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A Transformer-Integrated Traction Power Supply System with Comprehensive Power Quality Control

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Abstract—The traction power supply system (TPSS) is suffering from numerous power quality issues, such as negative-sequence currents, voltage sags, and harmonic pollution, which can jeopardize the stable operation of the utility grid and the traction system. This paper proposes a hybrid V/V transformer-integrated traction power supply system (HVV-TPSS) with comprehensive power quality compensation. The parallel converters (PCs) and series converters (SCs) of HVV-TPSS are integrated to the three-limb four-winding V/V transformer for current and voltage compensation without auxiliary transformers, thus enhancing space utilization. Moreover, HVV-TPSS can perform comprehensive power quality management, including current-related power quality issues and severe disturbances related to voltage faults and harmonics, not only strengthening the voltage compensation capability but also improving its integration level. By establishing the mathematical model of the hybrid V/V transformer unit, the fundamental compensation principles of current and voltage within HVV-TPSS are elucidated. Subsequently, this paper further analyzes the working principle for comprehensive power quality compensation of HVV-TPSS under steady-state and grid fault conditions, followed by the proposal of its comprehensive control strategy. Finally, the effectiveness of HVV-TPSS is validated through simulations and experiments.

Index Terms—Traction power supply system, hybrid V/V transformer, power quality, comprehensive compensation.

I. INTRODUCTION

IN recent years, electrified railways have undergone rapid development due to their high reliability, robust transportation capacity, and low operating costs [1]. However, the intrinsic operating characteristics of electric locomotives, such as asymmetry, nonlinearity, and impulsiveness, combined with the single-phase power supply mode of the traction power supply system (TPSS), have led to increasingly severe power quality issues, including negative sequence current (NSC), harmonic pollution, and reactive power [2]–[5]. Furthermore, complex faults in the utility grid can also cause voltage-related power quality issues, such as voltage sag, asymmetry, and background harmonics [6], [7].

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These power quality issues can pose significant threats to the reliable operation of electric locomotives, while increasing equipment losses, reducing the service life of the equipment, disrupting relay protection devices, and impacting the economic operation of adjacent power supply systems [8]. Moreover, the work in [9] discussed international standards, power quality indices, and detection methods for issues such as voltage sag, swell, and distortion in TPSS, highlighting the necessity for standardized identification and targeted mitigation of voltage-related power quality disturbances.

Currently, most research on power quality issues in electrified railways focuses on optimizing the TPSS topology and deploying external power quality compensators. With respect to TPSS topology optimization, it is noted that traction transformers are essential for the overall performance and economic efficiency. Compared to conventional transformers, balanced transformers, such as Scott and Ynvd transformers, have demonstrated efficiency in mitigating power quality issues through unique winding connections and electromagnetic balance design [10], [11]. However, balanced transformers have limitations such as inadequate dynamic response and lack of control flexibility. Consequently, relying solely on balanced transformers is neither sufficient to tackle current-related power quality problems, nor adequate to handle the voltage-related issues encountered.

In terms of external parallel power quality compensators, passive filters can offer low-cost harmonic filtering and reactive power compensation while risking grid resonance and overcompensation [12]. The static var compensator (SVC) enables dynamic reactive power compensation, but occupies a large physical area and tends to introduce harmonics [13]. The three-phase static synchronous compensator (STATCOM) possesses the capability to dynamically compensate for NSC and reactive power, yet its reliance on high-voltage side step-down transformers restricts its negative-sequence compensation capacity and cost-effectiveness [14].

The railway power conditioner (RPC) serves as a multifunctional compensator capable of eliminating NSC, suppressing current harmonics, compensating for reactive power, and enabling bidirectional transfer of active power between the two feeders through the back-to-back converter [15]–[19]. In [20], an adaptive impedance coupling hybrid power quality compensator (HPQC) was proposed in the co-phase railway power supply system, which features a wide compensation range. Although the co-phase traction supply system can fundamentally address current-related power quality issues, it entails a higher initial construction investment. The modular multilevel converter (MMC)-based RPC, utilizing a multilevel

topology with cascaded submodules, can surpass the voltage and capacity constraints intrinsic to the conventional two-level RPC [21]–[23]. However, the multilevel power quality solutions are composed of numerous electronic submodules, preventing their widespread adoption due to high hardware costs and insufficient reliability [24].

By employing current compensation to mitigate the impedance-induced voltage deviation, the aforementioned parallel current-compensated power quality solutions can indirectly alleviate voltage fluctuations and voltage imbalance. However, their voltage control capability is limited, and they cannot address severe disturbances such as deep voltage sags and asymmetric voltage faults.

To enhance the voltage control capability, reference [25] proposed a coordinated control strategy by combining the RPC with dynamic voltage restorer (DVR), which effectively ensures precise control of the feeder voltage at its rated value and enhances the voltage compensation capability in TPSS. Unified power quality conditioner (UPQC) [26] [27] consists of series and parallel compensation units, therefore providing coordinated management of both voltage-related and current-related power quality issues in TPSS. Nevertheless, the DVR is connected to the TPSS in an external series manner, while the UPQC adopts an external series-parallel connection. These configurations require additional step-down or series transformers, which leads to a dispersed system architecture with a large footprint, higher installation costs, and complex coordination strategies [28].

The transformer-integrated TPSS solution can reduce the reliance on step-down and series transformers, improving the system integration level and space utilization. A traction system integrates the multiplexed back-to-back converter into the β side flexible taps of the Ynvd transformer connection, rather than using the auxiliary transformers, thereby reducing installation space and initial cost [29]. However, most existing transformer-integrated solutions lack the functionality of series voltage injection, and their flexible voltage control capabilities are also limited.

This paper proposes a hybrid V/V transformer-integrated traction power supply system (HVV-TPSS) topology and its comprehensive control strategy. The hybrid V/V transformer is formed by connecting the α side and the β side hybrid transformer (HT) units in a VV connection. Unlike conventional approaches, HVV-TPSS integrates parallel and series converters into the three-limb four-winding transformers for current and voltage compensation, without the need for step-down and series transformers. This innovation significantly simplifies the system architecture. The main contributions of this paper are summarized as follows:

- 1) With a comprehensive power quality compensation capability, HVV-TPSS not only addresses power quality issues such as NSC and reactive power but also enhances voltage control capability for terminal traction loads. In addition, it eliminates the need for bulky step-down and series transformers, thereby improving space utilization and its system integration level.
- 2) Building on the current and voltage compensation mechanisms of the HT units, this paper establishes the comprehensive operational and control principles capable of ensuring

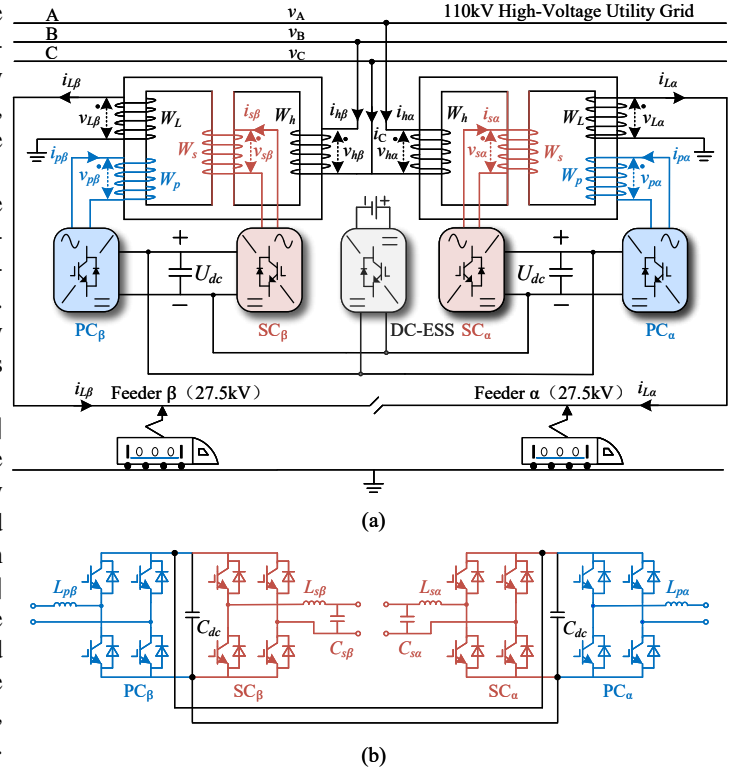


Fig. 1. Topology structure of the proposed HVV-TPSS. (a) Schematic diagram of the HVV-TPSS. (b) The converters and output filters of the HVV-TPSS.

robust current and voltage compensation in steady-state and grid fault scenarios.

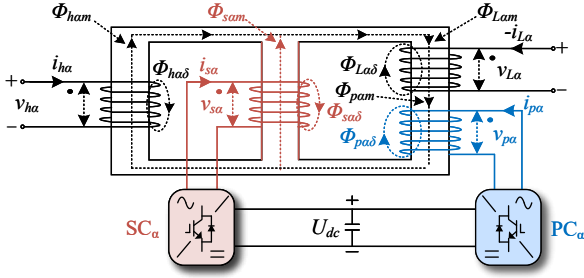
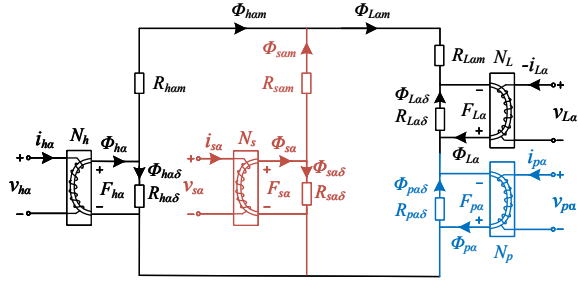
3) A comprehensive control strategy for the HVV-TPSS is proposed to realize current-related and voltage-related power quality coordinated compensation.

4) The proposed HVV-TPSS and its comprehensive control strategy are validated through simulations and scaled-down prototype experiments, demonstrating their feasibility and effectiveness under various operating conditions.

II. TOPOLOGY OF THE PROPOSED HVV-TPSS

Fig. 1 shows the topology of the proposed HVV-TPSS. The HVV-TPSS mainly comprises the hybrid V/V transformer, the traction network, and the DC energy storage system (DC-ESS). The hybrid V/V transformer is supplied by the 110 kV utility grid and delivers power to the 27.5 kV feeders α and β . Notably, the hybrid V/V transformer is formed by connecting the α and β side HT units in a VV connection. Each HT is constructed through the hybrid interconnection of a modified three-limb four-winding low-frequency transformer (LFT), a series converter (SC) and a parallel converter (PC), with both the SC and PC employing single-phase H-bridge converter.

In Fig. 1, the single-phase three-limb four-winding LFT consists of a three-limb transformer core, high-voltage winding W_h , low-voltage winding W_L , series winding W_s and parallel winding W_p . The two high-voltage windings are connected across the A-C and B-C phase-to-phase lines of the utility grid. The two low-voltage windings are connected to the two feeders. Each series winding connects to the AC side of its corresponding SC_y via an LC filter comprising the inductor

Fig. 2. Magnetic flux distribution of the α side HT unit.Fig. 3. Equivalent electric-magnetic circuit model of the α side HT unit.

L_{sy} and the capacitor C_{sy} ($y = \alpha, \beta$), while each parallel winding connects to the AC side of its corresponding PC_y through a filter inductor L_{py} . Moreover, the DC sides of the SCs and PCs in the two HT units are coupled back-to-back with DC capacitors C_{dc} and collectively interface with the DC-ESS through a bidirectional DC/DC converter.

As shown in Fig. 1, v_{xy} and i_{xy} ($x = h, L, s, p$, and $y = \alpha, \beta$) denote the voltage and current of the high-voltage, low-voltage, series, and parallel windings on the α and β sides, respectively. $P_{L\alpha} + jQ_{L\alpha}$ and $P_{L\beta} + jQ_{L\beta}$ represent the powers of the feeders α and β , respectively. U_{dc} refers to the voltage on the DC side.

III. OPERATING PRINCIPLE OF THE HVV-TPSS

A. Comprehensive compensation principle of the HT units

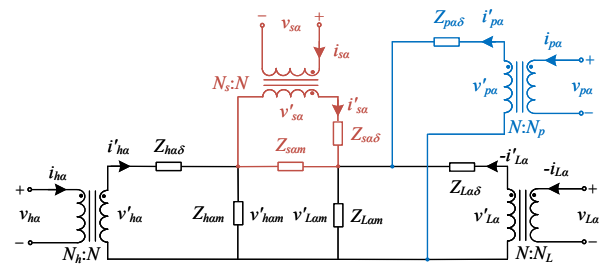
Taking the α side as an example, Fig. 2 shows the magnetic circuit structure of the single-phase HT unit, with the reference direction of the physical quantities. In Fig. 2, the number of turns in the corresponding winding is denoted by N_x ($x = h, L, s, p$). $\Phi_{x\alpha m}$ and $\Phi_{x\alpha\delta}$ denote the main magnetic flux and the leakage flux of each α side winding. The following analysis follows the methodology in [30].

Based on Fig. 2, the equivalent electric-magnetic circuit model of the α side HT is established in Fig. 3, where $R_{h\alpha m}$, $R_{s\alpha m}$ and $R_{L\alpha m}$ denote the magnetic resistances of the left, middle and right iron cores, respectively; $F_{x\alpha}$ and $R_{x\alpha\delta}$ denote the magnetomotive force and leakage magnetic resistance of each winding on the α -side, respectively.

