Application research on pilot protection method for multi-terminal hybrid line-commutated converter/modular multilevel converter-based high voltage DC system

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1 | INTRODUCTION

The line commutated converter-based high voltage dc (LCC-HVDC) has commutation failure problem in the inverter side [1]. In Guangdong power grids, the simultaneous commutation failure of several LCCs has occurred at the inverter sides due to the multi-infeed LCC-HVDCs [2]. To solve the simultaneous commutation failure problem, the hybrid HVDC system with LCC as rectifier and modular multilevel converter (MMC) as inverter is adopted, which integrates the merits of both technologies. Nowadays, the world’s first multi-terminal hybrid LCC/MMC HVDC project called Kun–Liu–Long is under construction in China, delivering hydropower from southwest China to the Guangxi and Guangdong grids [3].

The topology and operation mode of multi-terminal hybrid HVDC system are different from that of point-to-point LCC-HVDC system or flexible HVDC system. Therefore, the dc line protection methods of conventional HVDC system and flexible HVDC system cannot be directly applied to multi-terminal hybrid HVDC system. The dc line protections include unit protection and non-unit protection. Unit protections do not need the line boundary elements and have absolute selectivity, especially when encountering the high-impedance faults. The unit protections can be divided into two categories, which are differential protections and pilot protections.

Current differential protection is used as backup protection in the traditional HVDC systems [4]. However, a delay time of 500 ms and a block time of 600 ms are applied to prevent the maloperation under external fault conditions [5]. This increases the fault identification time of the protection when internal faults occur. References [5]–[9] have proposed improved differential current protection methods. Reference [6] proposes a high-speed differential protection for smart dc distribution system, which significantly reduces fault detection time (around...
2 ms). However, the current flow in line capacitance is neglected, which is not true in long transmission line. The current distribution in the dc line can be calculated by voltage and current measured at line end under the Bergeron model, which provides new idea for eliminating the influence of distributed capacitance current on differential current protections. In reference [7], the differential current at the selected setting point is calculated for fault identification. This method will not be influenced by the distributed capacitance of the dc transmission line. However, the calculation consumption is large. In reference [5], a novel differential protection scheme based on compensation of distributed capacitive current is proposed, for which a low-pass filter is required. A novel differential current protection scheme is proposed in reference [8], which is composed of the differential current unit and blocking unit. This method is only applicable to LCC-HVDC system. Using the travelling wave propagation theory, a travelling wave-based differential protection is proposed in reference [9]. The method requires accurate frequency-dependent dc line parameters and large calculation consumption. The differential protections transmit the measured current value at one end of the protected line to the other end through communication links, which requires large amount of data transmission. Moreover, the potential need for synchronised measurements in both sides of the protected line is an important issue about the implementation of the differential method for power lines [10].

Pilot protections generally transmit logic value, so data synchronisation is not required. In reference [11], a pilot protection based on the ratio of transient voltage between the two sides of the supplemental inductor is proposed. The method depends on dc line boundary and the band-pass filtering is required. Moreover, the method is influenced by the fault impedance. In reference [12], a pilot protection based on the cosine similarity between the travelling waves measured at two sides of the dc line is proposed. The method still requires large number of data transmission. Moreover, the cosine similarity is easily influenced by communication errors, oscillation, and fault detection unit. In reference [13], a pilot protection for dc distribution systems is realised by comparing the high-frequency impedance measurement differences. The continuous-wavelet transform is required to extract high-frequency signals. Moreover, the single pole-to-ground fault is not considered. The backup protection based on variation tendency of current is proposed in reference [14]. The coupling between the two poles of the dc transmission lines is not considered, which will lead to maloperation of the healthy pole in case of line faults. References [15] and [16] propose the directional pilot protections based on the polarity of the fault component current. Neither of the methods considers coupling effect. In reference [17], a dc filter specific frequency current-based pilot protection is proposed. The method is only applicable to LCC-HVDC systems and the time delay of 60 ms is required. A pilot protection for multi-terminal HVDC grid using voltage polarity of current limiting reactor is proposed in reference [18]. This method requires voltage transducers installed at two sides of the current limiting reactor. In reference [19], a pilot protection based on open-close filtering and multi-resolution morphological gradient is proposed. In reference [20], a pilot protection is achieved using the polarity of the initial current travelling wave. The wavelet transform modulus maximum (WTMM) is utilised to get the current polarity. Both methods require high sampling frequency and have high calculation consumption.

At present, there is lack of research on unit protection for multi-terminal hybrid LCC/MMC HVDC system. Moreover, there is no engineering application research on unit protection for HVDC system. Here, the characteristics of the current travelling waves during the internal and external faults are derived, respectively, based on Peterson's law. Then a travelling wave-based directional pilot protection for multi-terminal hybrid LCC/MMC HVDC system is proposed. Different topologies of the HVDC systems are fully considered in fault analysis to propose the novel pilot protection method. Therefore, the proposed method is suitable for different HVDC system topologies. Moreover, the proposed method is based on mode fault component travelling wave. Thus, it is independent of grounding systems and operation mode of the HVDC system and coupling effect between the two poles. Relying on the cooperation with State Grid Corporation of China, the hardware design of the HCM5000 dc transmission control and protection device is completed and the Kun–Liu–Long three-terminal hybrid LCC/MMC HVDC RTDS test platform is built according to the actual project. The proposed protection method is implemented to the relay protection device and verified in the test platform.

2 | REFRACTION AND REFLECTION OF FAULT CURRENT TRAVELLING WAVE

In this section, the refraction and reflection of the fault current travelling wave in the multi-terminal hybrid LCC/MMC HVDC system are characterised by transfer functions. The Kun–Liu–Long three-terminal hybrid LCC/MMC HVDC project is the first multi-terminal HVDC using LCC/MMC hybrid technology in the world, which is the research object here, as shown in Figure 1. In Figure 1, $R_a$, $R_{b1}$, $R_{b2}$, and $R_c$ are the installation sites of the measurements and relay protection devices. The
current limit inductors are installed at the outlet of the MMC stations and the smoothing reactor as well as the dc filter are installed at the outlet of the LCC station.

