

# Negative-Sequence Impedance Provision of A Virtual Admittance-Based Inverter-Based Resource

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**Abstract**—While some grid-forming (GFM) inverter-based resources (IBRs) have both positive- and negative-sequence control units equipped to provide desired negative-sequence impedance, we find that a virtual admittance-based GFM-IBR with only positive-sequence unit can also provide desired negative-sequence impedance for grid code compliance. The goal of this paper is to provide an analysis to relate the negative-sequence impedance provision with the control scheme. To this end, an electromagnetic transient (EMT) testbed is developed to examine an IBR's behavior subject to unbalanced grid faults. The negative-sequence voltage and current components are measured to calculate the equivalent negative-sequence impedance empirically. It is found that this impedance is indeed associated with the virtual impedance. The underlying mechanism is further discovered through complex vector-based analysis and frame conversion. The major takeaways are two-fold: (i) For a given control scheme, its negative-sequence impedance can be found analytically; (ii) inverter control can be designed more concisely to provide both positive- and negative-sequence impedances, in contrast of having more complicated design with both positive- and negative-sequence control units included.

**Index Terms**—Inverter-based resource, virtual admittance, negative-sequence impedance, complex vector, space vector.

## I. INTRODUCTION

THE proliferation of inverter-based resources (IBRs)—predominantly solar photovoltaic and wind generation—is changing the characteristics of power grids. One issue identified by the grid industry is that IBRs provide very small negative-sequence currents [?]. This feature has caused real-world issues during unbalanced fault conditions, e.g., difficulty in differentiating faulted phase in a single-line-ground fault [?], overcurrent in an open-circuit fault [?]. In both [?] and [?], grid-following IBRs with only positive-sequence control have been examined. EMT simulation results further confirm the real-world observations that those IBRs provide very small negative-sequence currents. Or their negative-sequence impedance is very large.

In addition to small magnitudes of negative-sequence currents, another issue is that the negative-sequence current provided by an IBR during a forward unbalanced fault may not lead the negative-sequence voltage [?]. In a traditional power grid, for a forward fault, this current phasor should lead the voltage phasor; while the current lags the voltage for a backward fault [?]. This issue of negative-sequence current not at the desired phase angle may lead to mis-operation of the negative-sequence based directional unit of a relay.

Recently, the grid industry is pushing forward new standards, e.g., IEEE 2800-2022 [?] or grid codes to require bulk power system (BPS)-connected IBRs providing sufficient negative-sequence currents and the currents should be in the desired phase angle range.

The above two issues can be mitigated by designing the inverter control so that the IBR can act as an inductive impedance in the negative sequence. When an IBR acts as a purely inductive impedance in the negative sequence, its exporting current  $\bar{I}_n$  leads the negative-sequence voltage  $\bar{V}_n$  by  $90^\circ$ . This feature fulfills the IEEE 2800-2022 requirement on negative-sequence current direction.

In both grid-following (GFL)-IBRs—which rely on a phase-locked loop (PLL) for synchronization, or grid-forming (GFM)-IBRs—which rely on a power-based control unit for synchronizing, negative-sequence control units have been designed to provide inductive negative-sequence impedance. For example, Germany's grid code VDE-AR-N 4120 requires wind farms to provide a negative-sequence reactive current that is proportional to the change in negative-sequence voltage [?]. The corresponding control design is shown in [?]. EPRI's solar PV EMT model [?] also shows that the solar PV can provide negative-sequence current and the design philosophy is same as that in [?].

The above two examples are related to GFL-IBR. For GFM-IBRs, there are various design philosophies [?]. Three designs are based on the existence of an inner current control and a power-based synchronizing unit. These designs differ in the outer loop control and they are termed as coupled voltage control [?] (proposed in 2007 by the Imperial College group and widely used in the literature), decoupled voltage control [?] (proposed in 2023 by the University of South Florida group), and virtual admittance control [?] (proposed in 2024 by Nidec ASI).

Comparison of the three types of GFM weak grid operation and low voltage ride-through has been made in [?] with the assumption that controls are implemented in the positively rotating  $dq$  frame only. On the other hand, both positive- and negative-sequence control units have been proposed and implemented. [?] has proposed the addition of a negative-sequence control unit implemented in the negatively rotating  $dq$  frame to introduce a negative-sequence inductive impedance for the coupled voltage control GFM. In the original paper describing the virtual admittance-based GFM control [?], there are two sets of controls implemented in the positive and negative rotating  $dq$  frames, respectively. They are termed as the positive- and negative-sequence control units and the negative-

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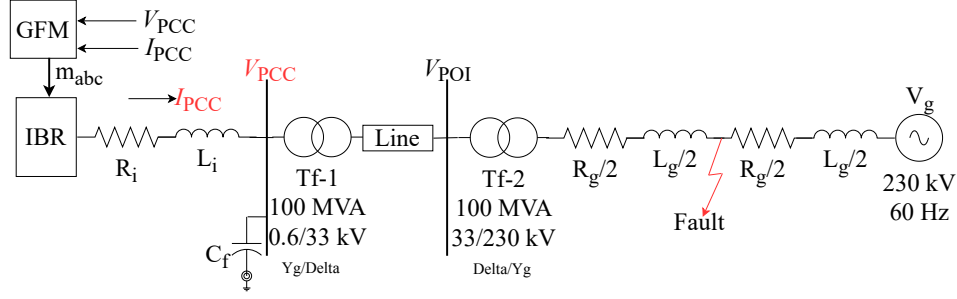


Fig. 1: GFM-based IBR connected to a grid. The fault is located on the 230-kV transmission line. The two transformers have  $Y_g/\Delta$  and  $\Delta/Y_g$  connections.

sequence control unit has been shown in EMT simulation to provide a negative-sequence inductive impedance [?].

**Goals:** The goal of this paper is to examine whether it is possible to provide negative-sequence inductive impedance **through the positive-sequence  $dq$ -frame control only**. In other words, this paper examines whether it is necessary to have a devoted negative-sequence control unit.

This inquiry was inspired by examining a synchronous generator. In the modeling perspective, it can be viewed as two coupled circuits in the  $dq$  frame. Yet, it is well known that a synchronous generator can provide both positive- and negative-sequence reactance. In fact, the negative-sequence reactance is shown to be related with the  $dq$ -frame reactance. The derivation has been given in the appendices of the classic book of power system analysis [?].

The design philosophy of the virtual admittance control is to emulate a Thévenin equivalent circuit using  $dq$ -frame control. Therefore, it is reasonable to further inquire if such design leads to negative-sequence impedance and complies with the grid code.

Ours is not the first that inquires the negative-sequence impedance provision capability of a GFM implemented in the  $dq$ -frame with only positive-sequence control. A recent paper [?] has examined the negative-sequence behavior for a GFM with balanced positive-sequence control only. This GFM control is the coupled voltage control type [?], with the PCC bus voltage being regulated using proportional integral (PI) controllers along with the cross-coupling voltage units and feedforward current units. The finding from [?] is that the negative-sequence impedance is primarily determined by the voltage PI control parameters. Numerical studies show that the negative-sequence impedance has a magnitude in the range of 1 pu to 0.5 pu, and an angle in the range of  $-200^\circ$  to  $-90^\circ$ , implicating that such a GFM fails to meet the IEEE Std. 2800-2022 requirement of providing inductive impedance. Therefore, a recommendation of [?] is that both positive and negative-sequence control units are necessary.

It will be interesting to see if a virtual admittance-GFM can provide negative-sequence impedance for grid code compliance.

**Our contributions:** To this end, we carry out an investigation through a detailed EMT simulation framework. We empirically quantify the negative-sequence impedance of a virtual admittance-based GFM-IBR under unbalanced fault conditions, observing a consistent pattern. We further carry

out complex vector-based analysis to reveal the relationship between the negative-sequence impedance and the virtual admittance control. This relationship based on the mathematical analysis corroborates the findings from the EMT simulation results. The analysis carried out in this paper builds a clear relationship between IBR controls and IBR's negative-sequence impedance. The key engineering finding of our paper is that a virtual-admittance GFM converter with only positive-sequence control implemented is capable of providing negative-sequence current and impedance that comply with grid code requirements.

**Paper structure:** The remainder of the paper is organized as follows: Section II introduces the virtual admittance-based GFM control scheme and the simulation testbed. Section III presents the simulation results under unbalanced fault conditions. Section IV provides a detailed mathematical analysis to derive the negative-sequence impedance's expression in terms of control. Section V concludes the paper.

