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Design-Oriented Mitigation of High-Frequency Resonance in DC Microgrids Based on Cable Length and Switching Frequency

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ABSTRACT

A dc microgrid comprises distributed energy sources and loads interconnected through a common dc bus via dc–dc converters. Their inherent switching behavior necessitates input filters to attenuate high-frequency components. However, interactions between cable impedances and converter filters can lead to high-frequency resonance, degrading bus voltage quality and system performance. This paper proposes a design-oriented mitigation approach for high-frequency resonance in dc microgrids. The method is based on a detailed analysis of the interaction between cable feeders and EMI filters, considering converter dynamic behavior and switching frequency effects. A damping strategy based on the optimal placement of an R–C branch is developed to suppress resonance and improve stability while ensuring compliance with voltage ripple limits derived from MIL-STD-704F. A key contribution of this work is establishing a direct relationship between cable length and the grid-forming converter switching frequency, providing practical design guidelines. This reveals that higher switching frequencies reduce the critical cable length required to trigger resonance, imposing more stringent constraints on dc microgrid layouts. The proposed approach is validated through analytical modeling, simulation results, and experimental tests on a 2 kW dc microgrid prototype, demonstrating significant reduction in high-frequency distortion and compliance with power quality limits.

KEYWORDS dc microgrids; EMI filters; high-frequency phenomena; long dc connections; mitigation method.

I. INTRODUCTION

Microgrids are currently used in a wide variety of applications. Figure 1 illustrates some examples, including renewable energy systems such as solar photovoltaic generation [1], wind power [2] and small-scale hydropower [3]; transportation systems comprising electric vehicles [4], aircraft [5] and ships [6]; industry [7], agriculture [8] and residential/commercial buildings [9]; and electrical energy storage applications [10]. Besides the many applications and all commercial benefits that microgrids can bring to the electrical sector, providing remote and isolated communities with access to electricity is one of the main advantages of the so-called microgrids [11]–[13]

In general, dc microgrids are formed by digitally feedback-controlled power electronic converters. All those converters are connected to a common dc bus. Although the control loops of each converter are designed with good stability margins, the interconnection of multiple converters

can cause stability degradation due to interactions [15]–[17]. High-frequency operation of power electronic converters is an important aspect to address, as it may lead to unwanted voltage and current oscillations. Analyzing these high-frequency interactions is crucial, particularly in systems where multiple power converters are interconnected. [18]–[20].

To mitigate the influence of switching-frequency ripple from one converter to another, the most common method is to add a low-pass filter (LPF) at the input terminals of the converters [21], [22]. The presence of an input filter changes the converter dynamics [23], but may also introduce effects in the high frequency region. Electromagnetic Interference (EMI) filters can cause system instabilities due to interactions between the filter and the converter [24].

In [18], [25] and [20], it is shown that interactions between converter filters can increase high-frequency current circulation, which can affect the dc bus voltage. The analysis ex-

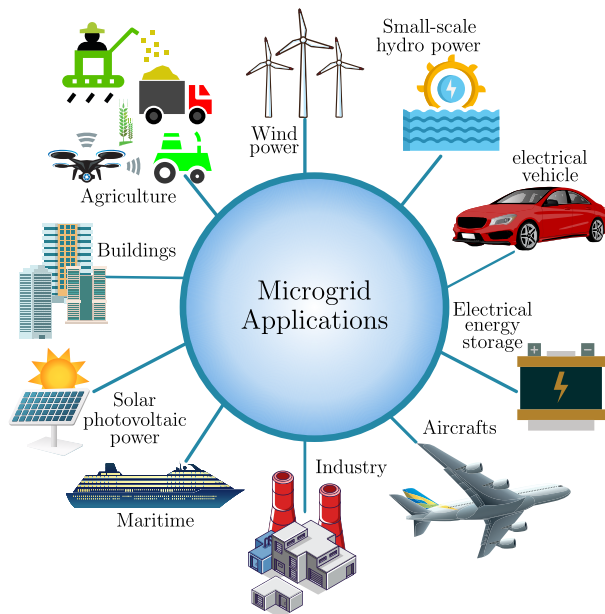


FIGURE 1. Microgrid applications examples. Source: [14]

plores the resonance between the EMI filter capacitance and the cable feeder. When this resonance occurs near the grid-former converter's switching frequency, voltage amplification occurs, leading to a degradation of the dc voltage quality and the introduction of high-frequency noise originated by the grid-former converter. The issue of connecting a power converter to a long cable feeder has become more significant with the rise of smart grids and low-voltage distribution networks, where the distance between the voltage source and the power converter can be substantial. Instability arises from the coupling between the cable and the converter's input capacitance, which is amplified by the small capacitance of the EMI filter at the converter's input terminal [26]–[28].

In dc systems, there are generally no strict regulations regarding the dc bus voltage quality. The IEEE Std 2030.10 [29] provides design, implementation, and operational guidelines for dc microgrids, particularly aimed at rural and remote areas with limited or no access to reliable electricity, focusing on the 48 V level for extra-low-voltage applications. Complementarily, IEC TR 63282, although not a formal standard, offers a technical report of standard voltages and power quality requirements for LVDC systems, supporting their wider adoption and standardization in modern energy applications. While both documents contribute valuable guidance toward the standardization of LVDC systems, MIL-STD-704F [30] stands out as a highly relevant reference due to its strict specifications and applicability to dc networks. This military standard establishes detailed electrical power characteristics, including voltage ripple limits for 270 V aircraft power systems, offering a rigorous benchmark for high-reliability dc applications.

