

Signal Processing for Fast Fault Detection in HVDC Grids

Willem Leterme, *Member, IEEE*, Mike Barnes, *Senior Member, IEEE*,
and Dirk Van Hertem, *Senior Member, IEEE*

Abstract—For multiterminal or meshed Voltage Source Converter (VSC) High-voltage Direct Current (HVDC) systems, high speed protection against DC faults is essential, as power electronic components cannot withstand the rapidly increasing fault currents which would otherwise result. Recently proposed DC fault detection methods were developed based on time domain simulations in EMT-type software, which requires considerable modeling and computational efforts and results in methods specifically designed for the HVDC grid under study. To simplify the initial design of DC fault detection methods, this paper proposes general guidelines based on fundamental theory and offers a reduced modeling approach. Furthermore, the impact of non-ideal measurements is investigated and a method to choose the filters that optimally discriminate these fault signals from noise, is proposed. The approach was evaluated in a case study on fault detection in a realistically dimensioned HVDC grid. The paper shows that the initial design of fast fault detection methods can be based on the relatively simple proposed guidelines and reduced models. The paper furthermore shows that a sufficiently high sampling frequency and a filter matched to the fault signal enable fault detection within hundreds of microseconds and discrimination of DC faults from transients not related to DC faults.

Index Terms—HVDC, matched filter, measurement, power system faults, power system transients.

I. INTRODUCTION

HVDC is expected to play an important role in the future European power system, with about 20000 out of 48000 km of projected new lines or line upgrades [1]. These will provide the transmission capacity needed to achieve the European targets for renewable energy and market integration. Compared to point-to-point connections, multiterminal or meshed Voltage Source Converter (VSC) HVDC systems require less infrastructure for the connection of large amounts of offshore renewable energy sources [2]. To comply with requirements on security of supply, these HVDC grids must be adequately protected against DC faults.

For HVDC grid protection, high speed fault clearing is essential as fault currents rapidly reach high values, whereas power electronic components in the grid only allow tight safety margins on operating voltages and currents. Various fault clearing strategies have been proposed in the literature. Selectivity in DC fault clearing can be achieved by using selective protection algorithms in combination with DC breakers at the end of every transmission line. Since DC breakers are more complex than their ac counterparts, alternative fault clearing strategies were developed to limit their use [3]. These strategies use converter ac breakers, converters with DC fault current blocking capability, a limited number of DC breakers or a combination thereof [4], [5]. Using these strategies, the HVDC grid is partly or entirely de-energized in case of DC side faults.

Each fault clearing strategy relies on high speed fault detection to meet the required speed of operation. The speed of fault detection depends on the one hand on the signals used and on the other hand on the delay introduced by the measurement unit and the digital filtering needed to discriminate the signal from noise or non-DC fault transients.

For existing VSC HVDC point-to-point connections, a limited amount of information is publicly available in the literature on line or cable protection. In [6], DC line undervoltage and overcurrent are indicated as common protections for VSC HVDC systems. Existing Line Commutated Converter (LCC) HVDC point-to-point connections mainly make use of voltage derivative and voltage magnitude to detect faults on DC lines [7], [8]. Additionally, detection criteria for DC line faults in HVDC, directly related to traveling waves, make use of a criterion which combines voltage and current derivative, as discussed in [9], [10].

In the recent literature, fault detection and identification methods for HVDC grid protection are mainly developed based on extensive time domain simulations in Electromagnetic Transient (EMT)-type software, as, e.g., in [11]–[13]. This results in methods which are specifically tailored for the fault clearing strategy or grid under study. In [14], two algorithms for selective protection of HVDC grids are evaluated. The first algorithm is based on the method introduced in [15] whereas the second uses voltage and current derivative criteria. In these studies, the measurement and filtering requirements of these methods are only partially assessed, as these studies highlight either the selection of fault detection signals [11], [12], [14] or the digital filter design [13]. An overview of similar fault detection methods can be found in [16].

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W. Leterme, (corresponding author, email: willem.leterme@esat.kuleuven.be) and D. Van Hertem are with EnergyVille/Electa research group, Electrical Engineering Department ESAT, KU Leuven, Belgium.

M. Barnes is with the Power Conversion Research Group, School of Electrical and Electronic Engineering, University of Manchester, Manchester M13 9PL, UK.

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Rather than develop new fault detection methods, this paper aims at providing a fundamental basis for the selection of fast fault detection methods for HVDC grid protection, prior to extensive studies in EMT-type software. In this paper, general guidelines, based on traveling wave theory, are proposed for the design of DC fault detection methods for HVDC grids. Furthermore, the impact of non-ideal measurements on these methods and the choice and specification of the digital filters that are optimally suited for fault detection in this noisy environment are investigated.

The proposed approach is demonstrated by a case study on fault detection in a realistically dimensioned HVDC test grid, which is based on a topology proposed in [17]. In [17], a high-level study is performed on the design and economics of a future offshore HVDC system based on independently operated point-to-point or three-terminal systems. In the case study of this paper, these systems are interconnected to generate one HVDC grid. This grid is divided into two protection zones by a single fast DC breaker. The purpose of the case study is to demonstrate the methodology for selection of fault detection signals to operate the breaker.

First, Section II explains transient components in voltages and currents resulting from DC faults in HVDC grids based on traveling wave theory. Next, Section III revises existing fault detection methods in HVDC applications and provides guidelines for the choice of fault detection method based on Section II. Thereafter, Section IV introduces the models used to analyze the fault transients and Section V presents the results of the case study. Finally, conclusions are drawn in Section VI.