In Fig. 3, Kirchhoff's first and second laws for magnetic circuits can be applied to the α side HT unit, and yields:

$$\Phi_{h\alpha m} + \Phi_{s\alpha m} = \Phi_{L\alpha m} = \Phi_{p\alpha m} \quad (1)$$

$$\begin{cases} F_{h\alpha} - F_{s\alpha} = \Phi_{h\alpha m} R_{h\alpha m} - \Phi_{s\alpha m} R_{s\alpha m} \\ F_{L\alpha} + F_{s\alpha} + F_{p\alpha} = \Phi_{L\alpha m} R_{L\alpha m} + \Phi_{s\alpha m} R_{s\alpha m} \end{cases} \quad (2)$$

Fig. 4. Equivalent circuit of the α side HT unit.

where,

$$\begin{cases} F_{h\alpha} = N_h i_{h\alpha}, F_{s\alpha} = N_s i_{s\alpha}, F_{p\alpha} = N_p i_{p\alpha} \\ F_{L\alpha} = -N_L i_{L\alpha} \end{cases} \quad (3)$$

As illustrated in Fig. 3, the total magnetic flux for each the α side winding $\Phi_{x\alpha}$ is composed of the main magnetic flux and the leakage flux:

$$\begin{cases} \Phi_{h\alpha} = \Phi_{h\alpha m} + \Phi_{h\alpha\delta} \\ \Phi_{L\alpha} = \Phi_{L\alpha m} + \Phi_{L\alpha\delta} \\ \Phi_{s\alpha} = \Phi_{L\alpha m} - \Phi_{h\alpha m} + \Phi_{s\alpha\delta} \\ \Phi_{p\alpha} = \Phi_{L\alpha m} + \Phi_{p\alpha\delta} \end{cases} \quad (4)$$

Using Ohm's law for magnetic circuits and the principle of balanced magnetomotive force at nodes, the magnetic force-resistance-flux relationship of the α side HT can be derived in (5) at the top of the next page.

Here, to simplify the analysis, we define that $i'_{x\alpha}$ is the winding current when the number of turns N_x is normalized to the number of turns N . Meanwhile, according to Faraday's law of electromagnetic induction, the relationship between the normalized voltage v' and the magnetic flux Φ is given by:

$$v' = N \frac{d\Phi}{dt} = j\omega N \Phi \quad (6)$$

The inductance of each magnetic circuit branch can be derived from the magnetic reluctance and winding turns:

$$L = N^2 / R \quad (7)$$

From (3)–(7), the equivalent circuit mathematical model of the α side HT can be obtained as shown in (8) at the top of the next page, where $Z = j\omega L$ is the inductive reactance.

From (8), the equivalent circuit of the α -side HT can be derived as shown in Fig. 4. Considering that the winding leakage reluctance is far greater than that of the core, the magnetic reluctance of the iron core can be neglected. Therefore, the magnetizing branch impedance can be disregarded, meaning that the branches $Z_{h\alpha m}$, $Z_{s\alpha m}$ and $Z_{L\alpha m}$ as shown in Fig. 4 are disconnected. Then, from (1)–(8) and Fig. 4, we obtain:

$$v_{h\alpha}/N_h + v_{s\alpha}/N_s = v_{L\alpha}/N_L = v_{p\alpha}/N_p \quad (9)$$

$$N_h i_{h\alpha} = N_s i_{s\alpha} = N_L i_{L\alpha} - N_p i_{p\alpha} \quad (10)$$

Therefore, the following inferences can be derived from (9) and (10). Under steady-state conditions, the two PCs can regulate parallel windings to provide current compensation, effectively guaranteeing high power quality operation of the TPSS. Under grid voltage fault conditions, the two SCs can control series windings to inject compensating voltages, thereby realizing rapid voltage support for the two feeders and securing the highly reliable operation of the traction system.

$$\begin{cases} F_{h\alpha} = -R_{h\alpha\delta}\Phi_{h\alpha m} + R_{h\alpha\delta}\Phi_{h\alpha} \\ -R_{s\alpha\delta}\Phi_{s\alpha} + (R_{s\alpha\delta} + R_{L\alpha\delta} + R_{p\alpha\delta} + R_{s\alpha m} + R_{L\alpha m})\Phi_{L\alpha m} - R_{L\alpha\delta}\Phi_{L\alpha} - (R_{s\alpha\delta} + R_{s\alpha m})\Phi_{h\alpha m} - R_{p\alpha\delta}\Phi_{p\alpha} = 0 \\ F_{L\alpha} = -R_{L\alpha\delta}\Phi_{L\alpha m} + R_{L\alpha\delta}\Phi_{L\alpha} \\ R_{s\alpha\delta}\Phi_{s\alpha} - (R_{s\alpha\delta} + R_{s\alpha m})\Phi_{L\alpha m} + (R_{s\alpha\delta} + R_{h\alpha\delta} + R_{s\alpha m} + R_{h\alpha m})\Phi_{h\alpha m} - R_{h\alpha\delta}\Phi_{h\alpha} = 0 \\ F_{s\alpha} = R_{s\alpha\delta}\Phi_{s\alpha} - R_{s\alpha\delta}\Phi_{L\alpha m} + R_{s\alpha\delta}\Phi_{h\alpha m} \\ F_{p\alpha} = -R_{p\alpha\delta}\Phi_{L\alpha m} + R_{p\alpha\delta}\Phi_{p\alpha} \end{cases} \quad (5)$$

$$\begin{cases} i'_{h\alpha} = -\frac{1}{Z_{h\alpha\delta}}v'_{h\alpha m} + \frac{1}{Z_{h\alpha\delta}}v'_{h\alpha} \\ -\frac{1}{Z_{s\alpha\delta}}v'_{s\alpha} + (\frac{1}{Z_{s\alpha\delta}} + \frac{1}{Z_{L\alpha\delta}} + \frac{1}{Z_{p\alpha\delta}} + \frac{1}{Z_{s\alpha m}} + \frac{1}{Z_{L\alpha m}})v'_{L\alpha m} - \frac{1}{Z_{L\alpha\delta}}v'_{L\alpha} - (\frac{1}{Z_{s\alpha\delta}} + \frac{1}{Z_{s\alpha m}})v'_{h\alpha m} - \frac{1}{Z_{p\alpha\delta}}v'_{p\alpha} = 0 \\ i'_{L\alpha} = \frac{1}{Z_{L\alpha\delta}}v'_{L\alpha m} - \frac{1}{Z_{L\alpha\delta}}v'_{L\alpha} \\ \frac{1}{Z_{s\alpha\delta}}v'_{s\alpha} - (\frac{1}{Z_{s\alpha\delta}} + \frac{1}{Z_{s\alpha m}})v'_{L\alpha m} + (\frac{1}{Z_{s\alpha\delta}} + \frac{1}{Z_{h\alpha\delta}} + \frac{1}{Z_{s\alpha m}} + \frac{1}{Z_{h\alpha m}})v'_{h\alpha m} - \frac{1}{Z_{h\alpha\delta}}v'_{h\alpha} = 0 \\ i'_{s\alpha} = \frac{1}{Z_{s\alpha\delta}}v'_{s\alpha} - \frac{1}{Z_{s\alpha\delta}}v'_{L\alpha m} + \frac{1}{Z_{s\alpha\delta}}v'_{h\alpha m} \\ i'_{p\alpha} = -\frac{1}{Z_{p\alpha\delta}}v'_{L\alpha m} + \frac{1}{Z_{p\alpha\delta}}v'_{p\alpha} \end{cases} \quad (8)$$

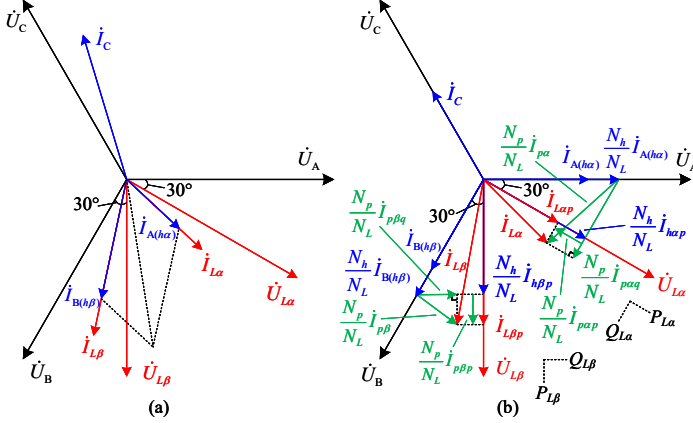


Fig. 5. Current phasor diagram with/without the PC compensation. (a) Without PC compensation. (b) With PC compensation.

Equations (9) and (10) are used for compensation-principle analysis and controller-oriented modeling, not for detailed nonlinear magnetic transient analysis of the transformer core. Under the designed operating range, the HT works in the linear or near-linear magnetic region. In this region, the magnetizing branch is a high-impedance shunt branch, and the magnetizing current of a properly designed power transformer is normally very small, typically below about 1% of the rated current [31]–[33]. Therefore, compared with the load current and the converter compensation current, the excitation current has a limited influence on the voltage/current relationship.

B. Parallel current compensation principle of the HVV-TPSS

By controlling the compensation current injected by PCs, HVV-TPSS can address current-related power quality issues such as NSC and reactive power. The parallel current compensation by PCs of the HVV-TPSS is shown in Fig. 5.

In Fig. 5, taking the phase A voltage of the grid as the reference phasor, the three-phase grid voltage phasors are represented as $\dot{U}_A$, $\dot{U}_B$, and $\dot{U}_C$, where U denotes the root-mean-square (RMS) value of the grid phase voltage in steady state.

In Fig. 5(a), under steady-state conditions and with load harmonics neglected, the current characteristic of the HVV-TPSS can be expressed in (11) without PC compensation.

$$\begin{bmatrix} I_+ \\ I_- \end{bmatrix} = \frac{1}{3} \begin{bmatrix} 1 & a & a^2 \\ 1 & a^2 & a \end{bmatrix} \begin{bmatrix} I_A \\ I_B \\ I_C \end{bmatrix} = \frac{\sqrt{3}}{3K} \begin{bmatrix} e^{j30^\circ} & e^{j90^\circ} \\ e^{-j30^\circ} & e^{-j90^\circ} \end{bmatrix} \begin{bmatrix} I_{L\alpha} \\ I_{L\beta} \end{bmatrix} \quad (11)$$

where the turn ratio between the high-voltage and low-voltage windings is $K = N_h/N_L$, and $a = e^{j120^\circ}$ is the rotation operator. I_A , I_B and I_C denote the RMS values of the three-phase grid currents. I_+ and I_- represent the RMS values of the positive-sequence and negative-sequence components of the grid current, respectively.

The current unbalance factor of the grid can be defined as:

$$\varepsilon = \frac{|I_-|}{|I_+|} \times 100\% \quad (12)$$

As shown in Fig. 5(b), once the two PCs are put into operation, the current phasor relationship of the HVV-TPSS can be derived from (10):

$$\dot{I}_{Ly} = \frac{N_h}{N_L} \dot{I}_{hy} + \frac{N_p}{N_L} \dot{I}_{py} \quad (13)$$

Given the continuous fluctuations of the two traction feeder currents, we assume $I_{L\beta} > I_{L\alpha}$. As illustrated in Fig. 5(b), the active current components satisfy the following relationship:

$$\dot{I}_{Lyp} = \frac{N_h}{N_L} \dot{I}_{hy p} + \frac{N_p}{N_L} \dot{I}_{py p} \quad (14)$$

where $\dot{I}_{Lyp}$ denotes the active current phasor of the two feeders, and $\dot{I}_{hy p}$ indicates the active current phasor of the two high-voltage windings. $\dot{I}_{py p}$ is the active compensation current phasor provided by the two PCs.

For active current compensation, the RMS values of the active current components injected by the two PCs are supposed to be equal to balance $I_{h\alpha p}$ and $I_{h\beta p}$, and yields:

$$I_{p\alpha p} = I_{p\beta p} = \frac{N_L}{N_p} \left| \frac{I_{L\alpha p} - I_{L\beta p}}{2} \right| \quad (15)$$

Therefore, the two PCs will also achieve a balanced transfer of active power between the two feeders. The transferred active powers of the two PCs are given by:

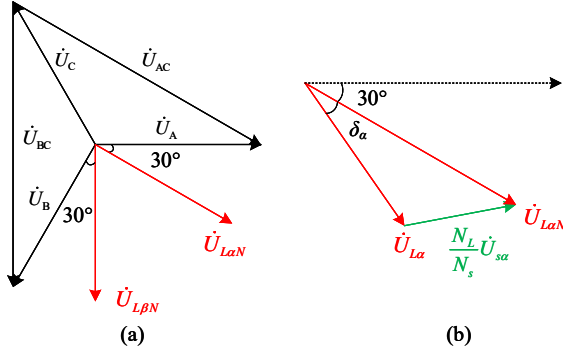


Fig. 6. Voltage phasor diagram with the SC compensation. (a) Without grid voltage fault. (b) α side SC voltage compensation under a general grid voltage fault.

$$P_{p\alpha} = -P_{p\beta} = \frac{P_{L\alpha} - P_{L\beta}}{2} \quad (16)$$

It can be derived from (14)–(16) that the active power outputs from the two high-voltage windings of the hybrid V/V transformer are equal and expressed as:

$$\begin{cases} P_{h\alpha} = P_{L\alpha} - P_{p\alpha} = \frac{P_{L\alpha} + P_{L\beta}}{2} \\ P_{h\beta} = P_{L\beta} - P_{p\beta} = \frac{P_{L\alpha} + P_{L\beta}}{2} \end{cases} \quad (17)$$

In Fig. 5(b), for reactive current compensation, the reactive current phasor components of the two PCs are derived as:

$$\begin{cases} \dot{I}_{p\alpha q} = \frac{N_L}{N_p} \dot{I}_{L\alpha q} + \frac{N_h}{N_p} \dot{I}_{h\alpha p} \tan 30^\circ \\ \dot{I}_{p\beta q} = \frac{N_L}{N_p} \dot{I}_{L\beta q} - \frac{N_h}{N_p} \dot{I}_{h\beta p} \tan 30^\circ \end{cases} \quad (18)$$

where $I_{L\alpha q}$ and $I_{L\beta q}$ are the reactive current phasor components of the feeder α and β , respectively.

