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A Low-High Voltage Continuous Ride-Through Control Strategy for Permanent Magnet Wind Turbines Based on Rotor Kinetic Energy

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Received: 25 December 2025; Accepted: 11 March 2026; Published: 24 September 2026

ABSTRACT: With the large-scale integration of wind turbines into power systems, the voltage at the grid connection point exhibits continuous oscillation characteristics, which significantly increases the ride-through difficulty and off-grid risk of wind turbines during faults. To address the above problems, this paper proposes a low-high voltage ride-through (L-HVRT) control strategy based on rotor energy storage. This strategy reduces the load on the unloading resistor by decreasing the active power output from the machine-side converter (MSC). To prevent overspeed disconnection during this process, an active power reference value for the MSC output is preset to proactively suppress rotor overspeed, thereby preventing the permanent magnet synchronous generator (PMSG) speed from exceeding the safety limit. Based on the PSCAD simulation platform, a corresponding simulation model is established to verify the feasibility of the proposed control strategy. The simulation results show that the strategy can effectively improve the continuous fault ride-through capability of wind turbines.

KEYWORDS: Permanent magnet synchronous generator; continuous ride-through; braking resistor; overspeed and grid disconnection

1 Introduction

With the increasing severity of environmental pollution and climate change, the penetration level of wind power has continued to rise [1]. To ensure the stable operation of power systems with large-scale wind power integration, wind turbines are required not only to possess low-voltage ride-through (LVRT) capability [2] and high-voltage ride-through (HVRT) capability [3], but also to withstand consecutive low- and high-voltage disturbances, namely low-high voltage ride-through (L-HVRT) capability [4].

Currently, extensive research exists regarding LVRT and HVRT. To enhance the ride-through capability of wind turbines during faults, existing solutions generally fall into two main categories: hardware-based improvements and control strategy optimizations.

Regarding hardware-based improvements, research focus typically lies in introducing additional auxiliary physical devices to balance system power or suppress fault currents. Specifically, Ref. [5] proposed utilizing the rapid power throughput characteristics of superconducting magnetic energy storage systems to absorb unbalanced energy during faults. Refs. [6,7] explored the application of superconducting fault current limiters in restricting short-circuit current impacts and protecting converters. Furthermore, Ref. [8] investigated the role of supercapacitor energy storage systems in smoothing DC bus voltage fluctuations.

Additionally, installing a dynamic braking resistor on the DC side of the converter is a common hardware solution [9], which maintains system stability by dissipating excess active power as thermal energy. Although these hardware-based schemes can significantly improve the LVRT and HVRT performance of the units, they are often accompanied by high equipment investment costs, large physical footprints, and complex operation and maintenance requirements. These economic and engineering implementation limitations have, to some extent, hindered their large-scale promotion and application in cost-sensitive commercial wind farms.

To overcome the limitations of hardware-based solutions regarding cost and engineering implementation, optimizing converter control strategies for fault ride-through has increasingly become the mainstream of research in both academia and industry. These methods aim to maintain system stability by utilizing the converter's inherent regulatory capabilities without incurring additional hardware costs. For LVRT conditions, Refs. [10,11] proposed strategies based on flux-weakening control to suppress the surge in DC bus voltage by adjusting stator current components, while Refs. [12,13] achieved rapid balance between input and output power by directly regulating the machine-side electromagnetic torque. Regarding HVRT conditions, Refs. [14,15] proposed a GSC-based reactive power priority control strategy, utilizing the absorption of inductive reactive power to assist in grid voltage recovery. However, most of the aforementioned control strategies focus on addressing single voltage-dip or voltage-swell faults. When facing the complex conditions of L-HVRT, relying solely on machine-side torque regulation can easily lead to severe rotor speed fluctuations due to power imbalance, while relying only on grid-side reactive power regulation is often limited by the converter's current capacity, making it difficult to simultaneously ensure DC-side overvoltage protection and rotor speed stability.

Currently, research on the L-HVRT of wind turbines in complex grid environments is gradually unfolding. To address system stability issues during continuous faults, scholars have proposed various improvement schemes from different perspectives.

Regarding control strategy optimization, Reference [16] introduced virtual synchronous generator (VSG) technology, which enhances the system's frequency and voltage support capabilities under continuous voltage disturbances by simulating the inertia and damping characteristics of synchronous generators. Ref. [17], addressing the limitations of a fixed reactive current proportional coefficient K , proposed a variable K value control strategy to avoid insufficient or excessive reactive power support during slight voltage dips or recovery stages.

In terms of hardware assistance, Ref. [18] explored the use of supercapacitor energy storage systems to cope with faults, smoothing DC bus voltage fluctuations through an adaptive coordinated control strategy and assisting in grid voltage recovery.

Additionally, Research on L-HVRT remains limited. Ref. [19] proposes an L-HVRT control strategy based on a braking resistor and dynamic reactive power support, in which the voltage operating range is dynamically partitioned according to different voltage sag and swell conditions during grid faults, and the corresponding reactive current references are accordingly assigned. In addition, a braking resistor is connected in parallel with the DC link to dissipate the active power imbalance ΔP between the MSC and the GSC.

For convenience of discussion, the control strategy proposed in [19] is hereinafter referred to as the conventional control strategy. This conventional strategy imposes stringent requirements on the braking resistor, and when the active power imbalance ΔP becomes large, it may result in the failure of the braking resistor.

The paper addresses the limitations of conventional L-HVRT control strategies by proposing a rotor-based energy storage control strategy for permanent magnet wind turbines. By exploiting the kinetic energy

stored in the PMSG rotor, the active power imbalance ΔP is effectively reduced, thereby alleviating the burden on the braking resistor. Simulations are carried out in PSCAD/EMTDC based on the technical parameters of an in-service PMSG-based wind farm, where a continuous fault ride-through model of the wind turbine is established to verify the effectiveness of the proposed control strategy.

2 Analysis of the Conventional Control Strategy

The grid-connected configuration of the PMSG-based wind turbine is illustrated in Fig. 1. In the figure, P_w denotes the output power of the wind turbine; P_s represents the active power generated by the PMSG; P_g is the active power delivered by the GSC; $\Delta P = P_s - P_g$ denotes the active power imbalance between the GSC and the MSC; C is the DC-link capacitor; U_{dc} represents the DC-link voltage; R denotes the braking resistor; R_1 and R_2 are the line resistances; and X_1 and X_2 are the line reactances.

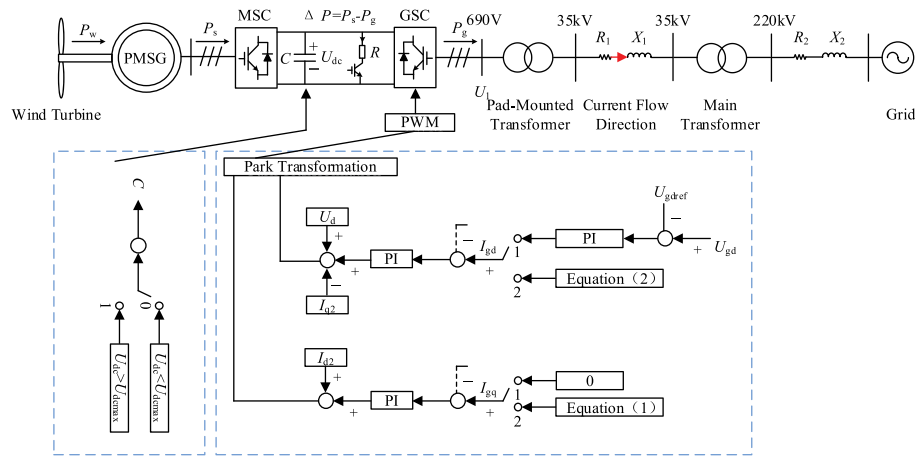


Figure 1: Grid-connected system structure and control diagram of a pmsg-based wind turbine under the conventional control strategy.

Under normal operating conditions, the wind turbine operates in the maximum power point tracking (MPPT) mode, where $P_w = P_s = P_g = P_{opt}$. The DC-link voltage U_{dc} is maintained at its reference value. This section analyzes the conventional control strategy during the L–HVRT process.

2.1 GSC Control Strategy

During L–HVRT, according to [19], the reference value of reactive current injected by the wind turbine into the power system, i_{gqref} is given by:

$$i_{gqref}^* = \begin{cases} -K_1 \times (0.9 - U_1^*)/\sqrt{2} & (\text{during LVRT}) \\ K_1 \times (U_1^* - 1.1)/\sqrt{2} & (\text{during HVRT}) \end{cases} \quad (1)$$

In the formula: K_1 is the proportional coefficient of the wind farm's dynamic reactive current; U_1 is the voltage at the point of common coupling (PCC); * denotes per-unit value.