## II. VIRTUAL ADMITTANCE BASED GRID-FORMING CONTROL

The testbed is shown in Fig. 1. It comprises a GFM-IBR, rated at 100 MW with a terminal voltage of 600 V, operating at 60 Hz. The low-voltage output is elevated to 33 kV via a step-up transformer. This 33-kV bus is connected to a 1-km overhead transmission line to an intermediate substation—the point of interconnection (POI). A second step-up transformer interfaces this network with the high-voltage transmission system by raising the voltage to 230 kV. On the 230-kV main grid side, the network Thévenin equivalent is modeled by an impedance (where  $Z_g = R_g + jX_g$  at the nominal frequency). A complete listing of the detailed system parameters is provided in Table I.

The control diagram of the GFM is shown in Fig. 2. This GFM control structure was originally proposed in [?]. The GFM control structure has an inner current control to ensure the current measurements tightly track the current orders. Its outer control is termed virtual admittance control, which aims to emulate the IBR as a constant voltage source  $\vec{v}_{\text{ref}}$  behind an impedance  $R_v + sL_v$ , when viewed from the point of common coupling (PCC) bus.

The design philosophy is characterized by the following equation. When viewed in the static frame, the constant voltage

TABLE I: Main Parameters of the System

| Description           | Item               | Value                | Per-Unit               |
|-----------------------|--------------------|----------------------|------------------------|
| Rated Power           | $S_{\text{rated}}$ | 100 MVA              | 1                      |
| Rated Voltage         | $V_{\text{rated}}$ | 600 V, 33 kV, 230 kV | 1                      |
| Nominal Frequency     | $f$                | 60 Hz                | -                      |
| Filter Resistance     | $R_i$              | 0.108 mΩ             | 0.03                   |
| Filter Inductance     | $X_{Li}$           | 0.72 mΩ              | 0.2                    |
| Filter Admittance     | $B_c$              | 0.289 mΩ             | 0.0803                 |
| Transformer Reactance | $X_{fmr}$          | 0.288 mΩ             | 0.08                   |
| Grid Resistance       | $R_g$              | 26.45 Ω              | 0.05                   |
| Grid Reactance        | $X_g$              | 264.5 Ω              | 0.5                    |
| Line Resistance       | $R_T$              | 0.365 mΩ             | $0.336 \times 10^{-4}$ |
| Line Reactance        | $X_T$              | 6.458 mΩ             | $0.593 \times 10^{-3}$ |
| Line Admittance       | $B_T$              | 0.211 mΩ             | $1.94 \times 10^{-5}$  |
| Line Length           | l                  | 1 km                 | -                      |

source  $\vec{v}_{\text{ref}}$ , the PCC bus voltage  $\vec{v}$ , and the IBR's exporting current  $\vec{i}$  are related as follows:

$$\vec{v}_{\text{ref}} - \vec{v} = (R_v + sL_v)\vec{i}. \quad (1)$$

In the  $dq$  frame (rotating at a speed of  $\omega$ ) against the static frame), the voltage and current relationship is as follows:

$$\begin{aligned} \bar{V}_{\text{ref}} - \bar{V} &= (R_v + L_v s + j\omega L_v)\bar{I}, \\ \Rightarrow i_d &= \frac{1}{R_v + L_v s} (v_{d,\text{ref}} - v_d + \omega L_v i_q), \\ i_q &= \frac{1}{R_v + L_v s} (v_{q,\text{ref}} - v_q - \omega L_v i_d). \end{aligned} \quad (2)$$

Accordingly, the virtual admittance control generates the  $dq$  current orders following the above characteristics:

$$i_{d,\text{ref}} = \frac{1}{R_v + L_v s} (v_{d,\text{ref}} - v_d + \omega L_v i_{q,\text{ref}}), \quad (3)$$

$$i_{q,\text{ref}} = \frac{1}{R_v + L_v s} (v_{q,\text{ref}} - v_q - \omega L_v i_{d,\text{ref}}). \quad (4)$$

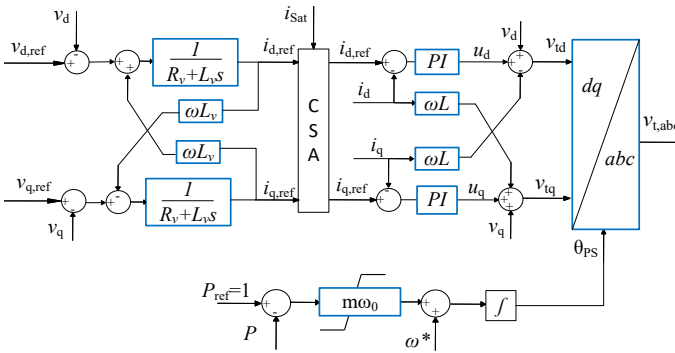


Fig. 2: Virtual admittance-based grid-forming control scheme incorporating with power synchronization.

This characteristics can be achieved by control implementation as shown in Fig. 2, where the  $dq$  voltage components of the PCC bus are compared with the references to generate errors. The errors are complemented by coupling terms and amplified by an admittance  $1/(R_v + L_v s)$  to generate the  $dq$  current references.

Besides the inner current control, the virtual admittance control that generates current orders, the control structure in Fig. 2 also includes the P-f droop. The P-f droop control, also

referred to as power synchronization, outputs the frequency and the synchronizing angle ( $\theta_{PS}$ ). The P-f relationship is defined in (5).

$$\omega_{pu} = 1 + m(P_{\text{ref}} - P) \quad (5)$$

Current limiters are strategically integrated into the GFM control scheme to mitigate the risk of excessive output from the PI controllers. These controllers can become susceptible to saturation under prolonged high-error conditions or excessive accumulation of integral error, e.g., during fault scenarios when voltages experience large dips.

$$\sqrt{i_{d,\text{ref}}^2 + i_{q,\text{ref}}^2} \leq I_{\text{Sat}} \quad (6)$$

The current saturation algorithm (CSA) is located in between the voltage loop and current loop. The block follows (6) and limits the values of  $i_{dq,\text{ref}}$  coming out from the outer loop control are limited to 2 pu. The discussion in this paper is limited to scenarios in which the IBR current does not reach saturation.

The GFM's control parameters are shown in Table II.

TABLE II: Control Parameters

| Description            | Item             | Value             |
|------------------------|------------------|-------------------|
| GFM Virtual Resistance | $R_v$            | 0.03 pu           |
| GFM Virtual Inductance | $X_v$            | 0.2 pu            |
| GFM Current Controller | $K_p, K_i$       | 0.4758, 3.2655 pu |
|                        | Bandwidth        | 134 Hz            |
| P-f Droop              | $m$              | 5%                |
| Current Limiter        | $I_{\text{Sat}}$ | 2 pu              |

#### A. Sequence impedance

It is easy to comprehend that based on the design philosophy, in the positive-sequence circuit, the IBR viewed from the PCC bus voltage appears as a constant voltage source behind an impedance. And this positive-sequence impedance is the same as the virtual impedance:  $Z_1 = R_v + jX_v$ .

It is also straightforward to see that, in the zero-sequence circuit, the IBR viewed from the PCC bus behaves simply as the choke-filter impedance. This characteristic arises because the converter voltage is generated by a  $dq$ -frame control. As a result, the synthesized  $abc$  voltage set does not contain any zero-sequence component. Therefore, in the zero-sequence network, the IBR effectively reduces to the choke filter. In this research, ground faults were applied on the 600 V side of the transformer. The measured zero-sequence impedance matches exactly the impedance of the choke filter.

Lastly, in the negative-sequence circuit, it is expected that the IBR impedance is also influenced by the virtual impedance. However, deriving an analytical expression requires more detailed analysis, along with EMT simulation studies for verification.

### III. NEGATIVE-SEQUENCE IMPEDANCE BASED ON EMT SIMULATION RESULTS

A key focus of this investigation is to characterize the negative-sequence impedance of the IBR by measuring the

negative-sequence current output from IBRs and the negative-sequence voltage component at the PCC bus. Simulations are conducted specifically for unsymmetrical fault types (e.g., single-line-to-ground, double-line, or double-line-to-ground). These faults are known to introduce negative-sequence voltage and current components.

On the other hand, it is critical to place the fault at the grid side, instead of the 33-kV POI bus. When an unbalanced fault, e.g., single-line-to-ground (SLG) fault, is placed at the POI bus, the effect of unbalance is almost indiscernible.

A comparison has been conducted to show the effect of fault location. Fig. 3(a) and Fig. 3(b) show the simulation results for the fault location at the middle of the 230-kV line and the 33-kV POI bus.

