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Control and Lifetime Assessment of a Bidirectional EV Charger with Reactive Power Support

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ABSTRACT This paper investigates the impact of reactive power support on the reliability of a bidirectional electric vehicle (EV) charger operating as a grid support asset. A 50 kW charger composed of a three phase VSI rectifier, an isolated dual active bridge converter, and a bidirectional DC-DC stage is considered. A reactive power control strategy is implemented to provide voltage support during grid voltage sags while maintaining battery charging operation under a CC–CV profile. The proposed control system is validated through simulations and experimental tests performed on the grid-side converter. In addition, a reliability assessment is carried out for the VSI IGBTs and DC-link capacitors using electrothermal modeling, lifetime models, Rainflow cycle counting, and Monte Carlo analysis. The results show that reactive power injection contributes to voltage support during sag events but increases the thermal stress imposed on the power semiconductors. For the investigated mission profiles, the provision of reactive power support significantly reduces the lifetime of the IGBT modules, whereas the impact on the DC-link capacitors remains limited. The presented results highlight the trade-off between ancillary service provision and converter lifetime, providing useful insights for the design and operation of bidirectional EV charging infrastructure.

KEYWORDS Bidirectional charger, electric vehicles, voltage sag compensation, reliability analysis.

I. INTRODUCTION

The rapid growth of electric mobility has imposed new challenges on power systems, not only due to the increase in energy demand but also owing to the need for greater flexibility and operational support. The expansion of the electric vehicle (EV) fleet requires a corresponding increase in charging infrastructure, which, in turn, assumes a strategic role in power system operation [1]. In this context, bidirectional chargers emerge as key elements capable of going beyond battery charging, actively contributing to the provision of ancillary services [2].

Among the ancillary services that can be provided by bidirectional chargers are frequency regulation, voltage support, reactive power compensation, harmonic filtering, voltage unbalance mitigation, and virtual inertia provision [3]. In particular, voltage sag compensation is of great interest, since such disturbances are common in distribution systems and may significantly affect power quality and the operation of sensitive loads. Reactive power injection during these events can contribute to voltage support and improve grid resilience [4]. These capabilities are closely related to the concepts of Vehicle-to-Grid (V2G) and Vehicle-to-Everything (V2X), in which electric vehicles evolve from passive energy consumers into distributed energy resources [5]. As a result, bidirectional chargers are considered an enabling technology

for renewable energy integration, peak-demand reduction, and the development of smarter and more resilient power systems [6].

However, providing grid-support services in addition to battery charging increases the electrical and thermal stress imposed on critical components such as power semiconductors and capacitors. Therefore, evaluating the reliability impact of ancillary services is essential to ensure that the benefits provided to the grid do not compromise system lifetime and availability [7].

Several studies have addressed EV charger grid integration and reliability from different perspectives. The study presented in [1] reviewed fast-charging architectures and converter topologies, whereas [2] investigated bidirectional charger control for ancillary services, including reactive power support. From a reliability perspective, [8] evaluated semiconductor thermal cycling and lifetime under fast-charging mission profiles, while [9] investigated control strategies for reducing electrothermal stress in power converters. System-level reliability assessment of EV charging architectures was further addressed by [10]. The impact of reactive power support on the electrothermal stress and lifetime of critical components in bidirectional EV chargers was investigated in [11], highlighting the reliability implications associated with ancillary service provision. Despite these

contributions, studies combining reliability assessment with detailed charger control design and experimental validation of grid-support operation remain limited. To address this gap, this work presents a detailed design of the control loops for the different charger stages, evaluates reactive power support for voltage sag compensation, and experimentally validates the grid-side converter. In addition, the reliability assessment quantifies the impact of reactive power support on the lifetime of the VSI IGBTs and DC-link capacitors, providing a comprehensive evaluation of the trade-off between grid-support capability and converter lifetime.

The remainder of this paper is organized as follows. Section II presents the bidirectional charger topology and control strategy. Section III addresses the reactive power support method adopted for voltage sag compensation. Section IV describes the reliability assessment methodology. Section V presents the simulation, experimental, and reliability results. Finally, Section VI concludes the paper.

II. BIDIRECTIONAL EV CHARGER TOPOLOGY

The proposed topology consists of a bidirectional Voltage Source Inverter (VSI) based on IGBTs with antiparallel diodes, enabling bidirectional power flow between the utility grid and the DC bus. In addition, the DC bus is composed of a capacitor bank and an isolated dual active bridge (DAB) converter, with a 1:1 turn ratio. The medium-frequency transformer (MFT) provides galvanic isolation between the grid and the battery. At the battery interface, three interleaved bidirectional buck converters are employed, enabling both battery charging and the transfer of energy from the battery to the utility grid whenever required. Figure 1 illustrates the complete topology. The charger has a rated power of 50 kW and is designed to operate when connected to a line-to-line voltage of 380 V, 60 Hz, three-phase utility grid. The nominal battery voltage is 400 V, operating at a charge and discharge rate of 0.5 C.

Each stage of the charger is equipped with a dedicated control structure responsible for regulating different levels of the system, including the reactive power exchanged with the grid, the DC-link voltage, the voltage at the secondary side of the DAB converter, and the electric vehicle battery charging process. Figure 2 presents the complete control architecture for all charger stages. Subsequently, the design procedure for determining the controller gains is presented in detail.

A. VSI CONTROL STRATEGY

The control stages of the bidirectional VSI rectifier comprise the phase-locked loop (PLL) synchronization control, the DC-link voltage and reactive power injection control loops, and the cascaded current control loops. The controller design methodology for these control schemes is presented in the following section.

1) SRF-PLL Control

For proper operation of the bidirectional charger, the control system must be synchronized with the utility grid. For this purpose, a Synchronous Reference Frame Phase-Locked Loop (SRF-PLL) is employed to estimate the grid phase angle and frequency [12]. The SRF-PLL transforms the three-phase voltages abc into the synchronous dq reference frame. When the d -axis is aligned with the grid-voltage vector, V_q converges to zero, while V_d represents the voltage magnitude. Thus, V_q is used as the synchronization error signal [13]. To reduce the influence of grid-voltage magnitude variations, the error signal [13] is normalized as

$$e = \frac{V_q}{\sqrt{V_d^2 + V_q^2}}. \quad (1)$$

The synchronization loop employs a PI controller designed from the equivalent second-order PLL model. A damping ratio of $\zeta = 0.707$ is adopted, while the natural frequency is selected as one third of the nominal grid frequency. For a 60 Hz grid, $\omega_n = 125.66$ rad/s, and the controller gains [13] are given by

$$k_{p,PLL} = 2\zeta\omega_n \quad (2)$$

$$k_{i,PLL} = \omega_n^2 \quad (3)$$

The PI output provides the frequency correction $\Delta\omega$, which is added to the nominal grid angular frequency and integrated to obtain the angle θ used in the Park transformation. The estimated angle is then constrained to the interval between 0 and 2π .