II. TRAVELING WAVES

In the first milliseconds after fault inception, the transient components in voltage and current can be analyzed using traveling wave theory [18]. A fault on a transmission line in a power system causes traveling waves which propagate with a certain speed on the faulted transmission line. At each discontinuity, these waves are partly reflected back to the fault location, creating a backward traveling wave on the faulted line, and partly transmitted to the rest of the network, creating forward traveling waves on other transmission lines. Consequently, traveling waves created by the fault are attenuated and distorted through propagation and reflections in the network before reaching the protection relay that must detect these waves.

The parts of traveling wave theory used to determine the suitable fault detection signals are briefly recapitulated below.

A. Propagation over Cables

For the single conductor case, the propagation of a wave on a cable with series impedance Z and shunt admittance Y , can be mathematically described using the propagation function $H_{\text{cable}} = e^{-\gamma x}$, in which x is the length of the cable section and γ is equal to $\sqrt{YZ}$. The cable shunt admittance Y and series impedance Z are determined by the cable geometry.

The voltage and current wave are related by the characteristic impedance Z_c , which is equal to $\sqrt{Z/Y}$. For high

frequencies, the characteristic impedance can be approximated by a real value Z_c' .

B. Reflection and Transmission of Traveling Waves

The reflection coefficient Γ_v describes the fraction of the voltage wave that is reflected at a discontinuity:

$$\Gamma_v = \frac{Z_1 - Z_c}{Z_1 + Z_c} \quad (1)$$

where Z_1 is the impedance of the cable termination. The transmission coefficient $T_v = 1 + \Gamma_v$ describes the fraction of the voltage wave that is transmitted to the rest of the network.

Analogously, the relationships between the incoming and reflected and transmitted current wave are given by $\Gamma_i = -\Gamma_v$ and $T_i = 1 - \Gamma_v$, respectively. The transmission coefficients for the main termination impedances are listed in Table I.

TABLE I
TRANSMISSION COEFFICIENTS FOR INDUCTIVE, CAPACITIVE AND RESISTIVE TERMINATION

	Inductor L	Capacitor C	Resistor R	n Cables
T_v	$\frac{2sL}{sL + Z_c}$	$\frac{2}{sCZ_c + 1}$	$\frac{2R}{R + Z_c}$	$\frac{2}{n + 1}$
T_i	$\frac{2Z_c}{sL + Z_c}$	$\frac{2sCZ_c}{sCZ_c + 1}$	$\frac{2Z_c}{R + Z_c}$	$\frac{n}{n + 1}$

For an inductive termination, T_v passes through high frequency components of incoming traveling waves whereas T_i has a low-pass characteristic. For high frequencies, T_v and T_i attain a value of 2 and 0, respectively.

If the cable termination is capacitive, T_v has a low-pass characteristic, whereas T_i has a high-pass characteristic. The high frequency components of the current waves are doubled and the voltage wave is largely attenuated.

For a resistive termination, considering a lossless line, T_v and T_i are independent of frequency. In the case where the cable is terminated by n cables with the same characteristic impedance, the transmission coefficients are similar to the resistive termination case. For these types of terminations, both the voltage and current waves contain high frequency components.

III. FAULT DETECTION IN HVDC GRIDS

Protection relays associated with HVDC breakers must detect a fault within a timeframe of milliseconds to allow faults to be cleared at a current that can still be managed cost-effectively with present breaker technology [19]. Within such a timeframe, the voltage and current measured at the relay location, $u_r(t)$ and $i_r(t)$, can be described by a superposition of the pre-fault voltage and current, u_0 and i_0 , and the transient components in voltage and current caused by the fault, $u_t(t)$ and $i_t(t)$ [20]:

$$u_r(t) = u_0 + u_t(t) \text{ and} \quad (2)$$

$$i_r(t) = i_0 + i_t(t) \quad (3)$$

where $i_t(t)$ is positive if the current flows in the direction of the transmission line.

The most straightforward way to detect faults is to compare the voltage or current magnitude, u_r or i_r , with a certain threshold u_{thr} or i_{thr} :

$$u_r < u_{thr} \text{ and} \quad (4)$$

$$i_r > i_{thr}. \quad (5)$$

Fast fault detection can be achieved by using the voltage and current for a relay at an inductive and capacitive termination, respectively. For fast fault detection at a resistive termination, voltage or current can be used.

To discriminate from pre-fault values and to speed up fault detection, the voltage or current derivative can be used:

$$\frac{du_r}{dt} < du_{thr} \text{ and} \quad (6)$$

$$\frac{di_r}{dt} > di_{thr} \quad (7)$$

in which du_{thr} and di_{thr} are thresholds for voltage or current derivative detection. Similar to the voltage and current magnitude, the signals that enable fast fault detection depend on the termination impedance.

In the literature, several discriminant or directional detection functions were based on signals S_1 and S_2 , defined as [20]–[23]:

$$S_1 = u_t(t) - R_c i_t(t), \text{ and} \quad (8)$$

$$S_2 = u_t(t) + R_c i_t(t) \quad (9)$$

in which R_c is used to approximate Z_c by a real value, e.g., $R_c = \lim_{\omega \rightarrow \infty} Z_c$. These signals extract the forward (S_1) or backward (S_2) traveling wave, respectively. In (8) and (9), i_t is positive if the current flows towards the other transmission line end. Using this convention, a forward (backward) wave is defined as a wave for which $i_t > 0$ ($i_t < 0$).

For a lossless line, at a discontinuity, S_1 equals twice the incoming voltage wave for forward waves whereas it is zero for backward waves. The advantage of a detection function based on (8) is its independency of the network behind the relay [22]. In cable systems, the values for S_1 deviate slightly from these ideal values due to the mismatch between R_c and Z_c .