Meanwhile, from (17) and (18), the reactive power outputs of the two high-voltage windings can be obtained:

$$Q_{h\alpha} = -Q_{h\beta} = -\frac{P_{L\alpha} + P_{L\beta}}{2} \tan 30^\circ \quad (19)$$

As a result, from (18) and (19), the reactive powers that the two PCs should compensate are given by:

$$\begin{cases} Q_{p\alpha} = Q_{L\alpha} + \frac{P_{L\alpha} + P_{L\beta}}{2} \tan 30^\circ \\ Q_{p\beta} = Q_{L\beta} - \frac{P_{L\alpha} + P_{L\beta}}{2} \tan 30^\circ \end{cases} \quad (20)$$

By employing PCs for current compensation, the three-phase grid current becomes symmetric and in phase with the grid voltage. At this point, the grid current unbalance factor approaches zero, and the power factor (PF) on the grid side is significantly improved, thereby resolving the power quality issues induced by NSC and reactive power.

C. Series voltage compensation principle of the HVV-TPSS

Through series voltage compensation, HVV-TPSS can swiftly compensate for grid fault voltage. The series voltage compensation by SCs of the HVV-TPSS is shown in Fig. 6.

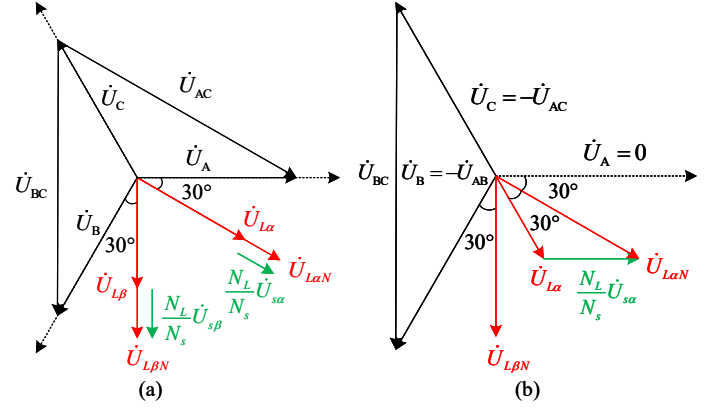


Fig. 7. SC voltage compensation phasor diagram. (a) Three-phase voltage sag of the grid. (b) Phase A SLG on the grid side.

In Fig. 6(a), under steady-state operating conditions before considering the voltage harmonics, the voltage phasors for the two feeders are given by:

$$\begin{cases} \dot{U}_{LaN} = \frac{1}{K} \dot{U}_{AC} = \frac{\sqrt{3}}{K} U e^{-j30^\circ} \\ \dot{U}_{L\beta N} = \frac{1}{K} \dot{U}_{BC} = \frac{\sqrt{3}}{K} U e^{-j90^\circ} \end{cases} \quad (21)$$

Under fault conditions, asymmetric voltage faults may lead to differing compensation voltages output by the two SCs. The voltage deviations for the two feeders are defined as follows.

$$dU_y = U_{Ly} / U_{LyN} \quad (22)$$

where U_{Ly} represents the RMS value of the feeder fault voltage without series voltage compensation. U_{LyN} denotes the rated RMS voltage of the feeders, while the phase shift angles between the fault voltage and the rated voltage of the feeders α and β are denoted as δ_α and δ_β , respectively.

During grid voltage faults, the two SCs generate compensation voltages with appropriate phase and magnitude for both feeders, maintaining the voltages of the traction loads at their rated level. In Fig. 6(b), in the case of a general grid voltage fault, taking the α side as an example, the voltage relationship of the HVV-TPSS can be obtained as follows:

$$\dot{U}_{LaN} = \dot{U}_{La} + \frac{N_L}{N_s} \dot{U}_{s\alpha} \quad (23)$$

$$U_{s\alpha} = \frac{N_s U_{LaN}}{N_L} \sqrt{dU_\alpha^2 - 2dU_\alpha \cos \delta_\alpha + 1} \quad (24)$$

In the case of $dU_\alpha \neq 0$, it follows that:

$$\delta_\alpha = \arccos \left(\frac{dU_\alpha^2 + 1}{2dU_\alpha} - \frac{N_L^2 U_{s\alpha}^2}{2N_s^2 U_{LaN}^2 dU_\alpha} \right) \quad (25)$$

During a three-phase symmetric voltage sag in the grid, the voltage fault phase shift angles of the feeders α and β are uniformly zero. The corresponding voltage compensation phasor diagram is depicted in Fig. 7(a). Under this condition, according to (24), the following relationship holds:

$$U_{sy} = \frac{N_s U_{LyN} (1 - dU_y)}{N_L} \quad (26)$$

During a phase A single line-to-ground (SLG) fault on the grid side, the voltage compensation phasor diagram is shown

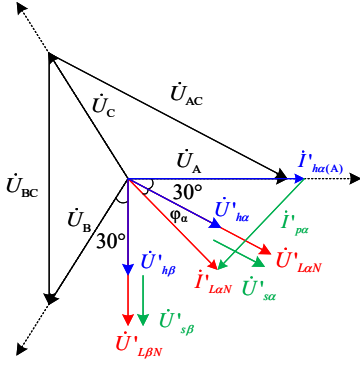


Fig. 8. Reduced phasor diagram of the windings on the α side under three-phase symmetric voltage sag condition.

in Fig. 7(b), where the shift angle for the feeder α is 30° .

From (21) – (25), a grid side phase A SLG fault results in:

$$\begin{cases} \dot{U}_{L\alpha} = \frac{1}{K} \dot{U}_{AC} = \frac{1}{K} U e^{-j60^\circ} \\ \dot{U}_{L\beta} = \frac{1}{K} \dot{U}_{BC} = \frac{\sqrt{3}}{K} U e^{-j90^\circ} \end{cases} \quad (27)$$

$$U_{s\alpha} = \frac{\sqrt{3} N_s U_{L\alpha N}}{3 N_L} \quad (28)$$

During this fault, the grid voltage of phase A drops to zero. As a result, the line voltages $\dot{U}_{AB}$ and $\dot{U}_{AC}$ are affected, while $\dot{U}_{BC}$ remains unaffected. Consequently, SC_α promptly injects a dynamic compensation voltage via the series winding, allowing continuous operation in the rated state for feeder α .

D. Operational safety constraints of the HVV-TPSS

Taking grid three-phase symmetric voltage sag as an example, this paper analyzes the operational safety constraints of the HVV-TPSS. For analytical simplicity, the voltages and currents in (9) and (10) on the α side are represented as phasors after reduction to the winding N :

$$\begin{cases} \dot{U}'_{h\alpha} + \dot{U}'_{s\alpha} = \dot{U}'_{L\alpha} = \dot{U}'_{p\alpha} \\ \dot{I}'_{h\alpha} = \dot{I}'_{s\alpha} = \dot{I}'_{L\alpha} = \dot{I}'_{p\alpha} \end{cases} \quad (29)$$

where $\dot{U}'_{x\alpha}$ and $\dot{I}'_{x\alpha}$ represent the voltage and current phasors of each winding on the α side, reduced to winding N .

Under grid three-phase symmetric voltage fault condition, the reduced phasor relationships among the windings of the α side HT are illustrated in Fig. 8. Then, from (21) to (26), it follows that:

$$dU_\alpha = \frac{U'_{h\alpha}}{U'_{L\alpha N}} \quad (30)$$

$$\begin{cases} U'_{ha} = U'_{L\alpha N} dU_\alpha \\ U'_{L\alpha} = U'_{L\alpha N} \\ U'_{sa} = U'_{L\alpha N} (1 - dU_\alpha) \\ U'_{pa} = U'_{L\alpha N} \end{cases} \quad (31)$$

During normal operation, according to (29), the compensation voltage of the α -side series winding shall not exceed its maximum withstand voltage $U'_{s\alpha, max}$ to satisfy the voltage constraint. Consequently, we obtain:

$$U'_{s\alpha} = U'_{L\alpha N} (1 - dU_\alpha) \leq U'_{s\alpha, max} \quad (32)$$

It is assumed that the power factor angles of the traction loads are constant and consistent. The power factor angle of the α side is denoted as φ_α . The rated active power of the feeders are given by:

$$P_{L\alpha N} = P_{L\beta N} = U'_{L\alpha N} I'_{L\alpha N} \cos \varphi_\alpha \quad (33)$$

where $P_{L\alpha N}$, $I'_{L\alpha N}$ and $U'_{L\alpha N}$ represent the α -side rated load active power, rated load current and rated load voltage, respectively. $P_{L\beta N}$ denotes the β -side rated load active power.

According to (29), when a voltage fault occurs in the system, regarding the current constraints, the high-voltage winding and series winding currents, denoted as $I'_{h\alpha}$ and $I'_{s\alpha}$, may exceed their safety limits. Accordingly, ensuring that these currents do not exceed their maximum allowable values $I'_{h\alpha, s\alpha, max}$ is one of the core operational safety constraints for the system.

From (17), (30) and Fig. 8, when both feeders reach their rated power, we obtain:

$$I'_{h\alpha} = I'_{s\alpha} = \frac{\sqrt{3}(P_{L\alpha N} + P_{L\beta N})}{3U'_{h\alpha}} = \frac{2\sqrt{3}P_{L\alpha N}}{3U'_{h\alpha}} \quad (34)$$

$$I'_{h\alpha} = I'_{s\alpha} = \frac{2\sqrt{3}I'_{L\alpha N} \cos \varphi_\alpha}{3dU_\alpha} \leq I'_{h\alpha, s\alpha, max} \quad (35)$$

The parallel converters are responsible for performing the current compensation, and the current of the α -side parallel winding, denoted as $I'_{p\alpha}$, is also at risk of exceeding its safety limit. It is therefore critical to ensure that this current does not exceed its maximum allowable value $I'_{p\alpha, max}$. From Fig. 8, we can obtain the constraint condition for $I'_{p\alpha}$, which can be expressed in (36) at the top of next page.

In summary, when a voltage fault occurs, it is necessary to ensure that the voltage of the series winding and the currents in the high-voltage winding, series winding, and parallel winding do not exceed their maximum limits, thus defining the safe operating constraints of the system.

IV. COMPREHENSIVE CONTROL STRATEGY OF THE HVV-TPSS

A. Comprehensive control principle of the HVV-TPSS

To achieve current and voltage related power quality control for the HVV-TPSS, this study proposes a comprehensive control strategy. This strategy employs indirect control: the two PCs function as controlled current sources, while the two SCs operate as controlled voltage sources. Fig. 9 shows the block diagram of the comprehensive control strategy. In addition, the proposed HVV-TPSS can effectively address harmonic problems in both the voltage and current domains via a multi-resonant quasi-proportional-resonant (QPR) control strategy [34]. Specifically, the series converters mitigate voltage harmonics through series voltage harmonics compensation, while the parallel converters suppress current harmonics through parallel current harmonics compensation.

B. Current control strategy for parallel converters

After current compensation by the two PCs, the phase current waveform of the high-voltage grid should be sinu-

$$I'_{p\alpha} = I'_{L\alpha N} \sqrt{1 - \frac{4\sqrt{3} \cos \varphi_{\alpha} \cos(\varphi_{\alpha} + 30^{\circ})}{3dU_{\alpha}} + \left(\frac{2\sqrt{3} \cos \varphi_{\alpha}}{3dU_{\alpha}}\right)^2} \leq I'_{p\alpha, \max} \quad (36)$$

soidal, balanced, and in phase with the positive-sequence phase voltage, while specifically satisfying the following conditions:

$$\begin{cases} i_A = \sqrt{2}I_+ \sin(\omega t) \\ i_B = \sqrt{2}I_+ \sin(\omega t - \frac{2\pi}{3}) \\ i_C = \sqrt{2}I_+ \sin(\omega t + \frac{2\pi}{3}) \end{cases} \quad (37)$$

where ωt is the grid phase voltage phase-locked angle provided by the decoupled double synchronous reference frame phase-locked loop (DSRF-PLL).

At this time, the high-voltage grid should solely produce active power between the positive-sequence voltage and the positive-sequence current. Consequently, I_+ can be determined from the positive-sequence voltage of the grid and the total power of the two traction feeders as follows:

$$I_+ = \frac{(P_{L\alpha} + P_{L\beta})}{3U_+} \quad (38)$$

where U_+ represents the RMS value of the positive-sequence phase voltage of the grid.

The reference values of active power required for the two traction feeders are given by:

$$P_{Lyref} = \frac{1}{2}(i_{Ly}v_{Ly} + i_{Ly}^D v_{Ly}^D) \quad (39)$$

where v_{Ly}^D and i_{Ly}^D denote the voltage and current 90-degree phase delayed values of the feeders α and β , respectively.

After current compensation by PCs, the d -axis positive-sequence voltage of the high-voltage grid, denoted as v_{gd+} , can be obtained using the dq coordinate transformation and a double frequency notch filter. Subsequently, the reference value of the d -axis positive-sequence current of the high-voltage grid, denoted by I_{gd+} , can then be derived as follows:

$$I_{gd+} = \frac{2(P_{L\alpha ref} + P_{L\beta ref})}{3v_{gd+}} \quad (40)$$

Thus, the reference current values for the two traction feeders, $i_{L\alpha ref}$ and $i_{L\beta ref}$, can be obtained as follows:

$$\begin{cases} i_{L\alpha ref} = \frac{N_h I_{gd+}}{N_L} \sin(\omega t) \\ i_{L\beta ref} = \frac{N_h I_{gd+}}{N_L} \sin\left(\omega t - \frac{2}{3}\pi\right) \end{cases} \quad (41)$$

The QPR controller can incorporate multiple resonant terms in parallel to achieve synchronous tracking and compensation of multiple specific-order harmonic components. The transfer function of QPR controller is given as follows:

$$G_{QPR}(s) = k_p + \sum_{h=1,3,5,7,\dots} \frac{2k_{r,h}\omega_c s}{s^2 + 2\omega_c s + (h\omega_0)^2} \quad (42)$$

where k_p and $k_{r,h}$ denote the proportional gain and resonant gain at each resonant frequency, respectively, with ω_0 and ω_c representing the resonant and cutoff frequency.

