# SCARLET

## *Superconducting CAbles foR sustainabLe Energy Transition*

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# *D5.1: Selection of electrical system architecture*

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# EXECUTIVE SUMMARY

The purpose of this deliverable is to describe the electrical system that could allow for the use of a 1 GW superconducting (SC) cable in different application cases using MVDC (< 100 kVdc) rather than HVDC voltage. More precisely, two MVDC system voltages are considered, namely +/- 50 kVdc and +/- 25 kVdc corresponding to the HTS and $MgB_2$ cable technologies, respectively. Currently, no converter exists for these voltage and power levels. This deliverable analyzes the state of the art of power electronics and high-power conversion solutions, and then proposes what is believed to be an industrially feasible solution.

In the particular case of offshore wind power export, a new conversion chain fitting inside the windmill allows for the power output right at the export voltage. Combined with the feasibility of the new onshore converter architecture, this makes it possible to suppress the costly offshore conversion platform. However, unlike resistive cables, an SC cable needs active auxiliary devices to enable its outstanding conducting properties. In this respect, designing the offshore power supply for the intermediate cooling machines of long SC cables is challenging. Different concepts are evaluated in this deliverable, keeping in mind the objective of limiting the cost of the small offshore platforms hosting those machines.

The change of paradigm induced by the use of superconducting cables (i.e., use of medium voltage associated with very high - respectively 10 kA and 20 kA - nominal current) has consequences on the requirements of protection and switching functions. Taking into account the new opportunities offered by the proposed conversion architecture, and the particulars of superconducting versus resistive cables, the architecture of a protection strategy is described. This strategy combines the advantages of Resistive Superconducting Fault Current Limiter and DC circuit breakers.

Last but not least, the high nominal current associated with medium voltage requires an optimization of the system architecture in light of the availability or feasibility of classical electrotechnical devices like transformers or disconnectors. Their technical limitations as well as design flexibility are considered in the proposed system architecture.

Though the focus is on the offshore wind export case, the proposed converter architecture and protection solutions are pertinent for other cases, as well: not only bulk production export, but also large industrial area feeding and point-to-point exchange.

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# 1. INTRODUCTION

SCARLET addresses connections on the order of 1 GW, at medium voltage up to 100 kVdc and using a superconducting cable. Point-to-point connections are considered, discarding for the time being Multi Terminal DC Grids at this voltage level.

To date, at this power level, point-to-point connections are built using two AC/DC converters at 320 kVdc or 525 kVdc. They either connect one offshore wind farm to onshore HVAC transmission grid or connect two HVAC transmission grids. The two HVDC converters occupy a very large surface on the order of magnitude of a football playground each.

The limitation of the permanent current capability of power electronics, switches and conventional cables pushes to increase the transmission voltage when gigawatts of renewable power are to be exported.

SCARLET proposes to use the properties of superconducting materials to export the GW power at medium rather than high voltage. Considering the export of offshore or onshore wind energy, the idea comes to match the windmill output voltage with the transportation voltage to suppress one DC conversion platform. Switching from HVDC transmission using two converters to MVDC transmission using only one converter (see Figure 1), and switching from around 2000 Adc nominal current to 10 000 Adc or 20 000 Adc nominal current requires not only switching from a conventional to a superconducting cable technology but also rethinking the full conversion chain along with the switching and protection devices.

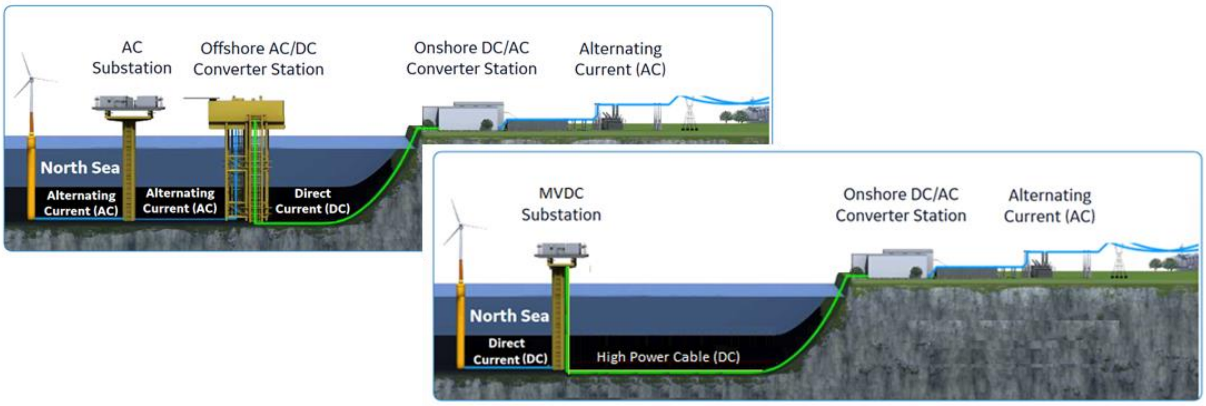


**Figure 1 : Basic incentive of switching from HVDC transmission to MVDC transmission applied to offshore wind energy export (original figure -top left- from GE Grid Solutions DOLWIN 3 website communication)**

Suppressing one converter (the offshore converter in Figure 1), allows for a very high simplification of the remaining offshore platform (see Figure 2) where the valve halls, transformer bays, Air Insulated Switchgear (AIS) switching bays could be suppressed, leaving DC busbars and MVDC switches to ensure disconnection for maintenance work as an example.

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**Figure 2 : Illustration of a 2 GW 525 kVdc offshore platform from the website of TENNET. The size of the platform is estimated with a footprint of ~ 8 000 m² and a volume of ~ 325 000 m³ not considering the supporting structure.**

Assumptions of offshore platform costs can be expressed in k€ /m² or k€/m³ depending on the sources. Based on [1], a cost of 42 k€/m² is used as a hypothesis for an offshore platform (this model would need to be improved especially for small platforms, where fixed costs would not be negligible with respect to variable costs). In this respect, a 50% reduction of the footprint leads to a gain of 168 M€, and a 75% reduction leads to a gain of 252 M€. This significant gain needs to be used positively for the other functions so that the overall cost is reduced compared to the HVDC export solution.

To enable MVDC superconducting cables as a means of power transfer, the architecture and design of the electrical system have to be reviewed. The MVDC converter design, the windmill voltage forming chain, the protection devices and protection strategy have to be modified so as to ensure the highest protection of the converter and superconducting cable and allow for a high service continuity upon a certain number of events.

Therefore, to establish the targeted MVDC export link, the impacted functions can be summarized as follows, keeping in mind that their cost should have a significant margin within the gain due to the offshore platform simplification:

- Switching from a HVDC XLPE cable to an MVDC superconducting cable and its auxiliary functions

- Transferring the AC to DC conversion to each individual windmill
- Converting offshore side AC switching functions into DC
- Adaptation of DC busbars
- Adaptation of onshore Conversion from HVDC to MVDC architecture
- Adaptation of the associated AC switchyard
- Implementation of DC protection devices

As a reminder, an objective of Work Package 1 of SCARLET is to evaluate more deeply this economic impact and quantify the potential gain of this concept.

## 2. GENERAL PRINCIPLE

Different application cases are described in the following paragraphs considering two families:

- Unidirectional family: the power always flows in the same direction.
- Bidirectional family: the power can flow in one or the other direction.

The behaviour and dimensioning of the electrical system and especially the SC cable will depend on these application cases. If a SC cable is thought to be used in any of these applications, the worst case of transient events has to be considered. As an illustration (Figure 3), let's consider a fault in the situation of power flow in one direction and power flow in the other direction, with the same absolute nominal current. The critical current of the SC cable is a factor (>1) of the nominal current. In the example of Fig. 3 this factor is 1.4. In case of fault, as the nominal current is high, the time for the current in the SC cable to reach its critical current will significantly depend on the initial power flow direction. In the illustration, the prospective current is evaluated without considering RSFCL action and DC CB action (protection strategy is described within section 6). In the example, depending on the power flow direction, the time to reach the critical current (Ic) is either in the range of 250 µs or in the range of 2.7 ms. This duration competes with the DC breaker breaking time and the RSFCL quenching time resulting in a possibly significant difference in the stress to sustain by the SC cable.

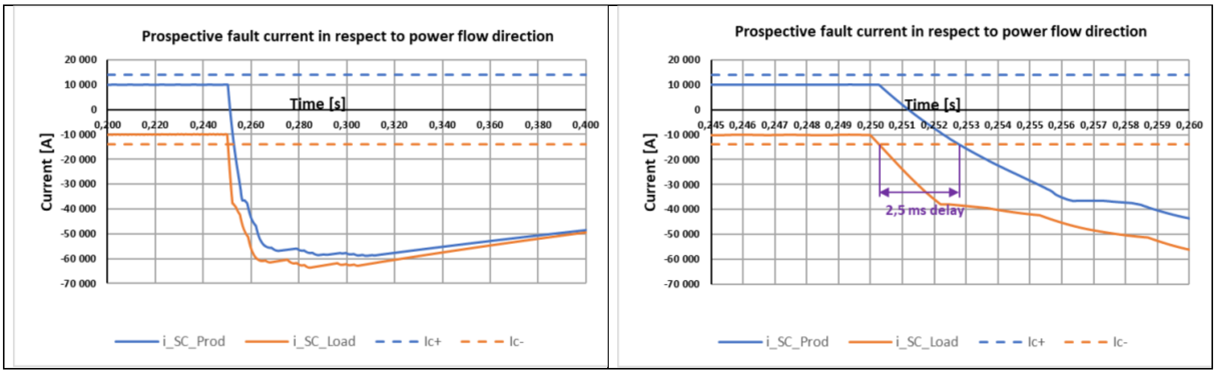


**Figure 3 : Illustration of the impact of the power flow direction (the right picture is a zoom on the initial phase of the fault) in a +/- 50 kVdc system.**

## 2.1 Unidirectional cases

Unidirectional cases are further distinguished between renewable energy export / transportation (later called Production configuration) case and feeding very large consumption centers (later called Consumption configuration). In these two cases, power always flows in the same direction when the system operates in normal conditions.

### 2.1.1 Production configuration

#### 2.1.1.1 Wind energy transfer

Wind energy production can be onshore or offshore, distance from wind park to shore can be in the order of 100 km. Power flows always from the wind park to the AC grid.

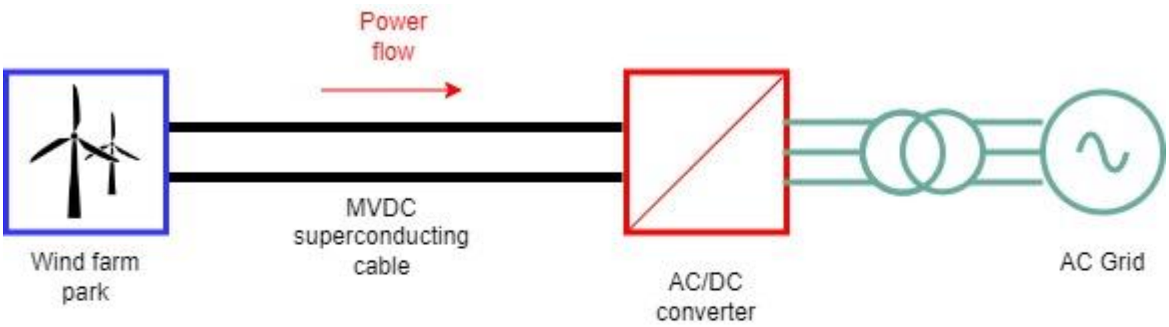


Figure 4 : Production case – Large bulk wind energy transfer

#### 2.1.1.2 Solar energy transfer

Also in the case of a very large solar farms of the order of magnitude of 1 GW, it could be imagined to transfer the power at medium DC voltage to the AC grid. Power flows always from the solar park to the AC grid.

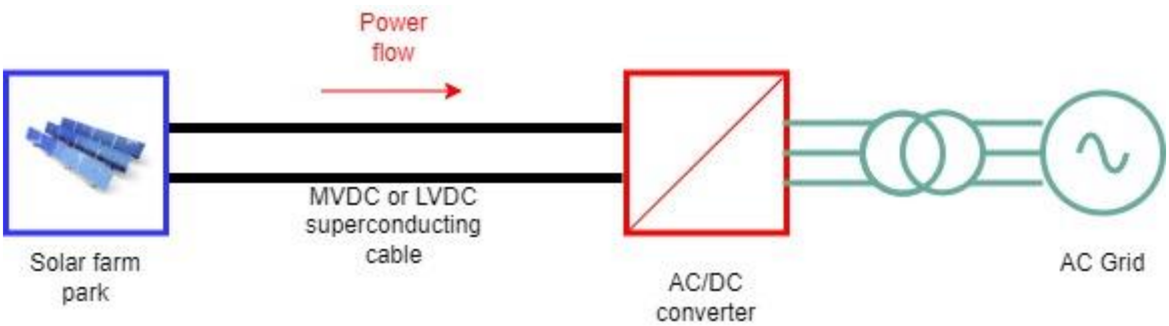


Figure 5 : Production case – Large bulk solar energy transfer

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### 2.1.2 Consumption configuration

Another kind of use of the MVDC SC cable is to feed a very large industrial area or port or even data center, directly in DC. One application case would be to use a MgB2 SC cable cooled with liquid hydrogen to make a dual energy distribution: electricity and hydrogen. The cable length could be of a few km up to 20 km.

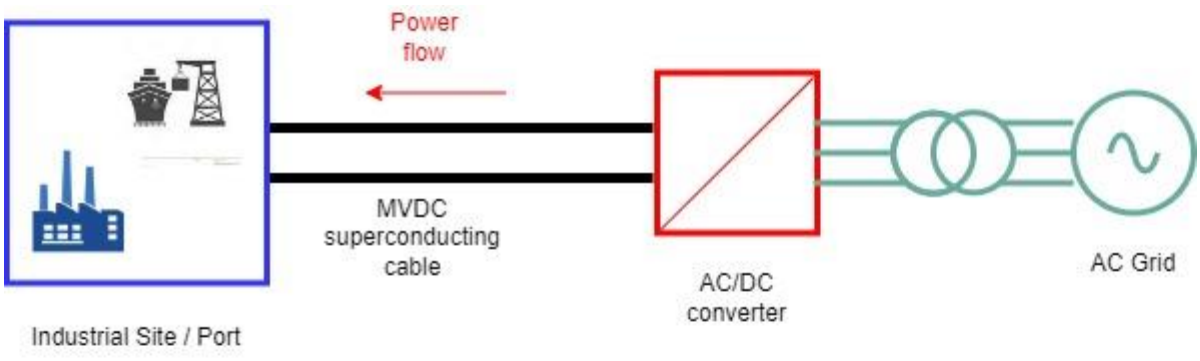


Figure 6 : Consumption case – Large industrial site supply

## 2.2 Bidirectional configuration

The power exchange between two HVAC systems can also be imagined in MVDC rather than in HVDC. Of course, here the gain is not so obvious because the two converters remain necessary, but the use of a superconducting cable can bring advantages like reduction of losses, reduced right of way, no need of derating due to ambient conditions (e.g., hot soil in summer), and possibly other advantages in operation due to the modularity of the MVDC converters.