Regarding the dc microgrid power quality issues discussed in [18], [20], [25], and considering MIL-STD-704F [30] as

a stringent reference for dc systems, this paper proposes a mitigation strategy for high-frequency disturbances in dc microgrids. The study focuses on a 2 kW dc microgrid composed of two buck converters interconnected through a long cable feeder, including an EMI filter and a low-pass filter, a configuration prone to high-frequency resonance due to cable–filter interactions. It is worth highlighting that this specific high-frequency resonance phenomenon is an extremely novel issue recently identified and documented in a very limited number of studies, primarily from our own research group, with no alternative mitigation methods currently available in the literature to address this interaction, making this work a pioneering solution.

This paper extends the work previously presented at COBEP 2025 [31] by incorporating simulation results and a more comprehensive analytical framework. In particular, the extended version provides a detailed investigation of the optimal placement of the proposed mitigation method, clarifies the selection and adaptation of the reference dc standard, and demonstrates strong agreement among analytical, simulation, and experimental results. Additionally, design-oriented insights are introduced that link cable length and converter switching frequency to resonance conditions and damping requirements. Further refinements have also been made to the manuscript and the analytical formulations.

This paper is organized as follows. Section II describes the dc microgrid topology, Section III presents the development of the proposed mitigation method design, Section IV shows a theoretical analysis, Section V presents the simulation results, Section VI presents the experimental validation of the mitigation approach, and Section VII concludes the paper. The main contributions of this work can be summarized as follows:

- **Analytical Design Guidelines and Impedance Shaping:** Development of a comprehensive analytical formulation that links damping branch parameters to system characteristics (such as cable inductance and switching frequency), demonstrating how the optimal placement at PCC₂ effectively detunes resonance and reshapes system impedance.
- **Cable-Frequency Interaction Analysis:** Establishment of a direct relationship between the electrical distance (cable length) and the grid-forming converter switching frequency, revealing that higher switching frequencies reduce the critical cable length required to trigger resonance.
- **Practical Design Insights and Robust Validation:** Derivation of practical design criteria for real-world dc microgrids (addressing scenarios such as 88-m cable lengths at 20 kHz) and comprehensive validation combining small-scale stability analysis, simulations, and experimental results on a 2 kW laboratory setup.

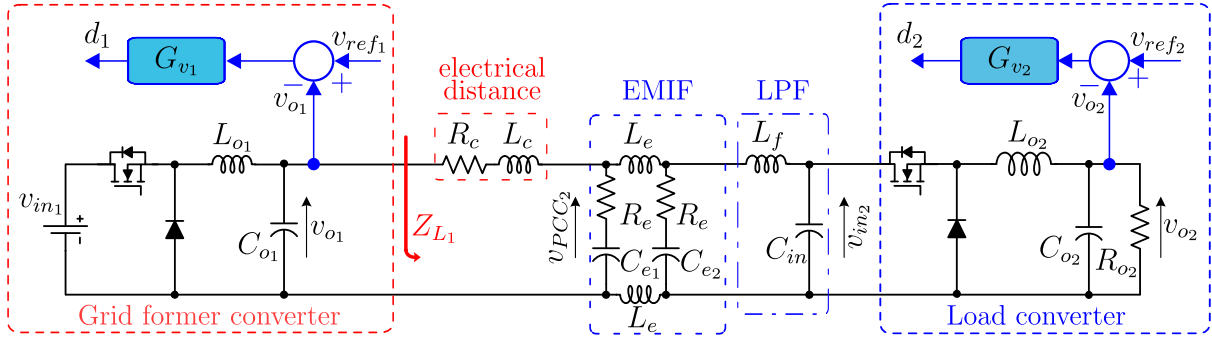


FIGURE 2. DC microgrid with two cascaded buck converters interconnected via a cable, an EMI filter, and a LPF

II. DC MICROGRID TOPOLOGY

The test-case scenario is a dc microgrid composed of two cascade-connected buck converters. The first buck converter can be seen as the dc grid former (GFC), and the second as a load converter. Figure 2 shows the dc microgrid, including a low-pass filter, an EMI filter, and the cable feeder, with the corresponding parameters detailed in Table 1.

TABLE 1. System parameters

Parameter	Value
Grid-Forming Converter (GFC) Parameters	
Source voltage	$V_{in1} = 380 \text{ V}$
GFC output voltage	$V_{o1} = 190 \text{ V}$
GFC output power	$P_{o1} = 4.75 \text{ kW}$
GFC inductance	$L_{o1} = 1.875 \text{ mH}$
GFC capacitance	$C_{o1} = 47 \text{ }\mu\text{F}$
GFC switching frequency	$f_{s1} = 20 \text{ kHz}$
Filter and Cable Parameters	
EMIF inductor	$L_e = 15 \text{ }\mu\text{H}$
EMIF capacitor 1	$C_{e1} = 150 \text{ nF}$
EMIF capacitor 2	$C_{e2} = 0.47 \text{ }\mu\text{F}$
LPF inductor	$L_f = 2 \text{ mH}$
LPF capacitor	$C_{in} = 940 \text{ }\mu\text{F}$
Cable feeder inductance	$L_c = 108.4 \text{ }\mu\text{H}$
Load Converter Parameters	
Load converter input voltage	$V_{in2} = 190 \text{ V}$
Load converter output voltage	$V_{o2} = 95 \text{ V}$
Load converter output power	$P_{o2} = 950 \text{ W}$
Load converter inductance	$L_{o2} = 2 \text{ mH}$
Load converter capacitance	$C_{o2} = 220 \text{ }\mu\text{F}$
Load converter switching frequency	$f_{s2} = 19.9 \text{ kHz}$
Load resistance	$R_{o2} = 14.1 \text{ }\Omega$

The connection cable inductance of $L_c = 108.4 \text{ }\mu\text{H}$ establishes a resonance between the cable feeder and the EMI capacitors, aligning it with the grid former's switching frequency. This specific configuration of the dc microgrid circuit is susceptible to generating resonance due to the interaction between the cable feeder and the front-end capacitors of the load converter.