To prevent overcurrent of the GSC, the reference value of active current of the GSC, i_{gdref} is given by:

$$i_{gdref}^* = \sqrt{(i_{gmax}^*)^2 - (i_{gqref}^*)^2} \quad (2)$$

In the formula: i_{gmax} is the maximum allowable current through the GSC.

According to Eqs. (1) and (2) and Ref. [19], the reactive power reference value Q_{gref} and active power reference value P_{gref} output by the GSC during the L-HVRT period are given by:

$$\begin{cases} Q_{\text{gref}}^* = -U_1^* \times i_{\text{gqref}}^* \\ P_{\text{gref}}^* = U_1^* \times i_{\text{gdref}}^* \end{cases} \quad (3)$$

Specifically, a strong physical coupling exists between the reactive current i_q and the active current i_d . During HVRT, the GSC is required by grid codes to inject a substantial amount of inductive reactive current i_q to suppress PCC voltage swells. Given that the converter's total current capacity i_{gmax} is fixed (constrained by the thermal limits of power electronic devices), and subject to the constraint $(i_{\text{gd}})^2 + (i_{\text{gq}})^2 \leq (i_{\text{gmax}})^2$, a significant increase in i_q inevitably reduces the available margin for active current i_d . This saturation of active current directly restricts the GSC's capability to export active power P_g to the grid, thereby exacerbating the power imbalance ΔP between the machine-side input and the grid-side output. Consequently, employing effective energy dissipation or transfer strategies—such as activating dynamic braking resistors or leveraging rotor kinetic energy storage—becomes critical.

2.2 MSC Control Strategy

During the L-HVRT period, under the traditional control strategy, according to Ref. [19], $P_{\text{sref}} = P_{\text{opt}}$.

In traditional control strategies, the machine-side active power P_s is maintained at the maximum power point (MPPT) value P_{opt} without dynamic curtailment, despite the reduction in grid-side power export capability. This is attributed to the large mechanical inertia of the pitch system, which prevents a rapid reduction of the mechanical torque T_m at the instant of fault inception. If P_s were drastically reduced (i.e., reducing the electromagnetic torque T_e) to mitigate ΔP , it would cause a severe torque imbalance ($T_m \gg T_e$), driving the rotor to accelerate rapidly and triggering overspeed tripping. Consequently, the traditional strategy adopts a logic of “maintaining machine-side power while relying on resistive energy dissipation,” which essentially prioritizes rotor speed stability at the expense of increased thermal stress on the braking resistor.

2.3 The Dissipation of ΔP

During the L-HVRT period, due to the limitations of the GSC capacity during LVRT and the grid power backfeed during HVRT [20], the power balance between P_s and P_g is disrupted, resulting in $\Delta P > 0$. This causes the DC-link voltage U_{dc} to rise, and may even damage the DC-side components. It is necessary to connect the braking resistor R to dissipate ΔP . The switching scheme of the braking resistor R is as follows:

- (1) When $U_{\text{dc}} > \text{the maximum allowable DC-link voltage } U_{\text{dcmax}}$, the braking resistor is connected to dissipate ΔP ;
- (2) When $U_{\text{dc}} < U_{\text{dcmax}}$, the braking resistor is disconnected from operation.

In summary, during the L-HVRT period, under the traditional control strategy, setting $P_{\text{sref}} = P_{\text{opt}}$ results in a large ΔP , which imposes a heavy burden on the braking resistor R and may even lead to its failure.

3 L-HVRT Control Scheme Based on Rotor Energy Storage

According to the analysis in Section 1, the active power imbalance ΔP should be reduced to prevent the failure of the braking resistor. As indicated by $\Delta P = P_s - P_g$, a reduction in P_s leads to a decrease in ΔP .

The rotor motion equation of the PMSG is expressed as:

$$\frac{d\Omega_r}{dt} = \frac{\Omega_1}{T_j} (T_m^* - T_e^*) \quad (4)$$

In the formula: d/dt is the differential operator; Ω_1 is the rated rotor angular velocity of the PMSG; Ω_r is the rotor angular velocity of the PMSG; T_m is the mechanical torque transmitted from the wind turbine to the PMSG rotor; T_e is the electromagnetic torque of the PMSG, with $T_e^* = P_s^*/\Omega_r^*$; T_j is the inertia time constant of the PMSG.

Reducing P_s will decrease T_e . According to Eq. (4), a decrease in T_e will cause Ω_r to increase, and in severe cases, Ω_r will exceed the limit [16].

Therefore, this section proposes an L-HVRT control strategy based on rotor energy storage. By setting a reasonable P_s , both the reduction of ΔP and the suppression of rotor speed over-limit are achieved. The control strategy proposed in this paper is as follows.

3.1 GSC Control Strategy

Under the control strategy proposed in this paper, the Q_{gref} and P_{gref} output by the GSC are the same as those under the traditional control strategy, as shown in Eq. (3).

3.2 MSC Control Strategy

The tuning of P_s is based on the following assumptions: (1) The voltage phase changes caused by voltage sags and swells are neglected; (2) T_m remains unchanged before and after voltage sags and swells; (3) The most severe scenario is considered, where the HVRT phase starts immediately after the end of LVRT; (4) n times of L-HVRT are performed, with the same duration for each L-HVRT, the same LVRT duration within each L-HVRT, and the same HVRT duration within each L-HVRT.

The n times of L-HVRT are illustrated in Fig. 2. Let t_0 be the start time of the 1st L-HVRT, and t_i ($i = 1, 2, \dots, n$) be the end time of the i -th L-HVRT. $T_{(i-1) \sim i}$ is the end time of the i -th LVRT and also the start time of the i -th HVRT.



Figure 2: Schematic diagram of the -time L-HVRT process.

In the figure: P_{sLi} is the active power output by the MSC during the LVRT phase of the i -th L-HVRT; P_{sHi} is the active power output by the MSC during the HVRT phase of the i -th L-HVRT. Δt_{Li} is the duration of the i -th LVRT; Δt_{Hi} is the duration of the i -th HVRT.

To ensure that the rotor speed Ω_r remains within the safe range throughout the entire fault ride-through period, it is necessary to inversely solve for the minimum active power allowed for the MSC output. Based on Eq. (4), we integrate the acceleration torque during the fault period over time:

$$\left\{ \begin{array}{l} \Omega_r(t_{0\sim 1}) = \Omega_r(t_0) + \frac{\Omega_1}{T_j} \int_{t_0}^{t_{0\sim 1}} (T_m^* - T_e(u)^*) du \\ \Omega_r(t_1) = \Omega_r(t_{0\sim 1}) + \frac{\Omega_1}{T_j} \int_{t_{0\sim 1}}^{t_1} (T_m^* - T_e(u)^*) du \\ \vdots \\ \Omega_r(t_{(n-1)\sim n}) = \Omega_r(t_{n-1}) + \frac{\Omega_1}{T_j} \int_{t_{n-1}}^{t_{(n-1)\sim n}} (T_m^* - T_e(u)^*) du \\ \Omega_r(t_n) = \Omega_r(t_{(n-1)\sim n}) + \frac{\Omega_1}{T_j} \int_{t_{(n-1)\sim n}}^{t_n} (T_m^* - T_e(u)^*) du \end{array} \right. \quad (5)$$

In the figure: $\Omega_r(t_i)$ is the rotor mechanical angular velocity at $t = t_i$, and u is the integration variable.