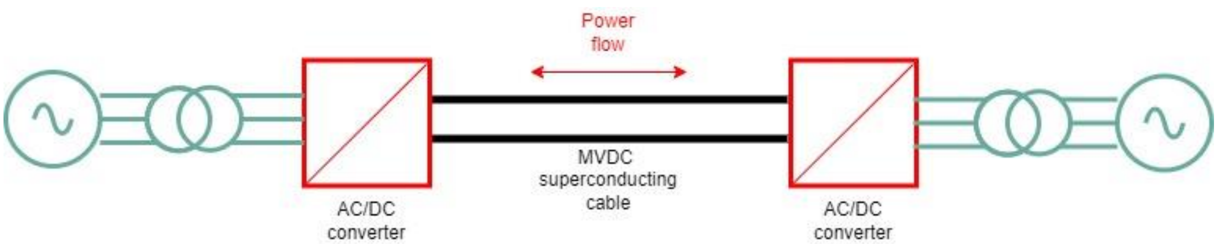


Figure 7 : Power exchange configuration

## 2.3 Selected study cases

Further studies in Work Package 5 will deal with the Offshore Wind Production case and the Consumption case both duplicated at +/- 50 kVdc and +/- 25 kVdc respectively applying the HTS cable model and the MgB2 cable model. Most of the results on these configurations are thought to cover the bidirectional case; results will be part of Deliverable D5.2.

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The aim of the detailed studies will be to validate the protection concepts, specify the protection devices and supply the cable manufacturers with temporal profiles of short circuit currents to be sustained by their cables.

# 3. 1 GW CONVERTER FEASIBLITY

## 3.1 Transmission architecture

### 3.1.1 State of the art

Existing point-to-point VSC HVDC transmission projects are based on two main architectures for the DC link: symmetric monopoles and bipoles. In a symmetric monopole, only one AC/DC converter is used in each substation[1]: although the DC link voltage is split equally across the ground (hence the symmetrical term), there is only one physical voltage source. Hence, in the case of a $\pm 50$ kV DC link, a single $100$ kV converter is used at each substation. On the other hand, bipolar schemes use two independent converters at each terminal: for the same voltage, two $50$ kV converters are required. This is illustrated in Figure 8.

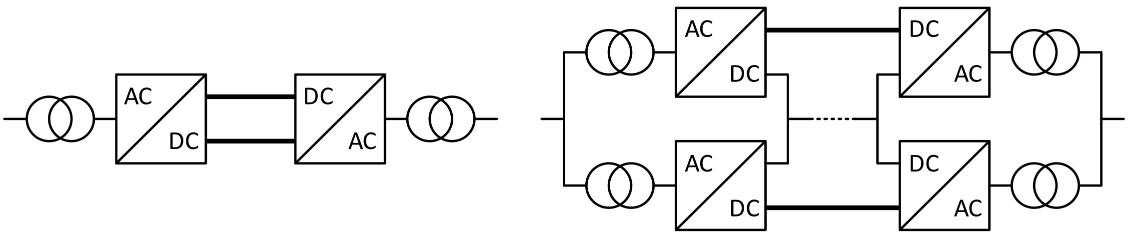


Figure 8 : Illustration of the most common DC transmission architectures, symmetric monopole (left) and bipole (right). In both schemes, the DC cables (bold lines) have symmetric voltages with respect to ground. The current return of the bipole (dashed line) can be either a medium voltage cable, a conduction path through earth, or an open circuit.

The main advantages of symmetric monopoles are their simplicity (lower total equipment count) and their relative low pole-to-ground fault currents, as their typically high earthing impedance strongly limits the fault current with respect to the pole-to-pole fault current (which is the same for both architectures). On the other hand, this mitigation strategy causes a transient overvoltage in the nonfaulty pole, close to twice its nominal value, which has to be accounted for in the insulation sizing.

On the other hand, bipolar schemes normally have a low impedance earthing[2], which leads to opposed benefits: the pole-to-ground fault current is of the same order of magnitude as its pole-to-pole counterpart, but no significant transient overvoltage is to be expected. More equipment is required, as two transmission system halves are effectively built, but it offers a certain degree of redundancy for the system, allowing a partial power flow after a critical failure:

[1] This includes potential series/parallel connection of units, as they still behave as a single converter.
[2] Bipoles feature a natural DC midpoint, which allows for any earthing resistance value. On the other hand, its absence on monopolar schemes (and need for reconstruction) will typically induce a high equivalent resistance.

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- If only two DC cables with no central return (rigid bipole configuration) are used, the transmission of half the rated power is still possible after a converter failure by means of a reconfiguration. The faulty converter and its corresponding in the other substation is bypassed, bringing the voltage of one pole to zero while the other one keeps its nominal value. However, the power flow is completely lost at the first cable fault, similarly to a symmetric monopole.

- If either a metallic return (third, low-insulation cable) or a ground return (current flow through earth) is implemented, not only the previous mitigation is possible, but it is also extended to the failure of one cable, also resulting in half the rated power capability.

To the authors' knowledge, most of VSC-HVDC systems (the picture being different for LCC) use a monopolar configuration, with rated voltages up to $\pm 320\ kV$ DC and power ratings up to around $1\ GW$. However, during last years, the increase of cable rating voltage up to $\pm 525\ kV\ DC$ has enabled an increase of power transmission up to $2\ GW$ (implying an increased rated current) by means of bipolar architecture scheme with dedicated metallic return. At this level of transmitted power (2 GW) the choice between bipolar and symmetric monopolar scheme is dictated by the risk of losing the total amount of power. Indeed, symmetric monopole entails 2 GW loss during pole to ground cable fault and/or converter fault, while bipolar configuration with metallic return only entails 1 GW loss, which is considered to be the acceptable loss of infeed for the European TSOs.

### 3.1.2 Proposed architecture

Considering the figures of merit described above and the corresponding characteristics of the SCARLET case studies, it was decided to select monopolar schemes in all cases. The main arguments backing this choice are detailed below:

- Although it is feasible, a bipolar scheme would feature more elements and thus increase the onshore substation complexity, which is already considerable. Thus, the monopole is preferred.

- Regarding the comparatively low voltage selection ($\pm 50\ kV$ in the most stringent case), the technical difficulty regarding the Transient Over Voltage (TOV) requirement of monopolar schemes (i.e., The ground fault of one pole temporarily doubling the voltage of the other one) was not considered as a showstopper, even for the superconducting cable.

- Given the high DC current ($20\ kA$ in the most stringent case), a parallel connection of several converter units is to be expected. Thus, the partial power capability during a converter outage is intrinsically obtained, as switchgear allows to eliminate a faulty converter, even in monopole.

To benefit from the advantages of this scheme, a high earthing impedance will be selected. Thus, it is assumed initially that the pole-to-ground fault are not stringent with respect to overcurrent, and that the transmission system insulation must be rated for a transient overvoltage of twice its nominal value.

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## 3.2 MMC converter architecture

### 3.2.1 Modular Multilevel Converters background

For the AC/DC conversion, Modular Multilevel Converters (MMC) have been adopted. This topology [2], which constitutes the state-of-the-art solution for DC transmission, has already been widely used in the last 10 years, and proven to be both effective and reliable. The “half-bridge” variant, which is the most common, most efficient, and least expensive is adopted. Its schematic is shown in Figure 9.

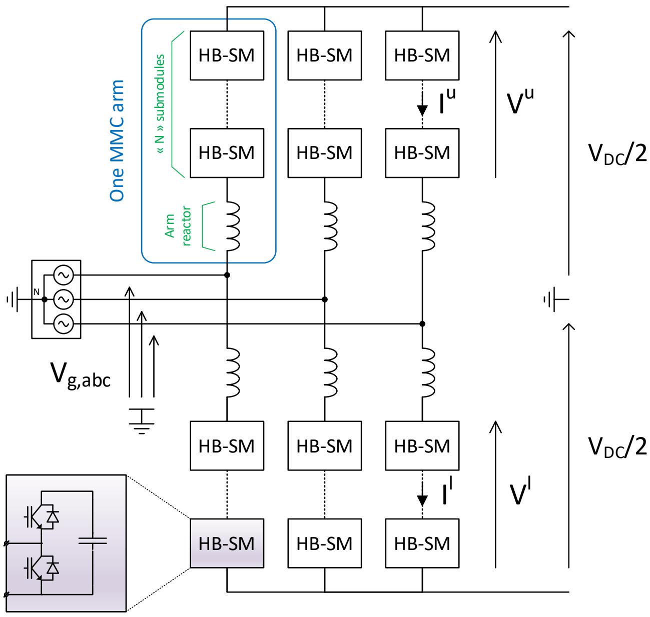


Figure 9 : Illustration of a Modular Multilevel Converter (MMC) with Half-Bridge (HB) submodules.

In this converter, equivalent subsystems called “submodules” are series-connected in stacks, forming controlled voltage source. Each submodule is composed of a capacitor and a pair of power semiconductors, mostly IGBTs (Insulated Gate Bipolar Transistors) or IGCTs (Integrated Gate Commutated Thyristors), which are controlled to either insert or bypass their corresponding capacitor. This degree of freedom is used to control the arm voltages, hence the arm currents, hence the AC and DC currents, while maintaining an internal energy balance. Examples of power semiconductors adapted to MMC applications are provided in Figure 10.

Figure 10: Illustration of high power, fully controlled semiconductors. From left to right: Mitsubishi CM1500HG-90X (4500V/1500A IGBT), Infineon P2000DL45X168 (4500V/2000A IGBT), Hitachi 5SHX35L4521 (4500V/1800A RC-IGCT).

Practically, the submodules are grouped in modules, arranged in converter towers, which are laid out in a temperature and humidity-controlled valve hall. This arrangement is presented in Figure 11.

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Figure 11 : HVDC submodule (left) and corresponding converter towers (right). Illustration from Siemens Energy [3].

The MMC structure is well known, however the ratings of the SCARLET case studies differ strongly from the common practice in HVDC, because of the comparatively low transmission voltage allowed by the superconducting cable at this power rating. This has two major consequences:

- As no market-available power semiconductors can handle $10\,kA - 20\,kA$, some kind of parallel connection is unavoidable. It can be performed at different levels, but the simplest solution for control, current sharing and protection is to connect full converters in parallel, with their arm reactors and dedicated transformer winding.
- In order to mitigate the number of parallel branches (converter + transformer + breakers and switchgear), which has an adverse impact on footprint, the highest reasonable current rating will be selected for each one. The two most limiting elements are the semiconductors and the DC circuit breakers.

To the authors' knowledge, typical $\pm 320\,kV$ HVDC system current ratings range from $1000\,A$ to $1500\,A$, with a corresponding rated power of $600 - 1000\,MW$. As a rule of thumb, the required rated current of the IGBT modules is close to the nominal DC current of the transmission link, although several calculation steps and design choices are involved. Indeed, the precise current rating selection for a given application depends on component availability on the one hand, and system choices on the other hand (e.g., cooling system sizing, efficiency optimization), but this order of magnitude is effective as a first estimate. For instance, the France-Spain link operated by INELFE [4] is rated for $1000\,MW, \pm 320\,kV$ ($1563\,A$) and uses $3300V - 1500A$ IGBT modules.

As stated previously, for the SCARLET project, targeting the highest current capability is beneficial. Currently, press-pack IGBT/IEGT[3] devices with rated current up to $3000\,A$ are available in the $4.5\,kV$ range, mostly without integrated free-wheeling diodes (Infineon P3000ZL45X168, Toshiba ST3000GXH31A, Littlefuse T2960BB45E), a few exceptions providing this diode integrated in a larger

[3] Injection-Enhanced Gate Transistor, a close variant of IGBT optimized for current conduction.

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packaging (Hitachi/ABB 5SNA3000K452300, CRRC TG3000SW45ZC-P200). Although their use is less common (and they are produced by less stakeholders), IGCTs can also provide very large rated currents and potentially higher blocking voltages: some examples of such MMC-adapted devices are the 5SHY 65L4521 (4500V/3200A, asymmetric: external diode required) and 5SHX 36L4520 (4500V/1800A, reverse conducting: no external diode needed). Although the internal diode is convenient, its absence is not a blocking point for half-bridge MMC submodules, which then feature a single clamp assembly with four power devices (two IGBT/IEGT/IGCT and two diodes).

Press-pack fitted MMC submodules are already available on the market. A first example of medium voltage full-bridge submodule is from Hitachi (formerly ABB), composed of Reverse-Conducting IGCTs, which is dedicated to various applications, like hydroelectric Pumped Storage Power Plants (PSPP) driving [5], Static Frequency Conversion (SFC) [6] and static reactive power compensation (STATCOM). An assembly of such submodules is presented in Figure 12.

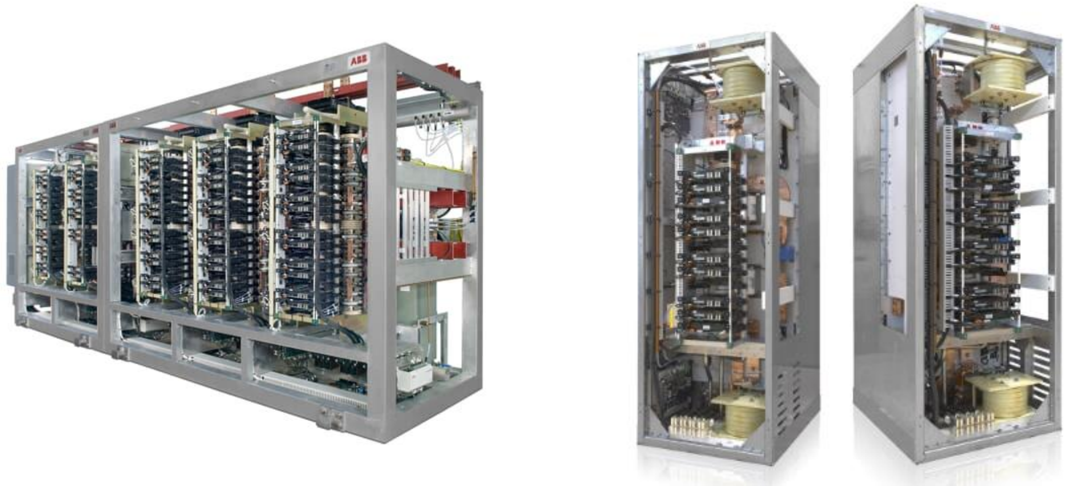

**Figure 12 : RC-IGCT medium voltage submodules assembly, from Hitachi Energy. A vertical slot, as detailed on the right-hand-side, contains two full-bridge submodules in a single mechanical clamp.**

A second example is the MM7 product range from GE Power Conversion [7], which features a versatile single-clamp, four-devices assembly, targeted towards various applications from static reactive power compensators (STATCOMS) to high power medium/high voltage variable speed motor drives, including MVDC transmission up to $100\ kV$. It is illustrated in Figure 13. The MM7 submodule served as a feasibility assessment for the proposed ratings of the SCARLET project converter.