This same resonance is analyzed in [25] where both converters are operated in open-loop, in [20] the converters

operate in closed-loop, and the focus is an analytical analysis for the dc microgrid under these circumstances, and in [18] the focus is an impedance-based stability analysis for dc microgrids with feedforward controlled connected converters. For the scenario explored in this paper, the resonance is tuned to $\approx 19.4 \text{ kHz}$.

A. Reference DC Standard

The MIL-STD-704F standard [30] specifies voltage ripple limits for aircraft electrical systems operating at 270 V. By adapting these ripple criteria to a 190 V dc microgrid, the maximum allowable voltage ripple can be estimated as 4.2 V (rms), corresponding to a peak-to-peak value of 8.4 V.

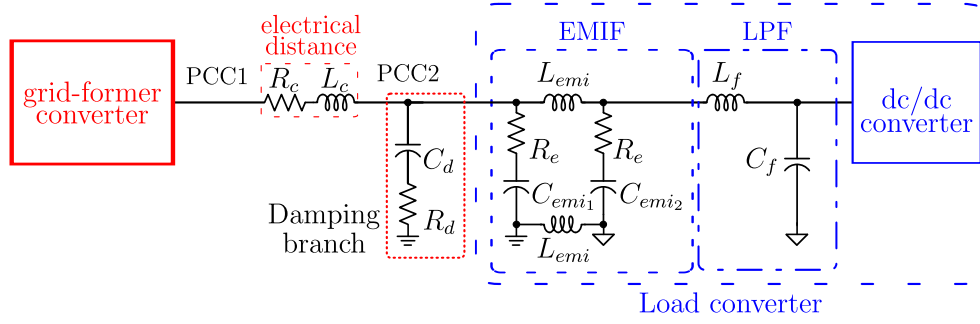
The previously considered ripple criteria do not account for frequency-dependent effects. In contrast, MIL-STD-704F [30] establishes limits on the amplitude of voltage oscillations as a function of frequency. For a 270 V electrical system, the maximum permissible distortion amplitude at 20 kHz is $0.977 V_{\text{rms}}$. Therefore, by adapting the MIL-STD-704F criteria, the maximum permissible distortion amplitude at 20 kHz for the 190 V dc microgrid under study is $0.687 V_{\text{rms}}$.

III. MITIGATION METHOD DESIGN

The proposed solution to address the degradation of the v_{PCC2} voltage, due to the resonance, is to add a $R - C$ damping branch in a manner that does not adversely affect the grid former converter controller. Adding any capacitor to the DC bus can cause instability to the grid-forming converter, since its controller was designed for a specific capacitance. However, when adding the $R - C$ damping branch, this is no longer possible due to the series resistance in the circuit.

The resistor provides stability to the circuit, allowing for more predictable control of the system's response. Without the damping effect, the response may be less controlled, leading to less reliable behavior. The resistor helps to smooth the energy transition within the system, reducing the probability of nonlinear or unpredictable behavior [32].

The remaining questions are where to connect the filtering mechanism and how to design it to address the high-frequency phenomena in dc microgrids. These considerations

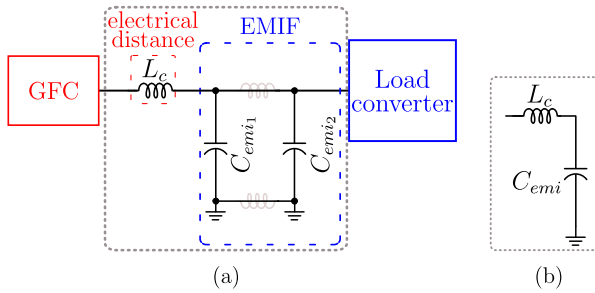

 FIGURE 3. DC microgrid and inclusion of a $R - C$ damping branch at PCC_2 in order to mitigate the HFR.

are critical for optimizing system performance, minimizing peak-to-peak voltage variations, and ensuring stable operation under conditions like the beating effect and the high-frequency resonance (HFR).

The grid design procedures must solve these issues and maintain the dc voltage within the designated ripple limits. However, connecting the $R - C$ damping branch at PCC_1 , would not solve the problem, as the HFR originates from the interactions between the cable feeder and the input filters of the load converter. The voltage gain will remain unchanged, and the point at which the grid impedance appears to be the lowest will also remain consistent.

The best approach involves connecting the $R - C$ damping branch at PCC_2 , as illustrated in Figure 3. Inserting the damping branch at PCC_2 will untune the HFR between the cable feeder and the EMIF capacitors. Additionally, it will increase the input capacitance of the load converter, reducing the voltage gain at PCC_2 . Consequently, the impedance observed from the grid side (PCC_1) will no longer reach its minimum at the grid-forming converter's switching frequency.

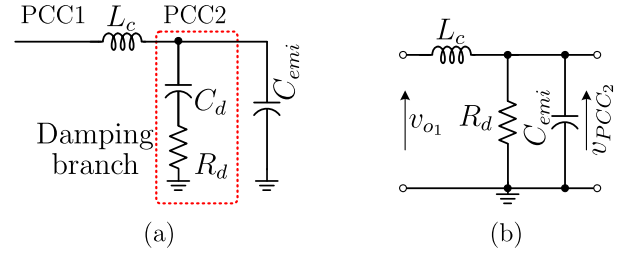
Based on the principle that resonance arises from the interaction between the cable feeder and the load converter's front-end capacitors, this interaction can be effectively modeled as a second-order low-pass filter. This analogy is illustrated in Figure 4, where the cable inductance and the converter's input capacitance form the key components of the filter.