The overhead line is of bipolar structure. The pole-to-mode transform is applied to decouple the interaction between the two poles, which can be expressed as follows:

\[
\begin{bmatrix}
\Delta n_0 \\
\Delta n_1
\end{bmatrix} = \frac{1}{\sqrt{2}} \begin{bmatrix}
1 & 1 \\
1 & -1
\end{bmatrix} \begin{bmatrix}
\Delta n_p \\
\Delta n_n
\end{bmatrix},
\]

(1)

\[
\begin{bmatrix}
\Delta i_0 \\
\Delta i_1
\end{bmatrix} = \frac{1}{\sqrt{2}} \begin{bmatrix}
1 & 1 \\
1 & -1
\end{bmatrix} \begin{bmatrix}
\Delta i_p \\
\Delta i_n
\end{bmatrix},
\]

(2)

where \(\Delta n_0\) and \(\Delta n_1\) are the positive-pole fault component voltage and current respectively; \(\Delta n_n\) and \(\Delta n_p\) are the negative-pole fault component voltage and current respectively; \(n_0\) and \(n_1\) are the zero-mode voltage and current respectively. \(\Delta i_1\) and \(\Delta i_0\) are the line-mode fault component voltage and current, respectively. \(\Delta n_0\) and \(\Delta n_1\) are the zero-mode fault component voltage and current respectively. Considering that the zero-mode voltage and current during the normal operation of the bipolar HVDC system is zero, \(\Delta n_0 = n_0\) and \(\Delta n_1 = n_1\).

In Figure 1, the reflection and refraction effects at line ends \(R_s, R_{s1}, R_{s12}\), and \(R_c\) are different. Therefore, three different situations are analysed, respectively.

### 2.1 Refraction and reflection at \(R_c\)

When \(f_2\) occurs at \(l_2\), the line-mode fault component Peterson equivalent circuit at MMC2 side is shown in Figure 2.

The fault-induced voltage at the fault point is approximately step wave. In Figure 2, the amplitude of the fault-point line-mode fault component voltage is \(u_{f1}\), the line-mode surge impedance of the dc line is \(Z_{c1}\), the current reference direction for measurement at \(R_c\) is from bus to line. The line-mode fault component current at \(R_c\) is the superposition of the fault current incident travelling wave and its reflected wave, which can be expressed as follows:

\[
\Delta I_{f1}(s) = \frac{-2u_{f1}}{s(Z_{c1} + sL_{dec})}.
\]

(3)

The transfer function \(H_{a1}(s)\) is defined to characterise the relationship between the fault current incident travelling wave and the current travelling wave after superposition, which can be expressed as follows:

\[
H_{a1}(s) = \frac{2Z_{c1}}{Z_{c1} + sL_{dec}}.
\]

(4)

When \(f_1\) occurs, the line-mode fault component Peterson equivalent circuit at MMC2 side is shown in Figure 3.

At this time, the line-mode fault component current at \(R_c\) can be expressed as follows:

\[
\Delta I_{f2}(s) = \frac{2u_{f1}}{s(Z_{c1} + sL_{dec})}.
\]

(5)

Then the transfer function \(H_{b1}(s)\) is defined to characterise the relationship between the fault current incident travelling wave and the current refraction wave, which can be expressed as follows:

\[
H_{b1}(s) = \frac{2Z_{c1}}{Z_{c1} + sL_{dec}}.
\]

(6)

### 2.2 Refraction and reflection at \(R_{s1}\) (\(R_{s2}\))

When \(f_1\) occurs at \(l_1\), the line-mode fault component Peterson equivalent circuit at MMC1 side is shown in Figure 4.

In Figure 4, \(\epsilon_{eq}\) is the equivalent capacitance of the MMC1 station. The line-mode fault component current at \(R_{s1}\) can be expressed as follows:

\[
\Delta I_{f3}(s) = \frac{-2u_{f1}}{s(Z_{c1} + sL_{dcb} + 1/\omega^2)}(Z_{c1} + sL_{dcb} + 2/\omega^2).
\]

(7)
The line-mode fault component current at $R_{a2}$ can be expressed as follows:

$$
\Delta I_{a2}(s) = \frac{2\alpha_{11}/s_{eq}}{Zc/(Zc + sL_{dca} + 2/2s_{eq})}.
$$

(8)

The transfer function $H_{a2}(s)$ can be expressed as follows:

$$
H_{a2}(s) = \frac{\Delta I_{a2}(s)}{\Delta I_{a}(s)} = \frac{2Zc (Zc + sL_{dcb} + 1/2s_{eq})}{(Zc + sL_{dcb}) (Zc + sL_{dcb} + 2/2s_{eq})}.
$$

(9)

The transfer function $H_{b2}(s)$ can be expressed as follows:

$$
H_{b2}(s) = \frac{\Delta I_{b2}(s)}{\Delta I_{b}(s)} = \frac{2Zc (Zc + sL_{dcb} + 1/2s_{eq})}{(Zc + sL_{dcb}) (Zc + sL_{dcb} + 2/2s_{eq})}.
$$

(10)

### 2.3 Refraction and reflection at $R_{a}$

When $f_2$ occurs at $l_2$, the line-mode fault component Peterson equivalent circuit at LCC side is shown in Figure 5.

In Figure 5, $l_1$ and $c_1$ are equivalent inductor and capacitor of the dc filter. The line-mode fault current component at $R_{a}$ can be expressed as follows:

$$
\Delta I_{a}(s) = \frac{-2\alpha_{1}(sL_{dcb} + sl_f + 1/2s_{eq})}{Zc (sL_{dca} + sl_f + 1/2s_{eq}) + 2s_{eq}L_{dca}(sl_f + 1/2s_{eq})}.
$$

(11)

The transfer function $H_{a}(s)$ can be expressed as follows:

$$
H_{a}(s) = \frac{\Delta I_{a}(s)}{\Delta I_{a}(s)} = \frac{2Zc (sL_{dcb} + sl_f + 1/2s_{eq})}{Zc (sL_{dca} + sl_f + 1/2s_{eq}) + s_{eq}L_{dca}(sl_f + 1/2s_{eq})}.
$$

(12)

When $f_2$ occurs, the line-mode fault component Peterson equivalent circuit at LCC side is shown in Figure 6.