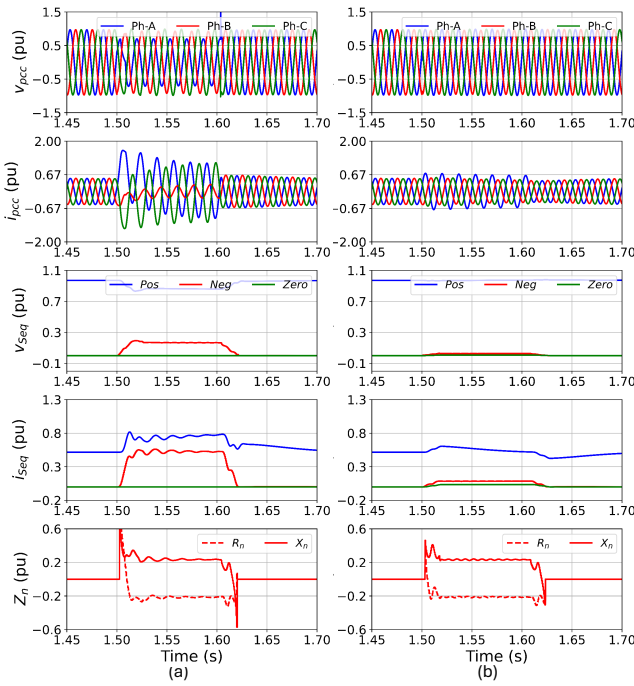


Fig. 3: Simulation results for the A-G Fault at different fault location. (a) Fault at the grid's 230-kV transmission line. (b) Fault at the 33-kV POI bus. For this scenario, the virtual impedance ( $R_v = 0.03$  and  $X_v = 0.2$  pu) and droop ( $m = 5\%$ ) are kept constant.

The simulation results show the instantaneous voltage measurements at the PCC bus (subplot 1), the IBR's exporting current (subplot 2), the magnitudes of the sequence components of the two measurements (subplots 3 and 4), and the negative-sequence impedance (subplot 5).

It can be seen that indeed the SLG fault at the POI bus introduces indiscernible unbalance in the measured PCC bus voltage. The underlying reason is that the fault is located in between the  $Y_g - \Delta$  and  $\Delta - Y_g$  transformer. Therefore, the zero-sequence circuit is open-circuited. This leads to a negligible fault current. Therefore, the measured PCC bus voltage appears nearly balanced.

In the case studies, the fault is placed in the middle of the 230-kV transmission line.

#### A. Case 1: Different Fault Scenarios

In case study 1, three types of faults (double-line, double-line-to-ground, and SLG) have been tested and the simulation results are shown in Fig. 4.

The measured negative-sequence impedances are shown for the three fault scenarios. Irrespective of fault conditions, the negative-sequence's impedance components ( $R_n$  and  $X_n$ ) remain nearly constant:  $R_n = -0.2$  and  $X_n = 0.2$ . For all fault scenarios, the virtual impedances ( $R_v = 0.03$  and  $X_v = 0.2$  pu) and the P-f droop ( $m = 5\%$ ) are kept constant.

#### B. Case 2: Different Droop Parameters

In case study 1, the P-f droop parameter is set to be 5%. In case study 2, the impact of the droop parameter is examined. The simulation results are shown in Fig. 5. For each droop scenarios, the virtual impedances ( $R_v = 0.03$  and  $X_v = 0.2$  pu) are kept constant and the fault applies is an A-G fault. Fig. 5 compares two droop settings: 1% and 10%. The negative sequence impedance components remain the same as  $-0.2 + j0.2$ .

#### C. Case 3: Different Virtual Impedances

Since variations in droop or fault types did not significantly affect the measured negative-sequence impedance, the next case study involves adjusting the virtual impedance values,  $R_v$  and  $X_v$ . Two settings of  $R_v + jX_v$  have been examined:  $0.03 + j0.2$  and  $0.2 + j0.2$ . The resulting negative-sequence impedance has two complex numbers:  $-0.217 + j0.235$  versus  $-0.048 + j0.441$  pu.

Fig. 6(a) illustrates the A-G fault response when  $R_v = 0.03$  and  $X_v = 0.2$ , while Fig. 6(b) depicts the A-G response when  $R_v = 0.2$  and  $X_v = 0.2$ . The f-P droop is held constant at  $m = 5\%$  throughout these tests.

The gain between the measured negative-sequence impedance and the virtual impedance, defined as

$$\text{Gain} = \frac{R_n + jX_n}{R_v + jX_v},$$

has been computed. Its average value is  $1.11 \times \sqrt{2} \angle 51^\circ$ .

The detailed values of negative-sequence impedance values for three droop parameters are shown in Table III.

TABLE III: Negative-sequence impedance vs. the virtual impedance

| <b>m=1%</b>   |                   |                                            |
|---------------|-------------------|--------------------------------------------|
| $R_v + jX_v$  | $R_n + jX_n$      | Gain                                       |
| $0.03 + j0.2$ | $-0.215 + j0.236$ | $1.162 \times \sqrt{2} \angle 50.86^\circ$ |
| $0.2 + j0.2$  | $-0.047 + j0.439$ | $1.104 \times \sqrt{2} \angle 51.11^\circ$ |
| <b>m=5%</b>   |                   |                                            |
| $R_v + jX_v$  | $R_n + jX_n$      | Gain                                       |
| $0.03 + j0.2$ | $-0.217 + j0.235$ | $1.118 \times \sqrt{2} \angle 51.25^\circ$ |
| $0.2 + j0.2$  | $-0.048 + j0.441$ | $1.11 \times \sqrt{2} \angle 51.21^\circ$  |
| <b>m=10%</b>  |                   |                                            |
| $R_v + jX_v$  | $R_n + jX_n$      | Gain                                       |
| $0.03 + j0.2$ | $-0.216 + j0.231$ | $1.106 \times \sqrt{2} \angle 51.61^\circ$ |
| $0.2 + j0.2$  | $-0.048 + j0.441$ | $1.11 \times \sqrt{2} \angle 51.21^\circ$  |

Table IV further shows four sets of virtual impedance and the resulting negative-sequence impedance for three types of faults.

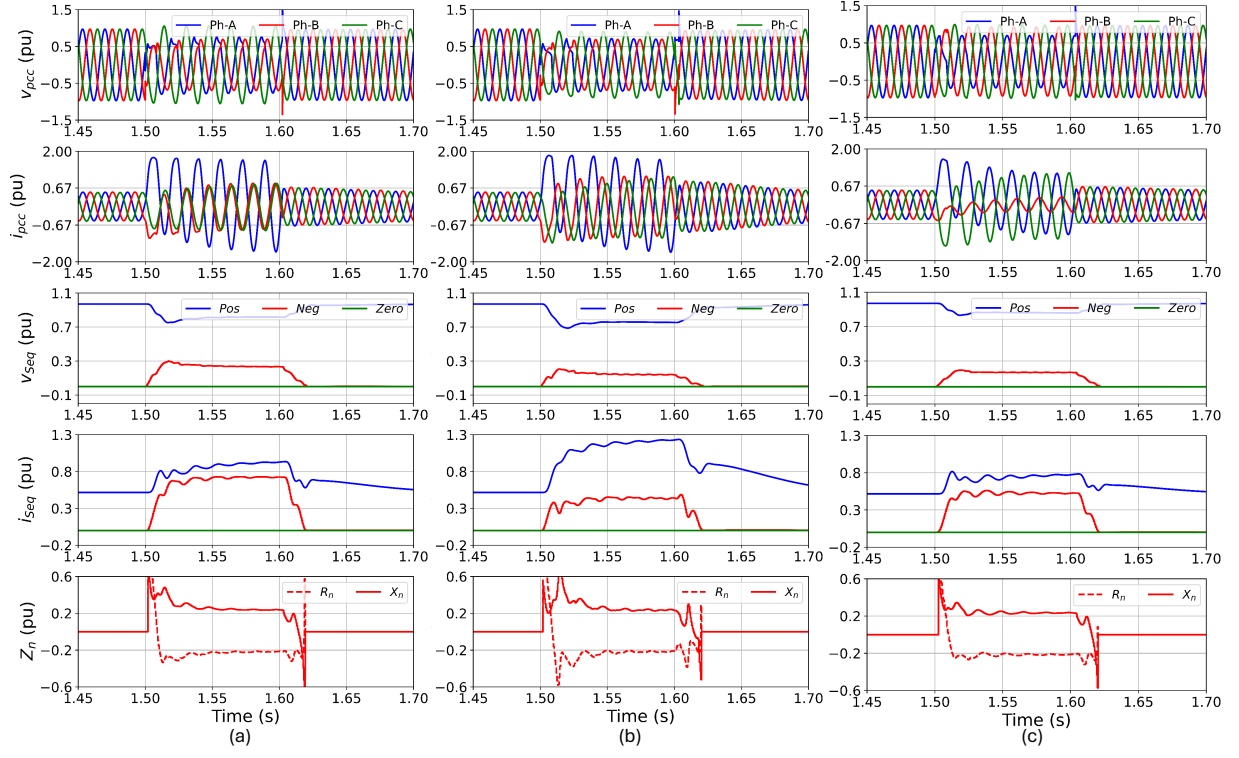


Fig. 4: Simulation results for three types of faults. (a) AB (b) AB-G (c) A-G. For each fault scenarios the virtual impedance ( $R_v = 0.03$  and  $X_v = 0.2$  pu) and droop ( $m=5\%$ ) are kept constant.