According to Eq. (5), integrating both sides simultaneously yields:

$$\left\{ \begin{array}{l} \Omega_r(t_{0\sim 1}) = \Omega_r(t_0) + \frac{\Omega_1}{T_j} \left(T_m^* - \frac{P_s^*}{\Omega_r^*} \right) (t_{0\sim 1} - t_0) \\ \Omega_r(t_1) = \Omega_r(t_{0\sim 1}) + \frac{\Omega_1}{T_j} \left(T_m^* - \frac{P_s^*}{\Omega_r^*} \right) (t_1 - t_{0\sim 1}) \\ \vdots \\ \Omega_r(t_{(n-1)\sim n}) = \Omega_r(t_{n-1}) + \frac{\Omega_1}{T_j} \left(T_m^* - \frac{P_s^*}{\Omega_r^*} \right) (t_{(n-1)\sim n} - t_{n-1}) \\ \Omega_r(t_n) = \Omega_r(t_{(n-1)\sim n}) + \frac{\Omega_1}{T_j} \left(T_m^* - \frac{P_s^*}{\Omega_r^*} \right) (t_n - t_{(n-1)\sim n}) \end{array} \right. \quad (6)$$

During the L-HVRT period, Ω_r shall satisfy $\Omega_r \leq$ the allowable upper limit of rotor speed $\Omega_{r\lim}$. Furthermore, according to Eq. (6), it can be derived that:

$$\left\{ \begin{array}{l} \frac{P_{sL1}^*}{\Omega_r^*} \geq \left(T_m^* - \frac{T_j}{\Omega_1} \times \frac{\Omega_r(t_{0-1}) - \Omega_r(t_0)}{t_{0-1} - t_0} \right) \\ \frac{P_{sH1}^*}{\Omega_r^*} \geq \left(T_m^* - \frac{T_j}{\Omega_1} \times \frac{\Omega_r(t_1) - \Omega_r(t_{0-1})}{t_1 - t_{0-1}} \right) \\ \vdots \\ \frac{P_{sLn}^*}{\Omega_r^*} \geq \left(T_m^* - \frac{T_j}{\Omega_1} \times \frac{\Omega_r(t_{(n-1)\sim n}) - \Omega_r(t_{n-1})}{t_{(n-1)\sim n} - t_{n-1}} \right) \\ \frac{P_{sHn}^*}{\Omega_r^*} \geq \left(T_m^* - \frac{T_j}{\Omega_1} \times \frac{\Omega_{r\lim} - \Omega_r(t_{(n-1)\sim n})}{t_n - t_{(n-1)\sim n}} \right) \end{array} \right. \quad (7)$$

During the L-HVRT period, the allowable increment of Ω_r is $\Delta\Omega = \Omega_{r\lim} - \Omega_r(t_0)$. Based on the durations of LVRT and HVRT in each L-HVRT, this paper tunes P_{sref} for the n-time L-HVRT period.

During the L-HVRT period, the increment of Ω_r is taken as:

$$\left\{ \begin{array}{l} \Delta\Omega_{L1} = \Omega_r(t_{0-1}) - \Omega_r(t_0) = \frac{\Delta\Omega}{n} \times \frac{\Delta t_{L1}}{t_1 - t_0} = \frac{\Delta\Omega}{t_n - t_0} \Delta t_{L1} \\ \Delta\Omega_{H1} = \Omega_r(t_1) - \Omega_r(t_{0-1}) = \frac{\Delta\Omega}{n} \times \frac{\Delta t_{H1}}{t_1 - t_0} = \frac{\Delta\Omega}{t_n - t_0} \Delta t_{H1} \\ \vdots \\ \Delta\Omega_{Ln} = \Omega_{r\lim} - \Omega_r(t_{(n-1)\sim n}) = \frac{\Delta\Omega}{n} \times \frac{\Delta t_{Ln}}{t_n - t_{n-1}} = \frac{\Delta\Omega}{t_n - t_0} \Delta t_{Ln} \\ \Delta\Omega_{Hn} = \Omega_r(t_{(n-1)\sim n}) - \Omega_r(t_{n-1}) = \frac{\Delta\Omega}{n} \times \frac{\Delta t_{Hn}}{t_n - t_{n-1}} = \frac{\Delta\Omega}{t_n - t_0} \Delta t_{Hn} \end{array} \right. \quad (8)$$

In the figure: $\Delta\Omega_{Li}$ is the increment of Ω_r at the end of LVRT during the i-th L-HVRT; $\Delta\Omega_{Hi}$ is the increment of Ω_r at the end of HVRT during the i-th L-HVRT.

Substituting Eq. (8) into Eq. (7) yields:

$$\left\{ \begin{array}{l} \frac{P_{sL1}^*}{\Omega_r^*} \geq \left(T_m^* - \frac{T_j}{\Omega_1} \times \frac{\Delta\Omega}{t_n - t_0} \right) \\ \frac{P_{sH1}^*}{\Omega_r^*} \geq \left(T_m^* - \frac{T_j}{\Omega_1} \times \frac{\Delta\Omega}{t_n - t_0} \right) \\ \vdots \\ \frac{P_{sLn}^*}{\Omega_r^*} \geq \left(T_m^* - \frac{T_j}{\Omega_1} \times \frac{\Delta\Omega}{t_n - t_0} \right) \\ \frac{P_{sHn}^*}{\Omega_r^*} \geq \left(T_m^* - \frac{T_j}{\Omega_1} \times \frac{\Delta\Omega}{t_n - t_0} \right) \end{array} \right. \quad (9)$$

From Eq. (9), it can be obtained that $P_{sLi} = P_{sHi}$. Consequently, the active power reference P_{sref} output by the MSC during the L-HVRT period is set as:

$$P_{sref}^* = P_{sLi}^* = P_{sHi}^* = P_{smin}^* = \Omega_r^* \left(T_m^* - \frac{T_j}{\Omega_1} \times \frac{\Omega_{r\lim} - \Omega_r(t_0)}{t_n - t_0} \right) \quad (10)$$

In the figure: P_{smin} is the minimum active power output by the MSC.

In practical implementation, this control strategy relies on real-time online calculations. The controller continuously monitors the PCC voltage U_1 ; upon detecting a voltage sag or swell, the system retrieves the most stringent ride-through duration requirements corresponding to the current voltage depth from a grid-code-based look-up table. By substituting these standardized parameters—rather than uncertain actual fault durations—into Eq. (10), the safety-critical reference P_{sref} is determined. This “worst-case design” approach guarantees that the rotor speed remains within safe limits for any fault duration complying with grid standards.

To prevent control interactions during severe voltage faults, the strategy employs an explicit decoupled control architecture. The GSC prioritizes reactive power injection to comply with grid codes, rendering its active power export capability P_g a passive variable. To isolate P_g fluctuations from MSC stability, the braking resistor serves as a critical decoupling unit. By rapidly switching the resistor to stabilize U_{dc} , effective dynamic isolation is achieved between the MSC’s active power loop and the GSC’s reactive power loop. This

Notably, during deep voltage faults, the “reactive power priority” principle often drives the GSC into current saturation, constraining its active current i_{gd} to near-zero levels. This grid-side saturation does not hinder the effectiveness of the MSC’s active power control. Conversely, since the GSC is virtually unable to export active power ($P_g \approx 0$), the MSC’s active power curtailment strategy based on [Eq. \(10\)](#) becomes critical. This machine-side “active power reshaping” mechanism operates independently of the GSC state, mitigating the risk of power surplus accumulation at the source caused by GSC saturation. Consequently, it effectively prevents resistor overloading and ensures safe ride-through under extreme fault conditions.

When the control strategy proposed in this paper is adopted, if $\Delta P > 0$, the braking resistor R still needs to be connected. The switching scheme of the braking resistor R is the same as that of the traditional control strategy.

The diagram illustrates the control architecture for a fault-tolerant system. It begins with reference signals Q_r and U_l entering a block labeled "Equation(10)". The output of this block is fed into a "Selector switch" which can be in a "Normal state" (output 1) or a "Fault Status" (output 2). The selector switch's output is multiplied by a "Fault Trigger / i_d Saturation Signal". The resulting signal is then multiplied by P_s to produce $T_e = P_{sref} / Q_r$. This signal is processed by a "PI" block to generate i_{sqref} . The reference current i_{sqref} is then compared with the actual i_{sq} and the result is integrated by a "PI" block to produce the u_{sq} component. Similarly, the reference current i_{sdref} (which is zero) is compared with the actual i_{sd} and the result is integrated by a "PI" block to produce the u_{sd} component. The u_{sq} and u_{sd} signals are then combined in a "Pulse Width Modulation" block to produce the final output MSC .

Figure 1 illustrates the control block diagram of the GSC. The diagram shows the control loops for the d and q axes. The d-axis loop starts with a reference voltage U_{dc} and a feedback signal $2P_{gref}/3U_1$, which are summed and passed through a PI controller to produce a reference current i_{gdref} . This current is compared with the actual d-axis current i_{gd} (via a selector switch) and the result is summed with a feedback signal from the $\omega_1 L_q$ inductor to produce the d-axis voltage u_{gd} . The q-axis loop starts with a reference current i_{gqref} (via a selector switch) and the actual q-axis current i_{gq} , which is summed with a feedback signal from the $\omega_1 L_d$ inductor and passed through a PI controller to produce the q-axis voltage u_{gq} . Both u_{gd} and u_{gq} are summed with grid voltages u_{1d} and u_{1q} to produce the final reference voltages for the Pulse Width Modulation (PWM) block, which drives the GSC. A fault trigger signal is used to switch between normal and fault states in the current feedback paths.