The line-mode fault component current at $R_{b}$ can be expressed as follows:

$$
\Delta I_{b}(s) = \frac{2\alpha_{1}(Zc + sl_f + 1/2s_{eq})}{s(Zc + sL_{dcb}) (Zc + sL_{dcb} + 2/2s_{eq})}.
$$

(13)

The transfer function $H_{b}(s)$ can be expressed as follows:

$$
H_{b}(s) = \frac{\Delta I_{b}(s)}{\Delta I_{b}(s)} = \frac{2Zc (Zc + sl_f + 1/2s_{eq})}{s (Zc + sL_{dcb}) (Zc + sl_f + 1/2s_{eq}) + Zc (2sl_f + 1/2s_{eq})}.
$$

(14)

### 3 CHARACTERISTIC OF THE FAULT CURRENT TRAVELLING WAVE

#### 3.1 Propagation distortion of travelling wave

Considering the frequency-dependent parameters of the dc transmission line, the transfer function of the dc line can be expressed as follows [21]:

$$
H_{f}(s,x) = \frac{1 - k_{a1}x}{1 + \tau_{a1}x},
$$

(15)

where $k_{a1}$ is the line-mode attenuation coefficient of the dc transmission line in unit length, $\tau_{a1}$ is the line-mode dispersion time constant of the dc transmission line in unit length, $x$ is the fault distance from the fault point to the line end. The distortion of the travelling wave on the dc line can be characterised by $H_{f}(s,x)$.

#### 3.2 Fault current travelling waves under different fault conditions

According to the derivation in Section 2, the frequency-domain expressions of the fault current travelling waves at line ends $R_{a}$, $R_{b_1}$, $R_{b_2}$, and $R_{a}$ when faults $f_1$, $f_2$, $f_1$, and $f_2$ occur respectively are shown in Table 1.
Polarity of fault induced current should be held as follows:

\[ f \]

The inverse Laplace transform of the fault current travelling waves in frequency domain under different fault conditions can be expressed as follows:

\[ \Delta I_{e} \cdot \overline{H_{d}}(s) \cdot \Delta H_{\text{ab}}(s) \cdot \Delta H_{\text{ad}}(s) \cdot \Delta H_{\text{bd}}(s) \cdot \Delta H_{\text{cd}}(s) \cdot \Delta H_{\text{ef}}(s) \]

The results can be expressed as follows:

\[ f \]

In Table 1, \( H_{\text{bd}}(s) \) can be expressed as follows:

\[ H_{\text{bd}}(s) = \frac{2Z_{1}}{2Z_{1} + sL_{\text{dc}}} \]

3.3 | Polarity of fault current travelling wave

The inverse Laplace transform is adopted to Equations (4) and (6). The results can be expressed as follows:

\[ L^{-1}[H_{\text{bd}}(s)] = \frac{2Z_{1}}{L_{\text{dc}}} e^{-2Z_{1} s} I_{\text{dc}} > 0 \]

Using the same method, all the transfer functions defined above will satisfy \( L^{-1}[H_{\text{bd}}(s)] > 0 \), \( L^{-1}[H_{\text{cd}}(s)] > 0 \)

Lemma: If \( f(t) \geq 0 \) and \( g(t) \geq 0 \), then \( f \ast g \geq 0 \).

Proof: For \( f(t) \geq 0 \) and \( g(t) \geq 0 \), the expression should be held as follows:

\[ \int_{-\infty}^{+\infty} f(\tau)g(t-\tau) d\tau > 0 \]

for \( f(t) \leq 0 \), \( f(t) \neq 0 \) and \( g(t) \geq 0 \), the expression should be held as follows:

\[ \int_{-\infty}^{+\infty} [-f(\tau)]g(t-\tau) d\tau < 0 \]

In Table 1, the multiplication of the transfer functions in frequency domain means the convolution of their corresponding time-domain expressions. According to the lemma, since all the corresponding time-domain expressions satisfy \( L^{-1}[H(s)] > 0 \), the polarity of the fault current travelling wave is determined by the polarity of the fault induced current travelling wave \( \Delta I_{e}(s) \) at the fault point.

3.4 | Polarity of fault induced current travelling wave at the fault point

When a forward positive pole-to-ground fault occurs, the fault component network at the fault point is shown in Figure 7(a). In Figure 7(a), \( U_{f} \) is rated voltage of the dc line. \( R_{f} \) is the fault impedance. The positive-pole fault point to ground current is \( 2\Delta i_{p} \). The positive and negative fault component voltages at the fault point are \( \Delta u_{p} \) and \( \Delta u_{np} \), respectively.

The fault condition at the fault point can be expressed as follows:

\[ \begin{cases}
-U_{f} + 2\Delta i_{p} R_{f} = \Delta u_{p} = \frac{\sqrt{2}}{2} (u_{f0} + \Delta u_{f1}) \\
\sqrt{2} (i_{f0} + \Delta i_{f1}) = \Delta i_{p} \\
\sqrt{2} (i_{f0} - \Delta i_{f1}) = \Delta i_{p} = 0.
\end{cases} \]
Then the amplitudes of the zero-mode and line-mode fault component current travelling waves at the fault point can be expressed as follows:

$$i_{f0} = \Delta i_{f1} = \frac{\sqrt{2}U_f}{Z_c + Z_{c1} + 4R_f}.$$  \(22\)

When a forward negative pole-to-ground occurs, the fault component network at the fault point is shown in Figure 7(b). The fault condition at the fault point can be expressed as follows:

$$\begin{cases} U_f + 2\Delta i_{fp} R_f = \Delta u_{fp} = \frac{\sqrt{2}}{2}(u_{f0} - \Delta u_{f1}) \\ \frac{\sqrt{2}}{2}(i_{f0} + \Delta i_{f1}) = \Delta i_{fp} = 0 \\ \frac{\sqrt{2}}{2}(i_{f0} - \Delta i_{f1}) = \Delta i_{fp} \end{cases}$$  \(23\)