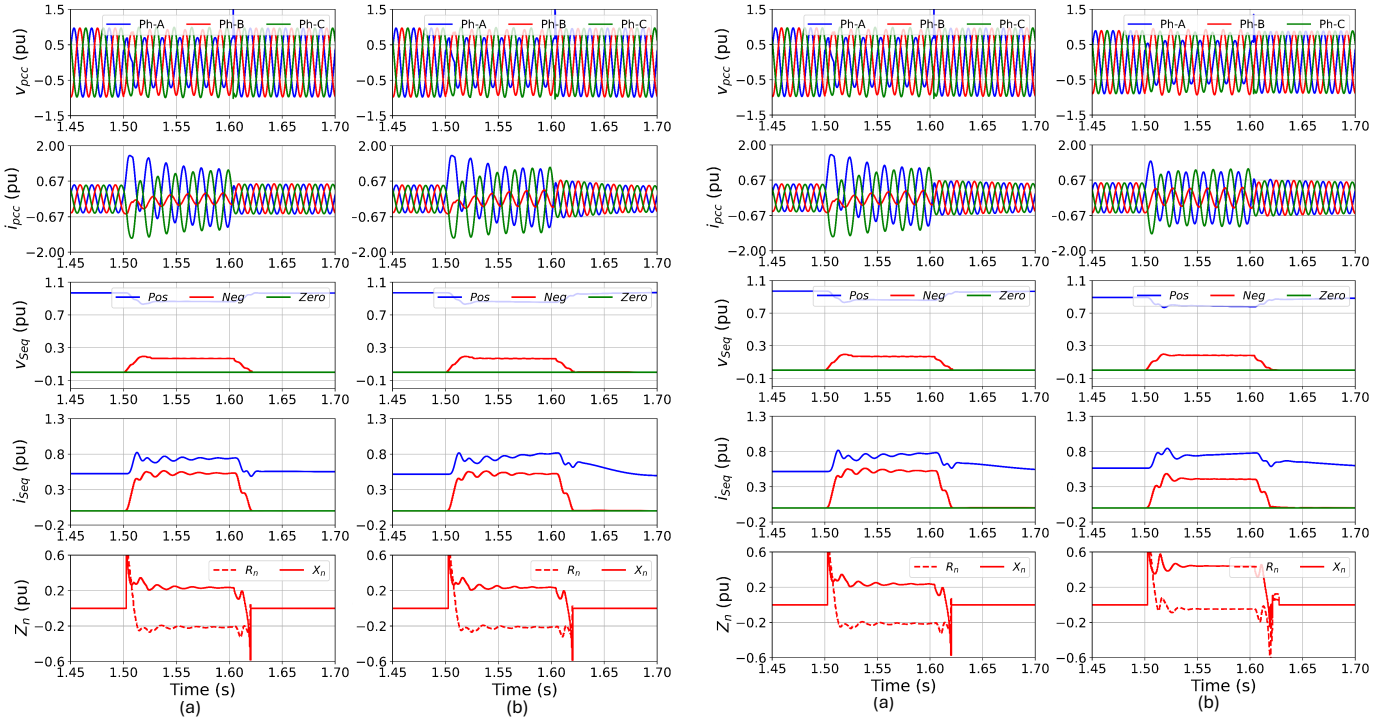


Fig. 5: Simulation results of the A-G Fault for different droop responses. (a)  $m = 1\%$  (b)  $m = 10\%$ . For this scenario, the virtual impedance is kept constant ( $R_v = 0.03$  and  $X_v = 0.2$  pu).

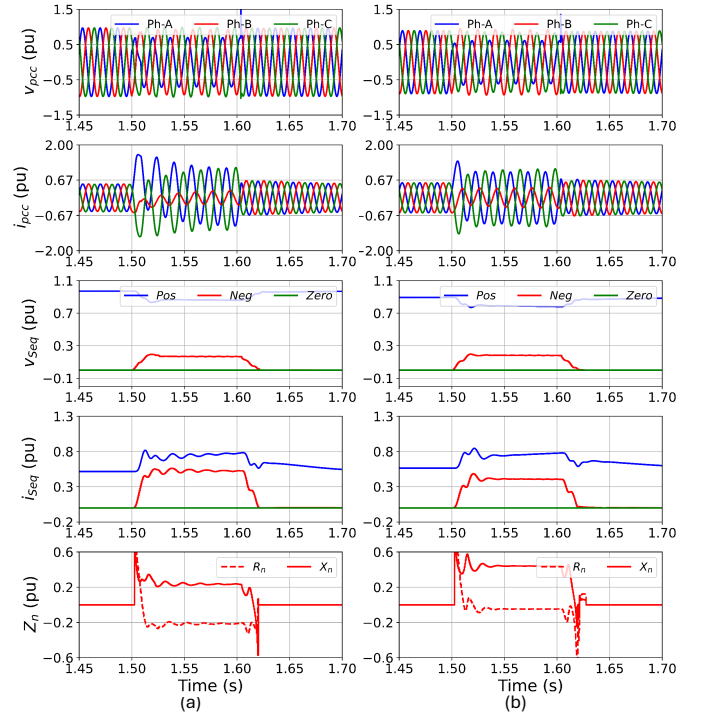


Fig. 6: Simulation results for an A-G fault for different virtual impedances. For this scenario, the droop ( $m=5\%$ ) is kept constant. (a)  $R_v = 0.03$ ,  $X_v = 0.2$  (b)  $R_v = 0.2$ ,  $X_v = 0.2$ .

#### D. Case 4: Impact of virtual admittance control

Finally, an additional case study has been conducted to examine the impact of the virtual admittance control in the GFM

on the measured negative-sequence impedance. Two scenarios are compared: with and without the virtual admittance control. When the virtual admittance control is disabled, the GFM has

TABLE IV: Negative-sequence impedance for different virtual impedance

| <b>AB Fault</b>  |                   |                                            |
|------------------|-------------------|--------------------------------------------|
| $R_v + jX_v$     | $R_n + jX_n$      | Gain                                       |
| $0.03 + j0.2$    | $-0.215 + j0.239$ | $1.124 \times \sqrt{2} \angle 50.51^\circ$ |
| $0.2 + j0.2$     | $-0.048 + j0.441$ | $1.11 \times \sqrt{2} \angle 51.21^\circ$  |
| $0.075 + j0.5$   | $-0.538 + j0.592$ | $1.118 \times \sqrt{2} \angle 50.79^\circ$ |
| $0.5 + j0.5$     | $-0.113 + j1.106$ | $1.112 \times \sqrt{2} \angle 50.83^\circ$ |
| <b>ABG Fault</b> |                   |                                            |
| $R_v + jX_v$     | $R_n + jX_n$      | Gain                                       |
| $0.03 + j0.2$    | $-0.218 + j0.241$ | $1.136 \times \sqrt{2} \angle 50.66^\circ$ |
| $0.2 + j0.2$     | $-0.051 + j0.442$ | $1.112 \times \sqrt{2} \angle 51.58^\circ$ |
| $0.075 + j0.5$   | $-0.592 + j0.6$   | $1.178 \times \sqrt{2} \angle 52.72^\circ$ |
| $0.5 + j0.5$     | $-0.12 + j1.136$  | $1.14 \times \sqrt{2} \angle 51.03^\circ$  |
| <b>AG Fault</b>  |                   |                                            |
| $R_v + jX_v$     | $R_n + jX_n$      | Gain                                       |
| $0.03 + j0.2$    | $-0.217 + j0.235$ | $1.118 \times \sqrt{2} \angle 51.25^\circ$ |
| $0.2 + j0.2$     | $-0.048 + j0.441$ | $1.11 \times \sqrt{2} \angle 51.21^\circ$  |
| $0.075 + j0.5$   | $-0.529 + j0.591$ | $1.109 \times \sqrt{2} \angle 50.36^\circ$ |
| $0.5 + j0.5$     | $-0.121 + j1.094$ | $1.101 \times \sqrt{2} \angle 51.31^\circ$ |

only the inner current control left which forces its  $dq$  currents to track their orders.