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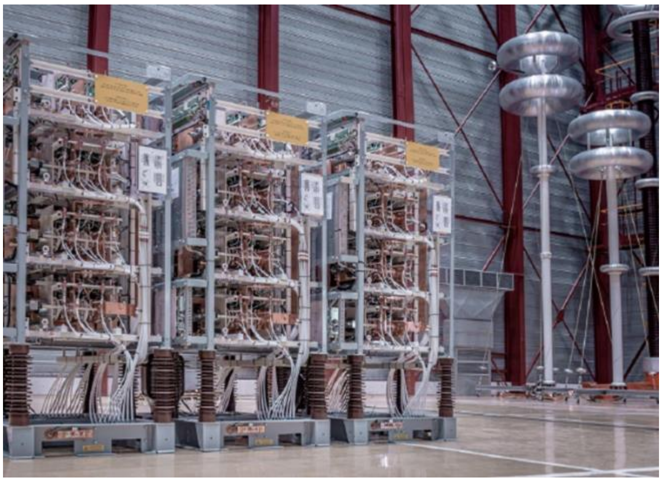

Figure 13 : MM7 converter towers from GE Power Conversion [7].

### 3.2.2 Selected converter feeder ratings

Given the information presented in the previous section, regarding both the semiconductor devices available and the existing medium voltage converter developments, a maximum converter rated current of $3000\ A$ DC was considered feasible. This results in at least 4 parallel feeders in $\pm 50\ kV$ ($10\ kA$), and at least 7 of them in $\pm 25\ kV$ ($20\ kA$). This is compatible with the ratings of the corresponding DC circuit breakers, which are assumed to be available up to $3000\ A$ as well. A rated current of $2500\ A$ was selected, according to the following observations:

- It does not increase the number of converter feeders in $\pm 50\ kV$ (4 are needed anyways). While it does increase the minimum feeder count from 7 to 8 in $\pm 25\ kV$, this will be shadowed by the practical requirement of an even feeder count, which will be stated in Section 3.5.
- This lower rated current increases the number of devices commercially available for a physical realization: not only $3000\ A$ press-pack devices are guaranteed to fit with a comfortable margin, but so is a parallel connection of two $1200\ A - 1500\ A$ flat-pack modules, and probably even $2000\ A$ press-pack devices with a careful thermal design.

Most (if not all) MMC applications are based on power semiconductors with blocking voltage ranging from $3300\ V$ to $6500\ V$, most using $4500\ V$ devices. Conveniently, this blocking voltage is also the one of most of the high-current press-pack component offer, such that it appears as a relevant choice for SCARLET applications. For their convenience (no bulky turn-on $dI/dt$ limiting circuit [8]) and higher number of manufacturers, IGBTs[4] are chosen over IGCTs, and a blocking voltage of 4500V is selected. The nominal capacitor voltage of each submodule is, according to the capabilities of these devices, selected at $2500\ V$, which is a safe choice.

Thus, not accounting for redundancy, the required number of submodules in each arm is the ratio between the DC link voltage and the submodule voltage: $2 \times 50\ kV \div 2500\ V = 40$ in $\pm 50\ kV$, and $2 \times 25\ kV \div 2500\ V = 20$ per arm in $\pm 25\ kV$. It is worth noting that in both cases, a total submodule

[4] IEGTs are close enough to IGBTs to be included in the same term at this stage, for reasons of conciseness.

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count of $4 \times 6 \times 40 = 8 \times 6 \times 20 = 960$ is needed in a conversion substation, which is much lower than what is typically encountered in $1\ GW$ HVDC applications (e.g. 1536 with $4.5\ kV$ devices in $\pm 320\ kV$, not including redundancy), which is due to the comparatively higher current rating of each submodule. The converter footprint may be lower because of this fact, associated to the much lower insulation voltage level, motivating an optimized AC yard architecture in order not to lose this lead because of the parallel connection constraints.

One last aspect regarding the number of submodules is the redundancy requirement. MMC converters are designed such that the failure of one submodule is transparent and does not jeopardize the outer power flow. Different solutions exist, some using dedicated fast mechanical bypass switches, and some based on control actions and/or the fail-to-short[5] property of press-pack modules, but in any case, faulty submodules are eliminated by shorting their outer terminals. Hence, the target reliability is obtained by including more submodules than necessary, such that some of them can fail without causing any power flow interruption, and they are replaced during the next maintenance operation. No detailed analysis was performed for SCARLET, but according to known examples in HVDC on the one hand, and SuperGrid Institute's experience on the matter on the other hand, a redundant submodule count of 10% was selected. Consequently, 44 submodules per arm will be used in $\pm 50\ kV$, and 22 submodules per arm in $\pm 25\ kV$.

Additionally, the number of parallel converters play a role in this choice, since a consumption of all the redundancy installed in an arm would, at worst, cause the loss of either 25% (1/4) or 12.5% (1/8) of the power capacity, and not 100% as if no paralleling was employed.

## 3.3 AC feeder side

### 3.3.1 Generalities

A typical HVDC AC/DC substation schematic is presented in Figure 14.

[5] Press-pack devices are designed such that they fail (whatever the cause) in a stable short-circuit mode.

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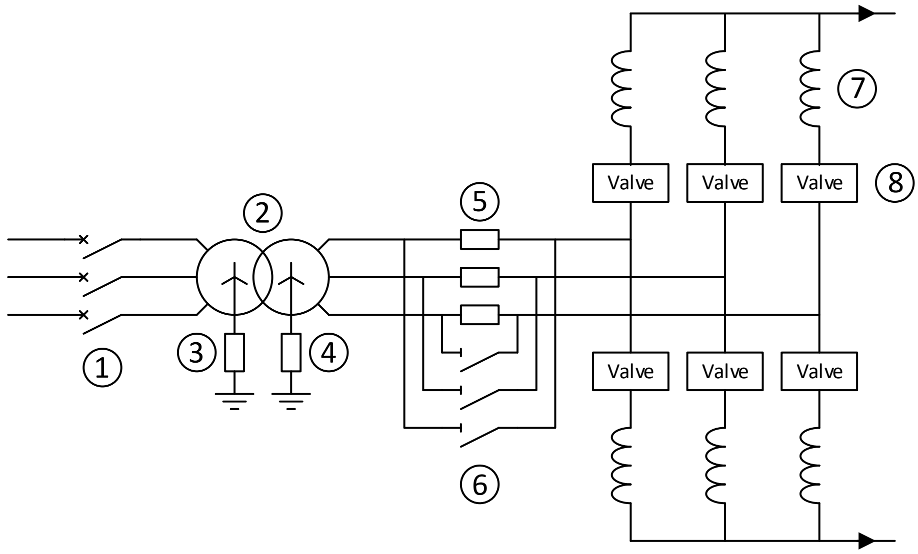


Figure 14 : Example of a simplified AC/DC substation schematic.

Its main elements are listed below, corresponding to the numbers in the figure:

1) A main AC circuit breaker, in charge of protecting all the substation against faults.
2) A transformer group, generally made of three single-phase transformers in HVDC[6], which adapts the incoming AC grid voltage to the converter voltage (which is related to the DC link voltage)
3) A primary-side earthing impedance, to comply with the grid code request with respect to zero-sequence impedance. A star coupling is assumed for the grid-side winding.
4) A converter-side earthing device, which ensures the balance of both poles voltages across ground while limiting a possible third-harmonic current circulation through the cable screens. It is pictured for a YNyn transformer, hence it is simply an impedance connecting the converter-side star point to the ground. Starpoint reactors are typically used when a YND transformer is used instead.
5) A precharge resistor bank, limiting the inrush current of the converter during energization. The DC cable is precharged at the same time.
6) A bypass switch, which eliminates this resistance after the precharge phase. It is normally a disconnector in high voltage, but load switches or circuit breakers can be used in medium voltage, providing more flexibility.
7) Arm reactors, which are needed in the MMC, and additionally mitigate the ripple currents due to the switching actions of the converter and limit its short-circuit current.
8) Finally, the arms itself, which are stacks of submodules as described previously.

[6] On the one hand, the three units are easier to transport and install than a single three-phase transformer; on the other hand, it reduces the expenditure associated with redundancy, as the spare unit is a single-phase transformer with only 1/3 of the rated power. Practically, 4 single-phase transformers are installed instead of 3, which is less costly than installing 2 three-phase transformers instead of 1.

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Many elements are not shown for reasons of conciseness: on the one hand, disconnectors and earthing switches for safety and maintenance; on the other hand, voltage and current transformers for control and protection purposes.

### 3.3.2 Parallelization

As discussed previously, the required parallelization brings an opportunity for redundancy and for partial power flow capability, but in order to be usable, it must come with an adapted substation architecture. To this end, three notable abilities are desired:

- The elimination of a faulty converter feeder must be allowed by dedicated switchgear.
- Its maintenance must be possible while the other feeders remain active.
- After maintenance, its reinsertion must be possible without de-energizing the full system.

As stated before, adverse effects of the large number of parallel converter feeders shall be mitigated in order to keep a low footprint, and maximizing these abilities could lead to an increased number of elements (no mutualization). Consequently, a tradeoff is desirable.

To this end, a particular attention was given to the connections, most of them being normally air insulated busbars. Indeed, to the author's knowledge, most onshore HVDC systems use Air Insulated Switchgear (AIS) equipment. First, it is proposed to use three-phase transformer units, which require no external coupling busbars connections. This is allowed by the converter feeder power which is, at most, $250\ \mathrm{MW}$ ($\pm 50\ \mathrm{kV}$ case). In order not to have eight $125\ \mathrm{MW}$ transformers, it is decided to group the $\pm 25\ \mathrm{kV}$ converters by pairs on a three-windings transformer.

Then, the precharge system is moved to the primary side: although it will require more insulation (the energy being comparable), it will be located with the mandatory grid (common busbar) side switchgear, and may be integrated well in its footprint. This will bring a drastic reduction of the transformer inrush current as a side benefit.

Using this approach, a direct cable connection from the transformer (oil/cable termination) to the valve hall (cable/air termination) is possible, allowing for a minimal footprint and a maximum layout flexibility. Next to this cable termination, an earthing switch is located for the maintenance of the valve hall. With this approach, the only remaining element on the transformer secondary side is the earthing impedance: it can either be cable-connected to the neutral point and located outside, or preferably directly integrated into the transformer oil tank.

Finally, integrating the current measurements inside the transformer terminations is common practice, and like the earthing impedance, voltage transformers/dividers could be integrated in the oil tank too, especially at this relatively low voltage level (around 66 kV RMS line-line in the most stringent ± 50 kV case). This type of complex transformer design is a common practice on high power, medium voltage diode rectifiers for variable speed motor drives, which require several secondary windings with different vector groups. Thanks to the common oil tank, integrated sensors and direct oil-to-cable terminations toward the converters, the global footprint remains limited. It is considered that, for

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SCARLET, the converter-side AC voltage is moderate enough ($66\text{kV}$ or $33\text{kV}$) to adopt a comparable design. This kind of integration has also been proposed for HVDC [9].

On the DC side, the SCARLET project adds DC circuit breakers and their associated $dI/dt$ limiting reactors, one per pole and per converter feeder (refer to section 6). Disconnectors and earthing switches are required too, and it is expected that their marginal footprint increase is negligible with respect to the breaker itself and its reactor. They connect each converter feeder to the common DC busbar, which goes to the superconducting fault current limiter (SCFCL), then to the superconducting cable termination.

## 3.4 Resulting +/- 50 kVdc architecture

Following these guidelines, the simplified single line diagram (SLD) of the $\pm 50\ \text{kV}$ substation is built and presented in Figure 15. The disconnectors, earthing switches, current and voltage transformers are not represented (different options of busbar topologies are possible as described in 4.3). The left-hand-side of the AC busbar, where two incoming AC feeders are drawn, is more related to the transmission grid substation than to the AC/DC conversion station itself: the only assumption is the presence of a circuit breaker on each incoming feeder, which will serve the protection strategy as depicted in section 6.

The MMC converters are not detailed, but they follow strictly the schematic presented in Figure 9, with six submodule stacks and six arm reactors connected on the DC-grid side. The ratings are as presented in Section 3.2.2: 44 submodules per arm, a nominal capacitor voltage of $2500\ V$ and a DC current rating of $2500\ A$.

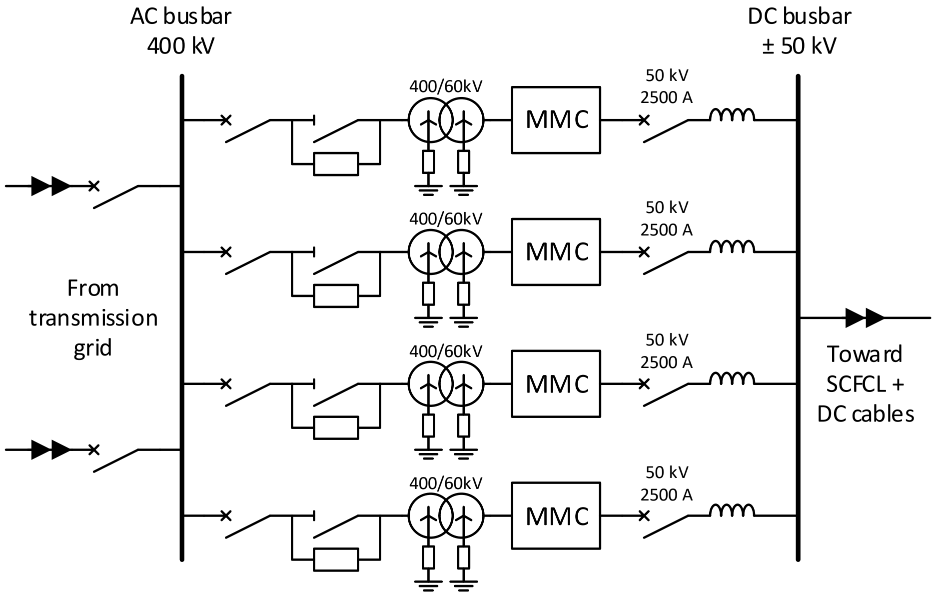


Figure 15 : Simplified Single Line Diagram of the proposed ±50 kV AC/DC substation architecture.

As mentioned previously, four 250 $MW$ three-phase, two-windings[7] transformers are used. Assuming a reactive power requirement of 40% of the active power (that is, 100 $MVAR$ per converter), 270 $MVA$ transformers are selected. Although it may be fine-tuned during the refined converter sizing step, a secondary-side voltage of 60 $kV$ is selected, adapted to the 100 $kV$ DC each MMC is providing.