Then the amplitudes of the zero-mode and line-mode fault component current travelling waves at the fault point can be expressed as follows:

$$i_{f0} = \Delta i_{f1} = \frac{-\sqrt{2}U_f}{Z_c + Z_{c1} + 4R_f}.$$  \(24\)

When a forward interpole fault occurs, the fault component network at the fault point is shown in Figure 8(a). When a forward double-pole grounding fault occurs, the fault component network at the fault point is shown in Figure 8(b). The fault condition at the fault point can be expressed as follows:

$$\begin{cases} -2U_f + 2\Delta i_{fp} R_f = \Delta u_{fp} - \Delta u_{fp} = \sqrt{2} \Delta u_1 \\ \Delta i_{fp} = -\Delta i_{fp} \end{cases}$$  \(25\)

Then the amplitudes of the zero-mode and line-mode fault component current travelling waves at the fault point when interpole fault or double-pole grounding fault occurs can be expressed as follows:

$$\begin{cases} i_{f0} = 0 \\ \Delta i_{f1} = \frac{\sqrt{2}U_f}{R_f + Z_{c1}}. \end{cases}$$  \(27\)

Using the same method, the amplitudes of the zero-mode and line-mode fault component current travelling waves at the fault point when backward faults occur can also be obtained, then the polarity of the fault current travelling waves under different fault types is shown in Table 2.

### Table 2: Polarity of the fault current travelling waves under different fault types

| Fault type                           | \(i_0\) | \(\Delta i_1\) |
|-------------------------------------|--------|----------------|
| Forward positive pole-to-ground fault | >0     | >0             |
| Forward negative pole-to-ground fault | <0     | >0             |
| Backward positive pole-to-ground fault | <0     | <0             |
| Backward negative pole-to-ground fault | >0     | <0             |
| Forward double-pole faults          | =0     | >0             |
| Backward double-pole faults         | =0     | <0             |

4 | TRAVELLING WAVE-BASED DIRECTIONAL PILOT PROTECTION FOR MULTI-TERMINAL HYBRID LCC/MMC HVDC SYSTEM

The flowchart of the protection scheme is shown in Figure 9, which consists of pilot protection criterion and the lighting strike interference identification. In Figure 9, \(t_{\text{wait}}\) is the time the protection at the local end waits for the logic signal from the remote end to arrive. \(t_{\text{trans}}\) is the signal transmission time for communication.

4.1 Pilot protection criterion

According to Table 2, the fault direction and the faulty pole can be determined using the zero-mode and line-mode fault component current travelling waves at one end of the dc line. Then the
internal and external faults can be distinguished by exchanging information with the opposite line end.

The pilot protection criterion can be expressed as follows:

\[
\begin{cases}
\Delta i_P > k_{rel}\Delta i_{\text{unb}} \\
\Delta i_Q > k_{rel}\Delta i_{\text{unb}},
\end{cases}
\]

(28)

where \( P \) and \( Q \) represent two ends of the protected dc transmission line. \( \Delta i_P \) is the line-mode fault component current travelling wave measured at \( P \) end, \( \Delta i_Q \) is the line-mode fault component current travelling wave measured at \( Q \) end, \( k_{rel} \) is the reliable coefficient, which is greater than 1. \( \Delta i_{\text{unb}} \) is the maximum line-mode fault component unbalance current during the normal operation.

During the normal operation of the bipolar HVDC systems, the zero-mode and line-mode fault component currents and voltages are zero. However, there is inevitably unbalance between the two poles of the dc transmission lines in practical engineering. According to the operation experience, the unbalance current between the two poles in operation is less than 10 A for the Kun–Liu–Long three-terminal hybrid LCC/MMC HVDC project, whose rated current is 3 kA. The unbalance current in a real project is less than 0.003 p.u. Moreover, the noise of 26 dB (5%) is considered for the application research. Considering a reliable coefficient \( k_{rel} \), the threshold value for pilot protection is \( k_{rel}\Delta i_{\text{unb}} = 0.06 \) p.u.

For the identified internal faults, the faulty pole identification criterion can be expressed as follows:

\[
\begin{cases}
\dot{i}_0 > k_{rel}\dot{i}_{\text{unb}} & \text{Positive – pole fault} \\
\dot{i}_0 < -k_{rel}\dot{i}_{\text{unb}} & \text{Negative – pole fault} \\
-k_{rel}\dot{i}_{\text{unb}} < \dot{i}_0 < k_{rel}\dot{i}_{\text{unb}} & \text{Double – pole fault},
\end{cases}
\]

(29)

where \( \dot{i}_{\text{unb}} \) is the maximum zero-mode unbalance current during the double-pole faults. The identification of the faulty pole does not need communication.

Considering that the unbalance current of the HVDC system during the double-pole faults is less than 0.006 p.u. and the system noise is less than 5%, the threshold value for faulty pole identification criterion is \( k_{rel}\dot{i}_{\text{unb}} = 0.06 \) p.u.

### 4.2 Identification of lightning strike interference

The overhead lines are easy to be affected by lightning strike. When the overhead line is struck by the lightning and a line fault occurs, the protection should correctly send trip signal. When the overhead line is struck by lightning but there is no fault occurred, the protection should not send trip signal. This kind of lightning strike is called lightning strike interference. Therefore, the line protection method should discriminate between the line faults and the lightning strike interferences.

Generally, lightning strike interference can be divided into induction lightning and direct lightning strike. Direct lightning strike can be divided into lightning strike on tower interference, lightning strike on lightning conductor interference, and lightning shielding failure interference (lightning strike on overhead line interference). The characteristics of induction lightning and lightning strike on tower or lightning conductor are same, which can be classified into one category for analysis and identification. The lightning shielding failure should be analysed separately.