When the current references are held constant and subjected to an unbalanced fault, the resulting negative-sequence current contribution is negligible, as illustrated in Fig. 7. In contrast, when both the virtual admittance control and the inner current control are active, more pronounced negative-sequence current is contributed by the GFM, as shown in Fig. 3(a). This difference in controller configuration significantly affects the measured negative-sequence impedance.

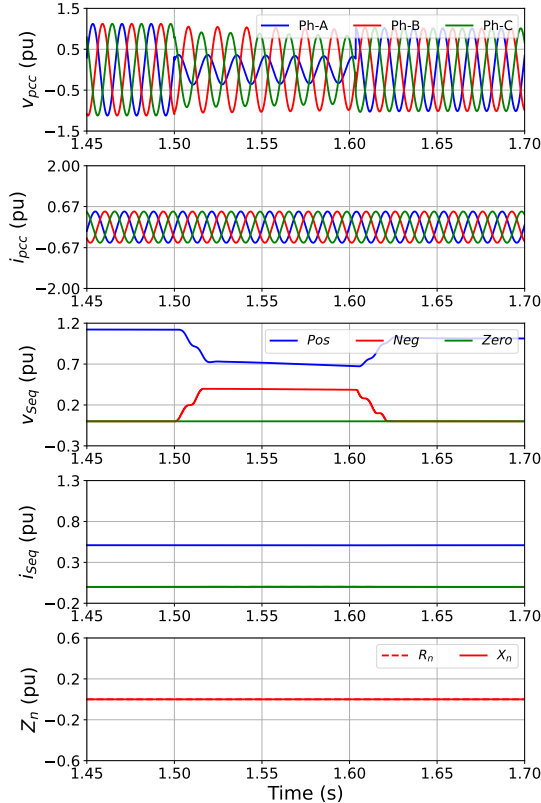


Fig. 7: Simulation results for an A-G Fault when the GFM has the inner current controller only. The outer loop is disabled.

*Remarks:* The above four sets of case studies show that for the virtual admittance GFM, its negative-sequence impedance is related to its virtual impedance setting, more specifically,  $R_v$  and  $X_v$  used in its  $dq$ -frame control. They are related by a constant complex gain.

#### IV. ANALYSIS

This section provides an analysis to explain why the measured negative-sequence impedance  $Z_n$  is related to the virtual impedance  $R_v + jX_v$  of the positive sequence by a constant complex gain. The analysis approach is to examine the IBR's voltage and current relationship first in the  $dq$  frame based on control design and then examine this relationship in the static frame. Furthermore, the specific relationship under the negative-sequence condition is derived.

Based on the control scheme, the  $dq$  frame current reference is a function of the reference voltage,  $dq$ -frame voltages and the virtual impedance components as shown in (7).

$$\bar{I}^{\text{ref}} = \frac{1}{R_v + sL_v} (\bar{V}^{\text{ref}} - \bar{V}_{\text{PCC}} - jX_v \bar{I}^{\text{ref}}), \quad (7)$$

where  $\bar{I}^{\text{ref}} = i_d^{\text{ref}} + ji_q^{\text{ref}}$ ,  $\bar{V}^{\text{ref}} = v_d^{\text{ref}} + jv_q^{\text{ref}}$ , and  $\bar{V}_{\text{PCC}} = v_d + jv_q$ . Rearranging the variables and expressing the current reference in terms of voltage and impedance function, the new equation becomes (8).

$$\bar{V}^{\text{ref}} - \bar{V}_{\text{PCC}} = (R_v + sL_v + j\omega L_v) \bar{I}^{\text{ref}} \quad (8)$$

The small signal relationship is as follows:

$$-\Delta \bar{V}_{\text{PCC}} = (R_v + (s + j\omega)L_v) \Delta \bar{I}^{\text{ref}} \quad (9)$$

The inverter's inner current control ensures that the  $dq$  current measurements can track the orders with a specified bandwidth. Therefore, the closed-loop transfer function from the order to the measurement can be approximated as a low-pass filter (LPF):

$$i_d = G_{\text{LPF}}(s) i_d^{\text{ref}}, \quad i_q = G_{\text{LPF}}(s) i_q^{\text{ref}}. \quad (10)$$

In the complex vector format:

$$\bar{I} = G_{\text{LPF}}(s) \bar{I}^{\text{ref}}. \quad (11)$$

Therefore, (9) becomes:

$$-\Delta \bar{V}_{\text{PCC}} = (R_v + (s + j\omega)L_v) \frac{1}{G_{\text{LPF}}(s)} \Delta \bar{I} \quad (12)$$

When the voltage and current are viewed from the static frame, the transfer function that represents their relationship should have  $s$  been replaced by  $s - j\omega$ . Therefore, in the static frame, (12) becomes (13):

$$-\Delta \vec{v}_{\text{PCC}} = \underbrace{(R_v + sL_v)}_{Z(s)} \frac{1}{G_{\text{LPF}}(s - j\omega)} \Delta \vec{i} \quad (13)$$

It can be seen that the impedance of the inverter viewed in the static frame can be expressed as  $Z(s)$ , where

$$Z(s) = \frac{R_v + sL_v}{G_{\text{LPF}}(s - j\omega)}.$$

At the positive-sequence conditions, the voltage and current space vectors can be expressed in the form of

$$\vec{v}_{\text{PCC}} = \bar{V}_1 e^{j\omega t} \text{ and } \vec{i} = \bar{I}_1 e^{j\omega t}.$$

Since according to (13):

$$-\Delta(\bar{V}_1 e^{j\omega t}) = Z(s) \Delta(\bar{I}_1 e^{j\omega t}), \quad (14)$$

the relationship between  $\bar{V}_1$  and  $\bar{I}_1$  becomes:

$$-\Delta\bar{V}_1 = Z(s + j\omega)\Delta\bar{I}_1, \quad (15)$$

At steady state, the positive-sequence impedance is  $Z_1$  and

$$Z_1 = \frac{R_v + j\omega L_v}{G_{\text{LPF}}(j0)} = R_v + jX_v,$$

since the LPF's steady-state gain  $G_{\text{LPF}}(j0)$  is 1.

Under the negative-sequence conditions, the voltage and current space vectors can be expressed in the form of

$$\vec{v}_{\text{PCC}} = \bar{V}_2^* e^{-j\omega t}$$

and

$$\vec{i} = \bar{I}_2^* e^{-j\omega t}.$$

These two space vectors are related by  $Z(-j\omega)$  (with  $s$  replaced by  $-j\omega$ ) at steady state:

$$-\Delta\bar{V}_2^* = \underbrace{(R_v - j\omega L_v)}_{Z(-j\omega)} \frac{1}{G_{\text{LPF}}(-j2\omega)} \Delta\bar{I}_2^* \quad (16)$$

The negative-sequence impedance relates the two negative-sequence complex vectors:  $\bar{V}_2$  and  $\bar{I}_2$ :

$$\begin{aligned} Z_2 &= \frac{-\Delta\bar{V}_2}{\Delta\bar{I}_2} = Z^*(-j\omega) = \left( \frac{R_v - j\omega L_v}{G_{\text{LPF}}(-j2\omega)} \right)^* \\ &= \underbrace{\frac{1}{G_{\text{LPF}}(j2\omega)}}_{\text{Gain}} (R_v + jX_v) \end{aligned} \quad (17)$$

*Remarks:*

- It can be seen that the negative-sequence impedance for a virtual admittance-based GFM is closely associated with the specified virtual impedance. The positive-sequence impedance is the same as the specified virtual impedance evaluated at the nominal frequency, while the negative-sequence impedance is the nominal-frequency virtual impedance amplified by a complex gain.
- It can be seen that if the inner  $dq$  current control is designed to be sufficiently fast, its closed-loop transfer function representing current-tracking performance will have a complex gain with a magnitude of 1 and a phase shift close to 0 degrees. In this case, the negative-sequence impedance is the same as the positive-sequence impedance, *i.e.*,  $Z_2 = Z_1 = R_v + jX_v$ . We can set the sequence impedance by choosing  $R_v$  and  $X_v$ . For example, the pair  $R_v = 0.03$  and  $X_v = 0.2$  comply with the grid code.