 FIGURE 4. (a) - Interaction between the cable feeder and the front-end capacitors of the load converter model; (b) - equivalent $L - C$ model.

The interplay of these elements determines the resonance frequency. This simplified representation provides valuable

insights into the resonance phenomenon, enabling the analysis and design of the mitigation strategy to address the issue of power quality in dc microgrids.

Figure 5(a) illustrates the circuit model following the connection of the damping branch at PCC_2 . Since the impedance of the damping branch capacitor must be significantly smaller than the resistance of the damping branch, the capacitor C_d can be neglected. Consequently, the final LRC model is represented in Figure 5(b).


 FIGURE 5. (a) - Complete equivalent model with the damping branch; (b) - simplified LRC model with the damping branch.

Since the resonance results from the interaction between the inductance L_c and the capacitance C_{emi} , which are tuned to the grid former converter's switching frequency, the resonance frequency is given by:

$$f_{res} = f_{s1} = \frac{1}{2 \cdot \pi \cdot \sqrt{L_c \cdot C_{emi}}}. \quad (1)$$

Given that the characteristic of a LRC circuit is expressed as:

$$\frac{v_{PCC2}}{v_{o1}} = \frac{1}{\frac{s^2}{\omega_o^2} + \frac{2 \cdot \xi \cdot s}{\omega_o} + 1}. \quad (2)$$

And knowing that:

$$\omega_o = 2 \cdot \pi \cdot f_{s1} = \frac{1}{\sqrt{L_c \cdot C_{emi}}}. \quad (3)$$

The EMI capacitor can be given by:

$$C_{emi} = \frac{1}{L_c \cdot \omega_o^2}. \quad (4)$$

By analyzing the circuit shown in Figure 5(b), the ratio between v_{PCC2} and v_{o1} can be expressed as:

$$\frac{v_{PCC2}}{v_{o1}} = \frac{R_d // Z_{C_{emi}}}{Z_{L_c} + R_d // Z_{C_{emi}}}. \quad (5)$$

Where:

$$Z_{L_c} = L_c \cdot s, \quad (6)$$

$$Z_{C_{emi}} = \frac{1}{s \cdot C_{emi}}, \quad (7)$$

$$C_{emi} = C_{emi1} + C_{emi2}. \quad (8)$$

Placing (4) in (7):

$$Z_{C_{emi}} = \frac{L_c \cdot \omega_o^2}{s}. \quad (9)$$

Developing (5) in a way that resembles the characteristic equation at (2), the resistance value for damping the circuit presented in Figure 5(b) can be found as:

$$R_d = \frac{L_c \cdot \omega_o}{2 \cdot \xi}. \quad (10)$$

Knowing that the perfect resistance value for an optimal damping is achieved when $\xi = 0.707$ and for an $L_c = 108.4 \mu\text{H}$ and a $\omega_o = 2\pi f_{s1}$, the optimal resistance value is:

$$R_{d_{opt}} = \frac{108.4 \cdot 10^{-6} \cdot 2 \cdot \pi \cdot 20 \cdot 10^3}{2 \cdot 0.707} = 9.63 \Omega. \quad (11)$$

Therefore, given that the impedance of the damping capacitor must be significantly smaller than R_d , the selected damping capacitance is $C_d = 470 \mu\text{F}$.

IV. Theoretical Analysis

The inclusion of a damping branch alters the impedance seen by the grid former converter and shifts ahead the resonance frequency of the cable-EMIF capacitor, causing it to no longer be tuned to the grid former switching frequency. Figure 6 shows the Bode diagram of the load sub-systems impedance for PCC_1 (Z_{L1}) and the voltage gain for the cable inductance $L_c = 108.4 \mu\text{H}$.

Figure 6(a) shows the case with no damping branch at PCC_2 , and Figure 6(b) shows the case with the inclusion of the $R - C$ damping branch at PCC_2 . To define the transfer functions G_{vv} and $G_{vv,mitig}$ shown in Figure 6, the load subsystem impedances at PCC_2 without and with the damping branch are expressed as:

$$G_{vv} = \frac{Z_{Load}}{Z_{Load} + Z_{Lcr}} \quad (12)$$

$$G_{vv,mitig} = \frac{Z_{Load,mitig}}{Z_{Load,mitig} + Z_{Lcr}} \quad (13)$$

where Z_{Load} and $Z_{Load,mitig}$ represent the load subsystem impedances at PCC_2 without and with the damping branch, respectively, defined as:

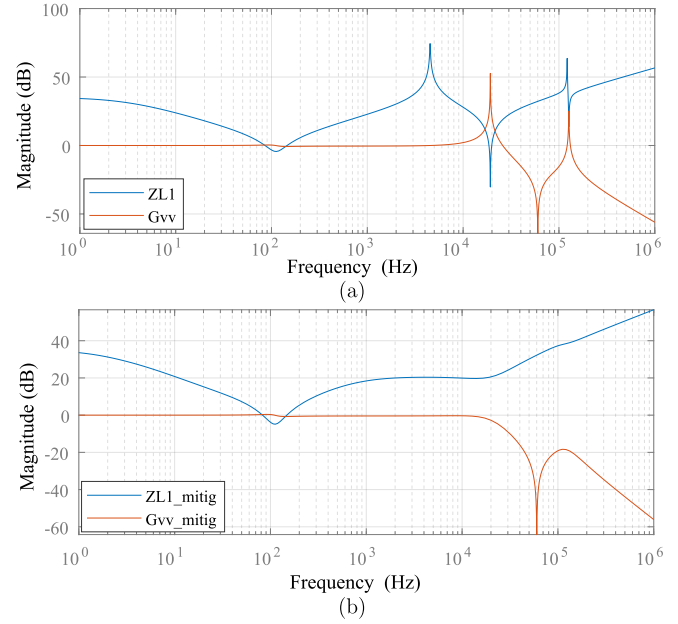


FIGURE 6. Bode diagram of the load sub-system impedance for PCC_1 (Z_{L1}) and the voltage gain for the cable inductance L_c (a) without the damping branch at PCC_2 and (b) with the damping branch at PCC_2