Figure 4: Control block diagram of the GSC for the control strategy proposed in this paper.

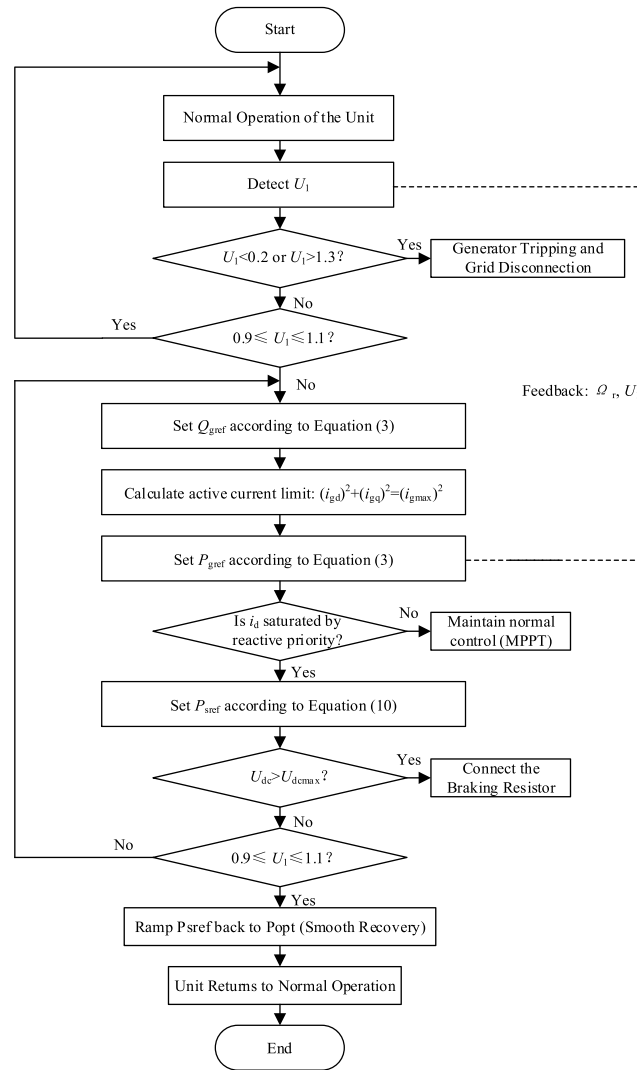


Figure 5: Flow chart of the control strategy proposed in this paper.

3.4 Comparative Analysis of Control Strategies

Eq. (10) constitutes the core contribution of this work, as it mathematically defines the optimal trade-off between “braking resistor load” and “rotor speed safety.” To illustrate the superiority of the proposed strategy in resolving this conflict, Table 1 presents a comprehensive performance comparison between the proposed method and two typical existing strategies: the traditional strategy [19] and the power balance strategy ($P_s = P_g$).

As detailed in Table 1, while the traditional strategy maintains rotor speed stability, it does so at the expense of sustaining high power output for extended periods. This results in extreme thermal stress on the braking resistor (ΔP maximization), significantly increasing the risk of component burnout. Conversely, the power balance strategy eliminates the thermal burden on the resistor by enforcing a reduction in machine-side power to match the grid side. However, this approach neglects the sustained mechanical torque input, causing rapid, uncontrolled rotor acceleration during faults, which ultimately leads to tripping due to overspeed protection.

Table 1: Performance characteristic comparison of different control strategies.

Strategy Name	MSC Control Principle	Impact on Ω_r	Impact on ΔP	Final Result/Potential Risk
Traditional Strategy [19]	Maintain maximum power output ($P_{sref} = P_{opt}$)	Remains stable	Maximized (Subjected to severe thermal stress)	Component thermal failure (high risk of resistor burnout)
Power Balance Strategy ($P_s = P_g$)	Force P_s to track P_g ($P_s = P_g$)	Severe overspeed (excess mechanical energy due to sudden drop in T_e)	Eliminated or negligible	Overspeed tripping (Triggers protection; ride-through failure)
Proposed Strategy	Power reshaping based on rotor energy storage (Eq. (10))	Safely controlled (strictly limited within the safety limit Ω_{rlim})	Significantly reduced (utilizes rotor inertia to absorb partial energy)	Successful ride-through (Optimal balance between resistor protection and speed safety)

In contrast, the strategy proposed in this paper effectively reconciles this conflict. By accurately calculating the maximum kinetic energy the rotor can absorb, the strategy achieves optimized active power reshaping. It avoids excessive resistor utilization while strictly confining the rotor speed within the safety limit Ω_{rlim} , thereby providing an optimal compromise for safe L-HVRT operation.

3.5 Mechanical Constraints and Control Stability

3.5.1 Mechanical Constraints and Shaft Stress Analysis

During L-HVRT, modulating the rotor speed necessitates strict adherence to mechanical constraints, particularly regarding shaft stress and torsional oscillations. Forcing P_s to strictly track P_g , as in the $P_s = P_g$ strategy, causes a severe step-drop in T_e . According to the two-mass drive train model, this significant transient mismatch between T_e and the slowly varying T_m induces high-frequency torsional oscillations, risking damage to the mechanical shaft.

The rotor kinetic energy-based strategy proposed in this paper inherently mitigates this mechanical constraint. Based on Eq. (10), the strategy derives an optimal, smoothed P_{sref} , preventing abrupt power drops. Consequently, the transient response of T_e is remarkably smooth, effectively eliminating torque step-changes. This approach decouples severe grid-side power fluctuations from the mechanical drive train, ensuring that shaft stress remains minimal and safely within operational margins throughout the fault ride-through process.

3.5.2 Interaction with MPPT and Smooth Recovery

Under normal conditions, the wind turbine operates in MPPT mode. During L-HVRT, however, the MPPT function is temporarily disabled to prioritize grid support and active power reshaping. Upon grid voltage recovery, an immediate switch back to the MPPT curve would cause a sudden surge in T_e , inducing a secondary mechanical shock. To prevent this, a smooth recovery mechanism is implemented. Following fault

clearance, P_{sref} is linearly ramped back to P_{opt} over a predefined recovery duration. This strategy smoothly releases the stored rotor kinetic energy into the grid, progressively decelerating the rotor while avoiding aerodynamic stalling and control instability.

3.5.3 Control Stability during Speed Modulation

Modulating the rotor speed by reshaping P_{sref} does not compromise the converter's dynamic stability. The proposed strategy functions as a dynamic saturation limit for the outer power loop. Because the inner current control loop has a significantly higher bandwidth—typically an order of magnitude faster than the outer electromechanical loop—the decoupled control structure is preserved. Furthermore, small-signal stability is maintained, as the PI controller tuning in the inner loop is unaffected by the transient variations of P_{sref} .

4 Simulation Analysis

To verify the effectiveness of the L-HVRT control strategy proposed in this paper, a simulation system for grid-connected operation of a 1.5 MW permanent magnet wind turbine of a certain model was built on the PSCAD simulation platform. At present, only China has put forward clear L-HVRT requirements. Therefore, the simulation platform built in this paper is based on the Chinese national standard [21].

4.1 Comprehensive Electrical Distance Based on Statistical Distance

The Chinese national standard has put forward specific requirements for the fault ride-through of wind farms. The L-HVRT requirements are shown in Fig. 6. During the L-HVRT period, the time requirements for the unit to maintain continuous operation without grid disconnection under different voltage sag and swell conditions are listed in Table 2.

The schematic diagram of the continuous ride-through requirements for wind farms specified in the Chinese national standard is shown in Fig. 6. The continuous ride-through requirements are as follows: (1) When a wind farm transitions rapidly from the low-voltage phase to the high-voltage phase, and the voltage at the wind farm grid connection point falls within the contour line shown in the shaded area, the wind turbines in the wind farm shall maintain continuous operation without grid disconnection; (2) The wind farm shall be able to withstand at least two consecutive low-high voltage ride-through events as shown in Fig. 6; (3) This paper clearly specifies the requirement for Δt_2 , and the requirements for Δt_1 and Δt_3 are listed in Table 2.