The VSI inverter/rectifier is controlled in the synchronous dq reference frame. With the d -axis aligned with the grid voltage, i_d regulates the active power, while i_q controls the reactive power exchanged with the grid. A cascaded control structure is employed, in which the outer DC-link voltage and reactive-power loops generate the i_d and i_q current references, respectively.

2) Current Control Inner Loop

The current controller design is based on the output filter model of the VSI rectifier, which consists of an inductance L and a series resistance R_L . In this work, the filter inductance was selected as $L = 2$ mH to limit the switching current ripple while preserving the dynamic performance of the current control loop. The input filter was designed to ensure that the converter output current Total Demand Distortion (TDD) remains below the 5% limit specified by the applicable IEEE standards [14]. Considering a grid frequency of 60 Hz, the corresponding inductive reactance is approximately 0.754Ω . Assuming that the filter resistance is forty times smaller than the inductive reactance yields $R_L = 18.9$ m Ω .

A PI controller was designed using the pole-zero cancellation method, where the controller zero is placed at

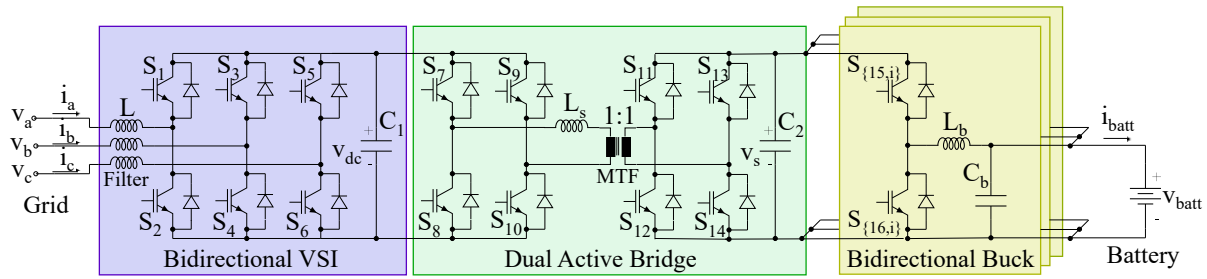


FIGURE 1. Topology used for the analysis of the bidirectional charger.

the dominant pole of the plant. The current-loop crossover frequency was chosen as one twentieth of the switching frequency.

Considering a switching frequency of 20 kHz, the adopted crossover frequency is 1 kHz, corresponding to an angular frequency of $\omega_{c,i} = 6283 \text{ rad/s}$. The controller gains [3] are given by

$$k_{p,i} = L\omega_{c,i} \quad (4)$$

$$k_{i,i} = R_L\omega_{c,i} \quad (5)$$

This design provides a high-bandwidth current loop with a fast dynamic response, allowing the i_d and i_q currents to accurately track their reference values.

3) DC Voltage Control

The DC-link voltage controller constitutes the outer loop associated with the direct-axis component of the control structure. Its primary function is to generate the current reference required to regulate the energy stored in the DC-link capacitor. The DC-link voltage was regulated at 700 V. Considering a grid phase-voltage peak value of $V_n =$

311.12 V, the power-to-current conversion gain [3] is defined as

$$K = \frac{1.5V_n}{V_{dc}} \quad (6)$$

The controller was designed using a pole-placement approach, in which two characteristic frequencies were specified for the desired closed-loop response, namely $f_{c1} = 100 \text{ Hz}$ and $f_{c2} = 10 \text{ Hz}$. Based on these specifications, the PI controller gains [3] are obtained as

$$k_{p,v} = -\frac{2\pi(f_{c1} + f_{c2})C}{K} \quad (7)$$

$$k_{i,v} = -\frac{4\pi^2 f_{c1} f_{c2} C}{K} \quad (8)$$

where C represents the equivalent DC-link capacitance (1190 μF). The selected poles ensure a slower response than the inner current loop, preserving the stability of the cascaded control structure while providing proper regulation of the DC-link voltage.

4) Reactive Power Control

The reactive power is regulated through the quadrature-axis current component i_q . The outer reactive power loop generates the current reference, which is subsequently tracked by the inner current controller.

Reactive power measurement is performed using a first-order filter whose time constant is selected as the inverse of the grid frequency. The reactive-power loop bandwidth was set to 50 Hz, ensuring slower dynamics than the current loop. The PI controller gains [3] are calculated as

$$k_{p,q} = \frac{2\pi f_{c,q} T}{1.5V_d} \quad (9)$$

$$k_{i,q} = \frac{2\pi f_{c,q}}{1.5V_d} \quad (10)$$

where $f_{c,q}$ is the crossover frequency of the reactive-power loop, T is the digital filter time constant, and V_d is the direct-axis voltage component. This strategy enables the charger to both inject and absorb reactive power while maintaining adequate dynamic separation between the control loops.

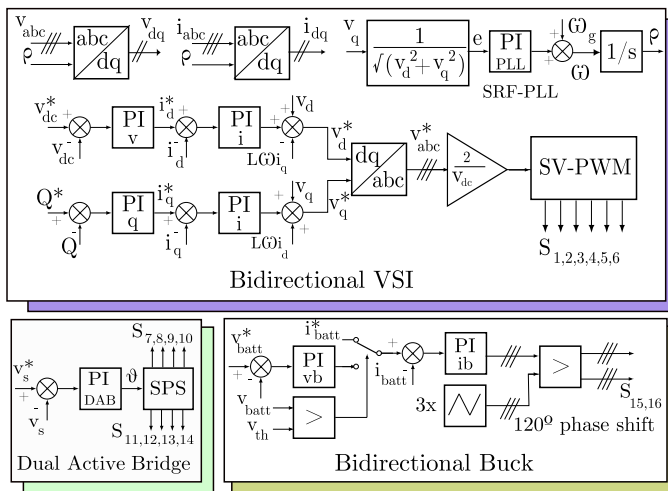


FIGURE 2. Control structure of the VSI rectifier, the DAB converter, and the bidirectional buck converter.

B. DAB CONTROL STRATEGY

The second stage of the charger consists of a Dual Active Bridge (DAB) converter composed of two full bridges interconnected through a medium-frequency transformer.