Finally, the DC circuit breakers are rated for $50\ kV - 2500\ A$ (each pole), with breaking capabilities to be defined.

## 3.5 Resulting +/- 25 kVdc architecture

Following the same full parallelization approach presented in the last section, eight three-phase transformers with an individual rating of 135 $MVA$ (125 $MW$) would be required. However, it was decided to use only four 3-winding transformers, with a primary rated for 270 $MVA$ and two 135 $MVA$ secondaries, each one feeding a separate MMC converter.

Keeping the pre-charge system on the primary-side, it forces both converters of a pair to be energized at the same time, so a fault can cause a permanent loss of $1/8$ of the nominal power capacity, but a temporary loss of 1/4 could be necessary to re-energize the faulty feeder after its maintenance. As a consequence, equivalent or better service continuity is obtained compared to the $\pm 50\ kV$ case, and better in any case than a monolithic HVDC converter (no parallelization), which was considered sufficient. This architecture results in the same high voltage AC yard (breakers and precharge system), with a marginal complexity increase in the medium voltage AC yard.

The resulting SLD is illustrated in Figure 16. Due to the lower DC voltage, the converter-side AC voltage has also been reduced: keeping the same ratio, a 30 $kV$ line-to-line RMS voltage is adopted on each winding.

[7] Not mentioning possible auxiliary windings. An auxiliary tertiary winding is a common practice in HVDC, providing one source of power for the control system (among others, like the local distribution grid).

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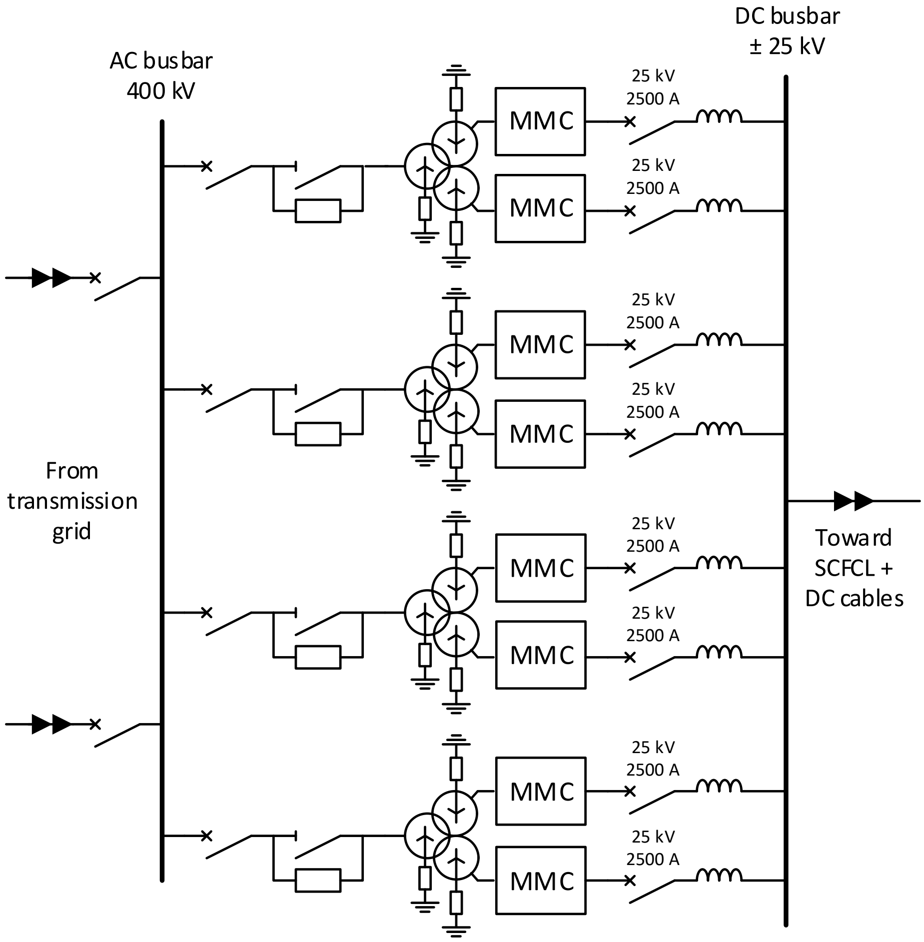


Figure 16 : Simplified Single Line Diagram of the proposed ±25 kV AC/DC substation architecture

The MMC converters feature less submodules (22 per arm), keeping the total count constant in the converter station, but are otherwise identical. Finally, the DC circuit breakers have a two times lower nominal voltage, with the same nominal current.

### 3.6 Expected ripple current characteristics

In HVDC, the harmonic currents caused by the switching actions of a MMC are normally negligible from the AC and DC PCC, which is a consequence of the high count of submodules (high number of voltage levels), and of the relative insensitivity of conventional cables to these low amplitude harmonic currents. However, medium voltage superconducting transmission raises two concerns with this regard:

- First, the lower DC link voltage allows a vast reduction in the number of submodules, which increases the voltage harmonic distortion mutatis mutandis;
- Second, ac components, superimposed on the DC component, induce AC losses in the superconducting tape or filament. Those losses are, at first approach, proportional to the product of the ac component amplitude by its frequency. The sensitivity to these losses depends case by case on the selected superconducting material and cable design.

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Consequently, the harmonic current emission from the DC link terminals shall be assessed carefully. Due to the complexity of the MMC converter itself and its control and modulation scheme, this task is not trivial, and will be performed during the project. At this stage, only guidelines can be provided.

The MMC can be represented as an equivalent Thevenin generator with an inductive impedance, which is a function of the arm, equivalent AC network and DC network inductances. The order of magnitude of this value is the mH. The cable link being inductive itself, with an estimated inductance of several hundreds of mH for 100 km, it provides a drastic decrease of the harmonic current emission of the converter. This is illustrated in Figure 17.

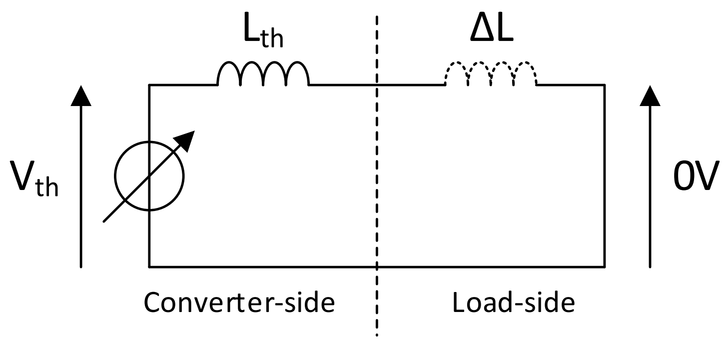


**Figure 17 : Equivalent circuit showing the influence of the cable link inductance on the DC current ripple produced by the MMC. The Thevenin parameters have yet to be determined but are estimated to a few kV at several kHz for the former, and a few mH for the latter. The cable inductance is estimated to be one or two orders of magnitude higher.**

The Thevenin voltage, on the other hand, depends on the converter control and/or modulation scheme. Although the number of levels is reduced with respect to HVDC (e.g., from 250 to 20 in the most stringent $\pm$25 kVdc), it is still significant. As submodules switch one after the other, voltage steps of only 2500 V are generated (one submodule capacitor voltage).

Owing to the low harmonic emission of the MMC, and the positive contribution of the cable link itself, the first analysis provided a good confidence in the cable-converter compatibility in the case of the SCARLET project. The total harmonic distortion of the DC cable ripple current is expected to be 1% at most, implying RMS ripple currents below 100-200 A. Its bandwidth should be limited to 10 kHz, with a nondeterministic spectrum, as the switching frequency is typically not chosen at an integer multiple of the fundamental frequency [10]. A finer analysis will be performed in future work once the low-level control scheme is determined.

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# 4. OFFSHORE WIND PARK

## 4.1 Offshore windmill conversion chain

As stated in section 1, one main interest of a superconducting transmission link for offshore windfarm export is the suppression of the bulky offshore AC/DC conversion platform. Hence, the collection grid needs to be in DC and at the transmission voltage level, that is, either $\pm 50\ kV$ or $\pm 25\ kV$. In this section, a typical AC-collected offshore wind farm electrical architecture will be described as a reference, then the proposed philosophy and architecture for the MVDC collection network will be presented. Fully-fed (type IV), high power, medium-voltage wind turbines are considered, with a power rating of $10\ MW$ per turbine.

### 4.1.1 Current typical conversion chain

Given the power level of the wind farm, a $66\ kV$ AC collector network is considered as a reference. An illustration of the wind turbine conversion chain is provided in Figure 18. The Rotor-Side Converter (RSC), the Grid-Side Converter (GSC) and the DC capacitor bank in-between are typically in the same enclosure. Some additional elements, like input and output LC filters and a DC bus chopper, are not presented.

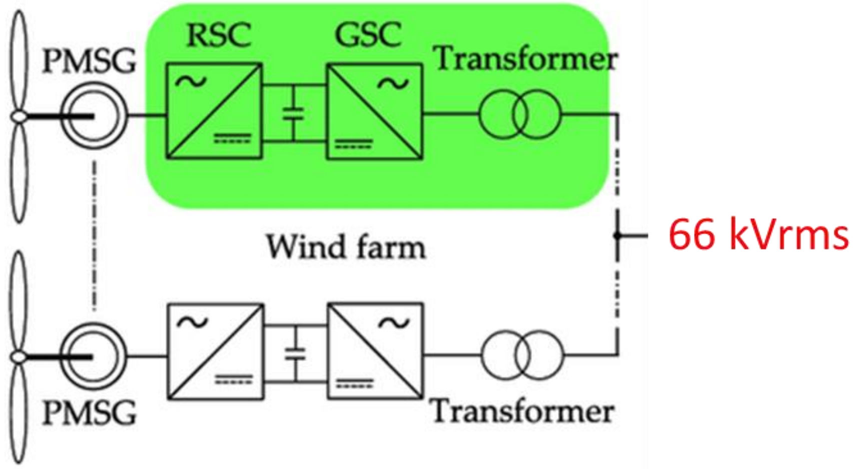


Figure 18 : Block diagram of a wind turbine made for AC collection. The conversion chain is highlighted in green.

As an example, the schematic of PCS6000 wind turbine converter from ABB [11] is presented in Figure 19, and its internal layout is shown in Figure 20. This converter is then connected, through medium voltage cables, to the turbine transformer increasing the voltage up to $66\ kV$. The primary winding of the transformer is connected to the collection network through $72.5\ kV$ Gas Insulated Switchgear (GIS).

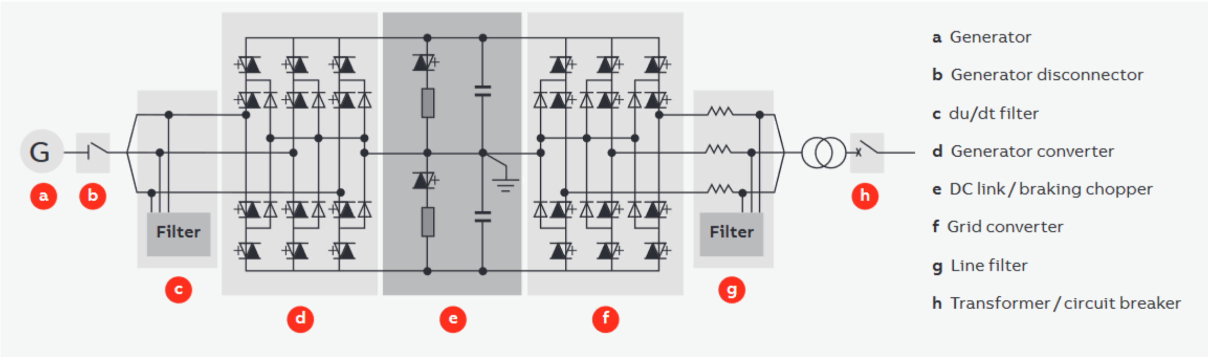


**Figure 19 : PCS6000 wind turbine converter [11], based on three-level NPC inverters with RC-IGCT semiconductors. The nominal ratings are 3.3kV (generator and grid) and 7 MW (8.5 MVA). A braking chopper is included on the DC bus.**

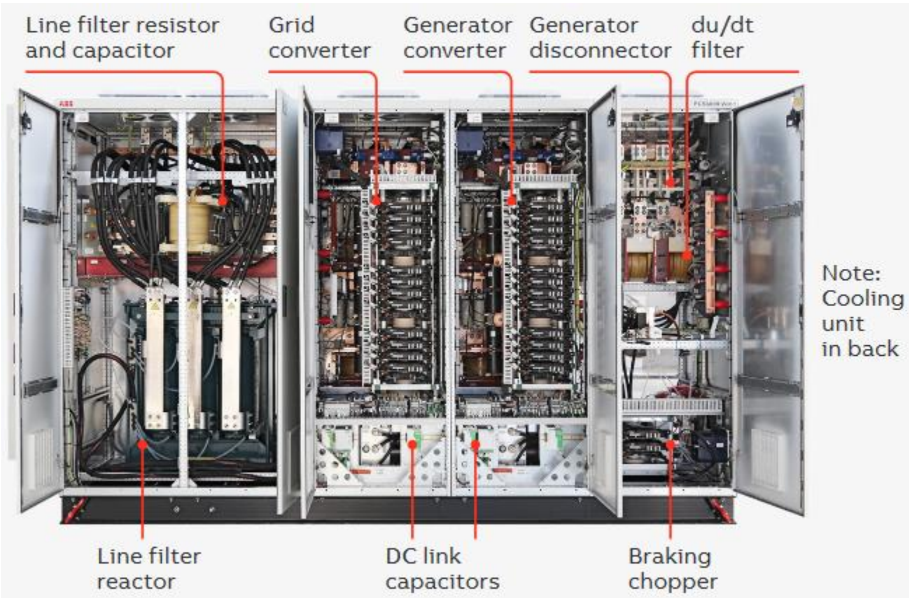


**Figure 20 : PCS6000 wind turbine converter internal layout [11], corresponding to Figure 19. All the medium voltage equipment (converters, filters and switchgear) is included inside the cabinet, excepted the braking resistor.**

This architecture is illustrated in Figure 21.

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**Figure 21 : A possible wind turbine electrical arrangement. The converter and transformer are typically located inside the nacelle, next to the generator, whereas the high voltage switchgear may be placed inside the mast.**

The generator side converter is controlling the wind turbine torque[8] and voltage, providing the best operating conditions. On the other hand, the grid side converter is regulating the intermediate DC link voltage, while providing additional services to the collection network, like reactive power support. The DC bus chopper is used in case of serious line disturbances, like large voltage sags.