$$Z_{Load} = Z_{ce} \parallel [Z_{le} + (Z_{ce} \parallel (Z_{Lf} + (Z_{in2} \parallel Z_{Cf})))] \quad (14)$$

$$Z_{Load,mitig} = Z_{ce} \parallel Z_{d_{pcc2}} \parallel Z_x \quad (15)$$

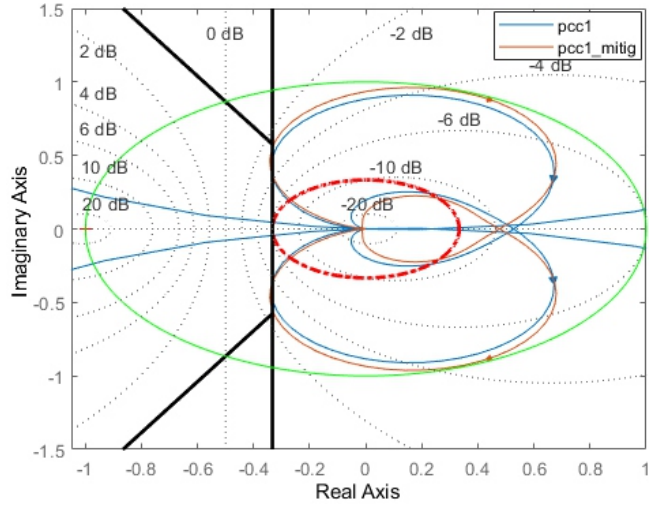
$$Z_x = Z_{le} + (Z_{ce} \parallel (Z_{Lf} + (Z_{in2} \parallel Z_{Cf}))) \quad (16)$$

with Z_{in2} representing the closed-loop input impedance of the load buck converter (converter 2), Z_{ce} the EMI filter capacitor impedance, Z_{le} the EMI filter inductor impedance, Z_{Lf} the input LPF filter inductor impedance, Z_{Cf} the input LPF filter capacitor impedance of the load buck converter, and $Z_{d_{pcc2}}$ the damping branch impedance used for mitigation.

To ensure that the system remains stable after connecting the damping branch at PCC_2 , the Nyquist contour of the minor loop gain (T_{MLG}) is analyzed at PCC_1 . For comparison and to quantify the effectiveness of the mitigation method, both cases are plotted: without mitigation and with HFR mitigation.

For a phase margin of 60° and a gain margin of -6 dB, Figure 7 shows the Nyquist diagram of the Minor Loop Gain for the system analyzed at PCC_1 with (orange plot) and without (blue plot) the connection of the damping branch at PCC_2 .

The non-mitigated system exhibits an inadequate phase margin of only 13.5° , thereby violating the established stability criteria. In contrast, the integration of the optimized $R - C$ damping branch at PCC_2 effectively reshapes the system frequency response, ensuring that the Nyquist contour


 FIGURE 7. Nyquist diagram of the Minor Loop Gain at PCC_1

comfortably respects the established stability margins and never trespasses the unit circle.

A. Design Guidelines for Damping Placement

Figure 8 shows the plot of the damping resistance (R_d) as a function of the cable inductance (L_c), varying from 50 μH to 500 μH , and the GFC switching frequency (f_{s1}) varying from 10 kHz to 100 kHz.

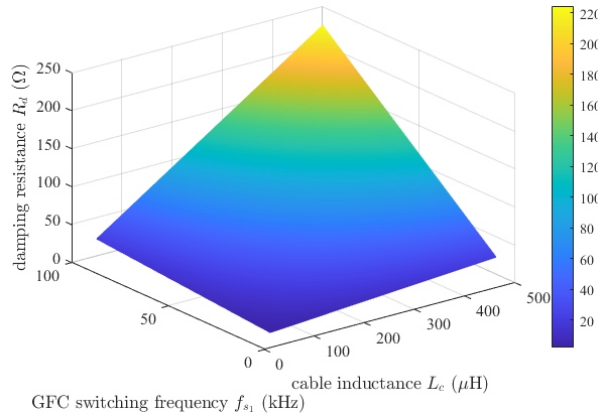

 FIGURE 8. Damping resistance (R_d) as a function of the cable inductance (L_c) and the GFC switching frequency (f_{s1})

Figure 8 shows that R_d increases with both L_c and f_{s1} , indicating a combined influence of cable inductance and converter switching frequency on the required damping resistance. Figure 8 provides important design insights into the relationship between cable feeder inductance, converter switching frequency, and the required damping resistance.

For a fixed switching frequency (f_{s1}) and damping ratio (ξ), the damping resistance R_d increases linearly with the cable inductance L_c . Since L_c is directly proportional to the cable physical length, this result indicates that the required damping effort scales with the electrical distance between converters. Figure 8 further shows that R_d increases with

both L_c and f_{s1} , highlighting a combined dependence on the cable length and the GFC switching frequency. This demonstrates that higher switching frequencies intensify high-frequency resonance effects, requiring stronger damping.