The DAB provides galvanic isolation and bidirectional power flow between the DC link and the battery-side stage [15]. In this work, the transformer has a unity turns ratio ($n = 1$), with nominal primary- and secondary-side voltages of 700 V. The converter operates at 10 kHz using Single Phase Shift (SPS) modulation with a fixed duty cycle of 50%. At the nominal power of 50 kW, a phase-shift angle of $\phi = 30^\circ$ and an equivalent series inductance of $L_s = 68.1 \mu\text{H}$ are adopted.

For the voltage-loop design, a first-order DAB model is considered. At the nominal operating point, the equivalent load resistance is $R_{\text{load}} = 9.8 \Omega$ and the output capacitance is $C_2 = 100 \mu\text{F}$. The equivalent load resistance was calculated from the nominal operating point as $R_{\text{load}} = V^2/P$, whereas C_2 was selected to limit voltage ripple while preserving adequate control dynamics. The output voltage is regulated by a PI controller whose output determines the phase-shift angle applied to the SPS modulation.

Following the pole-placement method, the crossover frequency is selected as one twentieth of the switching frequency, resulting in $f_{c,\text{DAB}} = 500 \text{ Hz}$. The controller gains [15] are given by

$$k_{p,\text{DAB}} = \frac{4\pi^3 f_{c,\text{DAB}} f_s L_s C_2}{V_1(\pi - 2\phi)}, \quad (11)$$

and

$$k_{i,\text{DAB}} = \frac{4\pi^3 f_{c,\text{DAB}} f_s L_s}{V_1(\pi - 2\phi) R_{\text{load}}}. \quad (12)$$

Thus, the PI controller regulates the DAB output voltage by adjusting the phase-shift angle between the two bridges.

C. BIDIRECTIONAL BUCK CONTROL STRATEGY

Battery charging is performed through a bidirectional buck converter, whose operating mode depends on the direction of power flow between the grid and the battery. The vehicle battery is charged using the constant current/constant voltage (CC–CV) method [16]. During the constant-current (CC) stage, the battery, rated at 400 V ($V_{th} = 400 \text{ V}$), is charged with a constant current of 125 A. Once the battery voltage reaches the nominal value ($V_{\text{batt}} > V_{th}$), the charging process transitions to the constant-voltage (CV) stage, in which the voltage is maintained at 400 V while the charging current gradually decreases until the state of charge (SoC) reaches 100%. Three converters operating in parallel are considered in order to reduce the current handled by each power switch. To minimize current ripple, a 120° phase shift is introduced between the triangular carrier signals, resulting in an interleaved converter topology [17]. When required, active power can be injected into the grid through the bidirectional buck converter by setting a negative current

reference in the control system. However, in this study, the primary focus regarding active power is to evaluate the battery charging control.

1) Current Control

The inner loop of the bidirectional buck converter is responsible for controlling the inductor current during the battery charging process. Since the battery voltage exhibits relatively small variations throughout charging, the plant can be approximated by an RL circuit.

The PI controller was designed using the pole-zero cancellation method, placing the controller zero at the plant pole. The crossover frequency was selected as a fraction of the converter switching frequency in order to provide a sufficiently high bandwidth for the current loop. The controller gains [6] are obtained as

$$k_{p,ib} = \frac{L_b \omega_{c,ib}}{V_{dc}} \quad (13)$$

$$k_{i,ib} = \frac{R_b \omega_{c,ib}}{V_{dc}} \quad (14)$$

where L_b is the converter inductance, R_b is the inductor series resistance, V_{dc} is the converter input voltage, and $\omega_{c,ib}$ is the current-loop crossover angular frequency. The switching frequency of the DC–DC converter is 25 kHz. The crossover frequency of the current loop is chosen as five times lower than the switching frequency, corresponding to 5 kHz [6]. This choice provides a fast transient response, while the integral action of the PI controller eliminates the steady-state error for constant charging current references.

2) Voltage Control

The battery voltage loop constitutes the outer control loop of the DC–DC converter and operates during the constant-voltage stage of the CC–CV charging profile. Since the current loop exhibits significantly faster dynamics, it can be approximated by a unity-gain block during the design procedure.

The PI controller was tuned considering the dynamics of the output capacitor C_b and the equivalent battery resistance R_o ($v_{\text{batt}} / i_{\text{batt}}$). The output capacitance was selected as 100 μF to limit voltage ripple, whereas the equivalent battery resistance was estimated from the nominal operating point (400 V, 125 A), resulting in $R_o \approx 3.2 \Omega$. The crossover frequency was set to 500 Hz, one order of magnitude lower than the current loop bandwidth, ensuring adequate separation between the cascaded control loops. The controller gains [6] are given by

$$k_{p,vb} = C_b \omega_{c,vb} \quad (15)$$

$$k_{i,vb} = \frac{\omega_{c,vb}}{R_o} \quad (16)$$

where C_b is the output capacitance (100 μF), R_o is the equivalent battery resistance (3,2 Ω), and $\omega_{c,vb}$ is the voltage-loop crossover angular frequency (500 Hz = 3 141 rad/s). The adopted strategy ensures a smooth transition between the constant-current and constant-voltage charging modes while maintaining the stability of the battery charging process.

Finally, the Table 1 summarizes the controller gains obtained for the control loops of the bidirectional VSI, DAB converter, and bidirectional buck converter.

TABLE 1. Controller Gains

Control Loop	K_p	Value	Unit	K_i	Value	Unit
SRF-PLL	$k_{p,PLL}$	177.7	rad/s	$k_{i,PLL}$	15791	rad/s ²
VSI Current	$k_{p,i}$	12.57	V/A	$k_{i,i}$	118.7	V/(A·s)
DC-Link Voltage	$k_{p,v}$	-1.23	A/V	$k_{i,v}$	-70.5	A/(V·s)
Reactive Power	$k_{p,q}$	388.9	A/VAr	$k_{i,q}$	0.673	A/(VAr·s)
DAB Voltage	$k_{p,DAB}$	0.00288	rad/V	$k_{i,DAB}$	2.94	rad/(V·s)
Buck Current	$k_{p,ib}$	0.0448	A ⁻¹	$k_{i,ib}$	0.448	(A·s) ⁻¹
Buck Voltage	$k_{p,vb}$	0.314	A/V	$k_{i,vb}$	981.75	A/(V·s)

III. REACTIVE POWER SUPPORT

Grid connected power electronic converters can provide ancillary services related to power quality and power system stability in addition to their primary energy conversion function. These services include frequency regulation, reactive power compensation, voltage support, harmonic mitigation, and voltage unbalance compensation [6]. In this context, bidirectional electric vehicle chargers can operate as grid support resources, since actively controlled converters allow the independent regulation of both active and reactive power exchanged with the electrical grid.