#### A. Current tracking system as a low-pass filter

The IBR inner current control design is based on the choke filter's dynamics, which relate the converter exporting current  $\vec{i}$ , the converter voltage  $\vec{v}_t$ , and the PCC bus voltage  $\vec{v}_{\text{PCC}}$  as the follows:

$$\vec{v}_t - \vec{v}_{\text{PCC}} = (R + Ls) \vec{i}, \quad (18)$$

where  $R$  and  $L$  are the choke filter's resistance and inductance.

In a  $dq$  frame with a rotating speed of  $\omega$ , the above relationship becomes the following:

$$v_{td} + jv_{tq} - (v_d + jv_q) = (R + L(s + j\omega))(i_d + ji_q), \quad (19)$$

$$(R + Ls)i_d = \underbrace{v_{td} - v_d + \omega Li_q}_{u_d} \quad (20)$$

$$(R + Ls)i_q = \underbrace{v_{tq} - v_q - \omega Li_d}_{u_q}. \quad (21)$$

The inner current control aims to adjust  $u_d$  (or  $u_q$ ) using the feedback signal of  $i_d$  (or  $i_q$ ). The error between the order and the measurement is fed to a PI controller to generate the output. This output is  $u_d$  (or  $u_q$ ). To arrive at the converter voltage signals  $v_{td}$  (or  $v_{tq}$ ) that can be used to change the converter voltage, the output should be compensated by a feedforward unit of the PCC bus voltage  $v_d$  (or  $v_q$ ) and a cross-coupling unit  $-\omega Li_q$  (or  $\omega Li_d$ ), as shown in Fig. 2.

It can be seen that the current order and the current measurement have the following relationship:

$$\left( K_p + \frac{K_i}{s} \right) (i_d^* - i_d) = u_d = (R + Ls)i_d. \quad (22)$$

$$\left( K_p + \frac{K_i}{s} \right) (i_q^* - i_q) = u_q = (R + Ls)i_q. \quad (23)$$

Therefore, the closed-loop system of the measurement tracking the order is expressed as follow:

$$\frac{\Delta i_d}{\Delta i_d^*} = \frac{\Delta i_q}{\Delta i_q^*} = \frac{\left( K_p + \frac{K_i}{s} \right) \frac{1}{R+Ls}}{1 + \left( K_p + \frac{K_i}{s} \right) \frac{1}{R+Ls}} \quad (24)$$

In the example testbed,  $R = 0.03$  while  $L = 0.2/\omega$ ,  $K_p = 0.4758$  and  $K_i = 3.2655$ . The resulting closed-loop transfer function is:

$$\begin{aligned} \frac{\Delta i_d}{\Delta i_d^*} &= \frac{K_p s + K_i}{Ls^2 + (K_p + R)s + K_i} \\ &= \frac{0.4758s + 3.265}{0.0005305s^2 + 0.5058s + 3.265}, \end{aligned}$$

which behaves as a LPF. Its bandwidth is 134 Hz. This system can be approximated by a 1st order LPF:

$$\frac{1}{\tau s + 1},$$

where  $\tau = \frac{1}{2\pi 134}$  s. Fig. 8 shows the Bode diagrams of the second-order transfer function and the first-order transfer function. It can be seen that the first-order transfer function  $\frac{1}{\tau s + 1}$  (where  $1/\tau$  is the bandwidth) can well represent the current tracking system.

In the control implementation stage, the  $dq$  frame is based on the synchronizing angle. The LPF approximation is valid

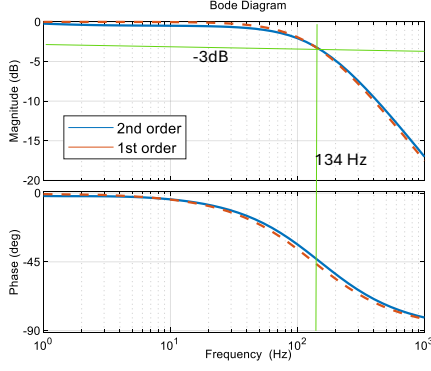


Fig. 8: Comparison of the 2nd order transfer function vs. the 1st order transfer function.

if the  $dq$  frame has a rotating speed close to nominal and the effect of the synchronizing angle dynamics can be ignored. This operation assumption is valid for the very fast period of a few cycles of the fault.

We further generate EMT simulation results to examine the current tracking performance. Fig. 9 shows the time-domain responses of the  $dq$  current measurements and their orders. From 1.50 s to 1.60 s, an unbalanced fault is applied. It can be seen that at the normal operating conditions, the measurements track their references very well. During the fault, the  $dq$  current measurements and orders all experience 120-Hz ripples. In general, the measurements lag their references with reduced magnitudes.

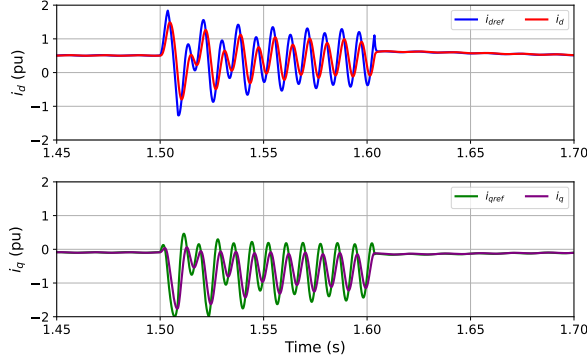


Fig. 9: Current responses during an unbalanced fault.

If the bandwidth of the  $dq$ -frame current control is designed to be around 120 Hz,  $G_{LPF}$  has a complex gain of  $0.707\angle-45^\circ$  at 120 Hz. In turn, the negative-sequence impedance's magnitude will be about  $\sqrt{2}$  times of the virtual impedance magnitude, while its angle is greater than the virtual impedance by  $45^\circ$ , or

$$\text{Gain} = \sqrt{2}\angle 45^\circ.$$

On the other hand, a higher bandwidth leads to more comparable positive- and negative-sequence impedance.

Simulation experiments have been carried out to examine the relationship between the current references and the current measurements. The outer control has been disabled so that only inner current control exists (along with the power syn-

chronization). A step change is applied to a current reference. The resulting current measurement is recorded. With the reference treated as an input and the current measurement as an output, their transfer function is estimated using MATLAB System Identification Toolbox transfer function estimation `tfest`. Fig. 10 shows the two step change experiment results, as well as the Bode diagram of the estimated transfer function.

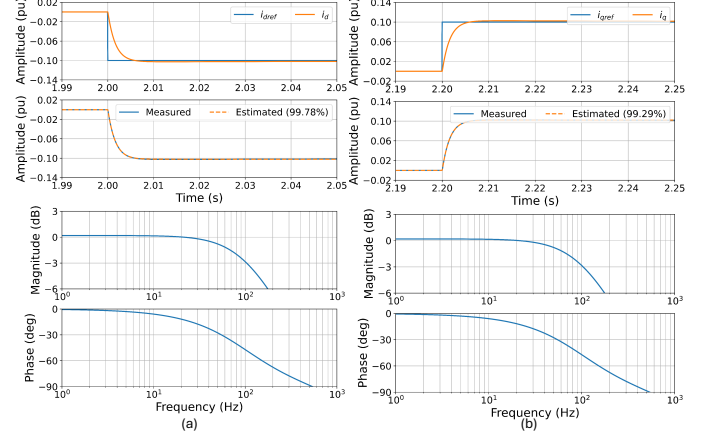


Fig. 10: Step response to show the  $G_{LPF}$  between  $i_{dq}$  and  $i_{dqref}$ . (a)  $i_{dref}$  to  $i_d$  (b)  $i_{qref}$  to  $i_q$

The transfer functions are found as:

$$G_{LPF} = \frac{11.74 \times 10^6}{s^2 + 18.9 \times 10^3 s + 11.54 \times 10^6}, \quad (25)$$

At 120 Hz, the low-pass filter exhibits a magnitude of approximately  $0.707\angle-53^\circ$ . The bandwidth of the current tracking system is 120 Hz. It can be seen that the current tracking system can be approximated as  $\frac{1}{\tau s + 1}$ , where  $1/\tau$  is the bandwidth or  $2\pi \times 120$  rad/s.

The analytical model of the current tracking shown in (24) has a bandwidth of 134 Hz, while the learned model in (25) has a bandwidth of 120 Hz. This is because (25) describes a system identified from step response data and the data were created using the EMT testbed, where the IBR is operating with the synchronizing unit in place in a relative weak grid. In the current control design stage, the dynamics associated with establishing the  $dq$  reference frame have not been considered. This is also consistent with the assumption used in the analytical model, where the  $dq$ -frame dynamics are neglected. In operation, these elements lead to a slightly slower current tracking response. Hence, (25) shows a bandwidth less than the bandwidth of 134 Hz.