Considering the studied case, where a cable inductance of $L_c = 108.4 \mu\text{H}$ corresponds to approximately 88 m of cable length [14], it can be inferred that longer feeder cables inherently increase the susceptibility to resonance phenomena and demand higher damping resistance values. An additional and important design insight can be obtained by explicitly relating the cable length to the microgrid GFC switching frequency. The resonance frequency of the cable-EMI filter interaction is given by (1). Since the cable inductance is proportional to its physical length, it can be expressed as:

$$L_c = L' \cdot \ell \quad (17)$$

where L' is the inductance per unit length (H/m) and ℓ is the cable length (m). Substituting, the cable length can be written as:

$$\ell = \frac{1}{(2\pi f_{res})^2 \cdot C_{emi} \cdot L'} \quad (18)$$

Thus, the cable length that leads to resonance is inversely proportional to the square of the switching frequency:

$$\ell \propto \frac{1}{f_s^2} \quad (19)$$

For two operating frequencies, this relationship can be expressed as:

$$\ell_2 = \ell_1 \left(\frac{f_1}{f_2} \right)^2 \quad (20)$$

This relationship provides a practical design guideline. Given the reference case where a cable length of approximately 88 m results in resonance at 20 kHz, the equivalent critical cable length for other switching frequencies can be estimated accordingly.

For instance, increasing the switching frequency to 30 kHz reduces the critical cable length to approximately 39 m, and at 40 kHz it decreases to approximately 22 m. This indicates that higher-switching-frequency converters are more prone to high-frequency resonance at shorter cable lengths.

Therefore, in dc microgrids employing a grid-forming converter operating at 20 kHz, resonance conditions may arise for cable lengths on the order of 88 m, requiring the inclusion of a damping branch to suppress high-frequency oscillations. For higher switching frequencies, this critical distance is significantly reduced, suggesting that damping elements may need to be placed more frequently along the feeder. Table 2 summarizes the relationship between the microgrid GFC switching frequency and the equivalent cable length for resonance conditions.

These results demonstrate that the proposed mitigation strategy is not only dependent on electrical parameters but

TABLE 2. Equivalent cable length for resonance as a function of GFC switching frequency

f_{sGFC} (kHz)	Cable length ℓ (m)
20	88
30	39
40	22
50	14

also strongly influenced by the microgrid's physical layout. Consequently, the design of dc microgrids must account for both the GFC switching frequency and the cable length between converters, as these factors directly impact resonance conditions, system stability, and compliance with power quality requirements.

V. SIMULATION RESULTS

To validate the analytical findings and evaluate the time-domain behavior of the proposed mitigation strategy, time-domain simulations were performed using the PSIM software environment. Since the analytical results showed that the HFR disappeared with the inclusion of the damping branch at P_{CC2} and the system has increased its stability margins considerably, the time simulation returns the same results, as shown in Figure 9 and Figure 10.

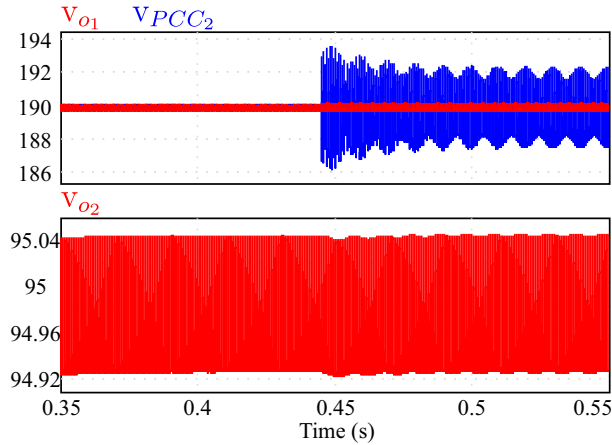


FIGURE 9. DC microgrid simulation result for the addition of a damping branch at P_{CC2} , moment of disconnection of the branch

At $t = 0.45$ s (see Figure 9) the damping branch is disconnected and the HFR at P_{CC2} is visible, before this point the damping branch was connected and there was no longer the HFR oscillations at P_{CC2} , actually the $v_{P_{CC2}}$ voltage had a ripple smaller than the output voltage of the grid former v_{o1} , as shows Figure 10 (b).

Figure 10 shows the simulation voltage ripple for the case where the damping branch is not connected, Figure 10(a), and for the case where the damping branch is connected to P_{CC2} , Figure 10(b).

Figures 11 and 12 show the FFT simulation results for the dc microgrid at a beating frequency of $f_b = 100$ Hz. Figure 11 shows the simulation FFT value for no damping

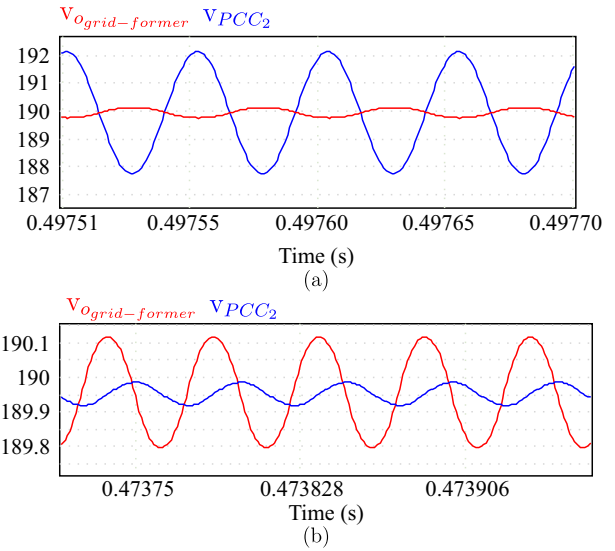


FIGURE 10. DC microgrid simulation result for the addition of a damping branch at P_{CC2} , (a) voltage ripple resulting without the damping branch at P_{CC2} , (b) voltage ripple resulting with the damping branch at P_{CC2} .

branch, and Figure 12 shows the simulation FFT value with the damping branch at P_{CC2} .