Ancillary services have become increasingly important in modern power systems due to the growing penetration of power electronic converters and highly dynamic loads, such as fast electric vehicle charging stations. In this context, converters can support system stability, reliability, and power quality through functions such as frequency regulation, voltage support, harmonic mitigation, voltage unbalance compensation, and reactive power support [2], [3]. In distribution networks, rapid variations in load demand may cause voltage fluctuations due to the voltage drops across grid impedances. Additionally, the interaction of multiple converters can lead to harmonic amplification and resonance phenomena, particularly in weak grids. Under these conditions, converters equipped with advanced control strategies can actively contribute to voltage regulation, reactive power compensation, and harmonic mitigation, thereby improving the overall power quality of the distribution system [3].

Voltage sags are characterized by temporary reductions in RMS voltage and may compromise the operation of sensitive loads and the stability of grid-connected systems. During these events, reactive current injection by the converter contributes to increasing the residual voltage at the point of common coupling, thereby supporting grid recovery [2]. As

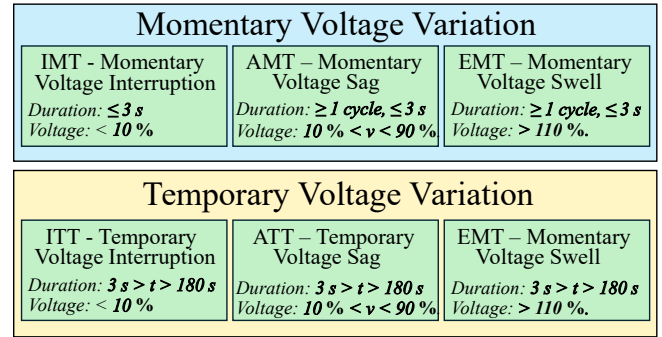


FIGURE 3. Grid voltage oscillation classification according to PRODIST Module 8.

a result, modern grid codes have established Low Voltage Ride Through (LVRT) requirements, which mandate that converters and generation units remain connected during temporary faults and actively contribute to voltage restoration. In addition to preventing cascading disconnections, LVRT requirements improve the robustness and stability of power systems. In several grid codes, such as those adopted in Germany, the injected reactive current must be proportional to the depth of the voltage sag in order to maximize voltage support during fault conditions [4].

In Brazil, the classification of short duration voltage variations is defined by PRODIST Module 8, issued by the Brazilian Electricity Regulatory Agency (ANEEL) [18]. As shown in Figure 3, voltage variations are classified according to both their duration and magnitude. Voltage sags correspond to RMS voltage levels between 0.1 p.u. and 0.9 p.u. and may be classified as momentary or temporary depending on their duration. Voltage levels below 0.1 p.u. are classified as voltage interruptions, whereas voltage levels above 1.1 p.u. are classified as voltage swells. Since the objective of the proposed reactive power support strategy is to mitigate voltage sags, a threshold of 0.9 p.u. is adopted as the criterion for initiating reactive current injection, in accordance with the limits established by PRODIST [18].

The adopted strategy establishes the relationship between voltage sag depth and reactive support through the positive-sequence gain K_+ , as illustrated in Figure 4. The values $K_+ = 0, 2.5$, and 10 were selected to represent the absence of support, moderate support, and aggressive support, respectively, allowing the influence of the gain on reactive current injection during voltage sags to be illustrated. It can be observed that a non-injection region exists for voltages close to the nominal value. When the positive-sequence voltage falls below 0.9 p.u., the requested reactive current increases proportionally to the voltage deviation and is limited by the converter rated current capability. Therefore, higher values of K_+ result in greater reactive current injection for the same voltage sag level, providing a more effective contribution to voltage recovery [4].

Considering the operating threshold of 0.9 p.u., the reactive current reference [4] is defined as

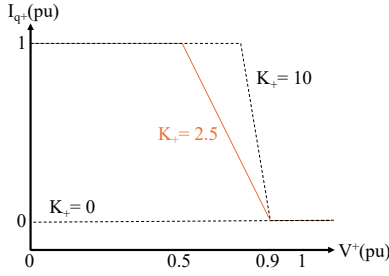


FIGURE 4. Reactive current injection curves as a function of the voltage sag level for different values of K_+ .

$$I_g^* = k_+ \left(0.9 - \frac{V_{g,d}^+}{V_g} \right) I_r \quad (17)$$

where I_g^* is the reactive current reference, K_+ is the positive-sequence gain, $V_{g,d}^+$ is the positive-sequence direct-axis component of the grid voltage, V_g is the nominal grid voltage, and I_r is the converter rated current. Accordingly, the reactive current reference is adjusted according to the depth of the voltage sag, allowing the converter to use its available current capability to provide dynamic voltage support to the electrical grid.

IV. RELIABILITY ANALYSIS

The reliability of bidirectional chargers is strongly related to the thermal behavior of their most critical components, particularly the IGBTs and the DC-link capacitors. During operation, electrical losses produce temperature rises that accelerate degradation mechanisms and consequently reduce component lifetime [7]. Therefore, reliability assessment is based on obtaining representative thermal profiles under realistic operating conditions, allowing the aging process of the components to be estimated over time.

The reliability assessment focuses on the VSI rectifier, since reactive power injection into the grid directly influences the lifetime of the semiconductors and DC-link capacitors in this stage, making it the most affected converter within the system. Although the DAB and bidirectional buck converters are also subjected to thermal stress during battery charging, their thermal behavior differs according to their operating characteristics [19]. The buck converter is predominantly subjected to long term thermal variations associated with the battery charging current, whereas the DAB is also subjected to short term thermal cycling due to its high frequency switching operation [20]. However, the objective of this study is to quantify the reliability impact associated with reactive power support. Since the reactive current required for voltage sag compensation is directly processed by the grid connected VSI, this stage is directly affected by the additional electrical and thermal loading associated with this ancillary service [21].

Battery lifetime is an important aspect of EV charging systems. However, reactive power support is processed by the grid side VSI and, as shown in Figure 9(b), has negligible

impact on battery current. Therefore, this study focuses on the converter components directly affected by the ancillary service, namely the VSI semiconductors and DC-link capacitors. Battery degradation analysis is left for future work.