Then, considering load harmonics, based on (10), the reference compensation currents for the two PCs are given by:

$$i_{pyref} = \frac{N_L}{N_p} \left(i_{Ly} + \sum_{h=3,5,7,\dots} i_{Lyh} - i_{Lyref} \right) \quad (43)$$

where i_{Ly} and i_{Lyh} denote the fundamental current and the h -th order harmonic current of the traction load, respectively.

The QPR control in the stationary coordinate system obviates the need for intricate coordinate transformations and cross-coupling procedures, notably diminishing computational intricacy. The two PCs employ QPR controllers to guarantee dynamic and accurate tracking control of AC compensation current signals from (43).

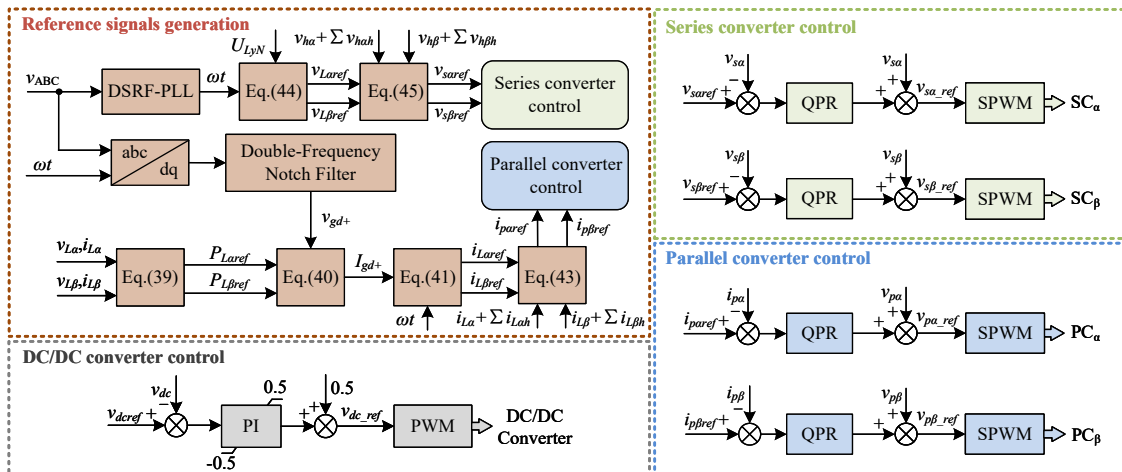


Fig. 9. Block diagram of the comprehensive control strategy for the proposed HVV-TPSS.

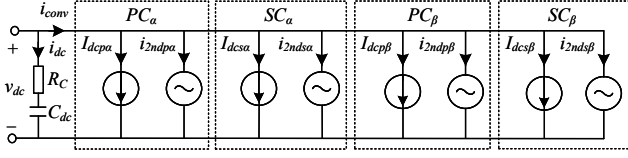


Fig. 10. DC-side simplified schematic diagram of the four converters of HVV-TPSS.

C. Voltage control strategy for series converters

The reference voltage values for the two traction feeders of the HVV-TPSS are as follows:

$$\begin{cases} v_{L\alpha ref} = \sqrt{2}U_{LyN} \sin\left(\omega t - \frac{\pi}{6}\right) \\ v_{L\beta ref} = \sqrt{2}U_{LyN} \sin\left(\omega t - \frac{\pi}{2}\right) \end{cases} \quad (44)$$

Consequently, based on (9), the output compensation voltage reference values for the two SCs are given by:

$$v_{syref} = \frac{N_s}{N_L} v_{Lyref} - \frac{N_s}{N_h} \left(v_{hy} + \sum_{h=3,5,7,\dots} v_{hyh} \right) \quad (45)$$

where v_{syref} denotes the reference compensation voltage values for the SCs. v_{hy} and v_{hyh} denote the fundamental voltage component and the h -th order harmonic voltage component on the high-voltage side of the V/V transformer, respectively.

After determining the reference compensation voltage from (45), QPR control implemented on the SCs can ensure accurate tracking of the AC voltage compensation signals. The two SCs adopted QPR controller can handle asymmetric grid voltage faults without the need for complex techniques such as sequence decomposers and positive-negative sequence controllers, thus reducing the computational burden.

D. Control strategy for the energy storage DC/DC converter

The DC control system serves to suppress DC voltage fluctuations, which is essential for the downstream converters to provide dynamic voltage and current compensation. As shown in Fig. 9, the DC control system first processes the error between the DC voltage v_{dc} and its reference value v_{dcref} through a proportional-integral (PI) controller. The resulting output signals are then normalized and fed to the PWM generator to produce modulation signals. These modulation signals are subsequently applied to the buck-derived DC/DC converter, thereby enabling bidirectional power regulation of the energy storage unit.

From the DC-side perspective, the four downstream H-bridge converters in the proposed HVV-TPSS, the two PCs and the two SCs, share the common DC bus and can be equivalent to four DC current sources $I_{dcp\alpha}$ and $I_{dcs\alpha}$ in parallel with four second harmonic current (SHC) sources $i_{2ndp\alpha}$ and $i_{2nds\alpha}$ [35], as shown in Fig. 10. Where, i_{dc} and R_C are the current and the equivalent series resistor (ESR) of the DC bus capacitor. Moreover, the downstream four single-phase converters draw an input current i_{conv} from the DC bus.

Based on Kirchhoff's current law, the superposition principle, and the power characteristics of the single-phase converter,

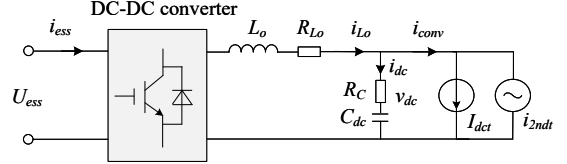


Fig. 11. Simplified schematic diagram of the equivalent DC-side model with a front-stage DC/DC converter.

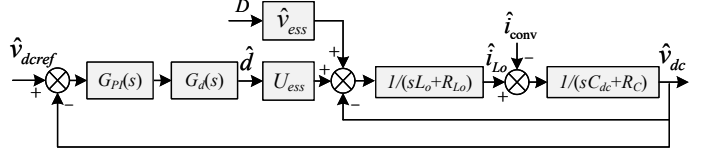


Fig. 12. Closed-loop small-signal model of the front-end buck-derived DC/DC converter.

the following equivalent relationship can be obtained:

$$I_{dct} = I_{dcp\alpha} + I_{dcs\alpha} + I_{dcp\beta} + I_{dcs\beta} \quad (46)$$

$$\dot{I}_{2ndt} = \dot{I}_{2ndp\alpha} + \dot{I}_{2nds\alpha} + \dot{I}_{2ndp\beta} + \dot{I}_{2nds\beta} \quad (47)$$

Therefore, the four single-phase converters sharing the common DC-ESS can be aggregated into an equivalent model with a front-stage DC/DC converter, as depicted in Fig. 11 [35]. Where, L_o , R_{Lo} and i_{Lo} denote the inductance, the ESR, and the inductor current of output filter inductor of the DC/DC converter, respectively. U_{ess} and i_{ess} are the voltage and current at the energy storage side.

Fig. 12 presents the closed-loop small-signal model for the front-end buck-derived DC/DC converter [35]. In this model, the transfer function of equivalent effect of delay can be denoted as $G_d(s)$. D denotes the steady-state duty cycle and $\hat{d}$ represents the small-signal perturbation of the duty cycle. The transfer function $G_{PI}(s)$ is given by:

$$G_{PI}(s) = k_p + \frac{k_i}{s} \quad (48)$$

where k_p and k_i are the proportional and integral gains of the PI regulator, respectively.

Under such conditions, the shared DC-link capacitor serves as the intermediate energy buffer among the converters. The equivalent model with a front-stage DC/DC converter is similar to the two-stage buck-converter topology described in reference [35], [36]. Therefore, after equivalently representing the four converters, the DC-bus stability can be analyzed in a manner similar to the existing DC-bus stability analyses reported in [35], [36]. The bidirectional DC/DC converter balances the power imbalance on the common DC bus, while the DC-link capacitor buffers transient energy fluctuations and partially attenuates the double-frequency ripple.

V. SIMULATION RESULTS

To validate the feasibility of the HVV-TPSS topology and its comprehensive control strategy, a HVV-TPSS simulation model depicted in Fig. 1 is constructed on the PLECS platform. The essential simulation parameters of the HVV-TPSS are outlined in TABLE I. The active and reactive traction loads are simulated by the resistors and inductors. The load

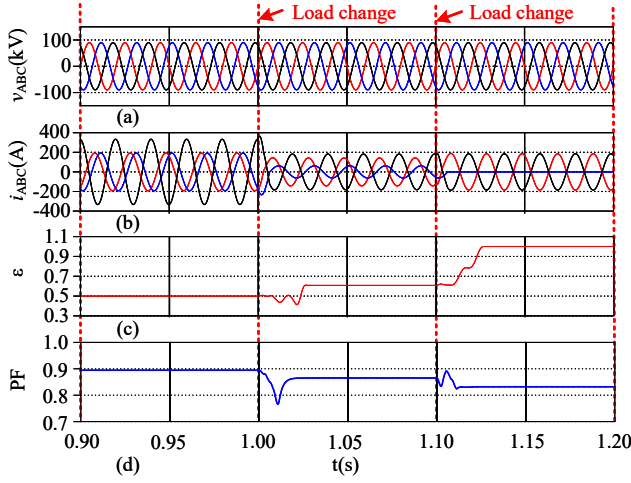


Fig. 13. Simulation results of the conventional V/V transformer-based TPSS under steady-state conditions.

harmonics are emulated by the diode bridge rectifier. The DC side is connected to the DC-ESS through a bidirectional DC/DC converter.

TABLE I
SIMULATION PARAMETERS OF THE HVV-TPSS

Parameter	Value
AC grid rated line voltage U_{hN}	110 kV
Grid frequency f_g	50 Hz
Rated capacity of the hybrid V/V transformer S_N	30 MVA
Hybrid V/V transformer turn ratio $N_h: N_s: N_p: N_L$	110:1.5:1.5:27.5
Rated voltage of feeders U_{LyN}	27.5 kV
DC side rated voltage U_{dc}	3600 V
DC side capacitance C_{dc}	40 mF
Series converter filter capacitance C_{sy}	220 μ F
Series converter filter inductance L_{sy}	0.2 mH
Parallel converter filter inductance L_{py}	0.2 mH
Switching frequency f_s	10 kHz

A. Steady-state simulations of the HVV-TPSS

To verify the current-related power quality management capability of the HVV-TPSS under steady-state conditions, steady-state simulations were performed under three typical operating cases for both the conventional V/V transformer-based TPSS and the HVV-TPSS; Case I: Symmetric load condition; Case II: Asymmetric load condition; Case III: Single-side load condition (with the other side unloaded).

The simulation conditions are specifically set as follows. From 0.9 to 1.0 s, the system operated in Case I, with both feeders carrying 13.4 MW + j6.7 Mvar rated capacity load. At 1.0 s, a load step change occurred. Subsequently, from 1.0 to 1.1 s, the system operated in Case II, where the feeder α carried a 9.6 MW + j5.6 Mvar load and the feeder β carried a 4.2 MW + j2.4 Mvar load. At 1.1 s, another load step change occurred. From 1.1 to 1.2 s, the system operated in Case III, where the feeder α carried a 12.0 MW + j8.0 Mvar load, while the feeder β was unloaded.

Fig. 13 illustrates the comparative simulation results of the conventional V/V transformer-based TPSS [37] in the three Cases. These results demonstrate varying degrees of NSC in the grid under various operating conditions, with grid current unbalance factors of 50%, 60.8% and 100%, respectively.

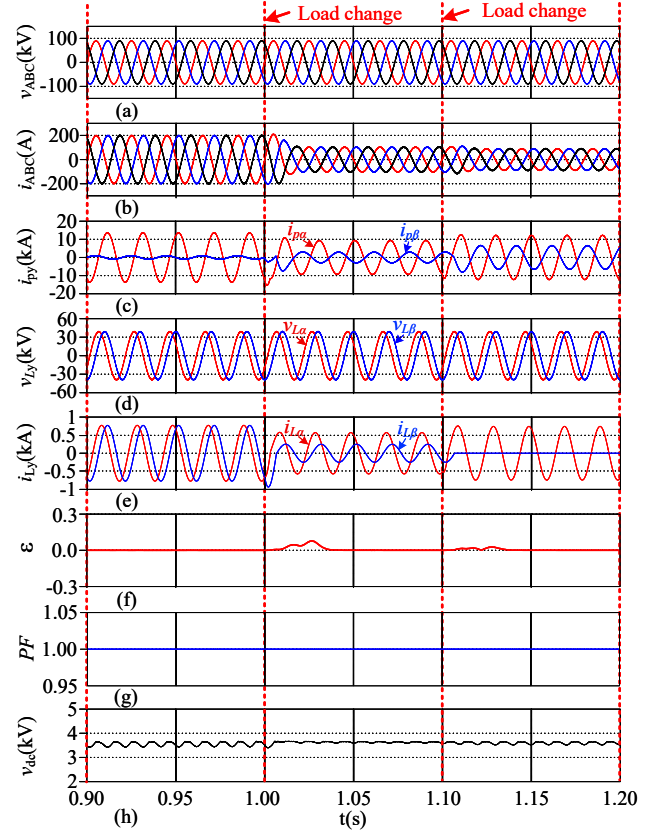


Fig. 14. Simulation results of the HVV-TPSS under steady-state conditions.

Furthermore, the system suffers from a generally unsatisfactory PF. These findings indicate that the power quality of the conventional V/V transformer-based TPSS is significantly affected by current related power quality issues.

Fig. 14 illustrates the simulation results of the proposed HVV-TPSS under steady-state conditions. Fig. 14(a)–(f) demonstrates that the three-phase grid current exhibits sinusoidal balance and remains in phase with the grid voltage through current compensation by PCs. The grid current unbalance factor is nearly zero, effectively suppressing the NSC. Fig. 14(g) indicates that the HVV-TPSS also significantly improves the grid-side PF, thereby enhancing the power supply efficiency. From Fig. 14(h), it can be seen that the DC voltage remains stable in different operating cases, demonstrating that the proposed system can effectively maintain the DC bus voltage and energy balance.