This experiment results confirm that the current tracking transfer function has a complex gain of  $0.707\angle-53^\circ$ . In turn, based on the analysis result in (17), the negative-sequence impedance  $Z_2$  relates the virtual impedance  $Z_v = R_v + jX_v$  as follows:  $Z_2 = \sqrt{2}\angle 53^\circ Z_v$ . This analysis result corroborates the findings of the complex gain in Table IV obtained by simulation experiments.

### B. Grid code compliance

The key engineering finding is that the virtual-admittance GFM converter's negative-sequence impedance is primarily

determined by the virtual impedance setting and the inner current control bandwidth. In particular, we have derived an analytic expression for the negative-sequence impedance  $Z_2$ :

$$Z_2 = \frac{1}{G_{\text{LFP}}(j2\omega)}(R_v + jX_v),$$

where  $G_{\text{LFP}}(s) \approx \frac{1}{\tau s + 1}$ .

If the design specification is for the current tracking achieving 120 Hz bandwidth, then  $\tau = \frac{1}{2\omega}$  where  $\omega = 2\pi \times 60$  rad/s. So when  $G_{\text{LFP}}(s)$  is evaluated for  $s = j2\omega$ ,

$$G_{\text{LFP}}(j2\omega) = \frac{1}{1 + j1} = \frac{1}{\sqrt{2}} \angle -45^\circ.$$

Therefore,  $Z_2 = \sqrt{2}/45^\circ(R_v + jX_v)$ . If  $R_v \approx 0$ ,  $Z_2$  will have an angle of  $135^\circ$ , implicating a negative resistance or  $R_n < 0$ . This can be fixed by two methods: tuning the virtual impedance or tuning the inner current control.

- Method 1 ensures that the net angle of  $Z_2$  is 90 degrees by setting  $R_v$  and  $X_v$  appropriately. For example, for the above case of current-tracking bandwidth at 120 Hz,  $R_v$  may be set to be equal to  $X_v$ . So that the angle of  $Z_2$  is 90 degrees.
- Method 2 ensures that the inner current control is sufficiently fast. A higher current control bandwidth can make  $G_{\text{LFP}}(j2\omega) \approx 1$  and  $Z_2 = R_v + jX_v$ .

Comparison of  $R_v$  at 0.03 vs 0.2 pu has been shown in Fig. 6 (where  $X_v$  is kept at 0.2 pu and the current control bandwidth is close to 120 Hz). It can be seen that if  $R_v$  is at 0.03,  $R_n$  is  $-0.2$  pu and  $Z_2 = -0.2 + j0.2 = \sqrt{2} \times 0.2/135^\circ$ . When  $R_v$  is changed to 0.2 pu,  $R_n$  becomes 0 and  $Z_2 = j0.4 = 0.4/90^\circ$ .

Comparison of current tracking bandwidth is shown in Fig. 11. Two inner current control settings are compared: 130 Hz bandwidth (PI control:  $0.47 + 2.36/s$ ) versus 290 Hz bandwidth (PI control:  $1 + 5/s$ ). It can be seen for the same  $R_v + jX_v = 0.03 + j0.2$ , the slower current control leads to  $Z_2 \approx -0.2 + j0.2$ , while the faster current control leads to  $Z_2 \approx 0 + j0.2$ .

In summary, a virtual-admittance GFM-IBR can be set to provide the desired negative-sequence impedance and comply with the grid codes. To have  $Z_1 = Z_2 = R_v + jX_v$ , the inner current control should be sufficiently fast (e.g., the bandwidth is greater than 240 Hz).

## V. APPLICATION CASE: A HYBRID POWER PLANT

It is clear that the GFM can provide a negative-sequence current with a desired phase angle. On the other hand, legacy grid-following (GFL) control does not have such capability. In this section, we show that for an IBR power plant consisting of legacy GFL-IBRs, if 50% IBRs are replaced by GFM-IBRs, this hybrid power plant's negative-sequence impedance is expected to be approximately  $2\sqrt{2}/53^\circ Z_v$ .

To verify the negative-sequence impedance provision capability of this GFM, a testbed of an IBR power plant is built. The circuit topology is shown in Fig. 12. The 100-MW power plant consists of two IBRs of the same power size, each 50 MW. Two cases are compared. In the original case, both IBRs

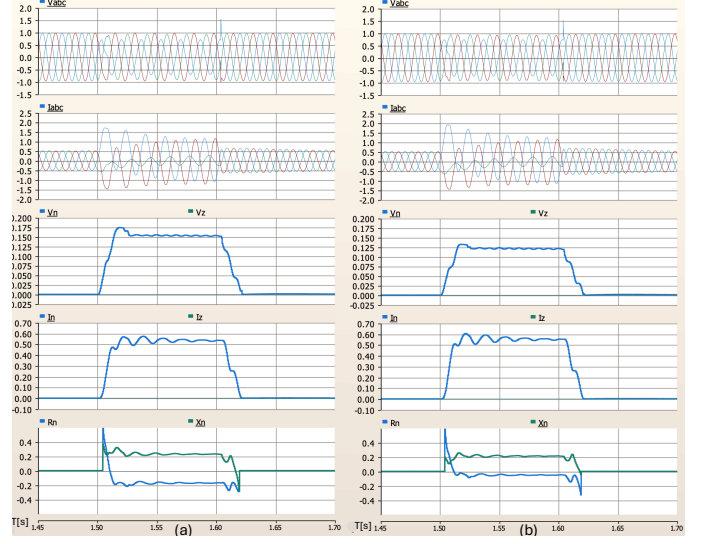


Fig. 11: Simulation results for an A-G fault for different inner current control bandwidth.  $R_v = 0.03$  and  $X_v = 0.2$ . (a) 130 Hz, PI controller:  $0.47 + 2.36/s$ . (b) 290 Hz, PI controller:  $1 + 5/s$

are GFL-IBRs. In the second case, one of the IBRs is replaced by the GFM-IBR.

The GFL control architecture is illustrated in Fig. 14. This control is implemented at the inverter level using the PCC bus voltage and current as inputs, while controls the inverter's terminal voltage. At the power plant level, the point of interconnection (POI) bus' voltage and current are used as measurements.

A frequency-power (f-P) droop controller is employed to determine the active power reference ( $P_{\text{ref}}$ ) for each inverter [?]. The frequency is measured by a PLL using the three-phase POI voltage. The per unit value of the frequency,  $\omega_{\text{POI}}$ , is then compared to a reference value  $\omega_{\text{ref}} = 1$  pu. The error is passed through a droop gain  $1/m$  (where a typical value of  $m$  is 5%) to generate the total power reference. This power reference is equally divided among the two inverters to ensure uniform power sharing.

A voltage-VAR controller is also implemented in the power plant control to regulate the POI voltage ( $|V_{\text{POI}}|$ ) to 1.0 p.u. and to provide the reactive power reference ( $Q_{\text{ref}}$ ) to each inverter. Due to power flow from the inverter PCC bus to the POI, the voltage magnitude at the PCC ( $|V_{\text{PCC}}|$ ) typically exceeds 1.0 p.u. (ranging from 1.02 to 1.05 p.u.), and its phase angle leads that of the POI.

In both frequency and voltage regulation control loops, a communication delay of 0.15 seconds is incorporated at the plant level. 0.15 s is a typical value of communication latency for IBR power plants [?].