As for induction lightning, lightning strike on tower interference, and lightning strike on lightning conductor interference, the voltages they induce on the positive and negative poles are basically the same, so the zero-mode voltage will have a larger value, while the line-mode fault component voltage will be approximately zero. While as for single pole-to-ground faults, the ratio of zero-mode voltage and line-mode fault component voltage is \( Z_{\text{r0}}/Z_{\text{c1}} \approx 1 \). As for double-pole faults, the ratio of zero-mode voltage and line-mode fault component voltage is 0. Therefore, the ratio of zero-mode voltage and line-mode fault component voltage can be used to identify these three kinds of lightning strike interferences, the criterion can be expressed as follows:

\[
\left| \frac{\sum \dot{u}_0}{\sum \Delta \dot{r}_1} \right| \geq k_{\text{thre}},
\]

(30)

where \( k_{\text{thre}} \) is set to be larger than \( Z_{\text{r0}}/Z_{\text{c1}} \).

The possibility of the lightning shielding failure interference is very low. The current waveform for lightning shielding failure interference can be expressed in the form of double-exponential function, which can be expressed as follows:

\[
i(t) = A\dot{I}_{\text{peak}}(e^{-\alpha t} - e^{-\beta t}),
\]

(31)
where $I_{\text{peak}}$ is the peak of the lightning current, $A$ is waveform coefficient, $\alpha$ and $\beta$ are parameters. According to the statistical results, the wave front time of lightning current is between 1 and 5 $\mu$s, and the half peak time of lightning current is between 20 and 100 $\mu$s [22]. The standard lightning wave specified in IEC standard is 1.2/50 $\mu$s, which means the wave front time is 1.2 $\mu$s, and the half peak time is 50 $\mu$s. Then, the parameters for lightning current are $A = 1.03725$, $\alpha = 0.014659$ $\mu$s$^{-1}$, $\beta = 2.4689$ $\mu$s$^{-1}$.

The current waveform for the lightning shielding failure interference is different from that for the dc line fault. Therefore, in reference [23], the calculated correlation coefficient is used for identifying lightning shielding failure interference. The identification criterion can be expressed as follows:

$$|\rho_{\text{rel}}| \leq \rho_{\text{set}} = 0.4,$$  

where $I_{\text{rel}}$ is the zero-mode current measured at the beginning of the line when an actual fault occurs; $|$ is absolute symbol. $I_{\text{ref}}$ is the reference for correlation calculation, which is the zero-mode current measured at the beginning of the dc transmission line when a metallic fault occurs at the midpoint of the dc transmission line. Due to the significant difference in the current waveforms between the lightning shielding failure interference and the dc line fault, the calculated correlation coefficient for lightning shielding failure interference will be much smaller than that for dc line faults. The threshold value $\rho_{\text{set}}$ can be set to 0.4 for HVDC systems.

5 | ULTRA-HIGH-SPEED PROTECTION PROTOTYPE BASED ON COMPACT DC CONTROL AND PROTECTION DEVICE

Our research team cooperates with the XJ Electric Co., Ltd. (in China) to develop the HCM5000 dc transmission control and protection device, as shown in Figure 10.

The configuration of the ultra-high-speed protection prototype is shown in Figure 10(b). The host chassis is equipped with one processor board SPU, one information exchange board SSM, and one high-speed communication board STM. The boards and their main functions and interfaces are as follows:

1. The main processor SPU has fast operation function and Ethernet communication function. It has high-speed communication interface to complete real-time data transmission and has 60044–8 interface to conduct 60044–8 communication. The backplane has exclusive high-speed bus to carry out exclusive daughter board. The board includes two Ethernet interfaces, two high-speed communication interfaces, and one LC optical interface.

2. The information exchange board SSM has the Ethernet switchboard and shared memory board, which can realise the status indication of chassis and the input and output of switching value. The board includes two Ethernet interfaces and Optical B code synchronisation.

3. The high-speed communication board STM can realise high-speed bus communication. The board includes one USB interface and six high-speed optical interfaces.

6 | TEST STUDY

The test system consists of control and protection system, RTDS, and background monitor system. HSMD interface device realises the signal forwarding between the RTDS and the ultra-high-speed protection device. The input and output signals are transmitted in optical fibres. The proposed pilot protection method is implemented to the HCM5000 device. The real-time simulation platform of Kun–Liu–Long three-terminal hybrid LCC/MMC HVDC system built by XJ company is used to fully verify the proposed method as well as the protection prototype. The closed-loop simulation scheme is adopted in the test study, which is connected to the operator’s control layer monitoring system. The RTDS test system and the closed-loop simulation structure are shown in Figure 11. The parameters of the RTDS test system are shown in Table 3. The project adopts hybrid FB and HB MMC, the ratio of FBs and HBs for the hybrid MMC is 8:2. The length of dc line $l_1$ is 900 km, the length of dc line $l_2$ is 500 km. Both the dc overhead lines are represented using the frequency-dependent parameter model available in RTDS.

In the test, the simulation step of the RTDS test system is 50 $\mu$s and the sampling frequency of the protection device is 20 kHz. The protection threshold $k_{\text{rel}}A_{\text{unb}}$ for relay protections at $R_a$ and $R_{b1}$ is set to 0.06 p.u. (180 A). The protection threshold $k_{\text{rel}}A_{\text{unb}}$ for relay protections at $R_{b2}$ and $R_c$ is set to 0.06 p.u. (90 A). The faulty pole identification threshold $k_{\text{rel}}A_{\text{unb}}$ for relay protections at $R_c$ and $R_{b1}$ is set to 0.06 p.u. (180 A). The faulty
FIGURE 11  Closed-loop simulation scheme. (a) Kun–Liu–Long real-time digital simulator (RTDS) test system; (b) closed-loop simulation structure

pole identification threshold \( k_{rel} \) for relay protections \( R_b \) and \( R_c \) is set to 0.06 p.u. (90A). The detailed protection results are given in Table 4, and typical recorded waveforms are shown in Figures 12 and 13. In Table 4, − and ± represent the negative pole-to-ground faults and double-pole faults respectively. \( \times \) represents that the protection algorithm identifies the faults as external faults. \( \sqrt{\text{ }} \) represents that the protection algorithm identifies the faults as internal faults. \( P \) represents that the protection algorithm identifies the faults as positive pole-to-ground faults, \( N \) represents that the protection algorithm identifies the faults as negative pole-to-ground faults, and \( D \) represents that the protection algorithm identifies the faults as double-pole faults.