The transient components in the voltages and currents can be obtained from the measurements by taking the first derivative or by subtracting a delayed version of the measured voltage and current [23].

IV. MODELS

A. Cables

Distributed parameter transmission line models are needed to simulate traveling waves on a transmission line [18]. In EMT-type software, the most accurate distributed parameter models are the frequency-dependent phase models, which relate the waves at both ends of a cable by the propagation function H_{cable} and the cable characteristic impedance Z_c [24]. These parameters are fitted in the frequency domain using rational functions, as described in [25]–[27].

B. Converters

In this paper, half-bridge modular multilevel converters are considered. Fault detection is assumed to occur during the capacitive discharge stage of the converter response during faults, i.e., stage I in [28]. During the capacitive discharge stage, the converter can be accurately modeled using an equivalent RLC-circuit, as verified in detail in [28]–[30], with an impedance given by

$$Z_{conv} = R_{conv} + sL_{conv} + \frac{1}{sC_{conv}} \quad (10)$$

where R_{conv} , L_{conv} and C_{conv} are the converter equivalent resistance, inductance and capacitance. The values for these equivalent parameters are obtained with the rules provided in [29].

C. Measurement Units

Measurement units consist of a primary sensor which converts the primary voltages to a low amplitude signal and electronics which process this signal before sending it to the relays. In this study, the limiting factor for the output bandwidth of the measurement units is assumed to be the design choices for filters within the sensor electronics to match the fault wave characteristics. Linearity of the sensors over a wide frequency bandwidth can be achieved with, e.g., an RC-voltage divider, a fiber optic current sensor or a Rogowski coil [31]–[34].

The sensor electronics are modeled by an analog-to-digital converter and a low-pass filter. In this paper, a Butterworth low-pass filter with an attenuation of -60 dB at $f_s/2$ is used, where f_s is the sampling frequency. The attenuation of -60 dB ensures adequate attenuation of the aliasing introduced by analog-to-digital conversion using a digital signal representation with 10 bits.

V. CASE STUDY

The case study investigates fault detection, measurement and digital filtering requirements for protection of the HVDC grid shown in Fig. 1, which is loosely based on the topology proposed in [17].

This HVDC grid accommodates power transfer from five Offshore Wind Farms (OWF A-E) to two separate onshore networks (connected through converters Onsh A, B and C and Onsh 1 and 2, respectively). The system configuration is a symmetric monopole with a pole-to-pole DC voltage of 600 kV. All converters are half-bridge modular multilevel converters with parameters shown in Tables II and III. The cable parameters were taken from [35]. In this paper, the bus bars at Hub 1 and 2 were not explicitly modeled, but the effect by their stray inductance and capacitances on the transient waveforms can be considered as a source of high-frequency noise.

To protect the grid with a limited number of fast DC breakers, a fast DC breaker is used only in link L2. This breaker, located close to Hub 2, divides the HVDC grid into two DC protection zones, each encompassing one hub. The DC breaker must swiftly isolate the healthy from the

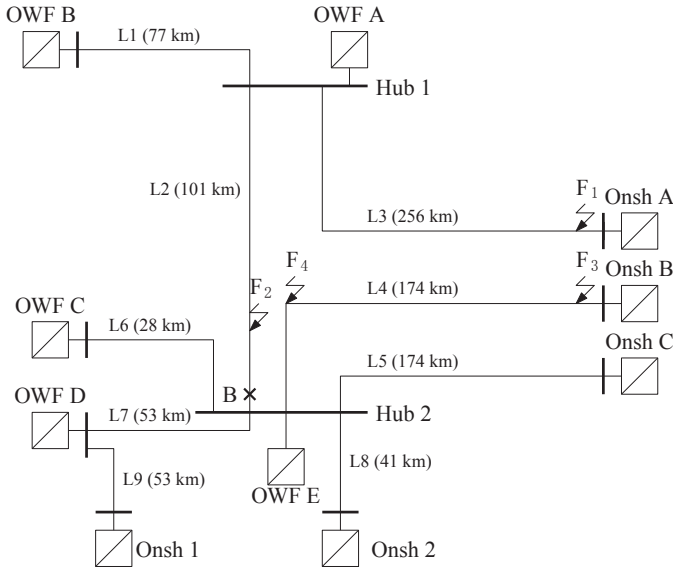


Fig. 1. HVDC grid topology and faults under study.

TABLE II
AC GRID AND CONVERTER PARAMETERS

	OWF A,B,C (D,E)	Onsh A,B (1,2,C)
Rated power	500 (300) MVA	1000 (500) MVA
Primary voltage	220 kV	400 kV
Secondary voltage	350 kV	350 kV
System X/R	10	10
System SCR	1	10
Transformer leakage reactance	15%	15%

TABLE III
CONVERTER PARAMETERS

	OWF A,B,C,D,E, Onsh 1,2,C	Onsh A,B
Submodule capacitance (C_{SM})	1.85 mF	3.7 mF
Arm inductance (L_{arm})	35 mH	50 mH
Arm resistance (R_{arm})	0.5 Ω	0.5 Ω
Series inductance (L_{DC})	10 mH	10 mH
Equivalent capacitance (C_{eq})	55.5 μ F	111 μ F
Equivalent inductance (L_{eq})	43 mH	53 mH
Equivalent resistance (R_{arm})	0.33 Ω	0.33 Ω

faulted section, for DC side faults in any of the protection zones. Consequently, the healthy part can continue operation, which limits the loss of infeed to an acceptable level for the connected ac networks. For DC side faults, this fault clearing strategy results in a larger interruption of power flow within the DC system compared to a selective one. However, the required number of fast DC breakers (and associated series inductors) is eighteen times lower, which results in a lower investment cost.