### 4.1.2 Proposed conversion chain to achieve the export DC voltage

In order to use a DC collection network, the wind turbine electrical architecture must be adapted. The generator and its associated converter are kept unchanged, such that the missing element is a DC-DC converter. For a $3.3\ kV$ generator, the DC link voltage is typically around $5\ kV$, so a $5\ kV \rightarrow 50\ kV$ $(\pm 25\ kV\ DC)$ or $5\ kV \rightarrow 100\ kV$ $(\pm 50\ kV)$ is required, with a rated power of $10\ MW$ fitting the selected nominal power of the wind turbine. A unidirectional converter, which is significantly simpler to design, is sufficient. Regarding this converter, two approaches were considered:

- Designing a completely new converter, based on a common unidirectional DC-DC architecture (e.g., a Phase-Shifted Full Bridge (PSFB) or an LLC converter);
- Adapting the AC conversion chain, using as many currently available equipment as possible.

[8] This torque control acts together with the blade pitch control.

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Although the former solution may produce a more optimized solution, including a smaller transformer, it is significantly more complicated to design and to industrialize, and the benefits are not guaranteed to be substantial. Additionally, the power semiconductors able to handle the required voltages and currents (large high voltage IGBTs and IGCTs) are not optimized for high frequency (more than a few 100Hz) operation, and conversely, fast semiconductors like SiC (Silicon Carbide) MOSFETS do not have enough voltage and current capabilities for the application and would require a large number of series/parallel connected devices or sub-converters.  Thus, the second option has been adopted. The proposed hardware is presented in Figure 22.

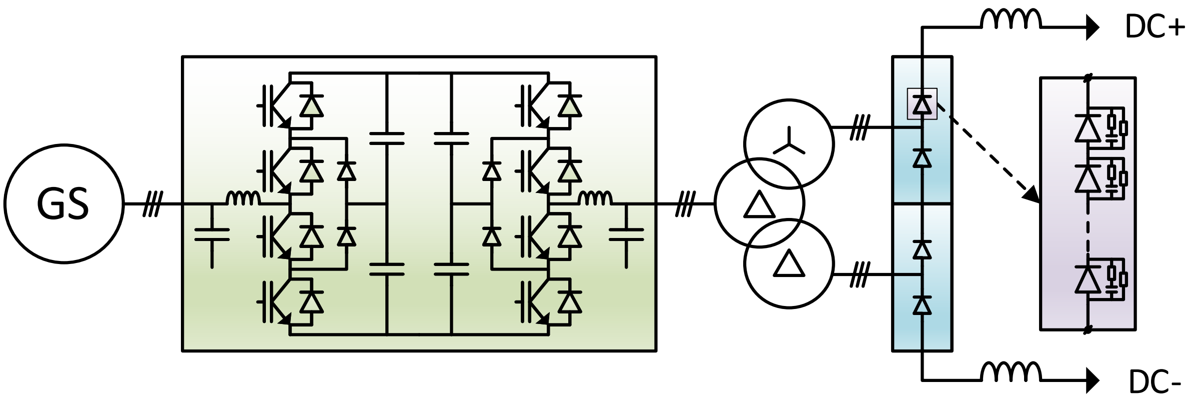


**Figure 22 : Proposed conversion chain architecture for DC collection. Each diode of the twelve-pulse rectifier shown in blue is an assembly of series-connected elementary diodes, with their snubbers and DC balancing resistors.**

Compared to the conventional AC solution, a three-windings transformer is used, and a three-phase, twelve-pulse rectifier bridge is introduced between the transformer and the collection network. Additionally, a smoothing reactor can be added on the DC side, although it may not be mandatory. It is worth noting that the high voltage side equipment is completely passive and can thus be conveniently integrated inside the transformer if the latter is oil-insulated. The number of pulses of the rectifiers is a design parameter, primarily impacting the number of secondary windings on the transformer and their vector group. The higher it is, the lower the input and output current ripples of the transformer will be, such that the AC input (LC) and DC output (L) filters can be made smaller. A twelve-pulse rectifier is adopted as a starting point.

This converter is controlled as follows:

- The generator-side converter operates as described previously, controlling the generator torque and stator voltage in order to obtain the maximum power output in the best conditions.
- The rectifier-side (previously grid-side) converter operates as a fixed-frequency, variable-amplitude voltage source. An internal control loop regulates the DC output current to its setpoint manipulating the inverter RMS voltage, while an external loop acts on this DC current reference to regulate the intermediate DC bus voltage.

Note that the described scalar control of the grid-side inverter has limited transient performance and serves mostly as an illustration and a starting point. More refined control laws are possible, and should be developed in coordination with the converter and filter sizing.

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## 4.2 String cable

Figure 23 shows the possible future trend for the offshore wind farm connection system by means of HVDC system, known as the TenneT's 2 GW standard solution [12]. By increasing the XLPE cable voltage up to 525 kV it is possible to connect up to 2 GW of power in bipolar configuration (1GW per pole). Each Offshore Wind Farm (OWF) generator block has a power of 500 MW at a voltage level of 66 kV[9] and feed one single converter transformer. Each pole of the MMC converter station is connected to two OWF generator blocks and their associated converter transformers. As it is understood, the maximum power of each generator block is probably limited by the maximum power rating of transformer and maximum rating current of the Gas Insulated Switchgear (GIS). For example, for IJmuiden Ver project[10], 4 two-windings transformers rated each 550 MW have been used [13] as shown in Figure 24. Two AC breakers are connected in parallel at each transformer in order to meet the required current rating of 5 kA, this implies that a GIS nominal current of around 2.5 kA is necessary, which is considered to be feasible based on the datasheet in [14]. Another transformer configuration proposed by Energinet is shown in Figure 25, where four three-windings transformers are used. To increase the energy availability, the OFW generator blocks are combined with a cross-coupling arrangement at offshore grid side [12] [15].

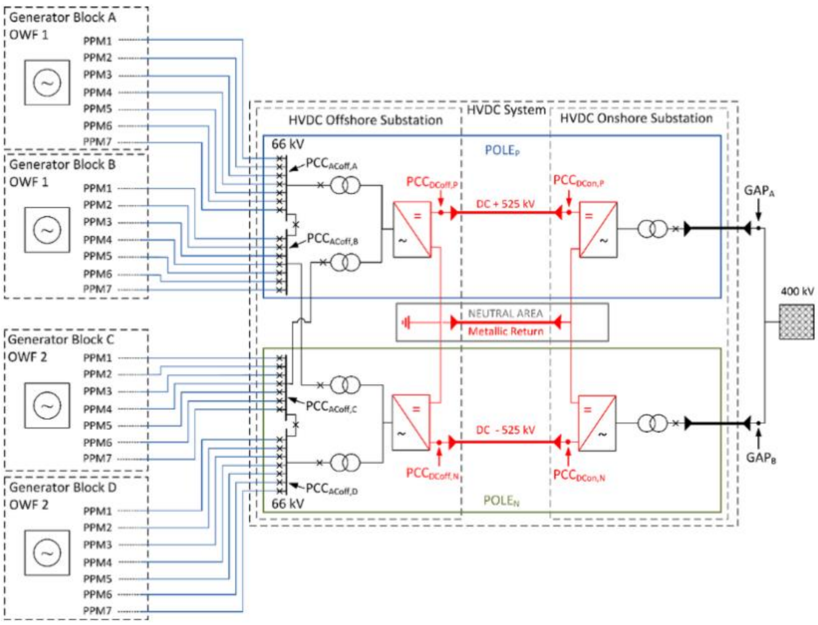


**Figure 23 Basic SLD of 2 GW offshore wind farm connection for HVDC system [12]**

[9] Text from [15]: "In connection with the future expansion of wind energy, it may be necessary to connect the wind turbines with a higher voltage due to longer distances between the offshore energy hubs and the wind power. By connecting the offshore wind farm via an offshore platform, the voltage level can be increased to, for example, 220 kV AC. The technology of the future is expected to develop so that wind turbine voltage level can be increased to 110 kV, 132 kV or 150 kV."

[10] Point to point HVDC system, 525 kVdc , 2 GW bipolar with DMR and Wind Turbine voltage level at 66 kV.

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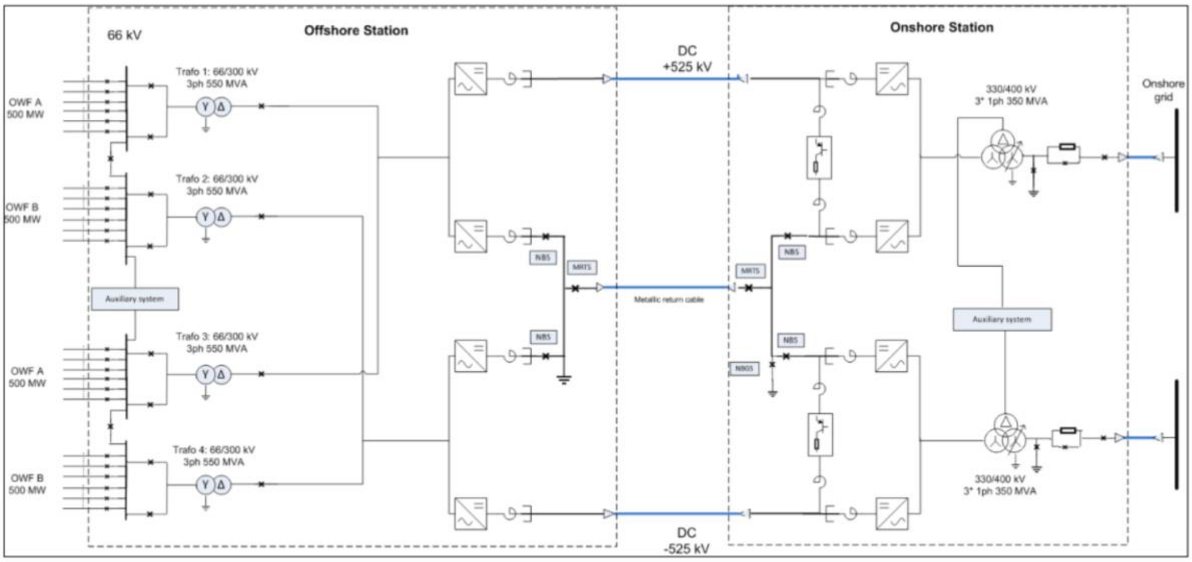


**Figure 24 Generic SLD of 2 GW offshore wind farm connection for IJmuiden Ver [13]**

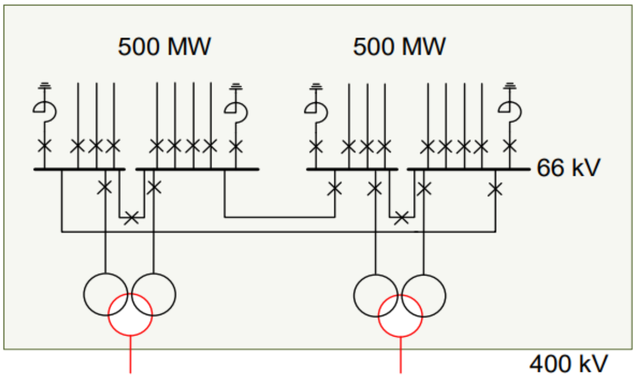


**Figure 25 Example of Wind Turbine connection at AC side with three-windings transformers** [15]

As explained in section 4.1.2, in this project, the proposed solution is to export the energy from each wind turbine to the DC transmission system by converting the voltage to $50\ kV$ ($\pm 25\ kV\ DC$) or $100\ kV$ ($\pm 50\ kV$). Considering those voltage levels, a generator block (or cluster) up to 200 MW is considered in this study. This implies that a rated current of 2 kA and 4 kA is considered for respectively a rated voltage of $\pm 50\ kV\ DC$ or $\pm 25\ kV\ DC$, see Figure 26. It is assumed that future DCCB can reach a rated current of 4 kA. A rated current higher than 4 kA would probably require complex parallelization of ultra-fast switches in the main branch [1]. For information, the existing HVDC CB manufactured for the China DC Zhangbei project has a DC rated current of 3 kA.

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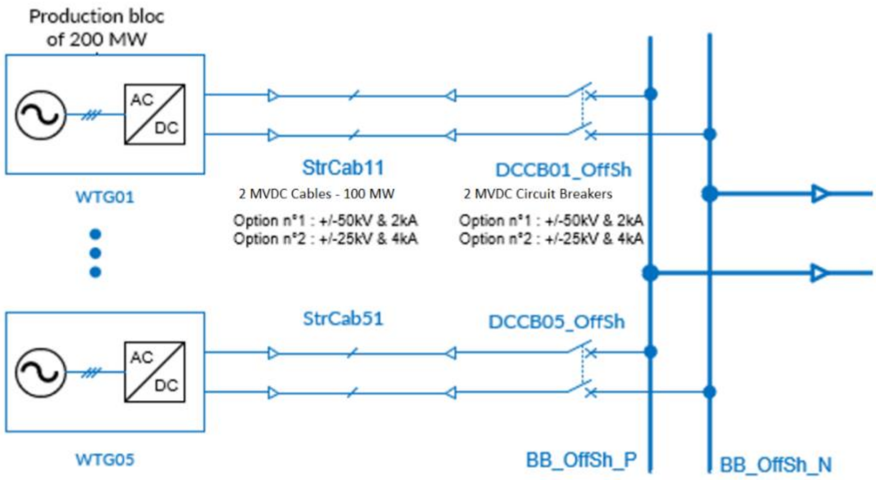


Figure 26 Wind farm production bloc for MVDC system

## 4.3 Collector bus bar

The choice of busbar configuration depends on several techno-economic aspects such as reliability, maintenance, capital expenditure and operational expenditure [16]. Example of possible busbar configurations are shown in:

- Single Busbar
- Double Busbar Single Breaker
- Ring Busbar
- One Breaker and a half
- Double Busbar Double Breaker

The choice of busbar configuration requires a techno-economic analysis which is out of scope of this project. More insight on the advantages and drawbacks for each busbar type can be found in [1].