In Table 4, numerous faults of different locations have been set to verify the proposed pilot protection method. It can be seen from Table 4 that the polarity of the line-mode fault component current \( \Delta i_l \) in case of forward faults is positive, and that for external faults is negative. The polarity of the zero-mode current \( i_0 \) in case of positive-pole faults is positive and that for negative-pole faults is negative. \( i_0 \) is nearly zero during the double-pole faults. Therefore, the test results are consistent with the theoretical analysis results in Table 2. The test results have indicated that the proposed method can correctly and fast identify internal and external faults. For the identified internal faults, the faulty pole can also be determined correctly.

6.1 Different fault impedances

In Table 4, the proposed method is verified under 500 Ω fault conditions. The fault impedance only influences the amplitude of the fault current travelling waves while the polarity of the fault current travelling wave is not influenced by the fault impedance, as shown in Figure 13(b) and (c). Therefore, the proposed method has high sensitivity when high-impedance faults occur.

6.2 Different fault types

In Table 4, the proposed method is verified under single-pole fault and double-pole fault conditions. The proposed method is not influenced by the fault types, as shown in Figure 12.

The proposed method uses pole-to-mode transform to decouple the coupling effect between the two poles. Therefore, the proposed method overcomes the maloperation caused by the coupling theoretically.

6.3 Commutation failures

Due to the firing angle of the rectifier is very small and the turn-off angle of the rectifier is very large, the rectifiers are in the turn-off state under the negative voltage for a relative long time in one cycle. The commutation failure will rarely occur at the rectifier unless the false firing pulse has occurred. On the contrary, the firing angle of the inverter is very large and the extinction angle of the inverter is very small. The converter valve at the inverter side is in the turn-off state under

| TABLE 3 | Parameters of the three-terminal hybrid line commutated converter (LCC)/modular multilevel converter (MMC) high voltage dc (HVDC) RTDS test system |
|---|---|---|
| **LCC** | **MMC\(_1\)** | **MMC\(_2\)** |
| Rated capacity (MW) | 2000 | 750 | 1250 |
| dc rated voltage (kV) | 400 | 400 | 400 |
| dc inductor (mH) | 150 | 100 | 75 |
| ac system voltage (kV) | 525 | 525 | 525 |
| Transformer ratio (integrated in each converter) | 525/175 | 525/220 | 525/220 |
| Transformer leakage inductance | 0.2 | 0.16 | 0.18 |
| Number of arm sub-module | / | 200 | 220 |
| Sub-module capacitance (mF) | / | 12 | 18 |
| Arm reactance (mH) | / | 55 | 40 |
TABLE 4 Protection results of the proposed methods

| Fault type          | $R_a$  | $i_i$ (kA) | $R_b$  | $i_i$ (kA) | $R_c$  | $i_i$ (kA) |
|---------------------|--------|------------|--------|------------|--------|------------|
| $f_i$ for 0Ω        | 0.19   | 0.21       | 0.19   | 0.20       | 0.19   | 0.19       |
| $f_i$ 0km 0Ω        | 0.27   | 0.24       | 0.19   | 0.20       | 0.19   | 0.20       |
| $f_i$ 15km 0Ω       | 0.26   | 0.24       | 0.19   | 0.20       | 0.19   | 0.20       |
| $f_i$ 300km 0Ω      | 0.45   | 0.21       | 0.21   | 0.20       | 0.21   | 0.20       |
| $f_i$ 300km 0Ω      | 0.45   | 0.21       | 0.21   | 0.20       | 0.21   | 0.20       |
| $f_i$ ±300km 0Ω     | 0.91   | 0.22       | 0.22   | 0.20       | 0.21   | 0.20       |
| $f_i$ 600km 0Ω      | 0.37   | 0.22       | 0.22   | 0.20       | 0.21   | 0.20       |
| $f_i$ 600km 0Ω      | 0.47   | 0.23       | 0.23   | 0.20       | 0.22   | 0.20       |
| $f_i$ 600km 0Ω      | 0.48   | 0.23       | 0.23   | 0.20       | 0.22   | 0.20       |
| $f_i$ 600km 0Ω      | 0.48   | 0.23       | 0.23   | 0.20       | 0.22   | 0.20       |
| $f_i$ 1Ω 0Ω        | 0.24   | 0.22       | 0.22   | 0.20       | 0.21   | 0.20       |
| $f_i$ 85km 0Ω       | 0.23   | 0.22       | 0.22   | 0.20       | 0.21   | 0.20       |
| $f_i$ 88km 0Ω       | 0.23   | 0.22       | 0.22   | 0.20       | 0.21   | 0.20       |
| $f_i$ 900km 0Ω      | 0.22   | 0.22       | 0.22   | 0.20       | 0.21   | 0.20       |
| $f_i$ ±88km 0Ω      | 0.19   | 0.19       | 0.19   | 0.18       | 0.19   | 0.18       |
| $f_i$ ±500km 0Ω     | 0.18   | 0.18       | 0.18   | 0.17       | 0.18   | 0.17       |
| $f_i$ ±800km 0Ω     | 0.18   | 0.18       | 0.18   | 0.17       | 0.18   | 0.17       |
| $f_i$ ±1600km 0Ω    | 0.18   | 0.18       | 0.18   | 0.17       | 0.18   | 0.17       |
| $f_i$ ±4800km 0Ω    | 0.18   | 0.18       | 0.18   | 0.17       | 0.18   | 0.17       |

For the multi-terminal hybrid LCC/MMC HVDC system, the LCC is used as rectifier and the MMC is used as inverter. Therefore, there is no commutation failure problem in hybrid HVDC system. Considering that the proposed method is also suitable for pure LCC-HVDC systems, a ±800 kV Yunnan-Guangdong pure LCC-UHVDC project in China is adopted to verify the performance of the proposed method under commutation failure. The specific parameters and structures of the pure LCC-UHVDC test system can be seen in reference [23].

