12) and the accuracy of the estimation should be improved.

In each of Fig. 11 (a) and Fig. 11 (b), the right figure is an enlargement of the left figure. In the results for the impedance-based analyses of the source and load sub-systems (Fig. 8 and Fig. 9), the characteristic gain peaks of 12Hz, 24Hz, 36Hz, and 48Hz are seen in #1. These results clearly agree with those shown in Fig. 3. In Fig. 8 (a) and Fig. 9 (a) (results of λ₁(s)), the locus of 24Hz shows the possibility of instability, and in Fig. 8 (b) and Fig. 9 (b) (results of λ₂(s)), 12Hz, 36Hz, and 48Hz can be seen as unstable frequencies. By contrast, both Fig. 11 and Fig. 12 (results of λ⁺(s) or λ⁻(s)) show that these four frequencies can be represented at once in a single Bode or Nyquist diagram. In addition to the four frequencies listed above, 40Hz and 80Hz are also identified in Fig. 8, Fig. 9, Fig. 11, and Fig. 12 and are considered candidates for unstable frequencies. However, these frequencies were not identified in Fig. 3. Additional investigation is needed to determine if these frequencies are actually potential unstable frequencies.

The high-frequency oscillation frequencies of the DC voltages of 312Hz, 324Hz, 336Hz, and 348Hz when the SCR of T1 is 0.92 in Fig. 3 correspond to 252Hz, 264Hz, 276Hz, and 288Hz in the synchronous reference frame, and they can be seen around the point (−1, j0) in the eigenvalue loci of Fig. 9 and Fig. 12. These frequencies are located farther from the abovementioned point than the low-frequency oscillation frequencies (12Hz, 24Hz, 36Hz, and 48Hz) seen in the AC powers in Fig. 3; this implies that the possibility of these unstable frequencies are also indicated by the AC impedance. However, as described in a previous work [14], in an AC system at BUS1, which was used as the split point, the unstable frequencies on the DC transmission line may have been canceled out and obscured in the loop gain. Therefore, detailed analysis should be performed using the DC perturbation. Moreover, these frequencies are also error factors in the estimated transfer function of MATLAB, and the accuracy of the estimation should be improved.

B. HVDC Transmission System with VSG Control for MMC2

Similar to Fig. 7 to Fig. 12, Fig. 13 to Fig. 18 illustrate the results for the impedance-based analyses of the system when the VSG control was implemented in MMC2. Fig. 13 represents the dq (MIMO) impedances of the source and load sub-systems (Z_s^{m}(s), Z_s^{n}(s)), and Fig. 14 and Fig. 15 represent the Bode and Nyquist plots of the two eigenvalues (λ₁(s) and λ₂(s)) of the minor-loop gain matrix (L(s) = Z_s Y_j Y_j(s)), respectively.

Fig. 16 represents the equivalent SISO impedances (Z_s^{μ}(s), Z_s^{τ}(s)), and Fig. 17 and Fig. 18 represent the Bode and Nyquist plots of eigenvalues (λ⁺(s) and λ⁻(s)), respectively. Both the low-frequency unstable frequencies (12Hz, 24Hz, 36Hz, 40Hz, 48Hz, and 80Hz) and high-frequency unstable frequencies (252Hz, 264Hz, 276Hz, and 288Hz), which were seen when PQ control was used for MMC2, with an SCR of 0.92 for T1, were not identified as unstable frequencies in the impedances, Bode plots, and Nyquist plots (MIMO, SISO) when VSG control was used for MMC2.
Conventional PQ and VSG controls are power controls placed in the upper layer of the same minor current loop control, and their frequency bands are typically 0.1 to several Hz. The unstable frequencies of 12Hz, 24Hz, 36Hz, and 48Hz on the synchronous reference frame (48Hz, 36Hz, 24Hz, and 12Hz on the stationary reference frame) identified in the above test results were located in a slightly higher frequency band than that of the power control frequency band. Therefore, the differences of the results may originate from the fact that the VSG control applied in this study intentionally sets the PLL and current controller gains as low to enable uninterruptible transition between stand-alone and grid-connected operation.
The VSG control employed in this study implemented the GFM-type Kawasaki topology, which is characterized by simulated synchronous generator characteristics that can be reproduced relatively easily and a virtual impedance mechanism. Such VSG control is expected to avoid unstable frequencies identified in the impedance-based stability analysis quite easily by employing software-configured virtual impedance. However, sensitivity analysis, by changing the control parameters, to improve the stability margin of the output impedance and mathematical analysis, such as a linear model of VSG control for consistency verification, are not included in this research phase and require continued research in the future. As the current control and PLL in its lower layers affect the response of the power control, it is necessary to carefully diagnose and verify which control variable is responsible for the difference in stabilities of the two power controls (PQ control and VSG control) used in MMC2.

CONFLICT OF INTEREST

The authors declare no conflict of interest.

AUTHOR CONTRIBUTIONS

Y. Hirase conducted the research; A. F. B. Masod, S. Kato, and K. Ohuchi performed the simulations; A. F. B. Masod, S. Kato, and K. Ohuchi conducted the data curation; all authors analyzed the data; all authors wrote the paper; all authors had approved the final version.

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REFERENCES


[8] E. Unamuno, A. Rygg, M. Amin, M. Molinas, and J. A. Barrena, “Impedance-based stability evaluation of virtual synchronous...


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