For IGBTs, conduction and switching losses increase the junction temperature, accelerating degradation mechanisms in the device interconnections [20]. The junction temperature is estimated using equivalent thermal models based on thermal resistances and capacitances. From the resulting temperature profiles, the IGBT lifetime can be estimated considering bond-wire degradation as the main failure mechanism, typically identified by an increase in the collector-emitter on-state voltage [21].

Among the available semiconductor lifetime models, the Bayerer model is one of the most widely adopted [21]. This model estimates the number of thermal cycles to failure according to

$$N_f = A \cdot (\Delta T_j)^{\beta_1} \cdot e^{\frac{\beta_2}{T_{jm}}} \cdot t_{on}^{\beta_3} \cdot I_{bw}^{\beta_4} \cdot V_{blk}^{\beta_5} \cdot D^{\beta_6} \quad (18)$$

where N_f is the number of cycles to failure, A is the technology factor, ΔT_j is the junction temperature swing, T_{jm} is the mean junction temperature, t_{on} is the heating time, I_{bw} is the current per bond wire, V_{blk} is the normalized blocking voltage, D is the bond-wire diameter, and β_1 to β_6 are experimentally derived fitting coefficients.

For electrolytic capacitors, the degradation process is mainly influenced by operating temperature and internal losses associated with the equivalent series resistance (ESR). Initially, the capacitor current is analyzed under steady-state conditions and decomposed into its harmonic components by means of the Fast Fourier Transform (FFT). Using these harmonic components together with ESR values dependent on frequency and temperature, the power dissipated in the capacitor can be determined. This dissipated power is then applied to a simplified thermal model, providing the temperature profile required for lifetime estimation [19].

The expected capacitor lifetime can be calculated as

$$L_{cap} = L_0 \left(\frac{V_c}{V_0} \right)^{-n_1} 2^{\frac{T_0 - T_h}{n_2}} \quad (19)$$

where L_{cap} denotes the capacitor lifetime, L_0 is the nominal lifetime specified by the manufacturer, V_c is the operating voltage, V_0 is the rated voltage, n_1 and n_2 are acceleration coefficients related to electrical and thermal stress, respectively, T_h is the operating temperature, and T_0 is the rated temperature of the device.

Since the operating conditions of the charger vary continuously over time, the obtained thermal profiles must be processed by cycle-counting techniques. For this purpose, the Rainflow algorithm is employed to convert complex temperature histories into equivalent heating and cooling cycles. Based on these cycles, the accumulated damage is evaluated using the Palmgren–Miner rule, which assumes that each operating condition consumes a fraction of the total lifetime of the component [22].

For IGBTs, lifetime consumption is determined by

$$LC_S = \sum \frac{N_i}{N_{f,i}} \quad (20)$$

where N_i is the number of occurrences of a given operating condition and $N_{f,i}$ is the corresponding number of cycles to failure.

Similarly, for capacitors, lifetime consumption is expressed as

$$LC_C = \sum \frac{t_i}{L_i} \quad (21)$$

where t_i is the time spent under a given operating condition and L_i is the estimated lifetime associated with that condition.

In both cases, the component is considered to have reached its end of life when the accumulated damage equals unity. Therefore, the combination of thermal profiles, degradation models, Rainflow cycle counting, and the Palmgren–Miner rule provides a methodology for estimating the lifetime of the charger's critical components under different operating conditions [23]. Figure 5 summarizes the reliability analysis workflow adopted for IGBTs and capacitors.

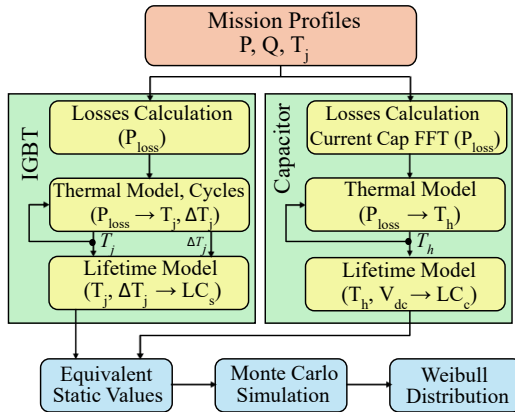


FIGURE 5. Flowchart for reliability analysis of IGBTs and capacitors.

V. RESULTS

Based on the implementation of the proposed topology and the control strategy developed according to the adopted methodology, this section presents the main results obtained. First, the battery charging performance is evaluated, with particular emphasis on the operation of the bidirectional Buck converter. Subsequently, the results of reactive power injection into the grid for voltage sag compensation are presented, followed by the experimental results of the charger. Finally, the main impacts of reactive power support on charger reliability are assessed considering the adopted mission profiles.

A. BATTERY CHARGING

The battery considered in the simulation corresponds to the battery pack used in the Tesla Model S, composed of cells

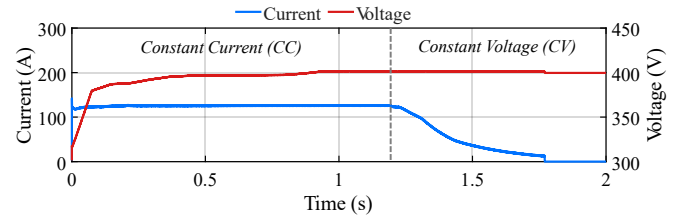


FIGURE 6. Battery charging under the CC-CV profile.

with a capacity of 3.4 Ah and a nominal voltage of 3.8 V, arranged as 96 series-connected cells and 74 parallel strings, resulting in an approximate capacity of 250 Ah. The battery pack was selected to represent a typical EV battery system with a nominal voltage compatible with the charger specifications, allowing the evaluation of the proposed charger under realistic operating conditions.

In the simulation, the battery capacity was reduced from approximately 250 Ah to 50 mAh exclusively to accelerate the state of charge dynamics and allow the complete CC–CV transition to be observed within a practical simulation interval. This corresponds to a time-scaling factor of approximately 5000. It should be emphasized that this reduction is not intended to reproduce the electrochemical or thermal dynamics of the full-scale battery pack. The converter charging current reference remains unchanged at 125 A; therefore, the electrical operating point imposed on the power-conversion stages is not scaled according to the reduced battery capacity. Consequently, the 50 mAh model is used only for validating the CC–CV control behavior and its transition between charging modes. The reliability assessment is performed independently using the converter mission profiles and the corresponding electrothermal models of the VSI components. Thus, the accelerated battery model does not directly scale or determine the semiconductor and DC-link capacitor thermal profiles used for lifetime estimation.

A smooth transition between the constant-current and constant-voltage stages can be observed. At the end of the charging process, as the current approaches zero, the current flow is interrupted.