Fig. 15 presents the power simulation results of the HVV-TPSS under steady-state conditions. In Fig. 15(a), the active power transfer by the two PCs balances the active power of the two high-voltage windings, adhering to the transfer and balance relationships outlined in (16) and (17). As depicted in Fig. 15(b), the two PCs also effectively transfer and fully compensate the reactive power of the two feeders, satisfying the compensation relationships described in (19) and (20).

Moreover, an additional simulation is carried out to verify the effectiveness of the proposed strategy under the fast changing conditions where the α and β side feeders have different power factors, fast-changing locomotive power demand, and high total harmonic distortion (THD) of the current. It

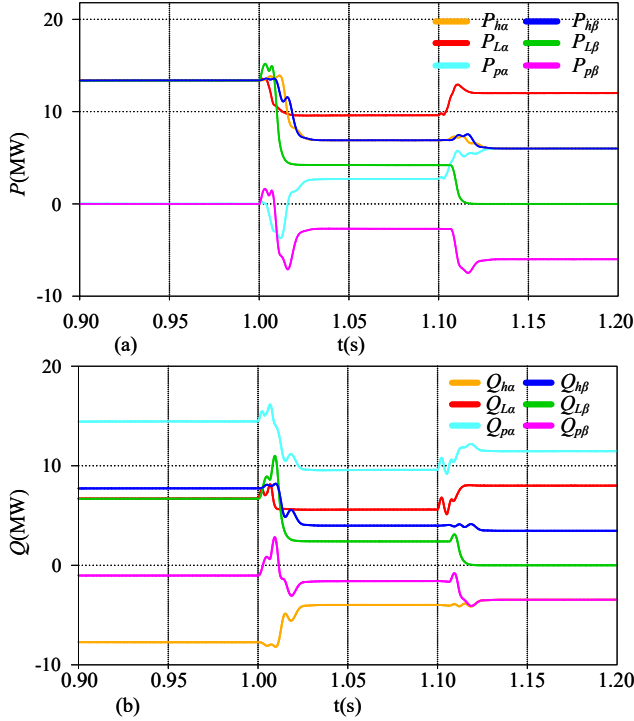


Fig. 15. Winding power simulation waveforms of the HVV-TPSS under steady-state conditions. (a) Active power. (b) Reactive power.

should be noted that under the harmonic condition, the powers mentioned below are the mean powers. Five operating states are considered in the simulation, as described below.

- State I: From 0.9 to 1.0 s, the system operated with both feeders carrying a 11.20 MW + j 4.80 Mvar load. The power factors of the α and β feeders are both 0.92, and the corresponding load current THDs are both 8.29%.
- State II: From 1.0 to 1.1 s, the feeder α carried a 8.17 MW + j 6.30 Mvar load and the feeder β carried a 5.44 MW + j 2.11 Mvar load. The power factors of the α and β feeders are 0.79 and 0.93, respectively. The corresponding load current THDs of the α and β feeders are 9.26% and 17.27%, respectively.
- State III: From 1.1 to 1.2 s, the feeder α carried a 5.44 MW + j 2.12 Mvar load and the feeder β carried a 10.03 MW + j 7.14 Mvar load. The power factors of the α and β feeders are 0.93 and 0.81, respectively. The corresponding load current THDs of the α and β feeders are 17.28% and 8.18%, respectively.
- State IV: From 1.2 to 1.3 s, the feeder α carried a 4.18 MW + j 1.34 Mvar load and the feeder β carried a 5.74 MW + j 2.40 Mvar load. The power factors of the α and β feeders are 0.95 and 0.92, respectively. The corresponding load current THDs of the α and β feeders are 22.99% and 16.19%, respectively.
- State V: From 1.3 to 1.4 s, the feeder α carried a 11.48 MW + j 6.37 Mvar load with a power factor of 0.87 and a current THD of 7.68%, while the feeder β was unloaded.

In Fig. 16 and Fig. 17, State I represents a symmetrical heavy-load condition, where both α and β feeders are heavily loaded. State II represents an asymmetric condition where the α feeder is heavily loaded and the β feeder is lightly loaded.

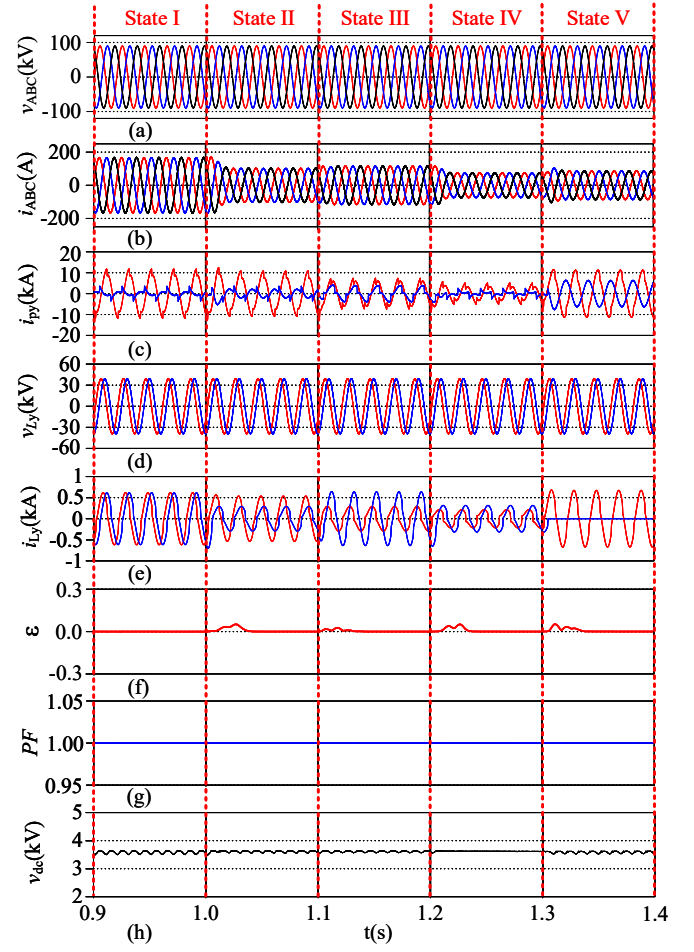


Fig. 16. Simulation results of the HVV-TPSS under the five operating States.

State III represents the opposite asymmetric condition, where the α feeder is lightly loaded and the β feeder is heavily loaded. State IV represents a light-load condition on both feeders. State V represents a single-side load condition. The five operating States changed every 0.1 s feature differentiated load power demands, with mismatched power factors between the two traction feeders under asymmetric conditions and pronounced current harmonic distortion across all scenarios.

Fig. 16 presents the simulation results of the HVV-TPSS under the five operating States, while Fig. 17 shows the winding power simulation waveforms of the HVV-TPSS under the five operating States. Under the fast-changing operating States, where the α - and β -side feeders have different power factors, rapidly varying locomotive traction-power demands, and strong current harmonics, the current-related power-quality compensation capability of the PCs remains effective. As shown in Fig. 16, the grid-side current unbalance is effectively reduced, and the grid-side power factor is improved. Although the load-current THDs are relatively high in several operating States, especially in State IV where the load-current THDs of the α and β feeders reach 22.99% and 16.19%, respectively, the compensated grid-side current THDs in the five operating States are only 0.71%, 1.13%, 0.99%, 1.51%, and 1.33%, respectively. These results indicate that the proposed HVV-TPSS effectively suppresses the propagation of load-side harmonic currents to the grid side.

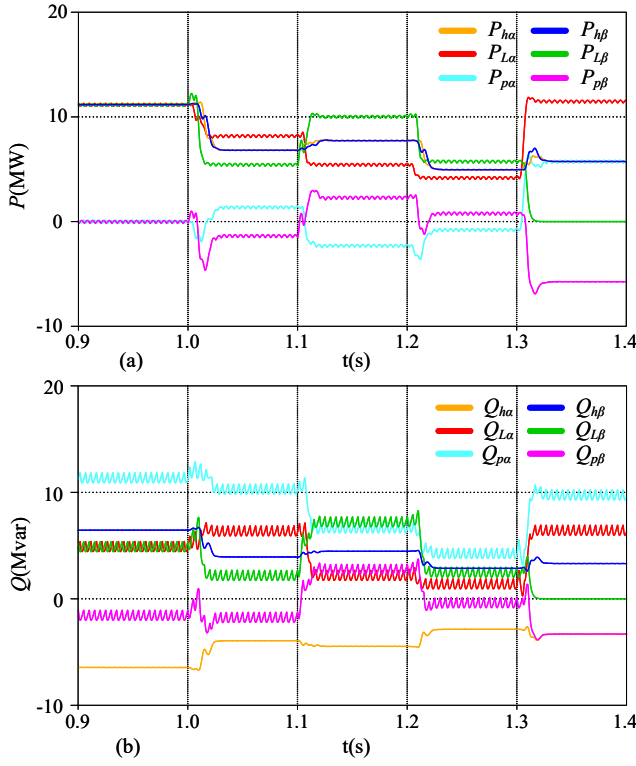


Fig. 17. Winding power simulation waveforms of the HVV-TPSS under the five operating States. (a) Active power. (b) Reactive power.

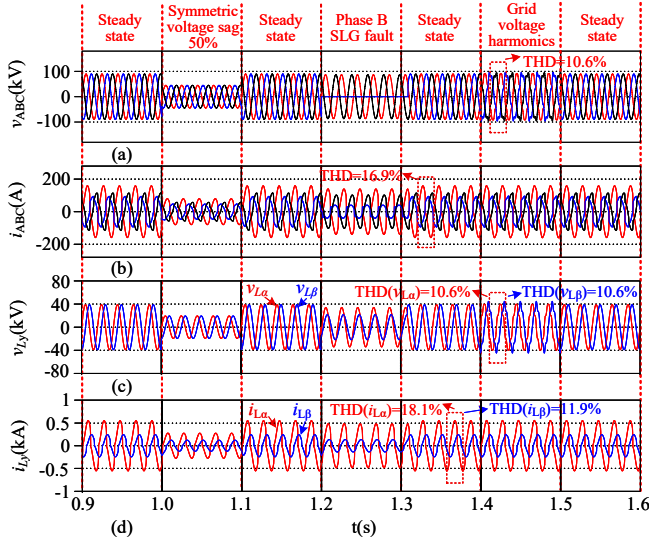


Fig. 18. Simulation results of the Scott-connected traction system under voltage fault and harmonic conditions.

Moreover, the winding-power waveforms in Fig. 17 and the quantitative mean active- and reactive-power results in Table II and Table III are consistent with the theoretical relationships in (15)–(20). Neglecting the small system losses, the mean load power, compensating power, and grid-side power satisfy the theoretical active-power balancing and reactive-power compensation requirements in all the five States. Therefore, the simulation results demonstrate that the proposed control strategy remains valid under unequal feeder power factors and different load characteristics.

Steady-state simulation results indicate that the proposed

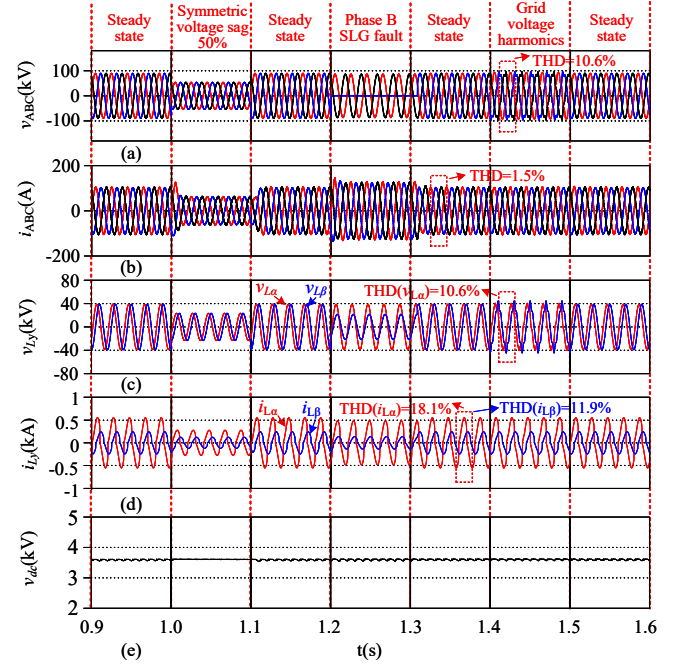


Fig. 19. Simulation results of the RPC-based traction system under voltage fault and harmonic conditions.

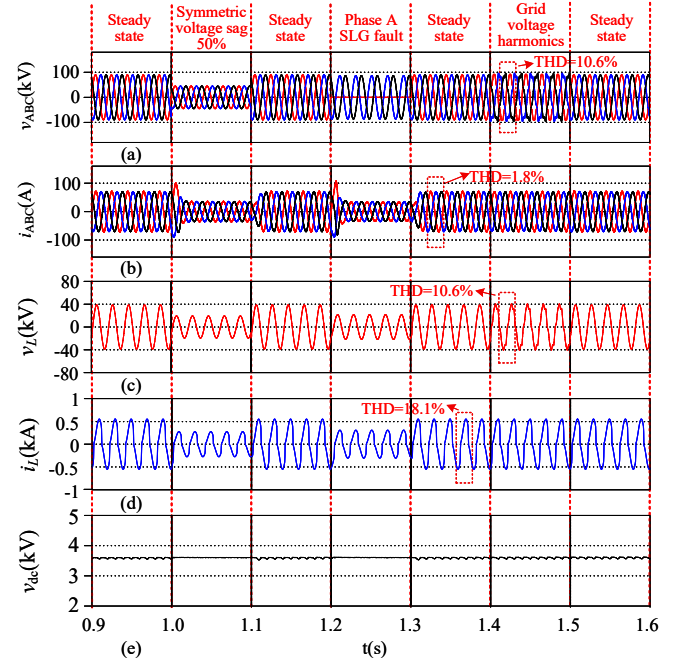


Fig. 20. Simulation results of the co-phase-connected traction system under voltage fault and harmonic conditions.