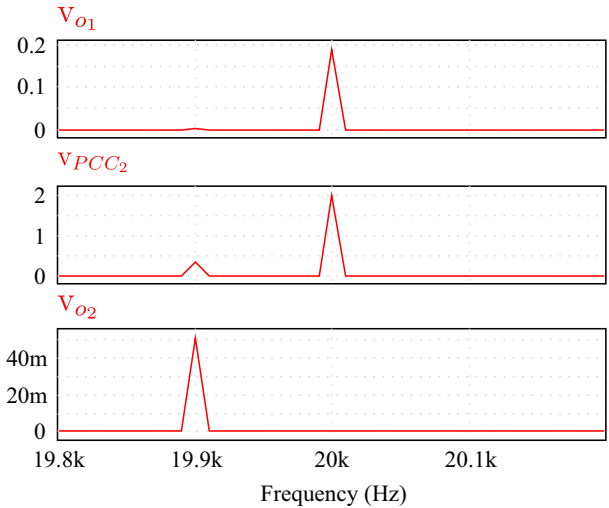


FIGURE 11. DC microgrid FFT simulation result for a beating frequency of $f_b = 100$ Hz and no damping branch.

Comparing both Figure 11 and 12 it is notable that both v_{o1} and v_{pcc2} distortion amplitudes at 20 kHz were reduced with the inclusion of the damping branch at P_{CC2} . v_{o1} distortion amplitude at 20 kHz was reduced almost twice and v_{pcc2} distortion amplitude at 20 kHz were reduced almost ten times.

With the chosen mitigation method implemented, the v_{pcc2} voltage meets the adapted maximum permissible distortion at 20 kHz for 190 V networks of $0.687 V_{rms}$.

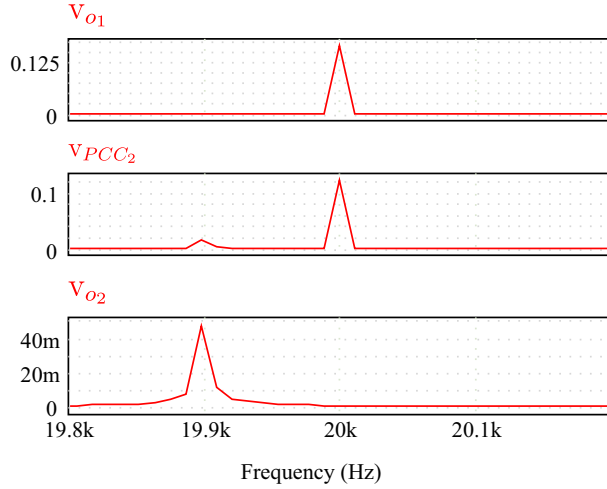


FIGURE 12. DC microgrid FFT simulation result for a beating frequency of $f_b = 100$ Hz and with the damping branch at P_{CC2} .

VI. EXPERIMENTAL RESULTS

Both the analytical and simulation results revealed that the oscillations amplified by the HFR were mitigated when the damping branch was added at P_{CC2} , significantly enhancing the system's stability margins. Figure 13 shows the 2 kW experimental setup. The experimental setup utilizes the same parameters specified in Table 1. The inclusion of the $R-C$ damping branch at the P_{CC2} was set up, and the results are presented in Figure 14.

The GFC output voltage is channel 4, green, 2 V/div and with an offset of 190, the P_{CC2} voltage is channel 3, blue, 2 V/div and with an offset of 190V, and the dc bus current (i_{Lc}) is channel 2, pink, 1 A/div, with time scale of 20 ms. At $t = 120$ ms (see Figure 14), the damping branch is connected and the oscillations caused by the HFR at P_{CC2} are mitigated; before this point, the branch was disconnected and the HFR oscillations at P_{CC2} were visible.

Using an adaptation of the MIL-STD-704F [30] to find the maximum permissible distortion amplitude at 20 kHz for dc networks of 190 V, Table 3 summarizes the experimental distortion amplitudes of the dc buses v_{o1} and v_{pcc2} with and without the mitigation method.

TABLE 3. Distortion amplitude at 20 kHz

Maximum permissible distortion for networks at 190 V	0.687 Vrms
Distortion amplitude without damping branch for v_{o1}	0.339 Vrms
Distortion amplitude without damping branch for v_{pcc2}	2.15 Vrms
Distortion amplitude with damping branch for v_{o1}	0.390 Vrms
Distortion amplitude with damping branch for v_{pcc2}	0.271 Vrms

The distortion at 20 kHz in the v_{o1} voltage increased after connecting the damping branch; however, it remains within the adapted permissible distortion limit. With the chosen mitigation method, the v_{pcc2} voltage meets the adapted maximum permissible distortion at 20 kHz for 190 V networks.

VII. CONCLUSIONS

This paper presented a mitigation method for high-frequency disturbances in dc microgrids caused by the interaction between cable feeders and the front-end capacitors of power converters. The analyzed scenario, composed of cascaded buck converters interconnected through a long cable and filtering stages, demonstrated that such interactions can lead to high-frequency resonance phenomena, significantly degrading dc bus voltage quality.