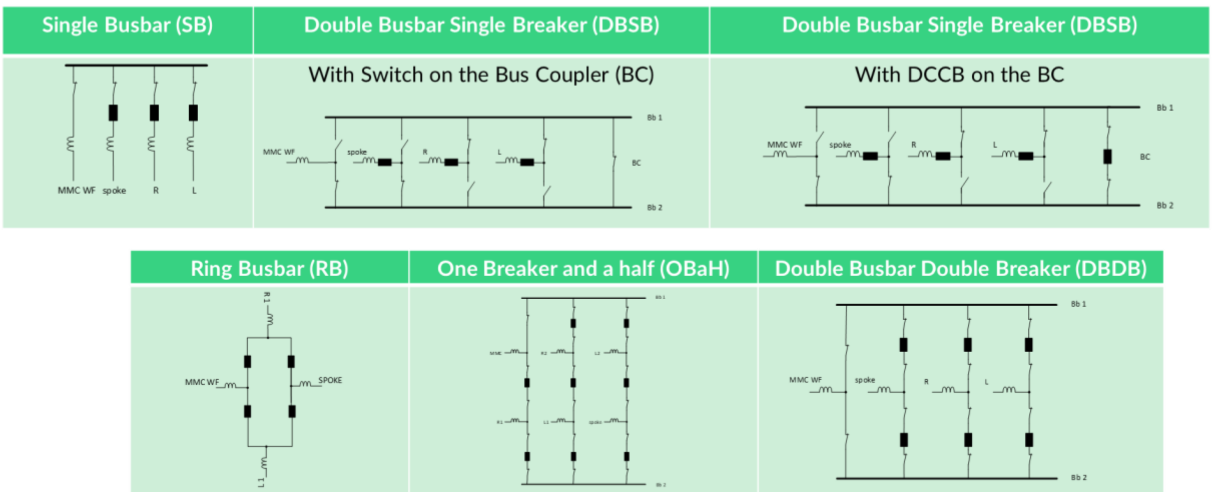


Figure 27: Example of busbar topologies [1]

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Within this report, single busbar topology can be considered as a starting point for the busbar topology. Each feeder can be connected to the busbar by means of a breaker or by means of a simple switch, depending on the selected protection strategy, as will be detailed in section 6.

# 5. SC CABLE

The SC cables architectures are established in Work packages 2 and 4, it is not the purpose of this deliverable to describe them in detail. However, a brief description can be given so as to focus on what parameters are of importance for their integration into the electrical network.

We focus on the architecture selected for the SCARLET demonstrators, that is shown in Figure 18. It consists of 2 monopole cables at + 50 kVdc or +25 kVdc and – 50 kVdc or –25kVdc.

## 5.1 SC cable structure

Each monopole cable is a concentric design where the superconducting conductor is wrapped around a central copper former. The former is made of multiple copper wires and ensures a mechanical support for the wrapping of the SC layer as well as a possible electric bypass and thermal buffer in the event of transient currents. The core of the superconducting cable, transporting the whole current in normal operation, consists of one or several layers of HTS tapes of a given width, helically wound onto the former as shown in Figure 28. In case of $MgB_2$ cables, wires are used instead of tapes arranged in multiple layers or petals wound as shown in Figure 29. The winding angle of the conductors impacts the magnetic coupling coefficients, and hence plays a crucial role in the current distribution among the layers. The number of layers is chosen according to the criteria that in no condition, the current transported by the individual tapes exceeds (including a safety margin) the critical current. If this condition is violated the superconductor enters the resistive state, producing dissipation. This may lead to the warmup of the whole cable producing long outage of the transmission system (recooling will be needed, and this may take up to tens of hours, as discussed in section 5.2) or even the permanent damage of the cable (both of the superconductor and electric insulation, if boiling of the coolant is reached due to overtemperature) if excessive overtemperature is reached. The design of the protection system and of the cable itself must be coordinated in such a way that even in the most severe fault scenario, no excessive overcome of the cable's critical current should occur. For achieving this objective, a Resistive Superconducting Fault Current Limiter (RSFCL) is included in the protection system in coordination with DC circuit breakers (see section 6). It limits the fault current amplitude and duration of the circuit, thus avoiding excessive, and costly, oversizing of the superconducting cable. The fault current limiter exploits itself the transition to the resistive state, warming up during the limiting action. However, its design can be optimised, in order to obtain a much shorter recovery of the nominal temperature than the cable, and it can also be electrically bypassed, allowing immediate restoring of the system. Next to the conducting core of the cable, we find a solid dielectric that has to withstand twice the system voltage in case of a pole to ground fault. On top of the solid insulation, we find a wrapped copper screen which is grounded at the cable extremities. The screen fixes the potential and evacuates the induced current in case of transients during short circuit or cable energization.

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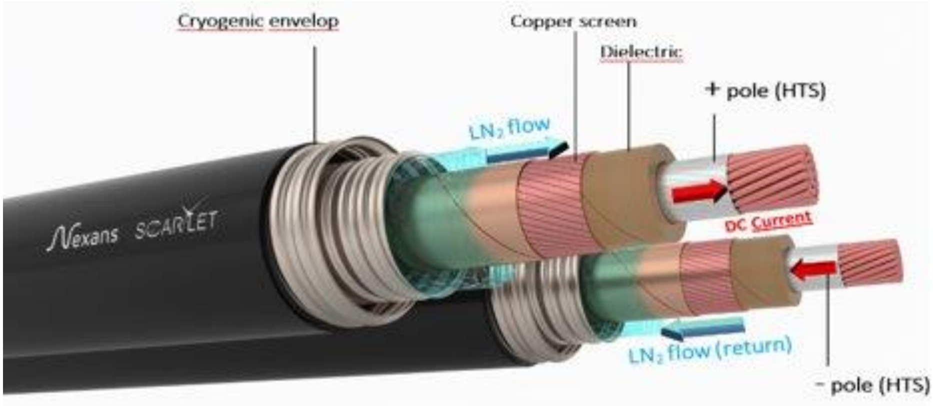


Figure 28 Two monopoles HTS cable as designed within Work Package 2

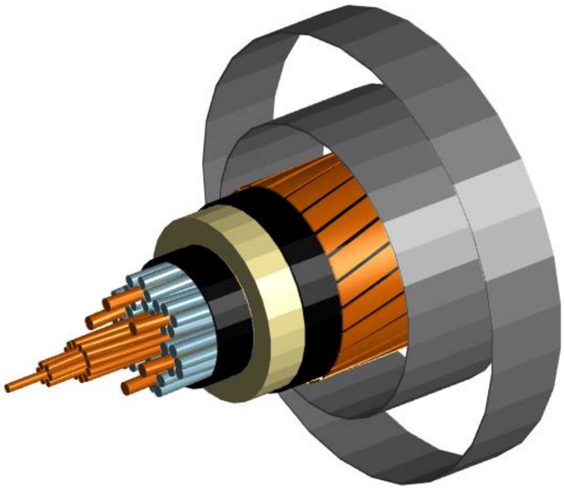

Figure 29. Layout of one MgB2 monopole cable as designed within Work Package 4

The cable is placed inside a double wall stainless steel cryostat. The two walls are thermally insulated with vacuum and spacers, while liquid nitrogen or liquid hydrogen flows inside the inner tube to cool down the cable. The internal and external pipes are grounded at the cable extremities together with the screen.

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The two monopole cables have the same structure, one is used for the cooling fluid one way circulation, the other is used for the cooling fluid return circulation.

The two cables can be separated by the distance "d", as it shown in Figure 30. The impact of this distance will be on:

- Installation logistics, cost and right of way: the simplest is to lay the two monopoles together at the same time in the same trench.
- Apparent magnetic field: adjacent cables will not exhibit an external permanent DC magnetic field, each of the cables screening the other.
- Mutual Inductance characteristics that have possibly an impact on short circuit current for fault at the load side of offshore side of the cable. The mutual inductance might also have an influence on transient current circulating in the different conductive layers of the cable.

Cable shield grounding also has an influence on the transient currents in the different conductive layers. Depending on the cable characteristics, it might be necessary to implement a grounding resistance of several hundreds of mOhms. Detailed studies will be necessary to give recommendations to the manufacturer.

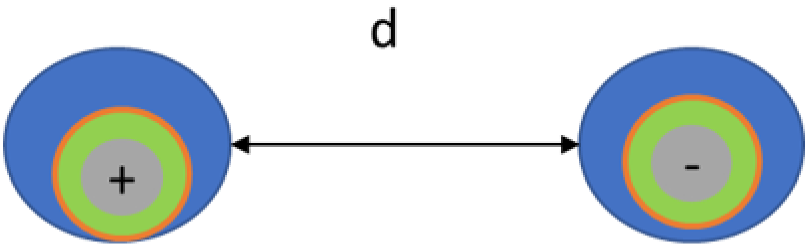


**Figure 30 2 monopole cables layout**

To evaluate the cable behaviour (more precisely the thermo-electrical behaviour of the conductive layers) in different shield grounding conditions and transient events (faults and loading), WP5 will establish the detailed electrical model of the cables. As the HTS cable differs from the MgB2 cable, two specific models will be built.

This electrical model will have to consider the capacitive and inductive components as well as the temperature and current dependent resistive components of the conductors. The inductive components will be built in the form of a direct and mutual inductance matrix, where all the mutuals between all the conductors will be evaluated: for instance, if we consider a cable structured with former + superconductive layer + shield + inner tube + outer tube, a 10 x 10 inductance matrix will finally be built so as to consider the coupling between all layers of the two monopoles laid at the given separation distance "d".

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## 5.2 Cryogenic machine auxiliary power supply

### 5.2.1 General considerations

The SC Cable needs machinery to maintain the coolant temperature and pressure at the right operational values all along the cable length. This is described in more detail in work packages 2 and 4. However, in work package 5, we are looking at the feasibility of the feeding of such machinery through a dedicated auxiliary power supply, especially in the case of an offshore cable. We will focus on the HTS cable, operating with $LN_2$ as a coolant, to establish the requirements of the auxiliary power supply. Results and conclusion of this study could then be transposed to the MgB2 cable solution. operating with $LH_2$. Note that, as regards as $LH_2$ is concerned, the flow conditions are not governed by the $MgB_2$ cable cooling requirements but by the business case of bulk $LH_2$ transportation, contrary to the HTS cable case where $LN_2$ flow conditions are exclusively determined by the HTS cable cooling requirements.

The main hypothesis we consider are the following:

- 100 km link.
- Power supply requirements
    - low voltage supply 3Ph - 400 Vac
    - Cryogenic machines: 2 MW every 25 km
    - Pumps
    - Chiller
    - Platform air conditioning
    - Life base

  It results in a total power supply estimation of 3 MW every 25 km.

- Consider that the first machinery is onshore and does not need to be powered by this dedicated line.
- Consider that we need a switching platform at the SC cable extremity, therefore it could advantageously be used to house cooling machinery.

Different distributions of the cryogenic intermediate platforms can be considered. A conceptually simple approach is shown in Figure 31, where the first cable section is cooled by the onshore substation, and each of the following sections is cooled by its shore-side extremity, through a dedicated offshore platform.

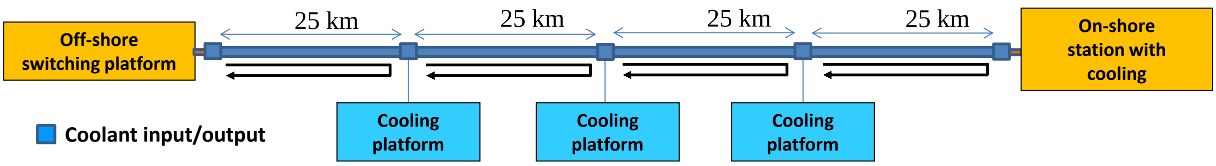


Figure 31 : A possible layout of the cooling substations, with single-sided cooling.

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Using this architecture, the last intermediate platform to supply is one section closer to shore than link end; that is, 75 km for the considered case study (4 sections of 25 km). However, it requires three intermediate platforms. It is assumed that the number of the latter has a significant impact on the overall cost, pleading in favor of larger but less numerous platforms. Following this rationale, more intricate but possibly more cost-effective schemes are possible, such as shown in Figure 32.

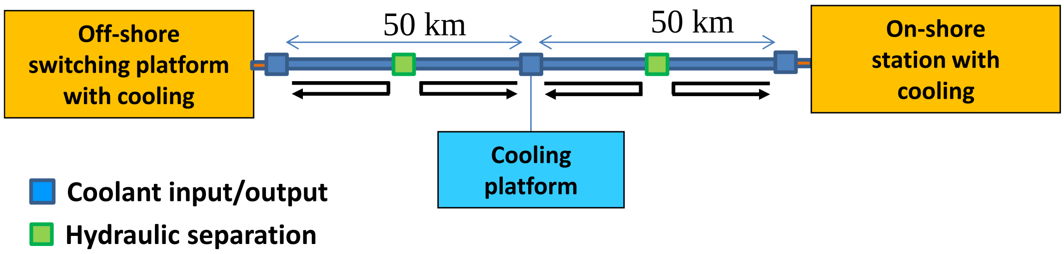


**Figure 32 : An alternative layout of the cooling substations, featuring double-sided cooling. The hydraulic length is kept unchanged, while the total number of platforms (terminal included) is halved.**

In this schematic, the hydraulic connectors at 25 km and 75 km loop the coolant flow from one cable to the other, isolating both sides. Thus, each cooling unit has the same burden as previously, but only one intermediate platform is necessary instead of three, in addition to the cable extremity switching platform. The existing terminal platform is now also used as a cryocooling station, and the intermediate platform contains two of the latter. It should be mentioned that the second option is more demanding with respect to the auxiliary power supply: besides the first quarter, that is directly consumed on shore, half the total power is transmitted to mid-length (50 km), and the remaining quarter is fed to the transmission system extremity (100 km). This is not, however, expected to change the conclusion of the following part.

### 5.2.2 Considered architectures

Two architectures of auxiliary power supply are introduced and discussed:

- Feeding of the auxiliary power through the SC cable itself, using the SC cable former and pick the power via a 50 kVdc– 1 kVdc converter and then a standard commercial 1 kVdc – 3 Ph 400 V inverter
- Supply with an additional three phase AC cable from shore

As detailed above, the platform load has a complex nature, including both direct-on-line motors (e.g., pumps, fans), variable speed motors (e.g., chillers) and auxiliary loads (e.g., life base, control and monitoring system), most of them being expected to operate with a three-phase AC supply at 400 V, with currently available options. Hence, for both options, it is proposed to focus on this voltage level and to assume a lumped load, even though this solution is probably suboptimal regarding the losses (number of cascaded conversion stages). Indeed, given the power level (3 MW), if the cryo-machines could be adapted, an intermediate medium voltage may be interesting for the largest rotating

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machines, which is then stepped down again for the remaining lower power motors and auxiliary loads. This implies, however, the development of specific cooling systems, which is out of scope of this project.

### 5.2.3 Tapping converter

In this option, the power required for the cryogenic machines to operate is fed by the DC cable itself, through an additional converter. Assuming a DC link voltage of $\pm 50\ kV$ (with no loss of generality), this converter transforms 100 kVdc into 3x400 Vac. For practical reasons, it is proposed to separate the latter in two conversion stages: one being a DC/DC converter stepping down the 100 kVdc into a low intermediate voltage (typ. 600-1000 V), which is then fed to a conventional three-phase, low-voltage inverter (e.g., 750 Vdc to 3x400 Vac). This architecture is presented in Figure 33.