At the inverter level, the GFL has a cascaded control architecture [?]. In the outer control, the measured active and reactive powers,  $P$  and  $Q$ , are compared with their respective references,  $P_{\text{ref}}$  and  $Q_{\text{ref}}$ . The resulting errors are processed through proportional-integral (PI) controllers to generate the current references in the  $dq$  frame. The references are then compared with the measured  $dq$  inverter currents, and the error are amplified by a PI controller. The outputs from the inner

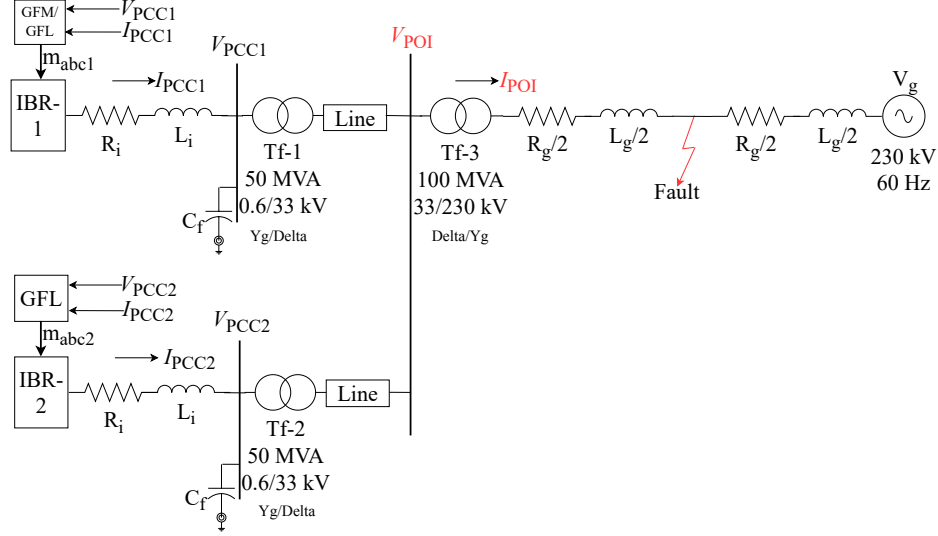


Fig. 12: Hybrid IBR power plant connected to the a grid. The fault location is provided on the grid. The POI measurements are used for fault analysis

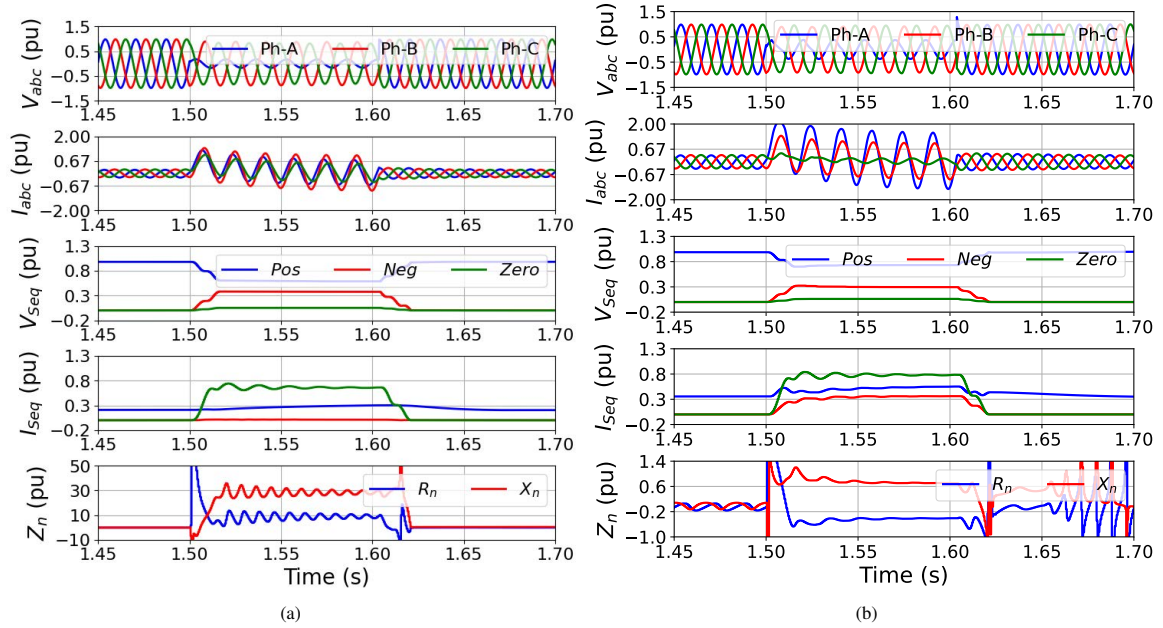


Fig. 13: Simulation results for an A-G fault. (a) Both IBRs are GFL-IBRs. (b) One IBR is replaced by a GFM-IBR.

current PI controllers, along with the cross coupling terms and feedforward PCC bus voltage, form the inverter's terminal voltage in the  $dq$  frame. Through  $dq$  to  $abc$  conversion by use the PLL output angle, the  $abc$ -frame voltage reference signals are generated, which drive the pulse width modulation to further generate switch signals to the inverter's switching gates.

The PLL is a critical component in the GFL control framework. It provides the synchronization angle  $\theta_{PLL}$ , enabling the transformation of three-phase voltage and current signals ( $v_{abc}$ ,  $i_{abc}$ ) into their corresponding  $dq$  components ( $v_d$ ,  $v_q$ ,  $i_d$ ,  $i_q$ ), and the conversion of the inverter's terminal voltage order from  $dq$  to  $abc$ . Under PLL-based synchronization, the  $v_q$  component is actively controlled and maintained at zero, ensuring that the synchronizing angle aligns to the PCC bus

voltage at steady state.

The parameters of the IBR power plant are shown in Table V.

The testbed simulated in PSCAD consist of two identical IBRs, each rated at 50 MW, with operational parameters of 600 V and 60 Hz. The voltage is first stepped up from 600 V to 33 kV via a step-up transformer. This voltage is then transmitted through a 1 km transmission line, connecting the IBRs to the broader transmission network. A second step-up transformer further increases the voltage from 33 kV to 230 kV to facilitate integration with the main grid. On the 230 kV side, the grid is represented by an impedance network comprising the resistance  $R_g$  and reactance  $X_g$ . A fault is introduced on the grid side, situated between the grid impedances. This configuration enables a comprehensive evaluation of the IBRs' contribution

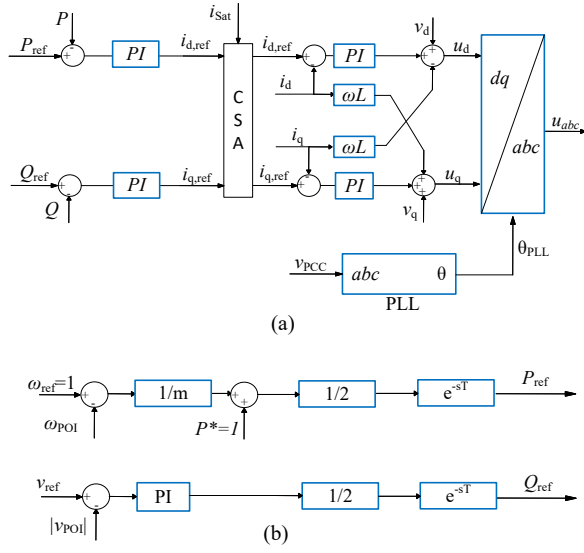


Fig. 14: Grid-following controller and the power plant control. (a) Grid following control; (b) Power plant Controller.

TABLE V: Control Parameters

| Description            | Item                 | Value             |
|------------------------|----------------------|-------------------|
| GFL P Controller       | $K_{Pp}, K_{Ip}$     | 0.25, 25 pu       |
|                        | Bandwidth            | 3 Hz              |
| GFL Q Controller       | $K_{Pq}, K_{Iq}$     | 0.4, 40 pu        |
|                        | Bandwidth            | 5 Hz              |
| GFL Current Controller | $K_{Pi}, K_{Ii}$     | 0.4758, 3.2655 pu |
|                        | Bandwidth            | 134 Hz            |
| Current Limiter        | $I_{Sat}$            | 2 pu              |
| PPC f-P Droop          | $m$                  | 5%                |
| PPC V-Q Controller     | $K_{Pvq}, K_{Ivq}$   | 0.4, 40 pu        |
| Time Delay             | $T$                  | 0.15 s            |
| PLL Controller         | $K_{Ppll}, K_{Ipll}$ | 60, 1400          |
| Bandwidth              | -                    | 13 Hz             |

to the generation of negative-sequence components. Fig. 12 shows the simulated testbed setup.

Fig. 13(a) shows the measured voltage and current at the PCC bus, as well as the sequence components. GFL injection of the negative-sequence currents during fault is very small. Through there is a rise in voltage, but the rise in current is not that significant. Hence the negative sequence impedance is quite large ( $X_n = 30$  pu).

IBR-1 is now set as GFM control. With this change of the controller, there is a significant negative sequence current as shown in Fig. 13(b). The provided negative-sequence impedance is approximately  $2\sqrt{2}Z_v/\angle 53^\circ$ , as expected.

## VI. CONCLUSION

In conclusion, this study demonstrates that the negative-sequence impedance exhibited by a virtual admittance-based GFM converter with only positive-sequence control unit during unbalanced faults is closely related to its specified virtual impedance. Through rigorous EMT simulation and complex vector-based analysis, we show that the negative-sequence impedance are predominantly dictated by the virtual impedance and the current tracking speed. This finding provides a critical design insight, affirming that the fault

current response can be tailored specifically through the virtual impedance implemented in a single  $dq$  frame, thereby enabling more concise inverter control design, while meeting the requirements of grid codes.



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