B. VOLTAGE SAG

Figure 7 illustrates the charger voltages and reactive power during a temporary voltage sag event. To evaluate the voltage support capability, an 80% balanced sag was applied to the three-phase grid voltage. During this event, the reactive power reference is adjusted according to the quadrature-axis current required for compensation, using $K^+ = 10$ as defined in (11). The resulting I_q^* is then converted into the reactive power reference Q^* for the control system, enabling voltage recovery support.

In the simulation, the grid voltage remains stabilized at its nominal value until 0.1 s. At this instant, the 80% voltage sag is applied and remains without control action until 0.125 s, allowing the sag condition to stabilize.

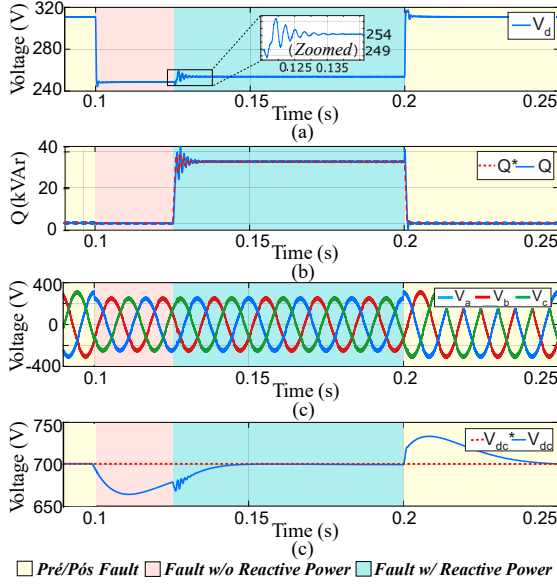


FIGURE 7. (a) Measured direct-axis voltage. (b) Reactive power reference and reactive power compensated at the grid. (c) Monitored three-phase voltages. (d) DC-link voltage during the transient periods.

After this interval, the reactive power reference is activated, and the grid voltage behavior is monitored until 0.2 s, when the voltage returns to its nominal value.

Figure 7 presents the behavior of the quadrature-axis voltage throughout the analysis interval, as well as the reactive power injected into the grid and the three-phase voltages. The behavior of the DC-link voltage is also shown, which is likewise regulated by the VSI rectifier.

During the sag event, the peak grid voltage reached approximately 248.9 V. After the activation of the reactive power support function, the voltage increased to approximately 253.9 V, corresponding to an improvement of about 2% in the residual voltage level. The results demonstrate that, although the contribution of a single charger is relatively limited, the coordinated operation of multiple chargers connected to the same network can provide a more significant impact on voltage sag mitigation and power system stability.

C. EXPERIMENTAL RESULTS

Although the complete bidirectional charger topology presented in Figure 1 comprises a grid side VSI, an isolated Dual Active Bridge (DAB) converter, and a bidirectional buck converter, the present work focuses exclusively on the experimental validation of the grid side converter. This implementation represents the first stage in the development of the complete charger, allowing the validation of the synchronization algorithm, current control loops, DC side operation, and reactive power control before integrating the remaining power conversion stages.

While the simulations were performed at full scale, the experimental validation was conducted at reduced scale and restricted to the VSI stage due to laboratory infrastructure limitations. The same control structure was maintained, with

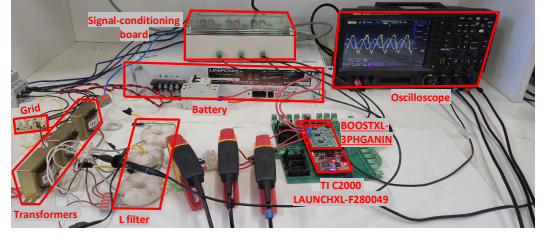


FIGURE 8. (Laboratory setup used for the experimental validation of the grid side bidirectional VSI, including the three phase transformer bank, interface inductors, BOOSTXL-3PHGANINV converter, TI C2000 LAUNCHXL-F280049C DSP, signal conditioning board, 48 V battery, and measurement instrumentation.

reference values, grid voltages, and battery specifications adjusted to the scale of the experimental platform.

Figure 8 presents the laboratory setup used for the experimental validation. The three-phase input is obtained from the 220-V utility grid through a bank of three single-phase transformers (127/12 V, 1.5 A) connected in a Y-Y configuration, providing a reduced-voltage grid for safe laboratory operation. Each converter phase is connected to the transformer secondary through a 3.5 mH inductor with a series resistance of approximately 0.7 Ω , forming the interface filter between the converter and the grid.

The grid-side converter was implemented using the BOOSTXL-3PHGANINV module controlled by a Texas Instruments LAUNCHXL-F280049C DSP. Current measurements were obtained from the converter's integrated sensors, while grid voltages were acquired through a dedicated signal-conditioning board. A 48 V, 20 Ah LiFePO₄ battery (Unipower UPLFP48-20E) was directly connected to the DC side, replacing the DAB and bidirectional buck stages. The SRF-PLL, current controllers, and reactive power control were implemented in PLECS using automatic code generation for the TI C2000 platform and executed in real time by the DSP. Experimental waveforms were acquired using a Rigol MSO5204 digital oscilloscope.

1) Validation of the Current Control Loops

Figure 9 presents the experimental response of the current control loops during step changes in the reference currents. In both tests, the three phase currents are shown together with the battery DC current, allowing the evaluation of the power exchanged with the DC side of the converter.

Figure 9a illustrates the transition from $i_d^* = 0$ A to $i_d^* = 1$ A while maintaining $i_q^* = 0$ A. As expected, the increase in the direct-axis current establishes active power transfer between the AC and DC sides, resulting in a corresponding increase in the battery current.

The three-phase currents remain balanced throughout the transient, demonstrating the proper operation of the current controllers during active power regulation.