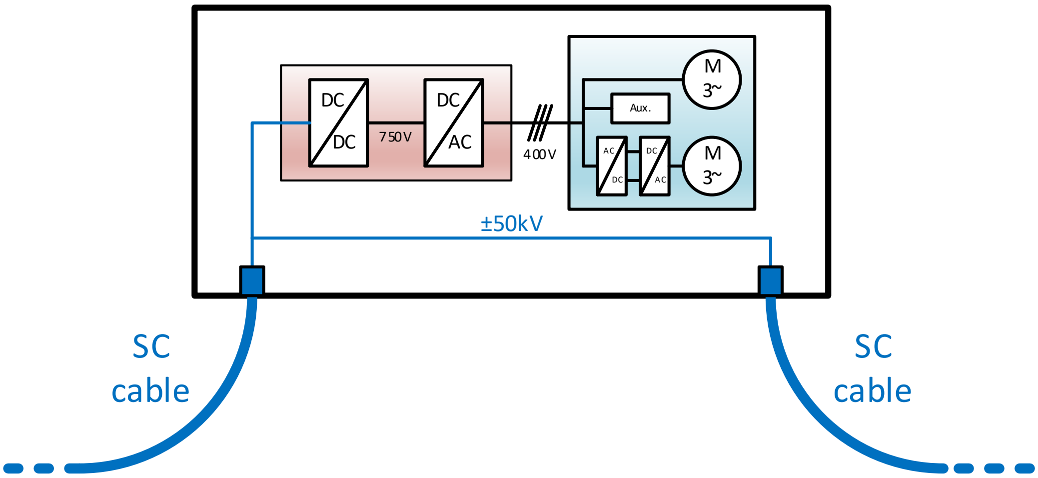


Figure 33 : Auxiliary supply principle with a tapping converter. All the platform load (in blue) is assumed to be fed in 3x400 V, which is supplied by the grid-forming auxiliary converter (in red) from the superconducting cable itself.

At first, the tapping converter appears to be a very attractive option: first, it makes the cryogenic platforms operate independently, with no electrical link besides the SC cables. Secondly, as the power is not taken from the shore but regularly on the main link, no additional transmission distance constraints are introduced by the auxiliary power system. Finally, no change is required on the superconducting cable system, keeping its manufacturing and installation cost as low as possible. However, the first analysis brought attention on several challenges and limitations:

- The 100 kVdc – 750 Vdc tapping converter is not commercially available and may require complex and expensive developments. Indeed, its complexity is not much inferior to that of the main AC/DC onshore converter, even though its current rating is very limited, due to its high voltage constraints. Indeed, due to the lack of a market, no commercially available AC/DC converter hardware is adapted to this voltage and current levels[11].

[11] This affirmation may look to be conflicting with the proposed solution for the wind turbines DC/DC converters in this report, which also has to manage this high voltage at comparable currents. What massively reduces the

- This DC-DC converter is likely to be significantly oversized due to the lack of appropriated power semiconductors (high voltage, low current), with adverse effects on its efficiency and footprint, impacting the footprint of the intermediate platforms. This increase is critical, as it must not outweigh the footprint reduction of the offshore conversion platform, which is one of the main expected benefits of superconducting transmission.

- The tapping converters introduce, by nature, a coupling between the main and auxiliary power systems. As a consequence, their possible contribution to faults, the stress they receive from the latter and their ability to quickly restore the power flow after faults must be carefully assessed.

- Instead of a simple hydraulic connection to the pipes for the coolant inlet/outlet, which is at ground potential, tapping the auxiliary power from the main line requires an access to the inner superconducting cables. The termination is thus more complicated, with electrical insulation, and may therefore require a specific development.

- Finally, before power transmission, the whole system must be cooled from ~300 K to ~77 K. Due to thermal constraint gradients of the cable accessories, the expected duration of this cooling phase is not less than 10 hours, during which all the cryogenic machines operate at full power. In other terms, the auxiliary power is maximum while the main cable is not superconducting yet, implying that its copper core must be able to carry it in steady state like conditions without any degradation and without adding significant heat load.

### 5.2.4 Feeder cable

Due to these limits, the more conservative option of an auxiliary cable system is also considered. In this case, a three-phase AC cable would be installed along with the main superconducting cable system, providing all the auxiliary power from the onshore AC grid. This architecture is presented in Figure 34.

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WT converter complexity is its unidirectionality in the increasing voltage direction, which allows for the use of a passive rectifier bridge on the high-voltage side. On the other hand, the auxiliary converter is unidirectional but step-down, such that its *high voltage* bridge must be active.

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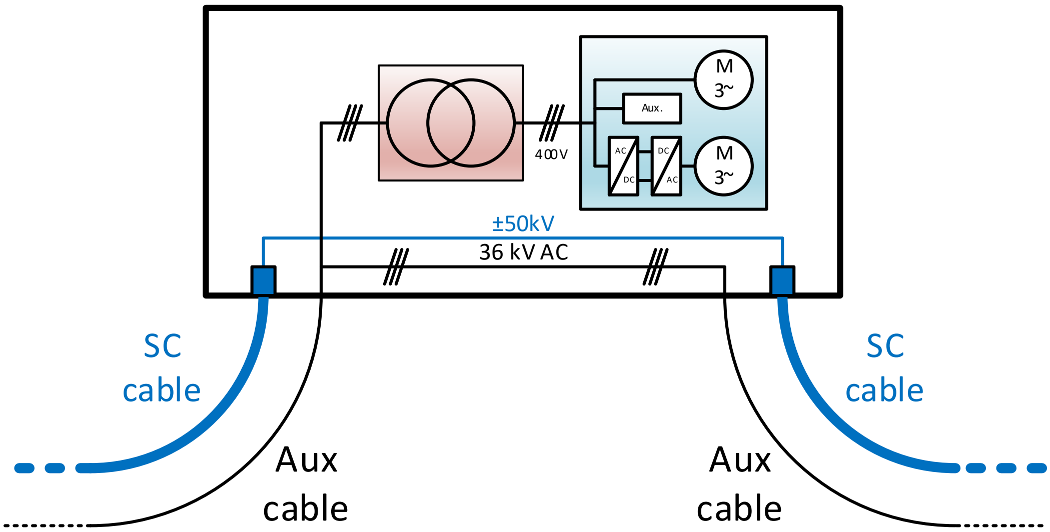


Figure 34 : Auxiliary supply principle with a feeder cable. All the platform load (in blue) is assumed to be fed in 3x400 V, which is supplied by a MVAC feeder cable through a local three-phase transformer (in red), from the shore power.

Echoing what has been done for the tapping solution, the following benefits are expected:

- Using only a power transformer at the onshore connection point, and a transformer on each intermediate platform, this solution is more simple, commercially available, and more reliable.
- As no correlation is forced between the main and auxiliary voltages, the latter can be tailored to the project needs regarding distance and auxiliary power consumption.
- Besides a possible magnetic coupling due to their proximity, the main and auxiliary power systems are electrically decoupled, which simplifies the fault management.
- As a corollary, the energization and cooling phase is vastly simplified, as the auxiliary cables are not superconducting and do not require to be heavily cooled down to be operational.

However, using an auxiliary cable also introduces drawbacks, among which are the following:

- This solution is likely to be unsuitable to very long links, because of the voltage drop and charging current of the cables. Mitigation solutions exist to a certain extent.
- For the ease and cost of installation, the auxiliary cables are likely to be bundled with the superconducting cable system or at least to be installed at the same time in the same trench.

Many design parameters are available, both regarding the electrical design and the ease of installation, such as the nominal voltage of the auxiliary supply, the cross-section and insulation thickness of each cable[12], and the use of reactive power compensation systems in the intermediate platforms. Although these choices remain to be decided, the initial assessment performed for the SCARLET project did not

[12] Using a higher voltage class cable (e.g., 66kV cable in 36kV RMS) reduces the capacitive charging current.

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raise feasibility issues for its length, configuration, and power requirements, therefore the use of an auxiliary cable is currently the preferred solution.

# 6. PROTECTION STRATEGY

To protect the electrical network, a selective fault clearing strategy combining DC circuit breakers and RSFCL is proposed that can be applied on production cases, consumption cases as well as the point-to-point cases. Combining the two types of protection devices relaxes the stresses on the superconducting cable, and, at the same time, the DC breaker reduces the burden on the RSFCL and also allows for continuity of service in case of loss of one of the parallel converters. This last advantage is a natural consequence of the parallel onshore converter architecture, as seen in sections 3.4 and 3.5. The faulty converter branch can be isolated within a few milliseconds thanks to the DC CB, and power can still flow with the remaining modules.

Moreover, each of the AC feeders has an AC circuit breaker that will naturally be tripped in case of fault on this feeder or its transformer. But important as well, it will be used as a backup breaker in case of failure of the DC breaker of the same converter branch. In such an event, due to the much longer opening time of the AC breaker, the current limitation property of the RSFCL will be of very high impact on the SC cable protection.

In this strategy, and to design properly the protection devices, a mapping (Figure 35) of the possible fault is necessary as well as defining the protection sequence in each case.

To simplify, we will distinguish pole to ground faults and pole to pole faults.

We will also distinguish faults on DC bus bar and SC cable from faults on one converter branch or one offshore string.

Pole to ground faults will have very small currents limited by the AC transformer neutral grounding impedances. The counterpart is the voltage shifting of the non-faulty pole that will have to be managed by all the components. That should be the case because this constraint is considered in the existing testing guides of HVDC equipment that will be used as guidelines or standards to qualify the electrical system components.

Pole to pole faults are the worst cases in terms of prospective currents and they will be a designing factor both for the protection devices and for the design of the ±50 kV and the ±25 kV cables. Depending on the location, the prospective fault current can reach several tens of kiloamperes (as illustrated in Figure 3).

Detailed simulations based on this fault mapping will be performed in Work Package 5 and presented in Deliverable D5.2.

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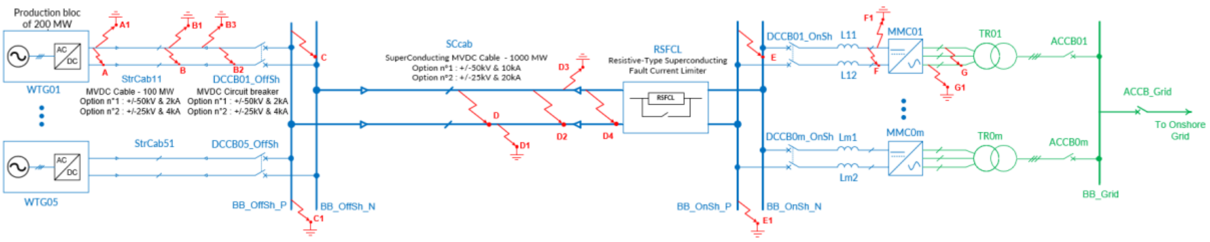


Figure 35: Mapping of fault cases

## 6.1 DC circuit breaker

DC circuit breakers differ from AC Circuit breakers in the sense that the fault current does not exhibit any natural current zero as illustrated in Figure 36. The device must generate its own interruption opportunities with these constraints:

- Interruption has to be faster than in AC within a few milliseconds instead of the typical value of 3 cycles of the power frequency.
- The DC CB has to absorb magnetic energy in order to force the current decaying to zero.

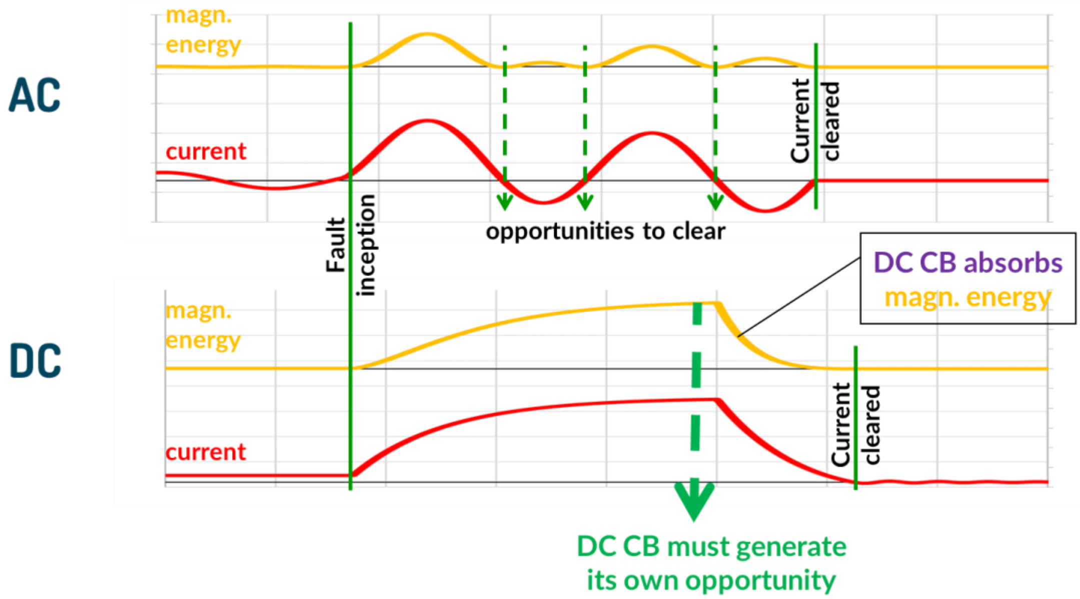


Figure 36: representation of a fault clearance in AC and in DC

Different technologies are proposed by manufacturers and research organisations as described in [17], and range from full electronic devices to mechanical devices with counter current injection, through hybrid devices combining both advantages of power electronics and mechanical devices.

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- All the technologies will have a limitation in permanent current capability in the order of 2500 A – 4000 Adc due to different reasons.
- Electronic or hybrid breakers use the benefit of the switching capability and rapidity of components like IGBTs.
- Mechanical breakers use the benefit of their simplicity but naturally have slower reaction times than electronic components (milli-seconds against micro-seconds). To minimize their opening time, the moving mass (therefore the mass of necessary copper to mitigate the temperature rise under nominal current condition) must be very low, impairing the permanent current capability.
- For the reason of permanent current, a DC CB cannot be inserted in series of the superconducting cable (where we target 10 kA or 20 kA nominal current depending on the SC cable technology) unless many modules are assembled in parallel which makes difficult the control, the synchronism, and the current sharing of the parallel modules.
- The parallel topology of the converter gives the opportunity to install the protection DC CBs in each of the parallel converter branches, that has a permanent current compatible with the CB, as depicted in Figure 15 and Figure 16. It also provides the advantage of selectively isolate any of faulty converter branch.
- The proposed DC CB for the SCARLET application is based on one module of a mechanical HVDC CB [18] as illustrated in Figure 37.
- One module is of 35 kVdc nominal voltage and can interrupt up to 20 kA in less than 5 ms.
- For the 50 kVdc application, two modules are used in series to reach 50 kVdc nominal voltage.
- For the 25 kVdc application, only one module can be used.


Figure 37: Full 525 mechanical DC breaker representation

One module is built according to the principle shown on Figure 38. It consists of 3 branches, namely the main branch where the permanent current passes through, the oscillating branch and the energy absorption branch.