A metallic positive pole-to-ground fault occurred at 100 km from the rectifier and a metallic double-pole fault occurred at 1400 km from the inverter are set to verify the proposed method, as shown in Figure 14. It can be seen from Figure 14 that the polarities of the line-mode fault component currents at two sides of the dc line are all positive and the proposed method can correctly identify the two faults as internal faults.
Then the zero-mode currents for positive-pole fault is positive, it is identified as positive-pole fault. The zero-mode currents for double-pole fault is nearly zero, which is correctly identified as double-pole fault.

When a 10 Ω single phase-to-ground fault occurs at the ac system at the inverter side, the fault causes the commutation failure at the inverter, as shown in Figure 15(a). When a metallic three-phase-to-ground fault occurs at the ac system at the inverter side, the fault causes the continuous commutation failure at the inverter, as shown in Figure 15(b). The commutation failure can be seen as a short circuit fault at the outlet of the converter at the inverter side, which is a backward fault for protection at inverter side. Therefore, the proposed pilot protection will not be influenced by the commutation failure. As shown in Figure 15, the polarity of the line-mode fault component current at the inverter side is negative for both the commutation failures. The proposed method can correctly identify commutation failure as external fault.

6.4 Lightning strike interferences

Typical lightning strike interferences, lightning strike on tower interference, lightning strike on lightning conductor interference, lightning shielding failure interference, and induction lighting, are set to evaluate the proposed method, as shown in Table 5. The waveform for lightning shielding failure can be

TABLE 5 Influence of lightning strike interferences on proposed method

| Lightning strike interference type | $\frac{\sum \mu_2}{\sum \Delta \mu_1}$ | $\rho$ | Lightning strike? |
|----------------------------------|-----------------|--------|-------------------|
| Lightning strike on tower        | $\gg \frac{Z_0}{Z_1}$ | --     | ✓                 |
| Lightning strike on lightning conductor | $\gg \frac{Z_0}{Z_1}$ | --     | ✓                 |
| Induction lightning              | $\gg \frac{Z_0}{Z_1}$ | --     | ✓                 |
| Lightning shielding failure      | $\frac{Z_0}{Z_1}$ | 0.25   | ✓                 |
seen from Figure 16, which is different from the waveforms for dc line faults. The correlation coefficient for lightning shielding failure is 0.25.

### 6.5 Converter faults

The converter fault mainly includes three types, which are (i) commutation failure at the inverter side, (ii) short circuit fault of converter main wiring circuit, and (iii) converter valve and its control system fault. The commutation failure is considered in Section 6.3, therefore, the primary concern in this part is fault types (ii) and (iii).

For fault type (ii), the diagram of main fault types of the converter can be seen from Figure 17. In Figure 17, K1 is converter valve short circuit fault, which includes: K1(1) is high voltage bridge common cathode valve group fault, K1(2) is high voltage bridge common anode valve group fault, K1(3) is low voltage bridge common cathode valve group fault, K1(4) is low voltage bridge common anode valve group fault. K2 is converter short circuit fault at the outlet of dc side, which includes: K2(1) is high voltage side to converter neutral point short circuit fault, K2(2) is converter neutral point to neutral dc bus short circuit fault, K2(3) is high voltage side to neutral dc bus short circuit fault. K3 is converter dc side fault, which includes: K3(1) is high voltage side to ground short circuit fault, K3(2) is converter neutral point to ground short circuit fault, K3(3) is neutral dc bus to ground short circuit fault. K4 and K5 are converter ac side faults, which includes: K4(1) is high voltage bridge ac side interphase short circuit, K4(2) is low voltage bridge ac side interphase short circuit.
FIGURE 17  Diagram of short circuit fault of converter main wiring circuit

TABLE 6  Influence of converter main wiring short circuit fault on proposed method

| Converter fault type | Relay protection at $R_a$ | Relay protection at $R_{a1}$ | Internal fault? |
|----------------------|---------------------------|-----------------------------|----------------|
|                      | $\Delta i_1$ (kA) | $i_0$ (kA) | $\Delta i_1$ (kA) | $i_0$ (kA) |          |
| K1(1)                | -0.19                  | -              | 0.19             | 0.19       | x        |
| K1(2)                | -0.19                  | -              | 0.19             | 0.19       | x        |
| K1(3)                | -0.19                  | -              | 0.19             | 0.19       | x        |
| K1(4)                | -0.19                  | -              | 0.19             | 0.18       | x        |
| K2(1)                | -0.19                  | -              | 0.18             | 0.18       | x        |
| K2(2)                | -0.19                  | -              | 0.19             | 0.18       | x        |
| K2(3)                | -0.19                  | -              | 0.20             | 0.19       | x        |
| K3(1)                | -0.18                  | -              | 0.22             | 0.21       | x        |
| K3(2)                | -0.18                  | -              | 0.19             | 0.19       | x        |
| K3(3)                | /                      | -              | /                | -          | x        |
| K4(1)                | -0.18                  | -              | 0.19             | 0.18       | x        |
| K4(2)                | -0.18                  | -              | 0.18             | 0.19       | x        |
| K5(1)                | -0.18                  | -              | 0.22             | 0.21       | x        |
| K5(2)                | -0.18                  | -              | 0.19             | 0.19       | x        |

side interphase short circuit, $K_5(1)$ is high voltage bridge ac side single phase-to-ground short circuit, $K_5(2)$ is low-voltage bridge ac side single phase-to-ground short circuit.

The detailed protection results in case of the aforementioned converter faults are shown in Table 6. Typical short circuit faults of the converter wiring circuit are shown in Figure 18.

For fault type (iii), the converter valve false firing and converter valve failure are considered to verify the proposed method, as shown in Figure 19. The continuous converter valve failure can be equivalent to fault at the outlet of the converter, which is a backward fault for protection at $R_a$. Therefore, the polarity of the line-mode fault component current at $R_a$ is negative in case of the continuous converter valve failure, as shown in Figure 19(a). The continuous converter valve false firing has little influence on the dc currents, as shown in Figure 19(b).