HVV-TPSS topology and its comprehensive control strategy can effectively address current-related power quality issues under various operating conditions with abrupt load changes.

B. Voltage fault and harmonic simulations of the HVV-TPSS

The simulation settings under voltage fault and harmonic conditions are as follows. From 0.9 to 1.6 s, the load powers of the feeders α and β are 9.4 MW + j5.6 Mvar and 4.4 MW + j1.8 Mvar, respectively. The THD of the load current on the α side is set to 18.1%, and that on the β side is set to

TABLE II
QUANTITATIVE DATA OF SIMULATION RESULTS OF ACTIVE POWER FOR HVV-TPSS UNDER THE FIVE OPERATING STATES.

State	$P_{L\alpha}$	$P_{L\beta}$	$P_{p\alpha}$	$P_{p\beta}$	$P_{h\alpha}$	$P_{h\beta}$
State I (0.9-1.0s)	11.20 MW	11.20 MW	0.03 MW	-0.03 MW	11.14 MW	11.12 MW
State II (1.0-1.1s)	8.17 MW	5.44 MW	1.37 MW	-1.37 MW	6.81 MW	6.81 MW
State III (1.1-1.2s)	5.44 MW	10.03 MW	-2.29 MW	2.29 MW	7.73 MW	7.74 MW
State IV (1.2-1.3s)	4.18 MW	5.74 MW	-0.78 MW	0.78 MW	4.94 MW	4.96 MW
State V (1.3-1.4s)	11.48 MW	0 MW	5.74 MW	-5.74 MW	5.74 MW	5.74 MW

TABLE III
QUANTITATIVE DATA OF SIMULATION RESULTS OF REACTIVE POWER FOR HVV-TPSS UNDER THE FIVE OPERATING STATES.

State	$Q_{L\alpha}$	$Q_{L\beta}$	$Q_{p\alpha}$	$Q_{p\beta}$	$Q_{h\alpha}$	$Q_{h\beta}$
State I (0.9-1.0s)	4.80 Mvar	4.80 Mvar	11.27 Mvar	-1.63 Mvar	-6.44 Mvar	6.48 Mvar
State II (1.0-1.1s)	6.30 Mvar	2.11 Mvar	10.22 Mvar	-1.82 Mvar	-3.95 Mvar	3.93 Mvar
State III (1.1-1.2s)	2.12 Mvar	7.14 Mvar	6.59 Mvar	2.68 Mvar	-4.46 Mvar	4.47 Mvar
State IV (1.2-1.3s)	1.34 Mvar	2.40 Mvar	4.18 Mvar	-0.45 Mvar	-2.86 Mvar	2.86 Mvar
State V (1.3-1.4s)	6.37 Mvar	0 Mvar	9.67 Mvar	-3.32 Mvar	-3.33 Mvar	3.31 Mvar

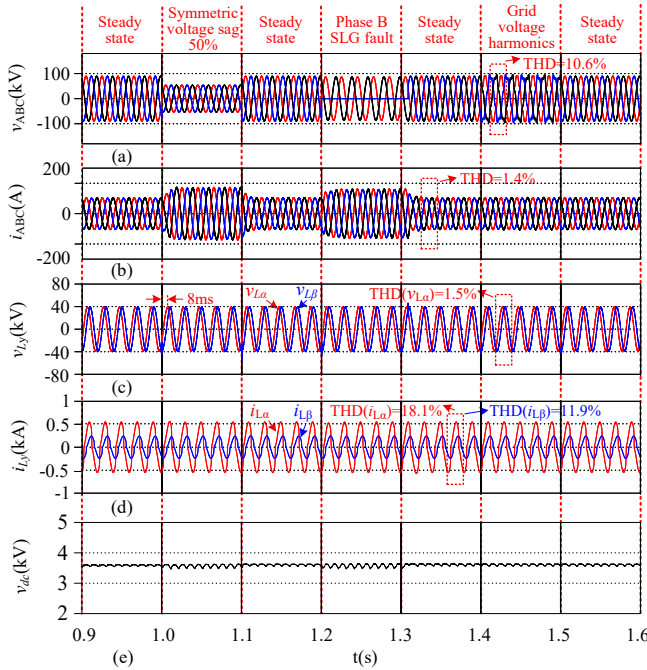


Fig. 21. Simulation results of the UPQC-based TPSS under voltage fault and harmonic conditions.

11.9%. A grid 50% three-phase symmetric voltage sag occurs from 1.0 to 1.1 s, followed by an asymmetric phase B SLG fault from 1.2 to 1.3 s. (Specifically, the co-phase connected traction system carries a 9.4 MW + j5.6 Mvar load with the load current THD set to 18.1%, and the asymmetric fault is set as a phase A SLG fault from 1.2 to 1.3 s.) From 1.4 to 1.5 s, the THD of the grid voltage is set to 10.6%.

As shown in Table IV, the control-performance data of Scott-connected, RPC-based, co-phase connected, UPQC-based and the proposed HVV-TPSS are obtained from the simulation results of Figs. 18 – 22. Except for the co-phase connected TPSS, the load voltage compensation error, response time and THD are derived from the α -side load. Specifically, the voltage compensation error and response time are captured under the 50% symmetric voltage sag condition from 1.0 to 1.1 s, while the grid current THD and load voltage THD are extracted from the time intervals of 1.3–1.4 s and

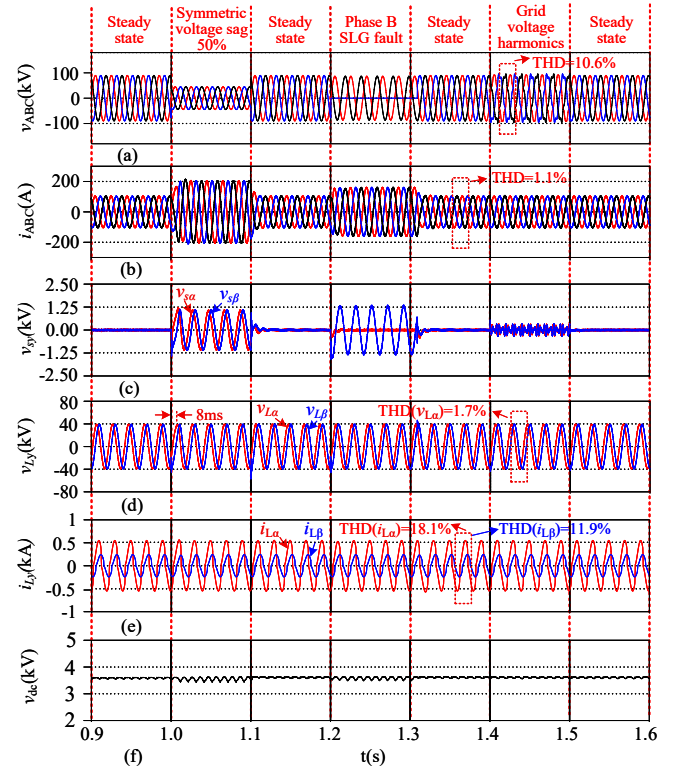


Fig. 22. Simulation results of the proposed HVV-TPSS under voltage fault and harmonic conditions.

TABLE IV
QUANTITATIVE CONTROL PERFORMANCE COMPARISON OF POWER QUALITY SOLUTIONS FOR TPSS

Solution	Voltage comp. error	Voltage response time	Grid current THD	Load voltage THD	DC ripple per.
Scott	-	-	16.9%	10.6%	-
RPC	-	-	1.5%	10.6%	1.50%
Co-phase	-	-	1.8%	10.6%	1.44%
UPQC	0.23%	8ms	1.4%	1.5%	2.02%
HVV-TPSS	0.27%	8ms	1.1%	1.7%	1.81%

1.4–1.5 s, respectively. Moreover, the DC bus voltage ripple percentage, which is abbreviated as DC ripple per. in Table IV, is obtained from 0.9 to 1.0 s.

Figs. 18 – 20 present the fault and harmonic simulation results of the Scott-connected, the RPC-based and the co-phase connected traction systems, respectively. As shown in Fig. 18(b), although the Scott-connected traction system can mitigate the NSC problem, a noticeable negative-sequence component still remains, and its grid current THD reaches 16.9% under steady-state conditions.

Fig. 19(b) and Fig. 20(b) show that RPC-based and co-phase-connected traction systems can effectively suppress grid-side NSC, with the grid current THD values of 1.5% and 1.8%, respectively. However, during symmetric and asymmetric grid voltage faults, the amplitude or phase of the load voltages in the Scott-connected, RPC-based, and co-phase-connected traction systems are all severely affected by the voltage disturbances. Moreover, Figs. 18 – 20 also demonstrate that, under distorted grid-voltage condition, the THD of the load voltage for these solutions is uniformly 10.6%, indicating their limited capability to suppress grid-voltage harmonics. Consequently, these solutions exhibit poor immunity to severe grid voltage disturbances, including background harmonics, deep voltage symmetric and asymmetric sags.

Fig. 21 and Fig. 22 present the fault and harmonic simulation results of the UPQC-based TPSS and HVV-TPSS, respectively. The UPQC-based TPSS and HVV-TPSS can achieve sinusoidal and balanced grid-side current that is in phase with the grid voltage whether in a steady-state condition or a fault condition. Through the current harmonic compensation controlled by the PCs, the grid-side current THD of the UPQC-based TPSS and HVV-TPSS can be suppressed to 1.4% and 1.1% under steady-state conditions, respectively. This indicates that the PCs of the UPQC-based TPSS and HVV-TPSS can protect the grid current from being polluted by the traction load current harmonics. In the event of symmetric and asymmetric voltage sags in the grid, the SCs of both the UPQC-based TPSS and HVV-TPSS promptly inject the precise compensation voltages, thus maintaining the terminal load voltages at near the rated value without phase angle shifts. Furthermore, under grid voltage harmonic condition, through the voltage harmonic compensation of the SCs, the α side load voltage THD of the UPQC-based TPSS and HVV-TPSS can be suppressed to 1.5% and 1.7%, respectively. This verifies that the UPQC-based TPSS and HVV-TPSS are capable of mitigating the disturbances imposed by grid voltage harmonics on the traction load voltage. In addition, the UPQC-based TPSS and HVV-TPSS achieves stable DC-link voltage regulation and reliable system energy balance.

Fig. 22 demonstrates that the proposed HVV-TPSS and its comprehensive control strategy can provide dynamic voltage support under symmetric and asymmetric grid faults, while simultaneously suppressing both voltage and current harmonics, thereby enhancing the voltage control capability.

VI. EXPERIMENTAL RESULTS

To further validate the comprehensive power quality management capability of the proposed HVV-TPSS, a scaled-down experimental platform was constructed under asymmetric load condition, as illustrated in Fig. 23. The main experimental

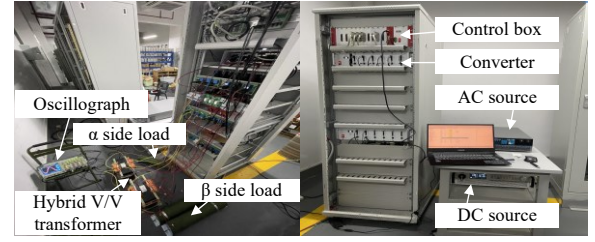


Fig. 23. Experimental platform of HVV-TPSS.

TABLE V
EXPERIMENTAL PARAMETERS OF THE HVV-TPSS

Parameter	Value
AC grid rated phase voltage	110 V
Hybrid V/V transformer turn ratio $N_h : N_s : N_p : N_L$	$\sqrt{3}:1:1:1$
DC side rated voltage	200 V
Series converter filter capacitance	10 μ F
Series converter filter inductance	5 mH
Parallel converter filter inductance	8 mH
Switching frequency	10 kHz
α/β side load resistance	30 Ω /50 Ω

parameters are summarized in Table V, with the α side and the β side load resistances set to 30 Ω and 50 Ω , respectively. The V/V transformer is composed of two 5 kVA single-phase hybrid transformers. The winding ratio of this three-column four-winding transformer is $\sqrt{3}:1:1:1$.

As shown in Fig. 23, the experimental platform consists of an HVV-TPSS prototype, a RTU-BOX204 control box, LC/L-type output filters, loads, AC power source, and DC power source. The converter consists of four H-bridge power electronic modules, each implemented using IGBT-based switching devices whose model is RTM-PEH8035IF. The converters operate at a switching frequency of 10 kHz. The parameter designs for the DC-link capacitor, L and LC filters in the converters are guided by [38] and [39]. The main processor of the control box is a 32-bit DSP (TMS320C28346) with a clock frequency of 300 MHz, which provides sufficient computational capability to implement the proposed control algorithms in real time.

Notably, the experimental verification focuses on the current-related power quality control and the fault ride-through capability of the proposed HVV-TPSS. Resistor loads were used to replace the traction loads in the experiment. It should be noted that the laboratory prototype uses resistor loads due to the limitation of the available experimental platform. Therefore, the experimental harmonic and dynamic tests mainly focus on voltage disturbances, including voltage sag and voltage harmonics. The AC grid and its voltage faults were emulated using a programmable AC power source, while the DC-ESS was emulated with a programmable DC power source.