In contrast, Figure 9b presents the response to a step change from $i_q^* = 0$ A to $i_q^* = 1$ A while maintaining $i_d^* =$

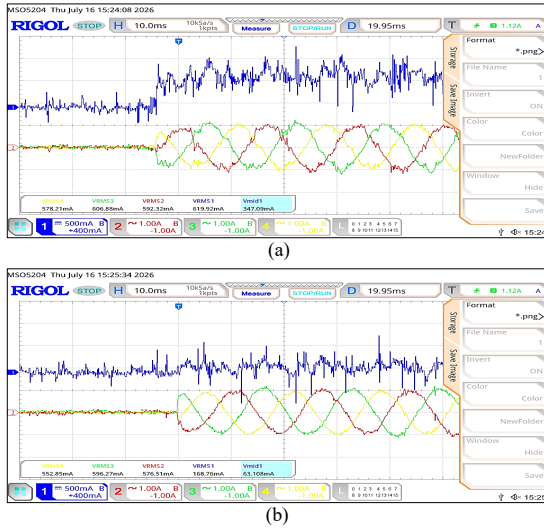


FIGURE 9. Experimental validation of the decoupled dq current control. (a) Step response from $i_d^* = 0A$ to $i_d^* = 1A$ with $i_q^* = 0A$, showing the increase in the battery DC current due to active power transfer. (b) Step response from $i_q^* = 0A$ to $i_q^* = 1A$ with $i_d^* = 0A$, demonstrating that reactive power control does not significantly affect the battery current.

0A. Under this operating condition, the converter exchanges only reactive power with the grid. Consequently, the battery current remains practically unchanged, experimentally confirming the decoupled control of the synchronous reference frame, in which the quadrature-axis current is responsible for reactive power regulation without modifying the active power transferred to the DC side.

It can also be observed that the measured battery current exhibits a relatively high ripple and noise level in both operating conditions. This behavior is expected because, at this stage of the experimental platform, the battery is directly connected to the DC side of the VSI. In the complete charger topology shown in Figure 1, the bidirectional buck converter will regulate the battery charging/discharging current, significantly reducing the current ripple imposed on the battery terminals.

2) Experimental Validation Under Different Operating Conditions

Figures 10, 11 and 12 presents the experimental waveforms obtained for different current references applied to the synchronous dq controller. Each subfigure shows the voltage and current of one phase measured at the secondary side of the transformer, allowing the relationship between the grid voltage and converter current to be evaluated under different operating conditions.

Figures 10a and 10b correspond to purely active-power operation, with $i_q^* = 0A$, and $i_d^* = 1A$ and $i_d^* = -1A$, respectively. As expected, reversing the sign of the direct-axis current changes the direction of active power flow between the converter and the DC side while maintaining

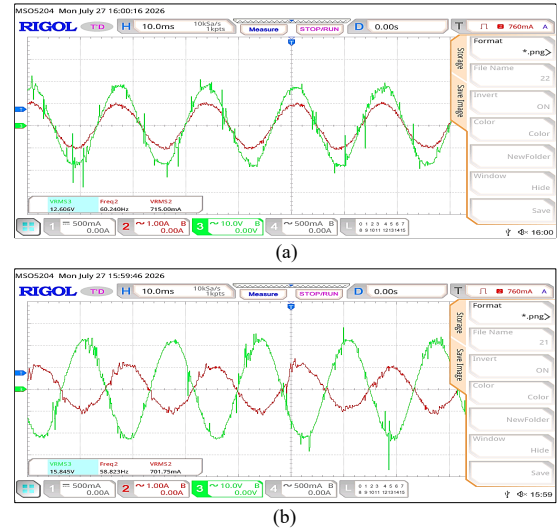


FIGURE 10. Experimental phase voltage and current waveforms measured at the secondary side of the transformer for different current references. (a) $i_d^* = 1A$, $i_q^* = 0A$; (b) $i_d^* = -1A$, $i_q^* = 0A$.

a predominantly in-phase relationship between the phase voltage and current.

Figures 11a and 11b illustrate purely reactive-power operation, obtained with $i_d^* = 0A$, and $i_q^* = 1A$ and $i_q^* = -1A$, respectively. In these cases, the current is displaced with respect to the grid voltage, confirming that the converter exchanges predominantly reactive power with the utility while the active power transfer remains negligible.

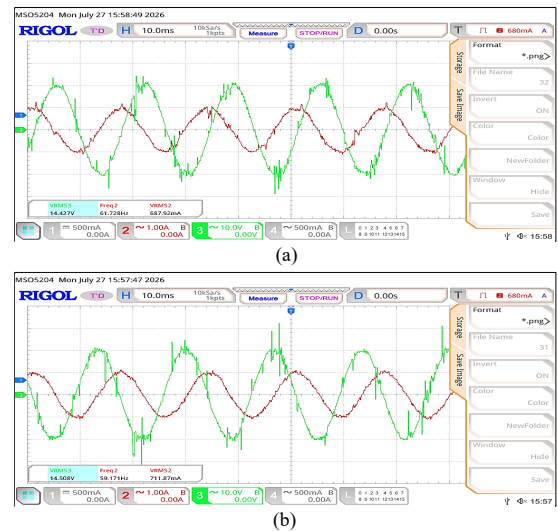


FIGURE 11. Experimental phase voltage and current waveforms measured at the secondary side of the transformer for different current references. (a) $i_d^* = 0A$, $i_q^* = 1A$; (b) $i_d^* = 0A$, $i_q^* = -1A$.

Finally, Figures 12a and 12b present operating conditions in which both current components are simultaneously commanded. In these cases, the converter exchanges active and reactive power simultaneously, resulting in an intermediate phase displacement between voltage and current. These ex-

perimental results demonstrate the capability of the proposed controller to independently regulate the active and reactive current components over different operating conditions, validating the synchronous reference frame control strategy implemented in the experimental platform.

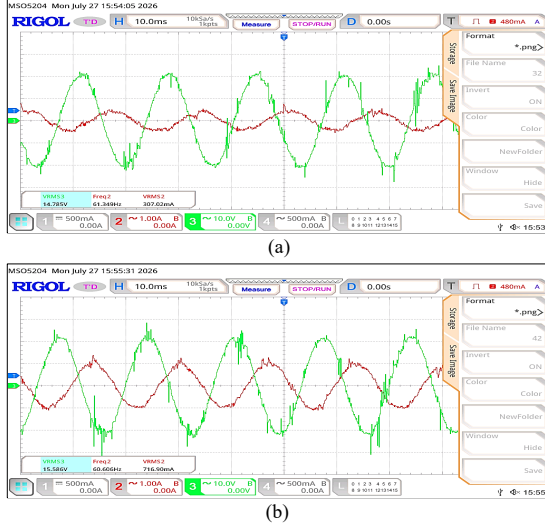


FIGURE 12. Experimental phase voltage and current waveforms measured at the secondary side of the transformer for different current references. (a) $i_d^* = 0.3\text{ A}$, $i_q^* = 0.3\text{ A}$; (b) $i_d^* = 0.7\text{ A}$, $i_q^* = 0.7\text{ A}$.

From a quantitative perspective, the experimental results confirm the expected relationship between the d - and q -axis current components. For purely active or reactive operation, a 1 A reference was applied to one axis while the other was maintained at 0 A.