The study showed that these oscillations may exceed the maximum permissible distortion limits specified in the adapted MIL-STD-704F standard at 20 kHz for a 190 V dc bus, thereby representing a critical power quality issue in practical dc microgrid implementations. To address this problem, a mitigation strategy based on the inclusion of a properly designed and strategically placed $R-C$ damping branch was proposed.

The analytical formulation provided clear design guidelines for the damping branch parameters, linking them to system characteristics such as cable inductance and switching frequency. The results demonstrated that the optimal placement of the damping branch at P_{CC2} effectively detunes the resonance condition and reshapes the system impedance, improving stability margins and reducing voltage amplification.

In addition, a key contribution of this work is establishing a direct relationship between cable length and the grid-forming converter's switching frequency. This relationship reveals that the electrical distance between converters, represented by the cable length, plays a critical role in defining resonance conditions. As a result, higher switching frequencies reduce the critical cable length required to trigger resonance, leading to more stringent design constraints in practical dc microgrids.

Based on this analysis, practical design guidelines were derived, indicating that, for a system operating at 20 kHz, resonance conditions may arise for cable lengths on the order of 88 m, requiring the inclusion of damping elements. At higher switching frequencies, this critical distance is significantly reduced, suggesting that damping strategies must be implemented more frequently along the feeder.

Both simulation and experimental results validated the proposed approach. A significant reduction in distortion amplitude at 20 kHz was achieved, particularly at P_{CC2} , where the voltage distortion was reduced by nearly one order of magnitude. Moreover, the mitigated system met the adapted standard limits, confirming the method's effectiveness in restoring acceptable power quality levels.

Overall, the proposed mitigation method provides a simple, low-cost, and effective solution for addressing high-frequency phenomena in dc microgrids without compromising converter stability. The results highlight the importance of considering cable-filter interactions and physical layout constraints in the design of dc systems.

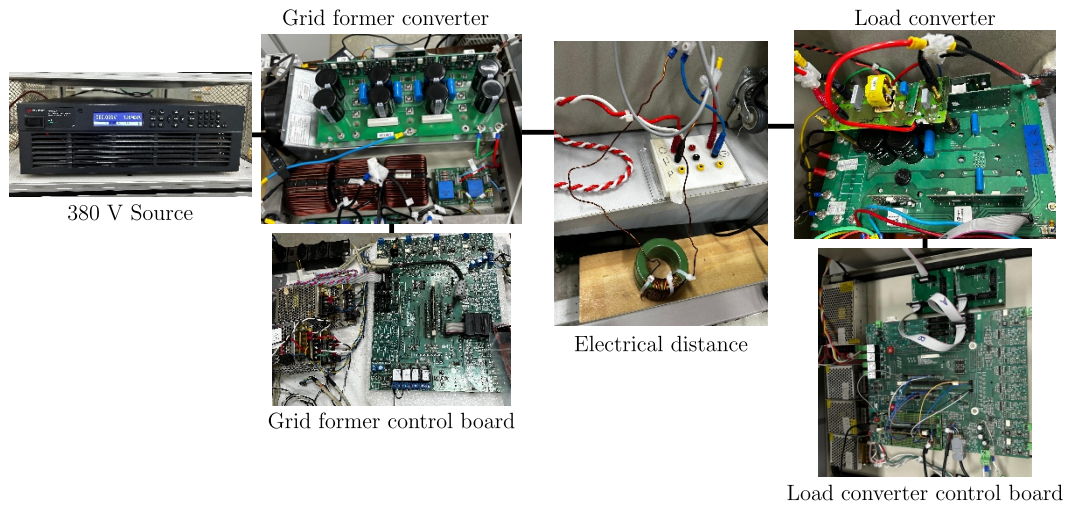


FIGURE 13. DC microgrid experimental setup

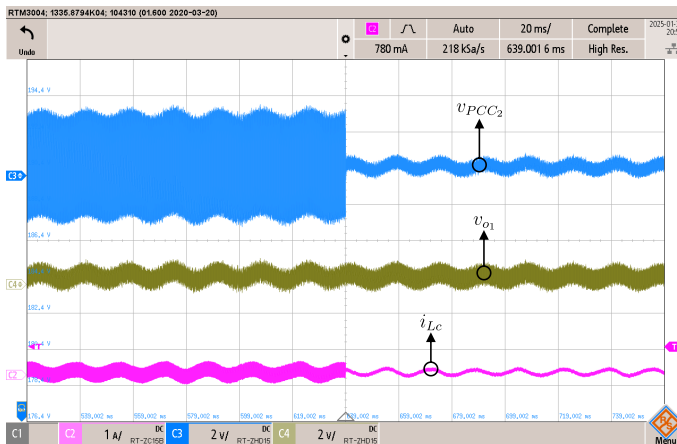


FIGURE 14. DC microgrid experimental result for adding a damping branch at P_{CC2} . CH4(green): grid former output voltage; CH3(blue): P_{CC2} voltage; CH2(pink): dc bus current

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AUTHOR'S CONTRIBUTIONS

D.P.DAMASCENO: Conceptualization, Data Curation, Formal Analysis, Investigation, Methodology, Project Admin-

istration, Writing – Original Draft. **M.P.DIAS:** Data Curation, Validation, Writing – Review & Editing. **J.C.UPEÑA:** Methodology, Supervision, Writing – Review & Editing. **J.A.POMILIO:** Conceptualization, Funding Acquisition, Project Administration, Supervision, Writing – Review & Editing.

PLAGIARISM POLICY

This article was submitted to the similarity system provided by Crossref and powered by iThenticate – Similarity Check.

DATA AVAILABILITY

The data used in this research is available in the body of the document.

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