A. Experimental results of the HVV-TPSS under steady-state conditions

Fig. 24 presents the comparative voltage and current waveforms on the grid side before and after the PCs of the HVV-TPSS are put into operation. Fig. 25(a) shows the parallel compensation current waveforms after the two parallel converters are put into operation. In Fig. 25(b), $v_{out-p\alpha}$ and $v_{p\alpha}$

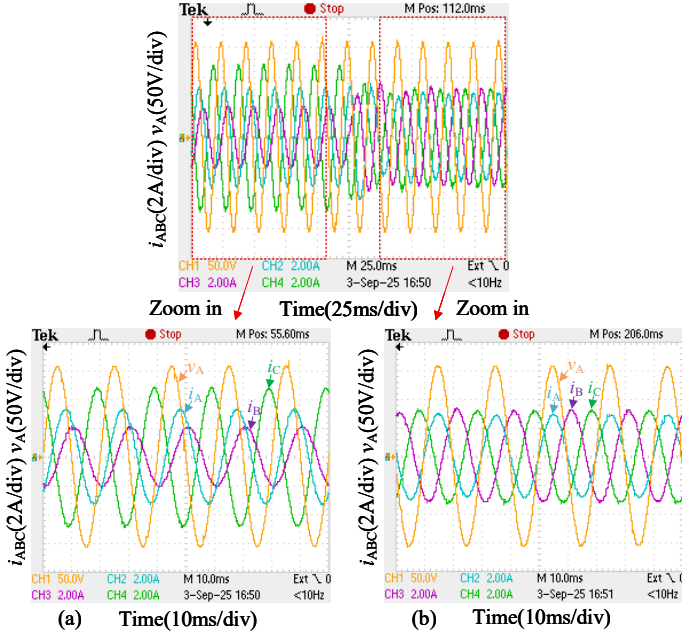


Fig. 24. Experimental results of the HVV-TPSS under steady-state conditions. (a) Before PCs are put into operation. (b) After PCs are put into operation.

TABLE VI
QUANTITATIVE EXPERIMENTAL COMPARISON OF GRID
CURRENT-RELATED POWER QUALITY BEFORE AND AFTER PC OPERATION.

Operating condition	Grid current unbalance factor	Grid side power factor	Grid current THD
Before PC operation	55.2%	0.93	3.19%
After PC operation	9.1%	0.99	3.08%

denote the output voltages of the parallel converter on the α side before and after filtering, respectively.

To verify the current-related power quality improvement shown in Fig. 24, the measured three-phase grid-side waveforms were quantitatively evaluated before and after the PCs were enabled, as summarized in Table VI.

As shown in Fig. 24 and Table VI, before the PCs are put into operation, the three-phase grid current contains a substantial negative-sequence component, with a measured current unbalance factor of 55.2%. After the PCs are put into operation, the three-phase grid current becomes sinusoidal, balanced, and the current unbalance factor decreases to 9.1%, effectively suppressing the grid current imbalance. Moreover, the grid-side power factor is also increased from 0.93 to 0.99 after PC operation, significantly improving the grid-side power factor. Meanwhile, the grid current THD remains almost unchanged, from 3.19% to 3.08%. Therefore, the operation of the PCs does not introduce additional harmonic distortion or deteriorate the harmonic quality of the grid-side current.

Fig. 26 shows the active power waveforms of the high-voltage windings and the parallel windings under the asymmetric load operating conditions. Before the two PCs are put into operation, the active powers of the two high-voltage windings are unequal because of the asymmetric loads. Taking into account system losses, after the two PCs are put into operation, the active powers of the two high-voltage windings become nearly balanced, which proves the power transfer and balancing functions of the two PCs.

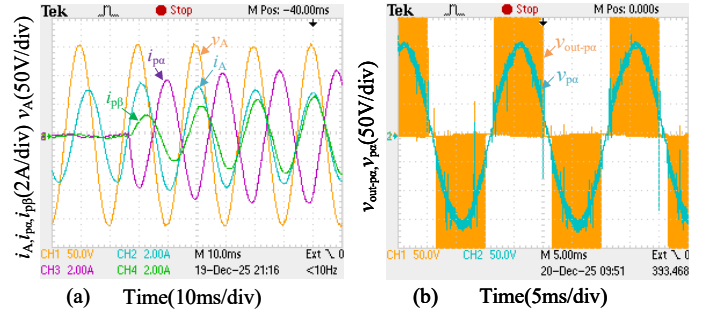


Fig. 25. Experimental waveforms of parallel converters under steady-state conditions. (a) Parallel converter current compensation waveforms. (b) Voltage waveforms of α -side parallel converter before and after the L filter.

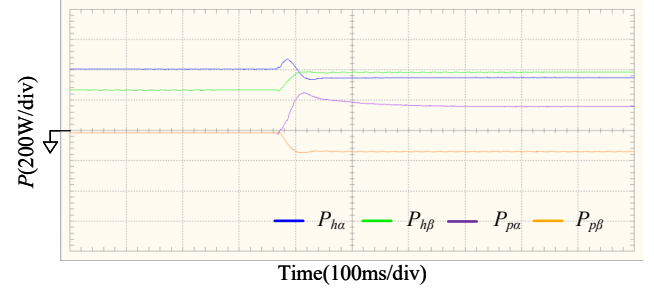


Fig. 26. Winding power experimental waveform of the HVV-TPSS under steady conditions.

The results of steady-state experiment show that the HVV-TPSS is capable of performing the effective current-related power quality compensation through PCs.

B. Experimental results of the HVV-TPSS under grid voltage harmonic and grid fault conditions

The core focus of this study is enhancing the voltage-related power quality compensation ability in TPSS, the experimental verification emphasizes the voltage harmonic control ability of the HVV-TPSS. During the test, a distorted grid voltage with notable harmonic content is applied. As shown in Fig. 27(b) and (c), when the THD of grid voltage reaches 7.95%, the THD of the load voltage on the β side is reduced to 2.28% through series voltage compensation. The experimental result confirms that series converters can accurately compensate the grid voltage harmonics and prevent those harmonics from affecting the load voltage.

Under grid fault conditions, comparative experiments were conducted on conventional V/V transformer-based TPSS and the HVV-TPSS in scenarios involving a 30% three-phase symmetric grid voltage sag and a 50% single-phase (phase B) asymmetric grid voltage sag.

Fig. 28 presents the experimental results of the conventional V/V transformer-based TPSS under grid fault conditions. As shown in Fig. 28(a) and (b), conventional V/V transformer-based TPSS exhibits severe transient disturbances in load voltage under symmetric and asymmetric grid voltage fault conditions, revealing its deficiency in voltage regulation.

Figs. 29 – 32 show the experimental results of the HVV-TPSS under symmetric and asymmetric grid fault conditions, respectively. In Fig. 30(a) and Fig. 32(a), $v_{out-p\beta}$ and $v_{p\beta}$

TABLE VII
QUANTITATIVE EXPERIMENTAL EVALUATION OF SC VOLTAGE COMPENSATION PERFORMANCE.

Condition	Peak series voltage	Modulation index	Load voltage phase-angle error	Grid voltage THD	Post-filter series voltage THD	Load voltage THD after compensation
Harmonic voltage	51.5 V	0.256	-0.35°	7.95%	14.32%	2.28%
Symmetric voltage sag	104.8 V	0.524	0.67°	1.5%	4.18%	2.39%
Asymmetric voltage sag	78.2 V	0.391	1.99°	1.98%	4.27%	2.12%

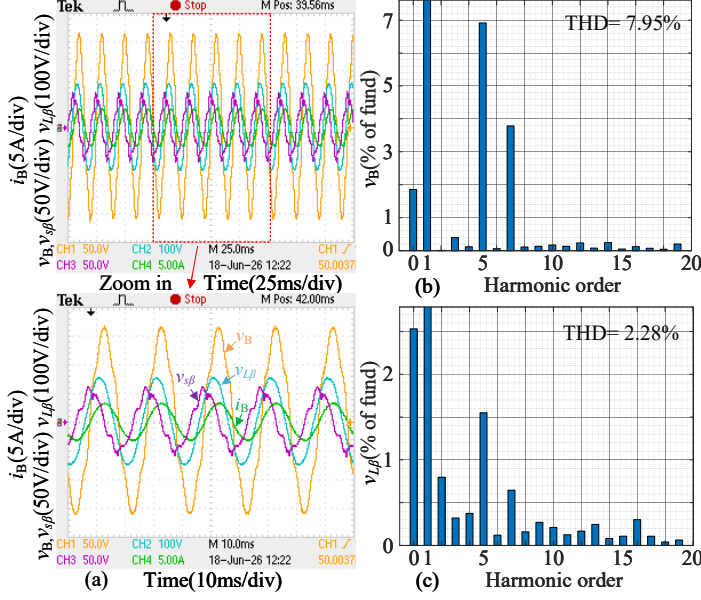


Fig. 27. Experimental results of HVV-TPSS under voltage harmonic conditions. (a) Series harmonic compensation under voltage harmonic conditions. (b) FFT analysis result of the B-phase voltage on the utility grid side. (c) FFT analysis result of the β side load voltage.

denote output compensation voltage of the β side series converter before and after filtering during the fault. In Fig. 30(b) and Fig. 32(b), $i_{out-p\beta}$ and $i_{p\beta}$ represent the output currents of the β side series converter before and after filtering during the fault.

As shown in Fig. 29(a) and Fig. 31(a), the three-phase grid current remains a symmetric sinusoidal waveform in phase with the grid voltage regardless of grid faults. As depicted in Fig. 29(b) and Fig. 31(b), SCs can rapidly and accurately compensate for both the amplitude and phase of the load voltage, ensuring the load voltage remains stable at the pre-fault rated state. Fig. 30 and Fig. 32 clearly illustrate the series converter-side voltage characteristics and the effectiveness of the output filters under fault conditions.

To quantitatively evaluate the voltage compensation performance of the SCs under grid voltage harmonic, symmetric voltage sag, and asymmetric voltage sag conditions, the peak injected series voltage, modulation index, load-voltage phase-angle error, grid voltage THD, post-filter series voltage THD, and load voltage THD after compensation were extracted from the experimental results, as summarized in Table VII. Specifically, the peak injected series voltage is extracted as the maximum value of the fundamental series-voltage component during the compensation interval. The phase-angle error is evaluated as the deviation between the fundamental phase angle of the compensated load voltage and its reference value.

According to Table VII, under the voltage harmonic condi-

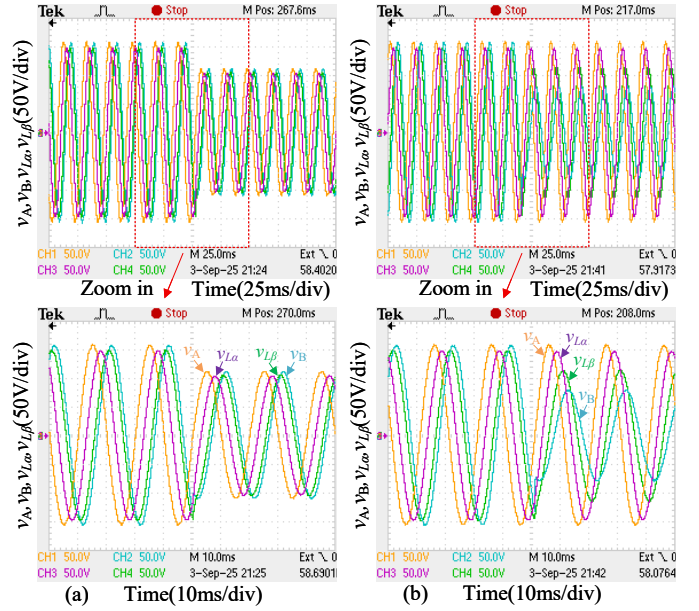


Fig. 28. Experimental results of the conventional V/V transformer-based TPSS under grid fault conditions. (a) Under symmetric fault conditions. (b) Under asymmetric fault conditions.

tion, the SC of the β side injects a peak series voltage of 51.5 V, while the corresponding modulation index is 0.256. The compensated β side load-voltage phase-angle error is -0.35° , and the compensated β side load-voltage THD is reduced to 2.28%. For the symmetric voltage sag condition, the β side SC provides a peak series voltage of 104.8 V, with a modulation index of 0.524. The compensated β side load voltage exhibits a phase-angle error of 0.67° , and the compensated β side load-voltage THD is 2.39%. During the asymmetric voltage sag, the peak injected series voltage of the β side SC is 78.2 V, corresponding to a modulation index of 0.391. The associated compensated β side load-voltage phase-angle error is 1.99° , whereas the compensated β side load-voltage THD is 2.12%.

Consequently, HVV-TPSS demonstrates fault ride-through and rapid voltage compensation capability under voltage harmonic, symmetric and asymmetric grid voltage fault conditions, preserving normal operation of loads unaffected by transient disturbances.

VII. DISCUSSION

This section provides a comprehensive discussion of the proposed HVV-TPSS in terms of control performance, investment cost, equipment volume, engineering feasibility, research limitations, and future work.

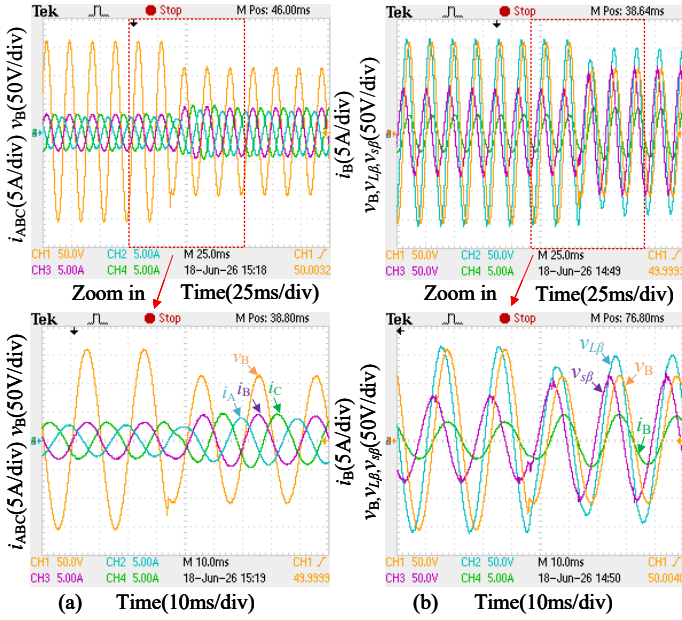


Fig. 29. Experimental results of the HVV-TPSS under symmetric fault conditions. (a) Current-related power quality control. (b) Voltage-related power quality control.