According to the simulations, it can be concluded that the converter faults are essentially backward faults for relay protection at the outlet of the converter at the line side. Therefore, the polarity of the line-mode fault component current will be negative in case of all kinds of converter faults. In this way, the proposed pilot protection method can identify the converter faults.
as external faults. The proposed method will not be influenced by the convert faults.

### 6.6 Adjacent ac system faults

When the phase A and phase B interphase fault and phase C phase-to-ground fault occurs at the adjacent ac system respectively, the recorded fault current travelling wave waveforms can be seen from Figure 20.

According to Figure 20, the protection at $R_a$ will identify the adjacent ac system faults as backward faults, therefore, the pilot protection can correctly identify the adjacent ac system faults as external faults. The pilot protection will not operate when the adjacent ac system faults occur.

### 7 CONCLUSION

Based on the analysis of the fault current travelling wave, a novel pilot protection method for multi-terminal hybrid LCC/MMC HVDC system is proposed. The proposed algorithm is implemented to the designed ultra-high-speed protection prototype and verified in the real project-based RTDS test system. The test results show that the proposed method can fast and correctly identify faults in the hybrid HVDC system. The proposed method meets the engineering requirement of HVDC system for line protection.

The research in our paper fully considers (i) faults at different locations, (ii) travelling wave refraction and reflection at different line boundaries, (iii) different topologies (multi-terminal or grids) and converter types (LCC or MMC), and (iv) different fault types (fault impedance and faulty pole). Moreover, the proposed algorithm is implemented to the protection device and verified in real-project-based RTDS system for application research. Therefore, this research has stronger theoretical basis and application prospect.
FIGURE 20 Recorded fault current travelling wave waveforms. (a) When a phase C phase-to-ground fault occurs at the adjacent ac system; (b) when a phase A and phase B interphase fault occurs at the adjacent ac system

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REFERENCES
1. Xu, Z., et al.: Hybrid high-voltage direct current topology with line commutated converter and modular multilevel converter in series connection suitable for bulk power overhead line transmission. IET Power Electron. 9(12), 2307–2317 (2016)
2. Hong, R., et al.: Study on improvement of VSC-HVDC at inverter side of Wudongde multi-terminal UHVDC for the problem of centralized multi-infeed HVDC. South. Power Syst. Technol. 11(3), 1–5 (2019)
3. Han, W., et al.: A three-terminal hybrid HVDC system based on LCC and hybrid MMC with DC fault clearance capability. In: 2019 10th International Conference on Power Electronics and ECCE Asia, Busan, Korea (South), pp. 1–7. IEEE, Piscataway, NJ (2019)
4. Takeda, H., et al.: New protection method for HVDC lines including cables. IEEE Trans. Power Del. 10(4), 2035–2039 (1995)
5. Zheng, J., et al.: A novel differential protection scheme for HVDC transmission lines. Int. J. Electr. Power Energy Syst. 94, 171–178 (2018)
6. Fletcher, S. D. A., et al.: High-speed differential protection for smart DC distribution systems. IEEE Trans. Smart Grid 5(5), 2610–2617 (2014)
7. Gao, S., et al.: Current differential protection principle of HVDC transmission system. IET Gener. Transm. Distrib. 11(5), 1286–1292 (2017)
8. Kong, F., et al.: Improved differential current protection scheme for CSC-HVDC transmission lines. IET Gener. Transm. Distrib. 11(4), 978–986 (2017)
9. Xue, S., et al.: Longitudinal travelling wave differential protection for flexible HVDC system based on Marti model. Proc. CSEE, 39(21), 6288–6299 (2019)
10. Monadi, M., et al.: Multi-terminal medium voltage DC grids fault location and isolation. IET Gener. Transm. Distrib. 10(14), 3517–3528 (2016)
11. Liu, J., et al.: Transient-voltage-based protection scheme for DC line faults in the multi-terminal VSC-HVDC system. IEEE Trans. Power Delivery 32(3), 1483–1494 (2017)
12. Wang, Y., et al.: A pilot protection scheme for transmission lines in VSC-HVDC grid based on similarity measure of traveling waves. IEEE Access 7, 7147–7158 (2019)
13. Jia, K., et al.: Transient high-frequency impedance comparison-based protection for flexible DC distribution systems. IEEE Trans. Smart Grid 11(1), 323–333 (2020)
14. Li, S., et al.: A novel integrated protection for VSC-HVDC transmission line based on current limiting reactor power. IEEE Trans. Power Delivery 35(1), 226–233 (2020)
15. Gao, S., et al.: Novel pilot protection principle for high-voltage direct current transmission lines based on fault component current characteristics. IET Gener. Transm. Distrib. 9(5), 468–474 (2015)
16. Li, M., et al.: Full-current-based directional pilot protection for VSC-D C distribution systems. IET Gener. Transm. Distrib. 13(16), 3713–3724 (2019)
17. Zhang, Y., et al.: A new protection scheme for HVDC transmission lines based on the specific frequency current of DC filter. IEEE Trans. Power Delivery 34(2), 420–429 (2019)
18. Huang, Q., et al.: A pilot protection scheme of DC lines for multi-terminal HVDC grid. IEEE Trans. Power Delivery, 34(5), 1957–1966 (2019)
19. Zhou, Y., et al.: A novel pilot protection based on open-close filtering and multi-resolution morphological gradient operators for HVDC transmission line. Proc. CSEE. https://doi.org/10.13334/j.0258-8013.pcsee.190118
20. Zou, G., et al.: A fast protection scheme for VSC based multi-terminal DC grid. Int. J. Electr. Power Energy Syst. 98, 307–314 (2018)
21. Zang, C., et al.: A novel traveling wave protection method for DC transmission lines using current fitting. IEEE Trans. Power Delivery. https://doi.org/10.1109/TPWRD.2019.2960368
22. Tang, L., et al.: Principle and implementation of ultra-high-speed travelling wave based protection for transmission line of flexible HVDC grid. Power Syst. Technol. 42(10), 3176–3186 (2018)
23. Zhang, C., et al.: Non-unit travelling wave protection method for dc transmission line using waveform correlation calculation. IET Gener. Transm. Distrib. 14(12), 2263–2270 (2020)

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