The main branch consists of a Vacuum Interrupter (VI) operated by an ultrafast magnetic actuator.

The oscillating branch consists of a pre-charged capacitor, an inductor (the LC oscillation frequency is on the order of a few kHz), a making switch that will establish the counter current into the main branch to reach a zero current in the VI. A resistor is also used in coordination with a bypass switch to adapt the charge of the capacitor prior to the counter current injection. This tunes the injected current to the fault current.

The absorption branch consists of a stack of Zinc Oxyde blocks.

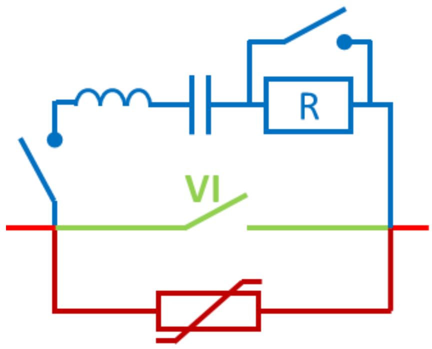


**Figure 38: Electrical scheme of the MVDC CB**

The principle of operation is illustrated in Figure 39 , which is a laboratory test record of the module:

The line current (in red) starts to decay to zero after the injected current (in blue) reaches the line current and the total current in the VI is interrupted (in green, the total current in the VI). After this interruption, a transient interruption voltage (TIV) is imposed across the MVDC breaker.

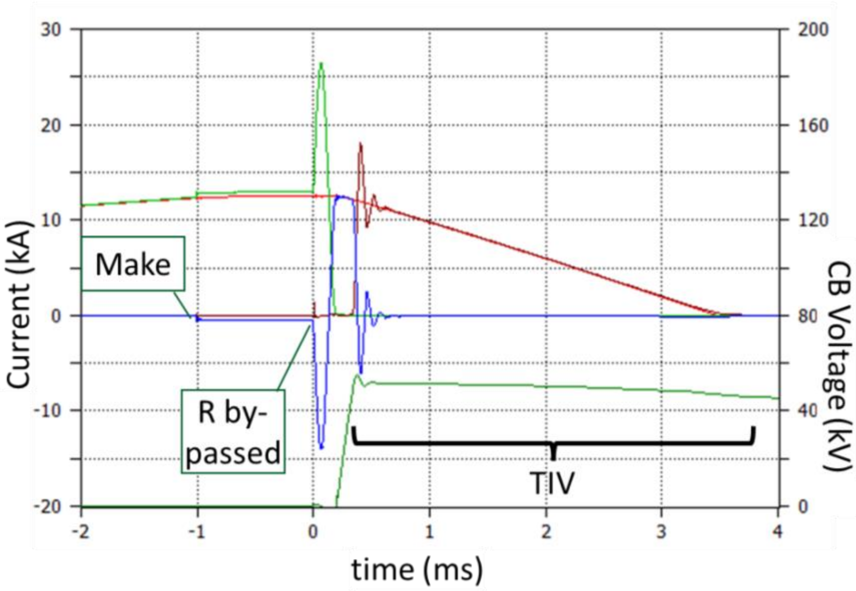


Figure 39: Illustration of fault interruption by the MVDC CB module

This is only when the magnetic energy of the source circuit is fully absorbed by the surge arrester bank, that the line current is fully extinguished (in the illustration, roughly at time 3.5 ms).

## 6.2 DC series reactor

DC series reactors are to be implemented in series with DC CBs in each converter branch, they are typically of dry air type.

They have the function to allow selective tripping and isolation of the faulty converter branch.

Their sizing is driven by the following constraints:

- Match the permanent current of the converter branch: it will define the conductor material and cross section to mitigate the reactor resistance.
- Limit the fault current seen by the DC CB in the worst case condition as illustrated in Figure 40: if a pole to pole fault occurs in branch 1, DC CB of Branch 1 has to be opened while the DC CBs of the other branches remain closed. The N-1 other converter branches will feed the fault of branch 1. The branch series reactor must be sized so that the total fault current seen by DC CB of branch 1 does not exceed its interruption capability, which is 20 kA. The typical expected inductance value to meet this requirement is in the range of a few mH [5 – 15 mH].

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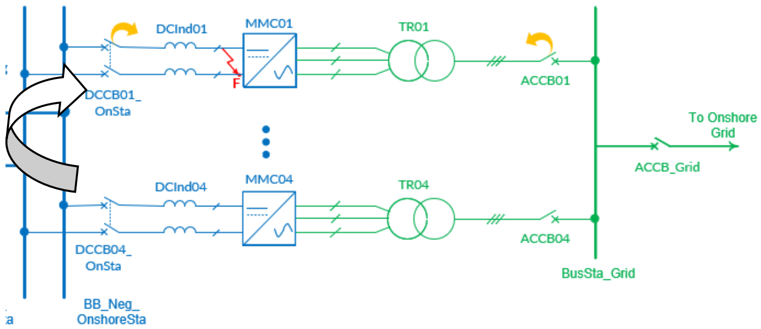


Figure 40: Illustration of converter fault selective interruption

- The series inductance is also dimensioned so to avoid the N-1 other converter blocking during the fault. This criterion is normally met if the reactor is sized to achieve selective interruption, yet it will have to be verified by simulations.

DC series reactor will also contribute to harmonic current filtering even though, the converter architecture itself natively generates low harmonic currents.

## 6.3 RSFCL

A Resistive Superconducting Fault Current Limiter (RSFCL) is a passive device exploiting the properties of superconducting materials to switch instantaneously from almost perfectly conducting state to resistive state based on instantaneous current value. In other words, if the current circulating in the conductor exceeds a threshold value, the conductor becomes almost instantaneously resistive.

This is illustrated in Figure 41. The left picture shows the transition on a one-meter long tape when the applied current exceeds 1000 A. The right picture shows the voltage and current across an assembly submitted to 4500 Vdc voltage source of 12000 Adc prospective short circuit current: the current is limited to ~3700 A and then stabilized to ~2500 A.

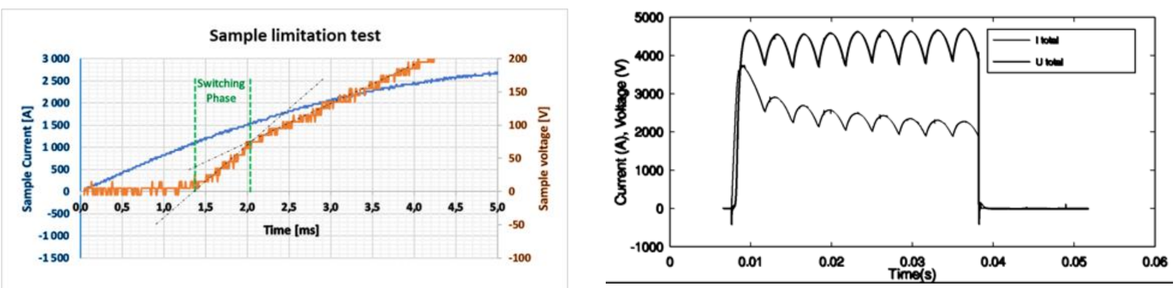


Figure 41: limitation test on superconductive tape sample (left) and wound assembly (right)

The properties of the superconducting tape are met if cryogenic temperature is maintained at the appropriate value. The proposed RSFCL will be made of HTS material operated in liquid nitrogen at a temperature around 70 K.

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RSFCL have been implemented at site by different manufacturers, some significant examples can be listed:

- MVAC RSFCL
  - AMPACITY project by NEXANS, installation of a 10 kV RSFCL together with a superconducting HTS cable [19], 2013
  - KEPCO substation by LS ELECTRIC, 22,9 kV – 2000 A device, 2022 [20]
- HVAC RSFCL
  - Moscow PowerGrid Substation by SUPEROX, 220 kV – 1200 A, 2019 [21]
- HVDC RSFCL
  - Nan'ao conversion station, 160 kVdc, 1000 Adc device, 2020 [22]

These references show the industrial basis for manufacturing such devices even at high voltage is available.

The proposed RSFCL will be installed in series with the SC Cable, it will then have the same permanent current capability. Its function is to avoid the SC cable to be stressed by faults that would lead to SC cable coolant boiling. This reduces the risk of cable fault, cable ageing, cable degradation of its solid insulation or its superconductive properties. Finally, it avoids the SC cable very long regeneration time after a quench.

Another option could be to implement a RSFCL in each converter branch instead of one RSFCL in series with the SC Cable. It would reduce the nominal current of each RSFCL and also suppress the need of the series DC reactor. However, this wouldn't be an economical option as it would highly increase the cost of the limitation function by multiplying the number of cryostats and machines to maintain the cryogenic temperature and operate in closed loop with compensation of the liquid nitrogen losses.

The design of the RSFCL in terms of tape characteristics selection and total length of conductor will require full system simulation including the SC cable model: there are interdependencies between converter fault current characteristics, DC CB characteristics, backup AC CB characteristics, SC cable characteristics and RSFCL characteristics.

As an illustration, the RSFCL critical current should be higher than the rated current but lower than the SC cable critical current. Another illustration is that the total energy to be dissipated by the RSFCL as well as the stress imposed on the SC cable depends on the backup protection scheme in case of DC CB failure.

The main design constraints of the RSFCL will be to manage the high nominal current, manage the energy dissipation during a fault (considering the worst-case conditions) together with designing the high current – high voltage current leads with the smallest possible size of the cryostat.

Within SCARLET, the RSFCL demonstrator will use a cryostat (Figure 42) of 4.4 $m^3$, with a height of 3.6 m and a diameter of 1.4 m. This is suitable for an electric isolation corresponding to a rated voltage of 50 kVdc.

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Figure 42: Limitation test on superconductive tape sample.

## 6.4 Disconnectors and earthing switches

For safety of maintenance operations, disconnectors and earthing switches will have to be installed on the DC busbars. A disconnector is designed to endure the electrical isolation in open position and to ensure the transmission of the permanent current. It also has to sustain a short circuit without degradation of its isolation property in open position. An earthing switch has to sustain the system and transient voltages when the disconnector is closed but it does not have to carry the permanent current. Like the disconnector, it has to sustain a short circuit current.

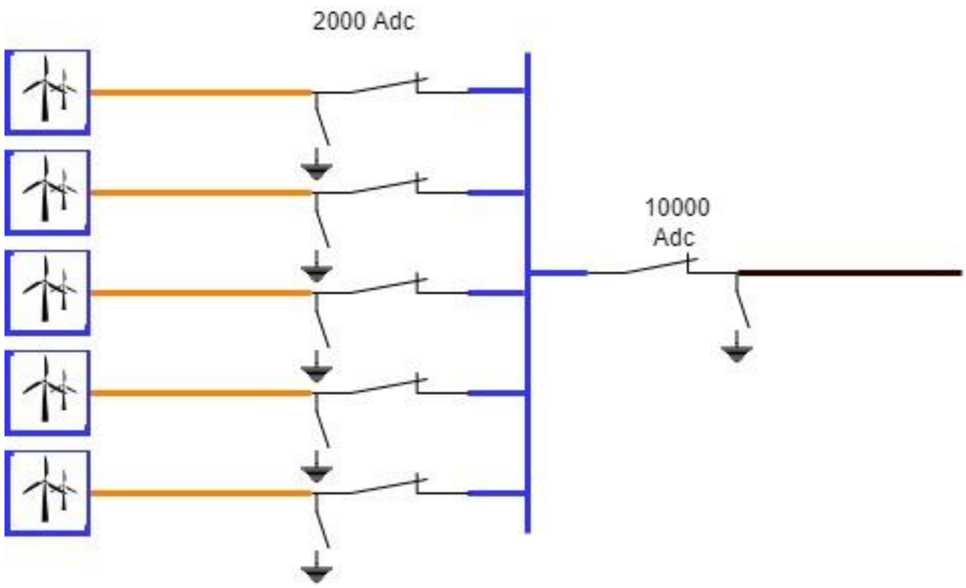


Figure 43: Illustration of fault interruption by the MVDC CB module

Medium Voltage DC switches do not yet exist on catalogue for voltage levels like 25 kVdc and 50 kVdc, however, devices can be found on the market of MVAC switches and adapted to the requirements of the MVDC application.

This is particularly the case for the string side switches where the nominal current requirement is of the order of 2000 – 4000 Adc. Different suppliers offer MVAC disconnectors covering this requirement. For instance, COELME-EGIC catalogue shows a 36 kV and 52 kV (CBD 36-52 kV [23]) product suitable for rated current up to 6300 A. Another example from SDCEM is the SBE type [24] with a rated current of 8000 A up to 38 kV and 3150 A for the 52 kV rated voltage.

As regards the SC cable side switches, the situation is different for the disconnector but not for the earthing switches (it does not need to carry the permanent current). Indeed, as the required permanent current is 10000 Adc or 20000 Adc, classical MVAC disconnectors do not fit though a development effort could be done to extend the existing range where currents of 10000 A are available but at a rated voltage of 24 kV. However, there is a niche market where high duty switches are available, that is the generator switchgear market where the voltage level goes up to 33 kVrms and the nominal current ranges from 6000 Arms to 33000 Arms. For the 25 kVdc – 20 kAdc application, such switches could be used. They could be offered for instance by companies like HITACHI [25]or GE Grid Solutions [26]. For the 50 kVdc – 10 kA application, this technology could be easily upgraded by the manufacturers to reach the appropriate isolation level.

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**Figure 44: Example of a Generator Circuit Breaker disconnector**

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# 7. CONCLUSIONS

In this deliverable, we showed that transporting 1 GW in direct current at medium voltage using a superconducting cable is possible provided several modifications of the classical HVDC schemes:

- the onshore conversion architecture is made of lower voltage MMC Converters connected in parallel implying less series connected submodules.
- these modules are feasible based on actual power electronics technology and industrial converters offered by different companies.
- a 1 GW +/- 25 kVdc or +/-50 kVdc can be of symmetrical monopole scheme that offers higher simplicity compared to bipolar scheme.
- the windmill conversion chain has to be modified in order to get directly the right transport output DC voltage instead of 3 phase ac voltage. This can be done at the simplest by keeping the actual wind turbine converter but adding in series a compact 3-winding transformer and 12-pulse rectifier.
- the converter parallel structure allows to use DC breakers that can advantageously perform selective protection.
- to achieve maximal protection of the SC cable, a RSFCL is proposed in coordination with the DC CBs.
- high nominal current switches are available on the market or could be upgraded by manufacturers to meet the high current ratings in the converter branches or windmill strings on one hand and on the common busbars on the other hand.

The next activity in Work Package 5 is the specification of stresses seen by the different components of this electrical network. They can be evaluated by implementing the proper component models, especially those of the superconducting cable and resistive fault current limiter. These models are developed to be implemented in standard Electro Magnetic Transient Programs that will be used to simulate the whole electrical system.

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