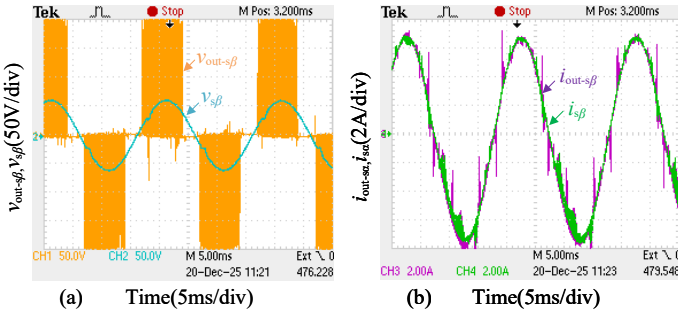


Fig. 30. Experimental waveforms of series converters under symmetric voltage fault condition. (a) Output compensation voltages before and after the LC filter of the β side series converter. (b) Current waveforms before and after the LC filter of the β side series converter.

A. Voltage control and harmonic suppression

Based on the analysis in Section V.B and the quantitative performance metrics reported in Table IV, including voltage compensation error, voltage compensation response time, grid current THD, and load voltage THD, Table VIII summarizes and compares the voltage and current control performance of the proposed HVV-TPSS against classical solutions.

Balance transformers like Scott transformer [10] lack the dynamic control capability and cannot independently address power quality issues related to voltage and current.

As indicated by the quantitative performance metrics in Table VIII, the RPC-based [15] and co-phase connected [20] TPSS solutions can perform multi-functional current-related power quality management, such as negative sequence control, reactive power support and current harmonic suppression. However, both RPC and co-phase traction supply system alleviate voltage fluctuations or voltage imbalance problems by means of parallel current compensation for impedance voltage deviation. And they cannot cope with severe disturbances such as deep voltage sags, asymmetric voltage faults and back-

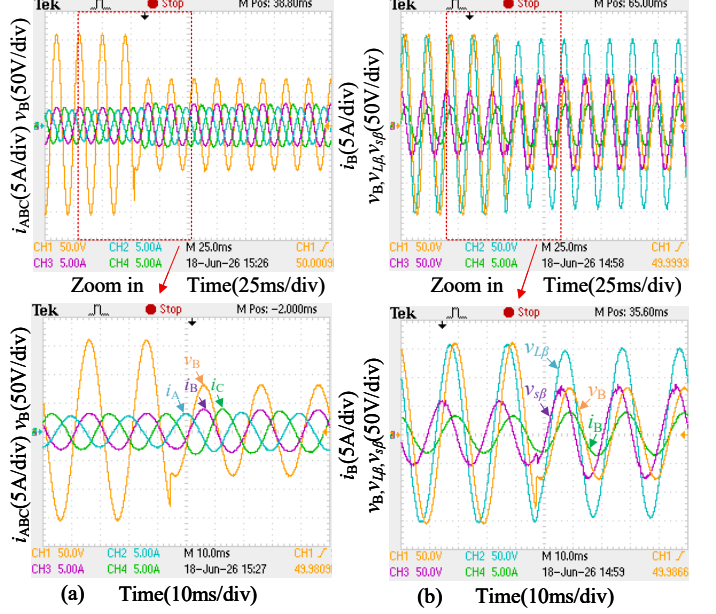


Fig. 31. Experimental results of the HVV-TPSS under asymmetric fault conditions. (a) Current-related power quality control. (b) Voltage-related power quality control.

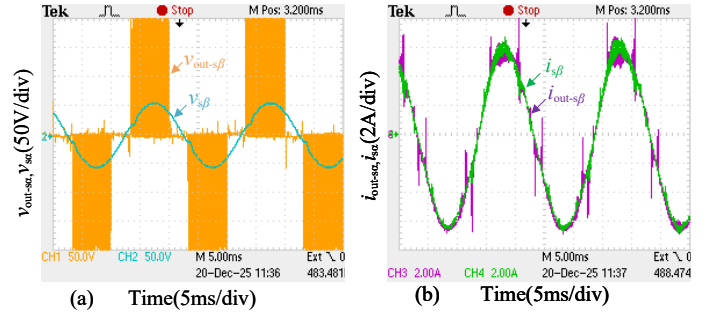


Fig. 32. Experimental waveforms of series converters under asymmetric voltage fault condition. (a) Output compensation voltage before and after the LC filter of the β side series converter. (b) Current waveforms before and after the LC filter of the β side series converter.

ground voltage harmonics. In conclusion, they have limited voltage compensation ability.

In addition to the strong current-related power quality management performance, the proposed HVV-TPSS and UPQC-based TPSS solutions [26] can perform series voltage compensation and have strong voltage control and harmonics suppression capabilities. When the voltage fault and voltage harmonics occur, they can rapidly inject a precise series compensation voltage, ensuring that the traction load voltage is maintained at the rated voltage value and not affected by the grid voltage harmonics.

B. Comparison of footprint, volume, transformer-related cost, insulation complexity, and field installation difficulty between the UPQC-based solution and the proposed HVV-TPSS

To make the comparison more transparent, we added an engineering estimation of footprint, envelope volume, and transformer-related cost. Since detailed outline dimensions of 110 kV/27.5 kV traction-specific single-phase transformers are usually project-specific and not always publicly available,

TABLE VIII
CONTROL PERFORMANCE COMPARISON OF POWER QUALITY SOLUTIONS
FOR TPSS.

Reference	Solution	Current control	Voltage control	Harmonic suppression
Ref. [10]	Scott connected	No	No	No
Ref. [15]	RPC-based	Strong	Weak	Partial
Ref. [20]	Co-phase connected	Strong	Weak	Partial
Ref. [26]	UPQC-based	Strong	Strong	Yes
HVV-TPSS	The proposed	Strong	Strong	Yes

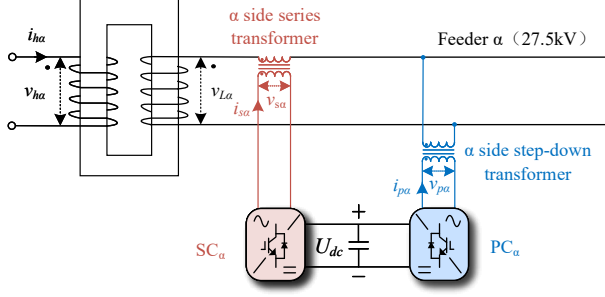


Fig. 33. The single-phase topology of the UPQC-based solution.

publicly available transformer data from SOJO Electric [40] are used only as proxy values for approximate comparison. The 110 kV, 16 MVA oil-immersed transformer has reference dimensions of 5400 mm × 4600 mm × 4800 mm, while the 35 kV, 8 MVA oil-immersed transformer has reference dimensions of 3800 mm × 3300 mm × 3300 mm. These values are used only as engineering proxies for footprint and envelope-volume estimation, rather than exact outline dimensions of traction-specific transformers.

An approximate engineering comparison between the UPQC-based TPSS and the proposed HVV-TPSS is given in Table IX. In the proposed HVV-TPSS, two 110 kV transformer units are required, and no external auxiliary transformers are needed. Therefore, the estimated footprint and envelope volume are 49.68 m² and 238.46 m³, respectively. In contrast, as shown in Fig. 33, the UPQC-based TPSS requires two 16 MVA 110 kV transformer units and four auxiliary transformers for series and parallel compensation in the α and β feeders. Its estimated footprint and envelope volume are 99.84 m² and 403.99 m³, respectively. Therefore, compared with the UPQC-based TPSS, the proposed HVV-TPSS eliminates four auxiliary transformers, reducing the estimated transformer footprint by 50.16 m², or approximately 50.2%, and reducing the envelope volume by 165.53 m³, or approximately 41.0%.

For cost estimation, the unit cost of a 16 MVA main transformer is taken as RMB 1.45–1.90 million, while that of a 8 MVA auxiliary transformer is taken as RMB 0.58–0.85 million. The data sources were obtained from the tender prices of different companies until Dec. 31, 2025. Accordingly, the estimated transformer-related cost of the UPQC-based TPSS is RMB 5.22–7.20 million, whereas that of the proposed HVV-TPSS is RMB 2.90–3.80 million. This corresponds to a cost reduction of RMB 2.32–3.40 million, or approximately 44%–47%.

It should be noted that the footprint, volume, and cost values are approximate engineering proxy values. The transformer

dimensions are estimated based on publicly available transformer data, rather than exact outline dimensions of traction-specific transformers. Final values may vary with insulation level, cooling system, manufacturer, and project-specific installation requirements.

From the insulation complexity perspective, the benefit of the proposed structure lies in the elimination of the four external auxiliary transformer insulation systems and the corresponding field-connected high-/medium-voltage interfaces. The reduction in insulation complexity does not mean that the insulation level of each winding is reduced. Each winding must still be designed according to its voltage level and insulation-coordination requirements.

From the installation perspective, the proposed HVV-TPSS does not remove the main traction transformer installation work. Its practical benefit is that the four auxiliary transformer foundations, lifting operations, oil-handling or vacuum-processing procedures, primary cable connection groups, protection and monitoring interfaces, and auxiliary transformer on-site tests required by the UPQC-based TPSS are removed. The additional complexity of the hybrid transformer is instead handled at the factory-design and manufacturing stage, where winding arrangement, insulation design, and routine tests can be controlled more systematically.

C. Engineering characteristics

The proposed hybrid V/V transformer provides galvanic isolation, voltage step-down, and insulation functions fully integrated within a modified hybrid transformer unit via multiple windings. Both the parallel and series converters interface with the traction system through dedicated transformer windings and L/LC filters, which are designed to attenuate high-frequency voltage components and limit dv/dt. Furthermore, the hybrid transformer provides additional leakage inductance, which contributes to the suppression of high-frequency disturbances. Therefore, with proper system design, the dv/dt imposed on the traction network and downstream equipment can be effectively constrained.

The proposed system is inherently modular at both the transformer and converter levels. Each hybrid transformer unit integrates its own parallel and series converters, which can be implemented using modular H-bridge power electronic units. This modular structure facilitates staged installation, redundancy design, and capacity scaling, and is compatible with current modular construction practices in traction substations. In the event of a fault of converters, both the proposed series converter and the parallel converters can be directly disconnected and repaired without affecting the functions of the transformer itself, indicating strong feasibility in terms of modularization and maintenance.

D. Limitations of the research

It should be noted that the experimental validation is conducted on a scaled-down low-voltage prototype with resistor loads and AC/DC programmable sources adopted to emulate the traction load and AC grid/DC-ESS under controllable

TABLE IX
APPROXIMATE ENGINEERING COMPARISON BETWEEN UPQC-BASED TPSS AND PROPOSED HVV-TPSS.

Item	UPQC-based TPSS	Proposed HVV-TPSS	Improvement
Main transformer units	2	2	No
Auxiliary transformers	4	0	100% eliminated
Total transformer units	6	2	66.7% reduction
Number of windings	12	8	33.3% reduction
Transformer footprint	99.84 m ²	49.68 m ²	50.2% reduction
Transformer envelope volume	403.99 m ³	238.46m ³	41.0% reduction
Estimated transformer-related cost	RMB 5.22-7.20 million	RMB 2.90-3.80 million	44%-47% reduction
Number of IGBTs	16	16	No
External auxiliary transformer foundations	4	0	100% eliminated
External auxiliary transformer installation and test objects	4	0	100% eliminated

laboratory conditions. Therefore, the experiments mainly verify the operating principle of the proposed HVV-TPSS, and the effectiveness of the control strategy, rather than fully reproducing the insulation stress, thermal stress, protection behavior, and overvoltage withstand in full-scale 27.5 kV railway applications.

While the basic insulation-coordination philosophy can refer to established railway traction transformer practices, the present scaled-down demonstrator does not constitute full validation of 27.5 kV implementation, which still requires dedicated verification of winding-to-winding electric-field distribution, creepage and clearance distances, impulse withstand, and partial-discharge margin. Furthermore, 27.5 kV TPSS scalability involves not only insulation design but also protection coordination, thermal management, and high-voltage converter realization, including coordinated protection for faults, electrothermal design for hybrid V/V transformer, and modular converter implementation.

E. Future work

Future work will investigate the scalability of this topology, including its modular extension and coordinated control for multi-train or multi-section systems. Besides, reducing DC-link capacitance demand is an important practical consideration. From (46) and (47), the total SHC i_{2ndt} is the phasor sum of four individual components. Under severe grid fault conditions, the SCs are required to inject compensation voltages and operate in close coordination with the PCs. The four SHCs superpose to either amplify or mitigate the DC bus disturbance current, offering a mechanism for targeted DC voltage ripple optimization in future work. In particular, the DC-link capacitor requirement can be further reduced by coordinating the DC-side ripple current phase among the series converters and parallel converters, such that part of the low-frequency power ripple is naturally canceled on the DC link. Advanced power balancing and ripple suppression strategies between the series and parallel converters will be investigated in future work to further optimize DC-link capacitor sizing and improve system lifetime and cost effectiveness [35], [36].

VIII. CONCLUSION

This paper introduces a hybrid V/V transformer-integrated traction power supply system with comprehensive power quality compensation. Using the equivalent mathematical model of the hybrid transformer units, the working principles of the HVV-TPSS are investigated under steady-state and grid fault conditions, along with the introduction of a comprehensive control strategy. Drawing on theoretical analysis, simulations, and experimental results, the main conclusions are derived.

1) Under steady-state conditions, HVV-TPSS can address current-related power quality issues through PCs using current compensation, effectively suppressing grid-side NSC and current harmonics, compensating for reactive power, and enabling active power transfer between the two feeders, thereby ensuring a high-quality power supply.

2) During grid voltage faults and harmonics, HVV-TPSS guarantees uninterrupted operation of electric locomotives by dynamically compensating for grid fault and harmonic voltages using SCs. The HVV-TPSS enhances voltage control capability and power supply reliability.

3) HVV-TPSS offers comprehensive power quality compensation capability and high operational reliability. Not only does it extend the fault ride-through capability of terminal traction loads, but it also minimizes the number of transformers required. Moreover, this design optimizes equipment integration and space utilization.

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