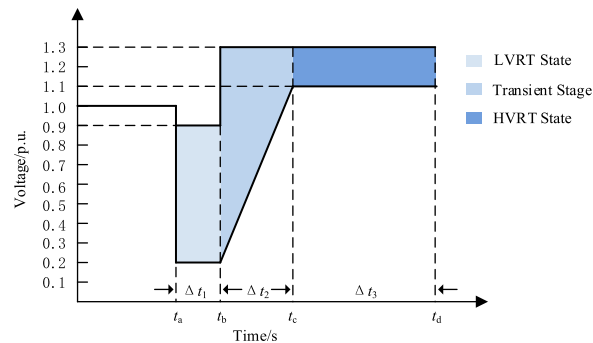


Figure 6: L-HVRT requirements for wind farms.

Table 2: The continuous operation time without grid disconnection that the unit shall maintain under different degrees of U_1 sag and swell.

U_1^*	The Continuous Operation Time That the Unit Shall Maintain without Grid Disconnection/s
[0.2, 0.9]	0.625
[1.1, 1.2]	10
(1.2, 1.25]	1.0
(1.25, 1.3]	0.5

Note: *: Denotes the per-unit (p.u.) value.

4.2 Simulation Parameters

In this paper, in accordance with the requirements of the Chinese national standard, the simulation analysis is carried out under the most severe conditions, where $\Delta t_2 = 0$ and the time interval between two L-HVRT events is also set to 0.

The rated wind speed of the certain model of wind turbine adopted in this paper is 12 m/s. The corresponding relationship between the wind speed V_w and the rotor speed Ω_r of the permanent magnet wind turbine used in the simulation is shown in Table 3, and the calculation results of T_m are also included in Table 3. The simulation parameters of the equipment used are listed in Table 4.

Table 3: Corresponding relationship between V_w and Ω_r of the wind turbine.

$V_w/(m/s)$	$\Omega_r/(r/min)$	T_m^*
≥ 12	17.3	1.02
11	17.13	0.81
10	16.95	0.68
9	16.26	0.51
8	14.71	0.34
7	13.32	0.26
6	12.63	0.15
5	9.69	0.087
4	8.65	0.06
3	8.3	0.027

Note: *: Denotes the per-unit (p.u.) value.

Table 4: The paraments used for the simulation.

Equipment	Parameter	Quantity
PMSG	Rated capacity	1.5 MW
	Rated Line Voltage	0.69 kV
	Rated speed	17.3 (r/min)
	$\Omega_{r\lim}$	20.76 (r/min)
	T_j	2 s

(Continued)

Table 4 (continued)

Equipment	Parameter	Quantity
GSC	U_{dc} Rated value	1.19 kV
	C	50 mF
	Rated phase current	1.255 kA
	i_{gmax}	1.1 p.u.
Route	R_e	1 m Ω
	L_e	0.5 mH
	R_1	0.17 Ω
	X_1	0.38 Ω
	R_2	3.4 Ω
	X_2	11.5 Ω
Power grid	U_1 Rated value	$\sqrt{2} \times 690 / \sqrt{3} = 563.38$ V
Discharge Resistor	R	4 Ω

In this paper, simulation analysis is carried out for three operating conditions. A voltage sag occurs at $t = t_0 = 2$ s. The degree and duration of the voltage sag and swell are set in accordance with [Table 2](#).

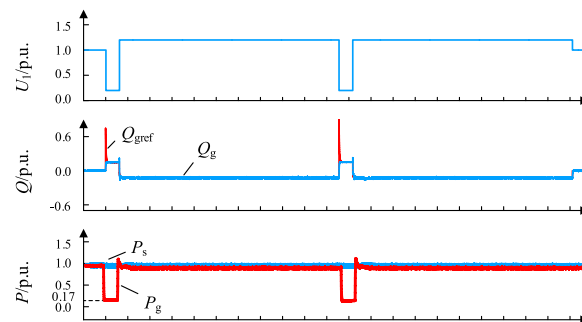
4.3 Simulation Results

4.3.1 Simulation Results of U_1 First Sagging to 0.2 p.u. and Then Swelling to 1.2 p.u. at $V_w = 12$ m/s

When $t = t_0 = 2$ s, U_1 repeats twice the process of first sagging to 0.2 p.u. and then swelling to 1.2 p.u.

(1) Simulation Results of the Traditional Control Strategy

The simulation results of the traditional control strategy are shown in [Fig. 7](#). Before the voltage sag, the wind turbine operates in a steady state, with $P_s = 1.0$ p.u., $P_g = 0.99$ p.u., and $U_{dc} = 1.0$ p.u. During LVRT, P_g is 0.171 p.u., which is consistent with P_{gref} ; during HVRT, P_g is 0.752 p.u., which also aligns with P_{gref} . During the L-HVRT period, $P_{sref} = P_{opt}$. When $U_{dc} > 1.1$ p.u., the braking resistor R is connected. During the LVRT period, the average value of ΔP is 0.83 p.u.; during the HVRT period, the average value of ΔP is 0.06 p.u.

**Figure 7:** (Continued)

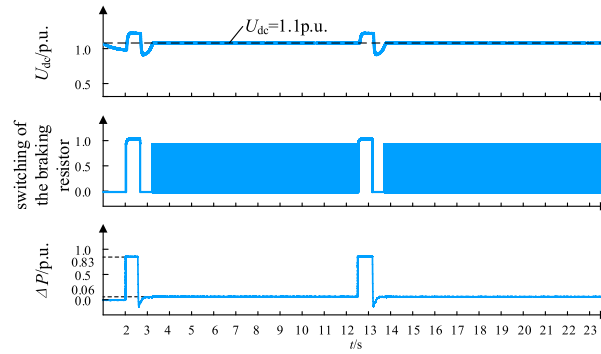


Figure 7: The simulation waveform of the traditional control strategy when U_1 drops to 0.2 p.u. and then rises sharply to 1.2 p.u.

(2) Simulation Results of Adopting the Control Strategy Proposed in This Paper

The simulation results of the control strategy proposed in this paper are shown in Fig. 8. During the L-HVRT period, the GSC adjusts the reactive current according to voltage support requirements. The simulation results show that $i_{gq} = -0.74$ p.u. during LVRT and $i_{gq} = 0.11$ p.u. during HVRT, which precisely meets the dynamic reactive current requirements specified in the national standard [21]. Limited by the maximum current capacity constraint of the converter, the available grid-side active power output P_g decreases accordingly, with $P_g = 0.171$ p.u. during LVRT and $P_g = 0.97$ p.u. during HVRT, both of which are consistent with the reference value P_{gref} . At this time, the proposed strategy simultaneously reduces the machine-side output power P_s to maintain power balance, with its reference value P_{sref} determined by Eq. (10). Since the reduction in P_s leads to an instantaneous deviation between the mechanical torque and the electromagnetic torque, the excess energy is converted into rotor kinetic energy, driving the rotor speed Ω_r to rise. When $U_{dc} > 1.1$ p.u., the braking resistor R is put into operation to assist in consuming the surplus power, and the average values of ΔP during the LVRT and HVRT stages are 0.82 and 0.05 p.u., respectively. Compared with the traditional control strategy, the proposed strategy not only prioritizes the reactive power support requirements of the grid but also effectively suppresses the DC-side overvoltage and mitigates the burden on the braking resistor by utilizing rotor inertia. This ensures that Ω_r is consistently kept within the safety limit, thereby achieving continuous fault ride-through for the wind turbine within the standardized fault durations (i.e., operating continuously for 0.625 s under a 0.2 p.u. voltage sag, and for 10 s under a 1.2 p.u. voltage swell).

(3) Simulation Results of Adopting the $P_s = P_g$ Control Strategy

The simulation results of adopting the $P_s = P_g$ control strategy are shown in Fig. 9. During the LVRT period, $P_{sref} = P_{gref} = 0.169$ p.u.; during the HVRT period, $P_{sref} = P_{gref} = 0.751$ p.u. When $U_{dc} > 1.1$ p.u., the braking resistor is connected. During the fault ride-through period, the average value of $\Delta P = 0$. Although the load on the braking resistor is reduced, $T_m > T_e$ continues to rise during the fault ride-through process due to Ω_r . When $t = 2.5$ s, Ω_r reaches Ω_{rlim} . In actual operation, this will trigger the overspeed protection action, leading to overspeed and grid disconnection of the wind turbine and failure of continuous fault ride-through.