A fast fault detection method for faults in both parts of the network is needed to quickly operate the DC breaker. For the fault detection, the voltage and currents are measured at Hub 2 (Fig. 2). The current through each line is measured, taking a positive value for a current in the direction of the line, and the voltage is measured at the hub ($u_{h,2}$) and cable side of the inductor L_s (u_2). The inductor L_s has a value of 50 mH and is connected in series with the DC breaker B. It is included in this study as all presently proposed fast DC breakers use a

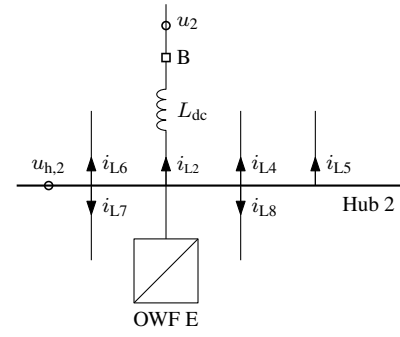


Fig. 2. Hub 2 breaker and measurement configuration.

series inductor to limit the rate of current rise [36]–[38].

The case study investigates detection of faults F_1 , F_2 , F_3 and F_4 , indicated in Fig. 1. All faults considered are solid pole-to-pole faults, modeled by a resistance of 0.01 Ω (default value proposed by PSCAD). Faults F_1 and F_3 occur at the converter terminals of converters Onsh A and B. These faults represent the worst case for fault detection in the Hub 1 and Hub 2 protection zones. Faults F_2 and F_4 occur at 25 km from Hub 2 on L2 and L4, respectively. Since these faults occur close to the breaker and consequently give rise to high breaker currents, the detection speed is important.

In the study, only low impedance faults are considered since these are most probable in cable systems. High impedance faults will create wave-fronts with a lower amplitude, and hence are more difficult to detect, but are out of scope for the detection methods investigated in this paper. Although in cable systems, pole-to-pole faults are less probable, these give rise to similar transient waveforms in the faulted poles as for pole-to-ground faults.

The results in Sections V-A-V-D were obtained with the models in Section IV implemented in MATLAB. In Section V-E, these results are verified against EMT-simulations performed using PSCAD.

A. Frequency Response

The frequency response for fault F_i was analyzed by means of the magnitude response of the transfer functions of the voltage and current at the fault location, $U_{f,i}$ and $I_{f,i}$, to the voltage and current at the breaker. The voltage and current at the breaker are U_2 and I_2 for F_1 and F_2 , and $U_{h,2}$ and I_4 for F_3 and F_4 .

1) Faults in Hub 1 Protection Zone

For both faults, the voltage transfer function has a greater content at higher frequencies than the current transfer function (Fig. 3), as the waves are reflected at the series inductor L_s after traveling over link L2. Furthermore, the magnitude response of the transfer function of F_1 is less than the one of F_2 for all frequencies. This is caused by two effects: first, the waves from F_1 are reflected at Hub 1, which results in only partial transmission of these waves to Hub 2 and second, the waves from F_1 are attenuated more as these propagate over a longer distance on the cables.

2) Faults in Hub 2 Protection Zone

For these faults, the maximal frequency that is passed through by voltage and current transfer functions is equal and limited by cable attenuation (Fig. 4). The magnitude response of F_3 is less than the one of F_4 as F_3 is located further away from Hub 2 than F_4 .

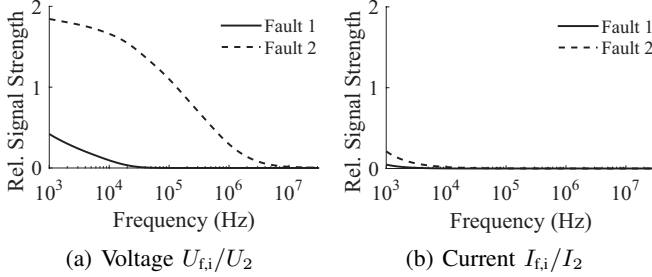


Fig. 3. Magnitude response for F_1 and F_2 .

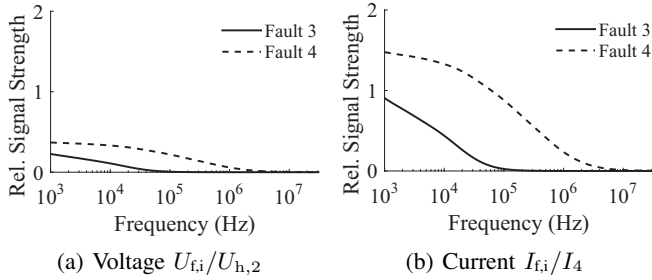


Fig. 4. Magnitude response for F_3 and F_4 .

B. Fault Detection Criteria

Faults in the protection zone of Hub 1 were detected with the magnitude and derivative of u_2 . This enables a high speed of operation by using the high frequencies in the voltage at the cable side of the inductor (Fig. 3(a)).

A detection function dS_{lj} based on (8) was used to detect faults in the protection zone of Hub 2, as faults F_3 and F_4 result in high frequency transients in both $u_{h,2}$ and the current through links L4–L8 (Fig. 4). For the detection function, the first derivative of these quantities was taken to remove the pre-fault offsets:

$$dS_{lj} = \frac{du_{h,2}}{dt} - R_c \frac{di_{Lj}}{dt} \quad (11)$$

in which $R_c = \text{Re}(Z_c|_{1 \text{ MHz}})$ and i_{Lj} is the current through link Lj . If dS_{lj} exceeds a threshold for any of the links L4–L8, indicated by a high value of dS_{lj} , B is tripped.