When both components were set to 0.3 A and 0.7 A, the resulting current-vector magnitudes were approximately 0.42 A and 0.99 A, respectively, with an expected phase displacement of 45° . Moreover, the negligible variation in battery current when i_q^* changes from 0 to 1 A at $i_d^* = 0$ confirms the decoupled regulation of active and reactive power.

D. RELIABILITY ANALYSIS OF LIFETIME EVALUATION

The reliability analysis of the charger was performed considering the VSI rectifier/inverter stage due to the number and impact of the IGBTs and DC-link capacitors on the overall system reliability. For the data analysis, the Infineon FF150R12KS2 IGBT module (1200 V, 150 A) [24] and TDK B43640 capacitors (340 μF) were considered [25]. The total capacitance of the DC-link capacitor bank is 1190 μF , consisting of two capacitors connected in series and seven parallel branches. The analysis considered the overall capability of the system to inject reactive power into the grid, regulate the DC-link voltage, and control the battery charging process according to the CC-CV profile. Mission profiles based on the active power consumed by the battery and the reactive power injected into the grid were adopted as input variables.

The annual mission profiles comprise 525 600 samples with a one minute resolution. The active power profile considers ten daily charging events, each lasting one hour at the rated power of 50 kW and distributed between 8:00 a.m. and 10:00 p.m. The reactive power profile includes four daily injection events of eight minutes each at 50 kVar. Based on these profiles, the component lifetime is first estimated for the vehicle charging only operation and then compared with scenarios that additionally provide reactive power support. Figure 13 illustrates the adopted daily active and reactive power mission profiles.

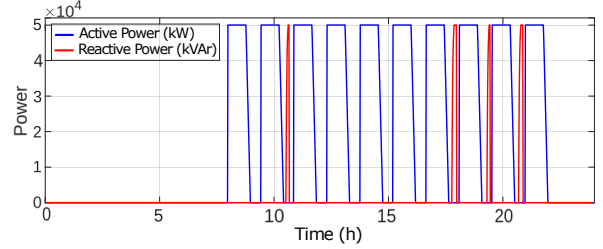


FIGURE 13. Daily active and reactive power mission profiles.

For the vehicle charging only scenario, the estimated lifetimes were 35.33 years for the IGBTs and 26.77 years for the capacitors. With reactive power support, these values decrease to 8.26 and 26.42 years, respectively. To account for parameter variations and uncertainties, Figure 14 shows the Monte Carlo unreliability curves for both operating conditions, where the dashed line indicates the B10 lifetime.

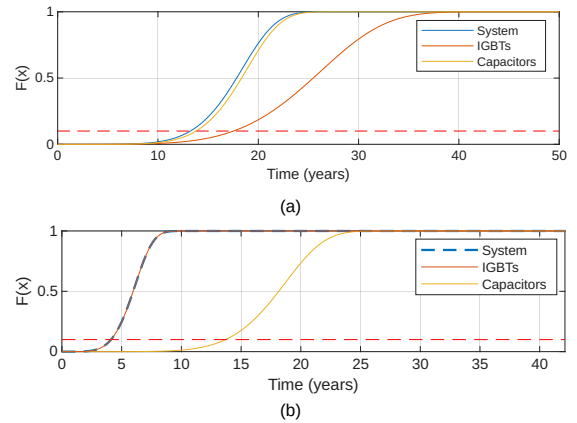


FIGURE 14. Unreliability curves of the IGBTs, capacitors, and the overall system considering: (a) vehicle charging only; and (b) vehicle charging combined with reactive power compensation.

Another scenario considers variations in the number of vehicles charged throughout the day, ranging from 1 to 10, which directly affects the active power mission profile. The static lifetime of the charger is calculated for each number of vehicles. Figure 15 presents the resulting curves for scenarios with and without reactive power injection into the grid. The fitted curves are also shown, indicating the lifetime trend as the number of charged vehicles increases.

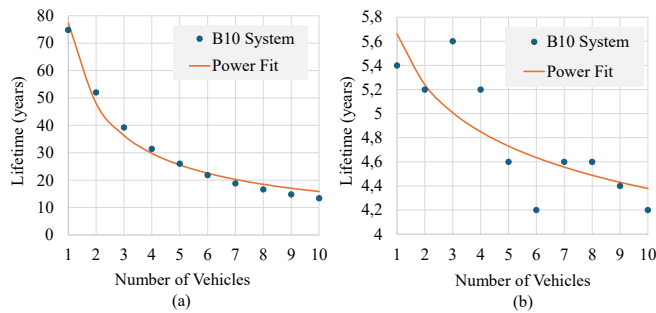


FIGURE 15. Service life of the chargers as a function of the number of daily charging cycles, considering: (a) without voltage sag compensation; (b) with reactive power compensation.

VI. CONCLUSION

This paper presented a 50 kW bidirectional EV charger capable of providing reactive power support during voltage sag events while maintaining battery charging under a CC–CV strategy. Simulation and experimental results validated the proposed control architecture, demonstrating independent active and reactive power regulation.

For an 80% balanced voltage sag, the proposed strategy increased the residual grid voltage from 248.9 V to 253.9 V, corresponding to a recovery of approximately 2%. The reliability assessment showed that reactive power support significantly affects the VSI semiconductors, reducing the estimated IGBT lifetime from 35.33 years to 8.26 years (77% reduction), while the DC link capacitor lifetime remained practically unchanged, decreasing from 26.77 years to 26.42 years.

The main contribution of this work is the quantitative assessment of the trade-off between voltage support capability and converter lifetime in bidirectional EV chargers, demonstrating that ancillary service provision can substantially affect semiconductor reliability. Furthermore, the results highlight a clear trade-off between grid support and converter reliability in V2G applications. Although reactive power injection improves voltage regulation and power quality during grid disturbances, it increases the thermal stress on power semiconductors, reducing their lifetime. Therefore, future V2G strategies should balance ancillary service provision with reliability and economic considerations to ensure the long-term sustainability of charging infrastructure.

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This work utilized the ChatGPT artificial intelligence system, developed by OpenAI, for grammatical review and

text enhancement. The technical content and ideas presented are solely the authors' responsibility.

AUTHOR'S CONTRIBUTIONS

M.V.SANTOS: Conceptualization, Investigation, Software, Writing. **M.A. FERREIRA:** Conceptualization, Investigation, Software, Writing. **J.V.M.FARIAS:** Methodology, Supervision, Validation. **E.M.S. BRITO:** Methodology, Supervision, Validation.

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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