In summary, under Operating Condition 1, the traditional control strategy will overload the braking resistor. In contrast, the control strategy proposed in this paper can not only reduce the load on the braking resistor but also prevent the wind turbine from overspeed and grid disconnection, thus achieving successful continuous ride-through.

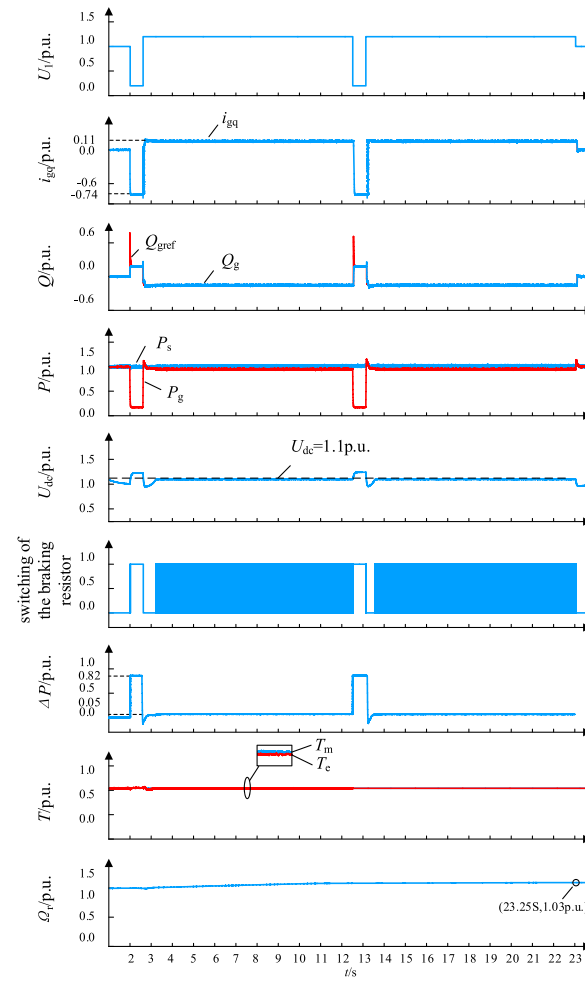


Figure 8: The simulation waveform of the proposed control strategy when U_1 drops to 0.2 p.u. and then rises sharply to 1.2 p.u.

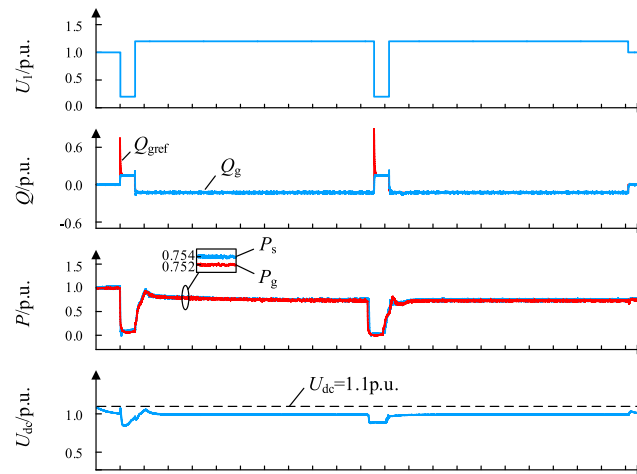


Figure 9: (Continued)

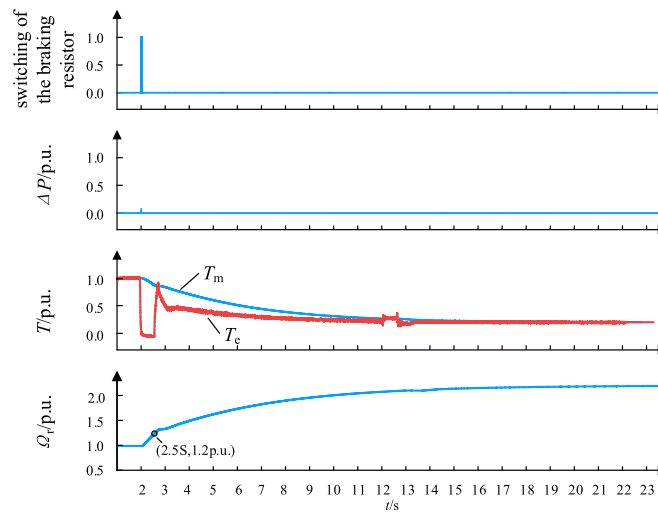


Figure 9: The simulation waveform of the $P_s = P_g$ when U_1 drops to 0.2 p.u. and then rises sharply to 1.2 p.u.

4.3.2 Simulation Results of U_1 First Sagging to 0.2 p.u. and Then Swelling to 1.25 p.u. at $V_w = 12$ m/s

When $t = t_0 = 2$ s, the process of first sagging to 0.2 p.u. and then swelling to 1.25 p.u. is repeated twice. Under this operating condition, the simulation results of the traditional control strategy are shown in Fig. 10, the simulation results of the control strategy proposed in this paper are shown in Fig. 11, and the simulation results of $P_s = P_g$ are shown in Fig. 12. The comparison of the average value of ΔP and Ω_{t2} is listed in Table 5.

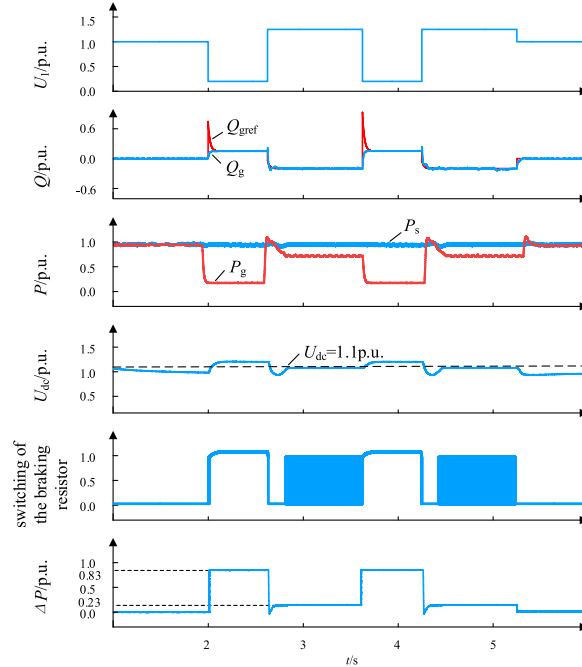


Figure 10: The simulation waveform of the traditional control strategy when U_1 drops to 0.2 p.u. and then rises sharply to 1.25 p.u.

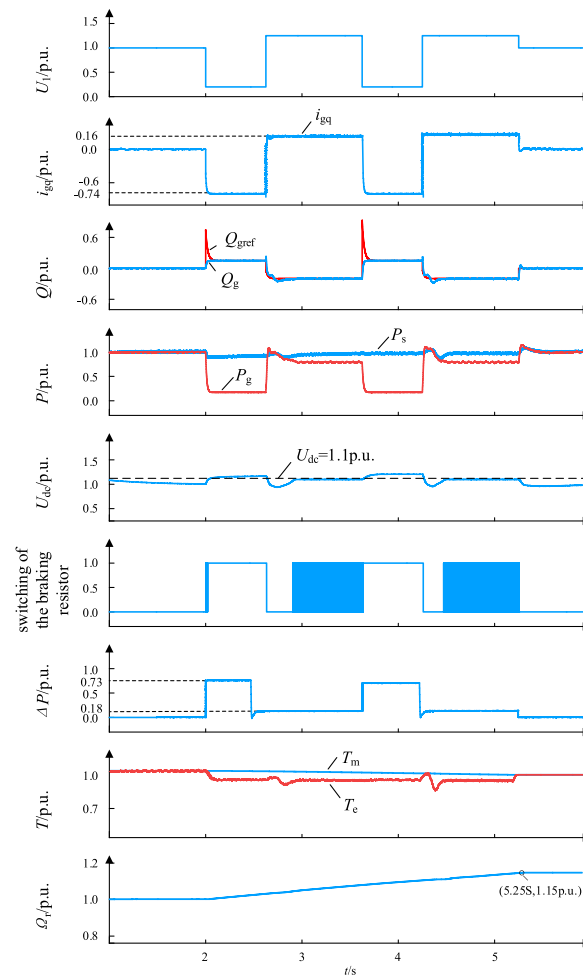


Figure 11: The simulation waveform of the proposed control strategy when U_1 drops to 0.2 p.u. and then rises sharply to 1.25 p.u.