In summary, the criteria used to trip B are:

$$u_2 < u_{thr}, \quad (12)$$

$$\frac{du_2}{dt} < du_{thr}, \quad \text{or} \quad (13)$$

$$dS_{lj} > dS_{thr}, \quad j \in \{4, 5, 6, 7, 8\}. \quad (14)$$

Suitable selection of the thresholds allows DC faults to be discriminated from non-DC fault transients and noise. The threshold u_{thr} was set to the lowest allowable voltage in normal

(non-voltage sag) operation, which was taken to be 80% of the nominal voltage. The thresholds $du_{thr,d}$ and dS_{thr} must be set to avoid false tripping of the breaker due to noise or non-fault transients. These thresholds are set after the measurement units, digital filters and noise level are determined.

C. Measurement Requirements

This section analyzes the effect of the measurement unit on the speed and sensitivity of the fault detection methods. For all signals used by the selected methods, the maximum frequency is determined by the cable and network characteristics in the forward direction of the relay rather than by the termination impedance. Therefore, in this section, only F_1 and F_2 are treated since the analysis of F_3 and F_4 leads to similar results.

The low-pass filter removes more high frequency content if the sampling frequency is lowered, as its cutoff frequency decreases. For faults close to the relay, e.g., F_2 , information in the high frequency region is lost even for high sampling frequencies up to 500 kHz (Fig. 5(a)). By contrast, if the sampling frequency is too low, the wavefront of the traveling wave is filtered out, e.g., in Fig. 5(b) for a sampling frequency lower than 50 kHz. In Fig. 5(b), the time domain response of the transfer function of F_2 was obtained for a step input of $-u_{DC}$.

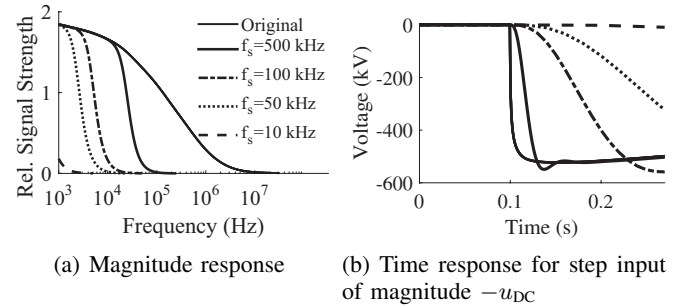


Fig. 5. Third order Butterworth low-pass filter applied to u_2 for F_2 .

Compared to a breaker opening time of several milliseconds, the time delay introduced by the low-pass filter on fault detection using (12) is insignificant for sampling frequencies higher than 50 kHz (for F_2 less than 100 μ s), but takes relatively high values (for F_2 between 300 and 400 μ s) for lower sampling frequencies (Fig. 6). The time delay introduced by the low-pass filter is defined as the difference between the time instants of fault detection with the original and sampled signal.

An increase in the order of the low-pass filter only significantly impacts the delay on fault detection, if the sampling frequency is lower than 50 kHz (Fig. 6). For the same sampling frequency, the higher order filter passes through more high frequency content since its cutoff frequency is higher compared to the lower order filter. The impact of the filter order on the fault detection delay is more pronounced for F_1 than for F_2 as the fault transfer function for F_1 passes through less high frequency content (Fig. 3).

Although the voltage derivative decreases with decreasing sampling frequency, it can be used to detect DC faults using

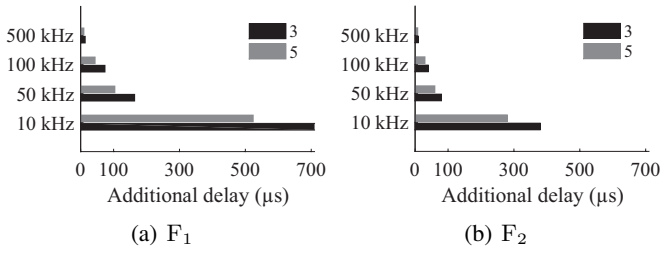


Fig. 6. Time delay introduced by third and fifth order Butterworth low-pass filter applied to u_2 on fault detection by (12).

sufficiently high (>50 kHz) sampling frequencies. With a third order filter and a sampling frequency of 50 kHz, the voltage derivative is $0.63 \text{ kV}/\mu\text{s}$ for F_1 and $3.28 \text{ kV}/\mu\text{s}$ for F_2 (Fig. 7). These values exceed the ones typically encountered during control actions in normal operation. Nevertheless, DC fault detection using the voltage derivative requires an appropriate digital filter if the signals are corrupted by noise, as described in the next section.

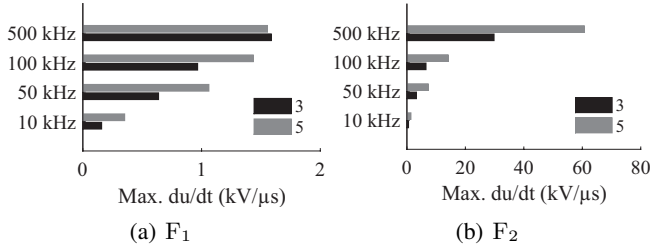


Fig. 7. Maximal voltage derivative of u_2 for different sampling frequencies and low-pass filters.

In conclusion, with a sufficiently high sampling frequency, the delay introduced by the filter is small compared with the delay introduced by breaker opening and the filter order has no significant impact on the filter delay. Since higher filter orders also result in a poorer transient response, it is recommended to keep the filter order low.