According to the comparative results in Figs. 10–12 and Table 5, under Case 2, the traditional control strategy fails to regulate the machine-side power, forcing the braking resistor R to bear the entire power deviation ΔP . Although the resistor burden can be completely eliminated by significantly reducing P_s (e.g., the $P_s = P_g$ strategy), the total conversion of mechanical energy into rotor kinetic energy causes Ω_{t2} to reach 1.5 p.u.. This severely exceeds the speed limit $\Omega_{rlim} = 1.2$ p.u., which would trigger overspeed protection and lead to grid disconnection in practical operation.

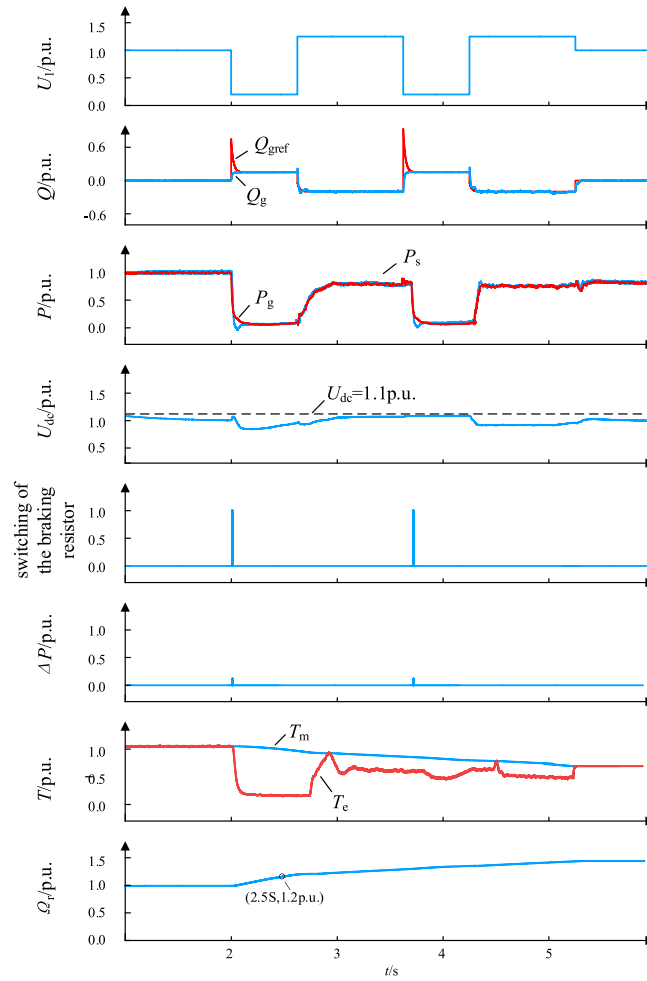


Figure 12: The simulation waveform of $P_s = P_g$ when U_1 drops to 0.2 p.u. and then rises sharply to 1.25 p.u.

Table 5: Comparison results of average ΔP and Ω_{t2} under different control strategies.

		Traditional Control Strategy	The Control Strategy Proposed in This Paper	$P_s = P_g$ Control Strategy
ΔP	LVRT	0.83	0.73	0
	HVRT	0.23	0.18	0
Ω_{t2}			1.15	1.5

In contrast, when the proposed control strategy is adopted, the average ΔP burden on the braking resistor R during the LVRT and HVRT stages is reduced to 0.73 and 0.18 p.u., respectively. Furthermore, Ω_{t2} only rises to 1.15 p.u., which is strictly below the overspeed tripping threshold ($\Omega_{rlim} = 1.2$ p.u.) specified by the national standard. This successfully alleviates the hardware stress while ensuring rotor speed safety, and smoothly completes the 1.0 s high-voltage (1.25 p.u.) ride-through period required by the grid code. When the voltage increases to 1.25 p.u., the grid-side converter must inject a higher level of reactive current

to suppress the overvoltage. Due to the physical constraint of the converter's maximum current capacity $i_{g\max}$, stronger reactive support further compresses the margin for grid-side active current, leading to more severe instantaneous power imbalance. Through the adaptive adjustment of Eq. (10), the proposed strategy converts more surplus energy into rotor kinetic energy in a controlled manner. This not only validates the effectiveness of the strategy under deep fault conditions but also intuitively reveals the positive correlation between reactive support intensity and rotor energy storage demand.

4.3.3 Simulation Results of U_1 First Sagging to 0.2 p.u. and Then Swelling to 1.3 p.u. at $V_w = 12$ m/s

When $t = t_0 = 2$ s, U_1 repeats twice the process of first sagging to 0.2 p.u. and then swelling to 1.3 p.u. Under this operating condition, the simulation results of the traditional control strategy are shown in Fig. 13, the simulation results of the control strategy proposed in this paper are shown in Fig. 14, and the simulation results of $P_s = P_g$ are shown in Fig. 15. The comparison of the average value of ΔP and Ω_{t2} is listed in Table 6.

According to the comparative results in Figs. 13–15 and Table 6, under the extreme high-voltage environment of Case 3, the GSC must output the ultimate level of reactive current to respond to grid support requirements, causing the current capacity conflict between reactive support and active power delivery to reach its peak. At this stage, the burden on the braking resistor under the traditional strategy is the heaviest. In contrast, through the adaptive adjustment of Eq. (10), the proposed strategy accurately directs the surplus power toward the rotor shaft system while strictly prioritizing grid reactive power support. As a result, the rotor speed reaches 1.164 p.u. at the end of the ride-through process, consistently remaining within the safety limit ($\Omega_{r\lim} = 1.2$ p.u.). This successfully accomplishes the severe 0.5 s high-voltage (1.3 p.u.) ride-through mandated by the grid code.

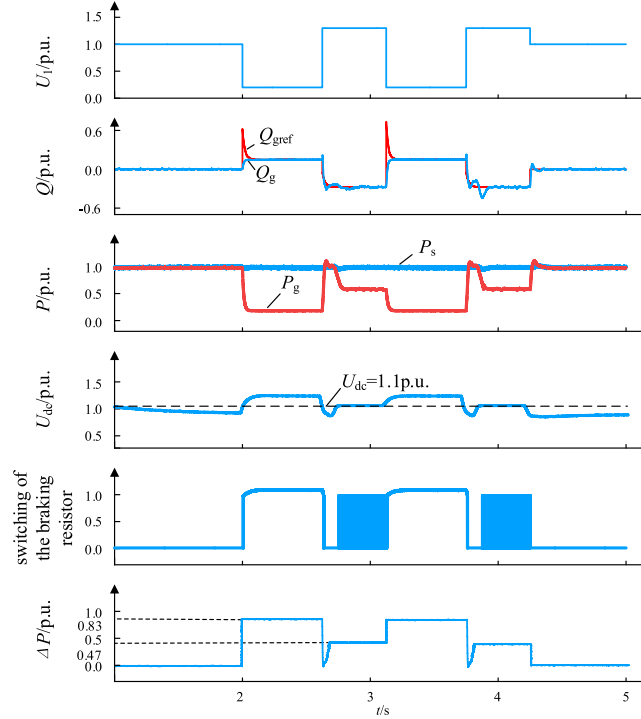


Figure 13: The simulation waveform of the traditional control strategy when U_1 drops to 0.2 p.u. and then rises sharply to 1.3 p.u.

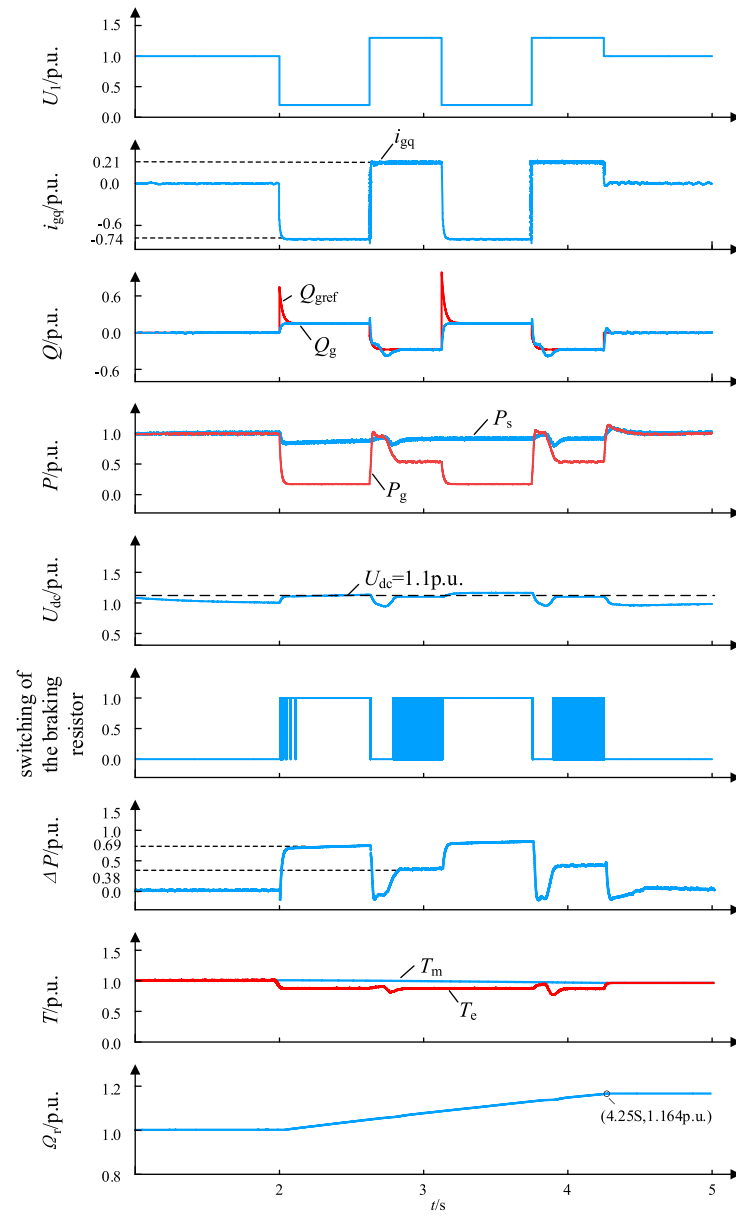


Figure 14: The simulation waveform of the proposed control strategy when U_1 drops to 0.2 p.u. and then rises sharply to 1.3 p.u.

Finally, based on the aforementioned analysis, the core technical contribution of this strategy in achieving continuous L-HVRT lies in the construction of a “decoupled control” architecture. Under this framework, the GSC is assigned to prioritize responding to grid voltage support by providing reactive power, while the MSC is tasked with independently managing rotor energy by regulating active power. This decoupling mechanism breaks the rigid coupling between grid-side capacity and machine-side control found in traditional strategies, enabling the system to flexibly utilize rotor inertia to ensure speed safety while strictly complying with grid connection codes for reactive support. It is precisely this control logic of “specialized roles and dynamic isolation” that guarantees the overall stability of the system under complex and continuous fault conditions.

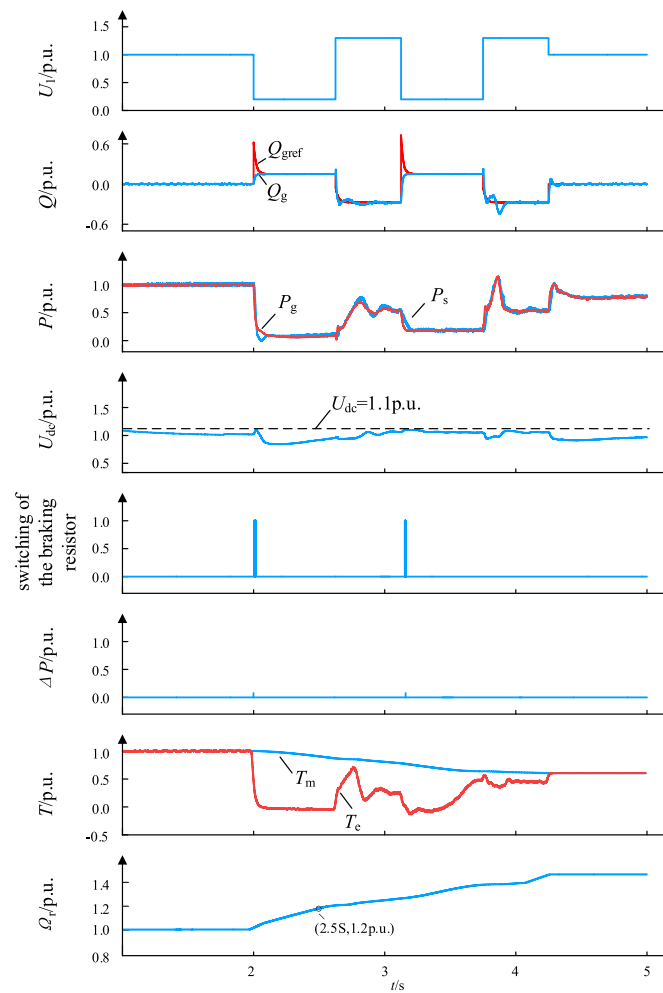


Figure 15: The simulation waveform of the $P_s = P_g$ when U_1 drops to 0.2 p.u. and then rises sharply to 1.3 p.u.

Table 6: Comparison results of average ΔP and Ω_{t2} under different control strategies.

		Traditional Control Strategy	The Control Strategy Proposed in This Paper	$P_s = P_g$ Control Strategy
ΔP	LVRT	0.83	0.69	0
	HVRT	0.47	0.38	0
Ω_{t2}			1.164	1.6

5 Conclusion

During the L-HVRT period, when the traditional control strategy is adopted, the active power reference value P_{sref} output by the MSC remains unchanged, while the grid-side converter (GSC) output power reference value P_{gref} is set in accordance with the requirements of Chinese national standards. The entire ΔP between the MSC and the GSC is borne by the braking resistor, which increases the load on the braking resistor.

To address the above problems, this paper fully considers reducing the load on the braking resistor on the basis of the original control strategy, and proposes an L-HVRT control strategy based on rotor energy storage. This strategy can not only reduce the load on the braking resistor but also prevent the rotor speed from exceeding the limit. On the PSCAD simulation platform, a continuous ride-through simulation model of a 1.5 MW permanent magnet wind turbine is built in accordance with Chinese national standards, and a comparative simulation analysis is carried out between the control strategy proposed in this paper and the traditional control strategy. The simulation results show that the proposed control strategy can reduce the load on the braking resistor and effectively prevent the rotor speed from exceeding the limit, which verifies the effectiveness of the proposed control strategy.

Acknowledgement: Not applicable.

Funding Statement: The authors received no specific funding.

Author Contributions: The authors confirm contribution to the paper as follows: study conception, task division, content planning, model development, simulation validation, and draft manuscript preparation: Jian Wang, Xiaye Wang; progress, direction oversight, and final manuscript review: Gongqiang Yang; data support, practical validation, and final manuscript review: Jieyan Wang. All authors reviewed and approved the final version of the manuscript.

Availability of Data and Materials: Due to the nature of this research, participants of this study did not agree for their data to be shared publicly, so supporting data is not available.

Ethics Approval: Not applicable.

Conflicts of Interest: The authors declare no conflicts of interest.

Nomenclature

Symbol	Description	Unit
P_w	Output power of the wind turbine	p.u.
P_s	Active power generated by the PMSG (machine-side)	p.u.
P_g	Active power delivered by the GSC (grid-side)	p.u.
P_{opt}	Optimal active power tracking value (MPPT)	p.u.
P_{sref}, P_{gref}	Reference values of P_s and P_g	p.u.
ΔP	Active power imbalance between MSC and GSC	p.u.
Q_g, Q_{gref}	Actual and reference reactive power of the GSC	p.u.
U_{dc}	DC-link voltage	p.u.
U_{dcmax}	Maximum allowable DC-link voltage	p.u.
U_1	Voltage at the point of common coupling (PCC)	p.u.
i_{gq}, i_{gd}	Reactive and active current of the GSC	p.u.
i_{gmax}	Maximum allowable current capacity of the GSC	p.u.
K_1	Proportional coefficient of dynamic reactive current	–
T_m	Mechanical torque transmitted to the PMSG rotor	p.u.
T_e	Electromagnetic torque of the PMSG	p.u.
Ω_r	Rotor mechanical angular velocity	p.u.
Ω_{rlim}	Allowable upper limit of rotor speed (overspeed threshold)	p.u.
T_j	Inertia time constant of the PMSG	s

(Continued)

(continued)

Symbol	Description	Unit
C	DC-link capacitance	mF
R	Braking resistor	Ω
V_m	Wind speed	m/s
*(<i>superscript</i>)	Denotes the per-unit (p.u.) value	–

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