D. Matched Filter

The matched filter minimizes the effect of noise on fault detection by maximizing the Signal-to-Noise Ratio (SNR) of its output signal compared to its output noise. For white noise, the impulse response $h(t)$ of the matched filter is

$$h(t) = Cs(t_0 - t) \quad (15)$$

in which $s(t)$ is the input signal, C is an arbitrary constant and t_0 ensures causality of the filter [39]. The SNR at the filter output is given by

$$\left(\frac{S}{N}\right)_{\text{out}} = \frac{2E_s}{N_0} \quad (16)$$

in which E_s is the energy of $s(t)$ and $N_0/2$ is the power spectral density level of the noise. For white noise, the SNR of the matched filter can be improved only by increasing the filter length, which increases E_s .

The matched filter to detect DC faults, assuming a DC fault as a step input, is in digital form

$$h^l[k] = \begin{cases} -1/\sqrt{l} & \text{for } 0 \leq k \leq l/2 \\ 1/\sqrt{l} & \text{for } l/2 < k \leq l \end{cases} \quad (17)$$

where l is the filter length. This filter corresponds to a first order Walsh function or a dilation of the Haar wavelet. It is matched to the signal obtained at the terminal for a fault on a lossless line. This signal is equal to the sum of the pre-fault DC voltage and twice the negative DC voltage for faults in both protection zones (cf. Sections II and III and Fig. 5(b)). In the design of the matched filter, the propagation of the waves over the network is neglected, as it is impossible to match a filter to faults on all locations in the grid.

A schematic of the fault detection scheme which implements the criteria for fault detection given in (12)–(14) is shown in Fig. 8. The inputs for the fault detection scheme are the sampled values of u_2 , $u_{h,2}$ and i_{Lj} . Since the matched filter is of the form of (17), it can be used to implement the criteria based on the first derivative, (13) and (14). The matched filter outputs are denoted by MF_u^l and MF_{Sj}^l . These signals, along with the sampled values of u_2 are presented to the threshold devices. If the output of any of the threshold devices, s_u , s_{du} or s_{dir} , becomes one, B is tripped.

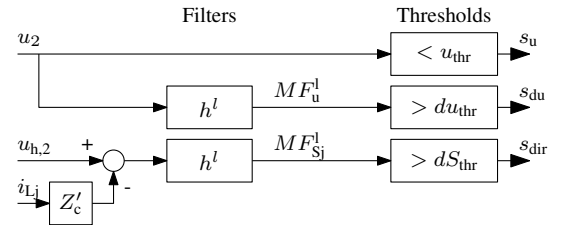


Fig. 8. Schematic of fault detection.

The filter can be matched to faults at various locations by adapting its length l , as shown by Fig. 9 and Fig. 10. With a window length of 2 samples, the filter output MF_u^2 for F_2 is higher compared with the one for F_1 (Fig. 9). This filter is not suited to detect to remote faults in a noisy environment as, e.g., for F_1 , the filter output MF_u^2 remains below the maximal filter output for white noise with a SNR of 40 dB, MF_{noise}^{40dB} (Fig. 10(a)). With a window length of 16 samples, the filter can be used for detection of close as well as remote faults since for both F_1 and F_2 , MF_u^{16} exceeds MF_{noise}^{40dB} (Fig. 10). For this study, a sampling frequency of 100 kHz and associated third order Butterworth low-pass filter were used. Table IV indicates the frequency around which the peak response of the matched filter is situated. With different combinations of f_s and l , a peak response at similar frequencies can be obtained for the matched filter.

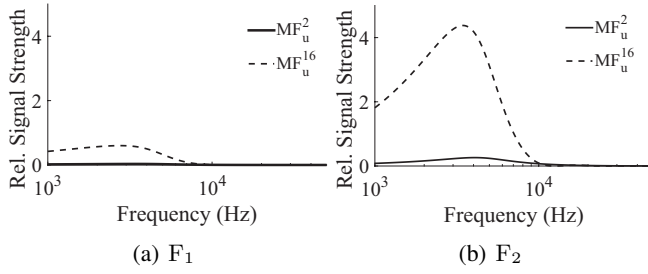


Fig. 9. Magnitude response for matched filters of length 2 and 16 applied to u_2 .

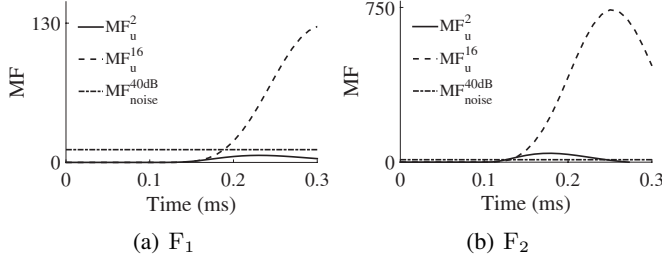


Fig. 10. Time response for matched filters of length 2 and 16 applied to u_2 and maximum filter output for 40 dB white Gaussian noise MF_{noise}^{40dB} (step input of $-u_{DC}$).

To evaluate the minimally required SNR for each filter length, a noisy signal was generated and its maximum value, MF_{noise} , was compared with the maximum value of the filter output for F_1 and F_2 (Fig. 8). The filter output for F_1 and F_2 were obtained using the same method as for Fig. 10. If MF_u^l is below MF_{noise} , the noise level is unacceptable. In this study, white Gaussian noise was used to encompass noise introduced through various kinds of sources, e.g., partial reflections of traveling waves at cable segments, measurement noise, quantization noise, noise due to electronics or noise due to converter switching. The SNR in dB is expressed relative to the nominal voltage, i.e., 300 kV.

With the matched filters, relatively high noise levels can be tolerated before faults can no longer be detected. Table IV shows the lower limits for the SNR for fault detection of F_1 and F_2 with matched filters in case the measurement output signal is corrupted by white Gaussian noise. The required SNR decreases with increasing filter length or with decreasing distance of the fault to the relay location. Due to space restrictions, the analysis is limited to this case. The impact of the low-pass filter on the acceptable SNR can be subject to further investigation using the proposed method, considering different combinations of f_s and l .

TABLE IV
REQUIRED SNR FOR DETECTION OF F_1 AND F_2 WITH MATCHED FILTERS OF DIFFERENT LENGTH

l	f_s/l (kHz)	SNR F_1	SNR F_2
2	50	47	30
4	25	37	22
8	12.5	30	14
16	6.25	22	6

E. Comparison with PSCAD

To verify the results obtained in Sections V-A- V-D, the system of Fig. 1 was modeled in PSCAD. Four converters (Onsh A, OWF A, OWF E and OWF C), located close to the fault, were modeled by a continuous Modular Multilevel Converter (MMC) model with anti-parallel diodes, which was verified for use in DC fault studies in [40]. The wind farms and the controls of the connected MMCs were modeled as presented in [41]. The remote converters were modeled by equivalent RLC-models introduced in Section IV. The cables were modeled using the frequency-dependent (phase) model. All faults occur at $t = 5$ ms and the calculation time step used in the study was $1 \mu s$.

For the fault detection methods, the pole-to-ground voltages at both sides of the series inductor L_s and currents through the links L4–L8 were measured. The voltage and current samples were filtered using a third order Butterworth low-pass filter and downsampled to 100 kHz. The threshold on the pole-to-ground voltage, u_{thr} , was set to 240 kV, i.e., $0.8u_{DC}/2$. To obtain the derivatives for (13) and (14), matched filters of length 2 and 16 were applied. The thresholds for the derivative criteria were chosen to allow fault detection if the signals are corrupted by white Gaussian noise with an SNR of 40 dB (compared to a voltage level of 300 kV). This led to a threshold value of 11.78 for du_{thr} and dS_{thr} (Fig. 8).

1) Faults in Hub 1 Protection Zone

The fault detection signals for faults in the Hub 1 protection zone confirm the result from Table IV that an SNR of 40 dB is not sufficient to detect F_1 with a matched filter of length 2. Fault F_1 is detected using the voltage threshold and the matched filter of length 16, but remains undetected by the filter of length 2 since MF_u^2 remains below 11.78. Therefore, for F_1 , s_{du}^2 remains zero whereas s_{du}^{16} and s_u indicate fault detection at $t \approx 7$ ms, i.e. the time at which the wave created by F_1 reaches B (Fig. 11(a)). By contrast, F_2 is detected using the voltage threshold and the matched filters of both lengths. For this fault, all detection signals s become 1 at $t \approx 5.2$ ms, i.e., the time at which the wave created by F_2 reaches B (Fig. 11(b)).

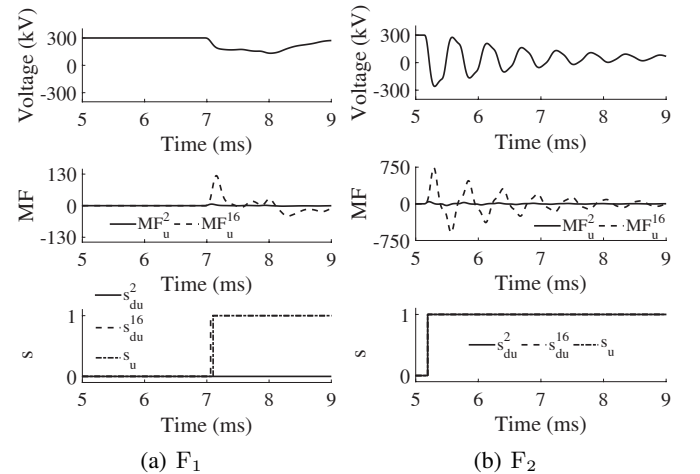


Fig. 11. Voltage u_2 , matched filter output of u_2 for filter lengths 2 and 16 and noise threshold, and fault detection signals.

The time delays for fault detection of F_1 and F_2 are 110

and $45 \mu\text{s}$, where the time delay is defined as the difference between the time instant of fault detection and the wave arrival time at the relay. Although F_1 is detected earlier by the matched filters implementing (13), the time difference between detection by (12) and (13) is limited to $30 \mu\text{s}$ (3 samples).

2) Faults in Hub 2 Protection Zone

The detection function (14) detects F_3 and F_4 with a delay of 45 and $67 \mu\text{s}$ after the wave arrival time, respectively (Fig. 12). Also in this case, the difference in time delay for the filter of window length 16 compared to the one of length 2 is limited, e.g. $10 \mu\text{s}$ (one sample) for F_4 .

Additionally, the peak value of MF_{S4}^{16} for detection of F_4 (Fig. 12(b)) is approximately the same as MF_u^{16} for detection of F_2 (Fig. 11(b)). This demonstrates that (8) takes a value of approximately twice the incoming wave for forward faults.

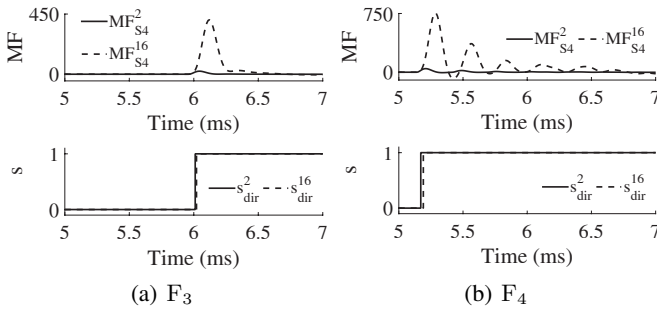


Fig. 12. Matched filter output for filter lengths 2 and 16 applied to S_1 , and fault detection signals.

3) Non-DC Fault Transients

The proposed fault detection methods can distinguish transients initiated by ac faults at converters OWF A and E from DC faults. As an example, two three-phase faults, $F_{ac,A}$ and $F_{ac,E}$, were applied at the ac grid side of the converter transformers of OWF A and E, respectively. As the DC voltage for $F_{ac,A}$ and $F_{ac,E}$ remains above 80% of the nominal voltage, i.e., above 240 kV, the ac faults are not detected with (12) (Fig. 13). With (13)–(14), ac faults are not detected since, for both cases, the output of the matched filters remain below the threshold selected for a noise level of 40 dB, e.g., in Table V, MF_u for $F_{ac,A}$ and $F_{ac,E}$ are one order of magnitude lower than for a noisy signal with SNR 40 dB. By contrast, with a window length of 16 samples, the filter output for DC faults for the appropriate fault detection criterion is an order of magnitude higher than those for non-DC faults or noise. As an example, for F_3 , MF_{S4}^{16} is 401.1 compared to 11.78 for the noisy signal (Table V).

TABLE V

MAXIMUM FILTER OUTPUTS (FIG. 8) FOR DC FAULTS AND NON-DC FAULT TRANSIENTS

Fault Type	MF_u^2	MF_{S4}^2	MF_u^{16}	MF_{S4}^{16}
F_1	6.65	0.05	126.21	1.05
F_3	0.42	22.35	9.42	401.10
$F_{ac,A}$	0.15	0.01	2.78	0.29
$F_{ac,E}$	0.02	0.08	0.52	1.73
Noise with SNR 40 dB	11.78	11.78	11.78	11.78

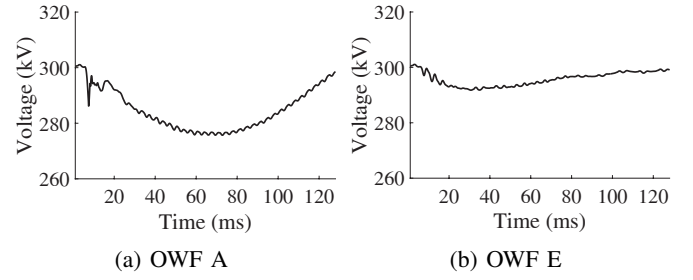


Fig. 13. Voltage u_2 for $F_{ac,A}$ and $F_{ac,E}$.

VI. CONCLUSION

Suitable fault detection signals for the fastest fault detection method given a termination impedance can be found by identifying the high frequency components of the transient voltage and current waveforms resulting from a DC fault. In the proposed approach, the frequency content of the waveforms at the relay location is estimated through calculating the transfer function of the voltage and current waves at the relay used for fault detection to the ones at the fault location. The DC fault detection methods designed using the approach can detect DC faults within hundreds of microseconds, which favors fast interruption of the fault current.

Even if the fault detection signals are corrupted by noise, faults can be detected quickly by using a sufficiently high sampling frequency and a matched filter for fault detection. Within the assumptions of the case study, to limit the delay on fault detection, the lower limit on the sampling frequency is found to be in the order of 50 kHz if a 10 bit signal representation and associated low pass filter are used. Application of matched filter theory for fault detection leads to filters which implement the first order derivative on the signal or an averaged version thereof. With these fault detection signals and associated filters, the fault detection methods were found to be insensitive towards transients which are not initiated by DC faults, such as faults at the converter ac terminals.

The paper mainly focused on cable-based systems, as we anticipate such systems will form the major part of multiterminal VSC HVDC systems or HVDC grids in early stages of development. Although fundamental traveling wave theory for overhead line-based systems is the same as for cable-based systems, conclusions regarding choice of fault detection method and digital filter might need to be slightly adapted.

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Willem Leterme (S'12) received the M.Sc. and Ph.D. degree in Electrical Energy Engineering from the University of Leuven (KU Leuven), Leuven, Belgium in 2012 and 2016, respectively. Currently, he works as Post-Doctoral Researcher at KU Leuven in the research group Electa, department of Electrical Engineering. His research interests include multiterminal VSC HVDC and VSC HVDC grids, and in particular the protection algorithms for these systems.



Mike Barnes (M'96–SM'07) received the B.Eng. and Ph.D. degrees from the University of Warwick, Coventry, U.K. in 1993 and 1998 respectively. In 1997, he joined the University of Manchester Institute of Science and Technology (UMIST, now merged with The University of Manchester), Manchester, U.K., as a Lecturer. He is now a Professor there and his research interests cover the field of power-electronics-enabled power systems.



Dirk Van Hertem (S'02–SM'09) received the M.Eng. in 2001 from the KHK, Geel, Belgium and the M.Sc. in Electrical Engineering from the KU Leuven, Belgium in 2003. In 2009, he received the Ph.D., also from the KU Leuven. In 2010, he was a member of EPS group at the Royal Institute of Technology, in Stockholm, Sweden, where he was the program manager for controllable power systems for the EKC2 competence center. Since spring 2011 he is back at the University of Leuven where he is an Assistant Professor in the Electa group. His

special fields of interest are power system operation and control in systems with FACTS and HVDC and building the transmission system of the future, including offshore grids and the supergrid concept. He is an active member of IEEE PES and IAS and CIGRÉ.