Centralized Control Strategies for HVDC Connection of Offshore Wind Farms based on Diode Rectifier Units

by

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To my family

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PUBLISHED AND SUBMITTED WORKS

- **Journal papers**

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- A. Nami, J. L. Rodriguez-Amenedo, S. Arnaltes, M. Á. Cardiel-Álvarez and R. A. Baraciarte. "Frequency Control of Offshore Wind Farm With Diode-Rectifier-based HVdc Connection". *IEEE Transactions on Energy Conversion*, vol. 35, no. 1, pp. 130-138, October 2019, doi: 10.1109/TEC.2019.2949892. This material has been fully included in the Chapter 3 of the Thesis. The material from this source included in this thesis is not singled out with typographic means and references.
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- **Conference papers**

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Abstract

The possibility of using diode rectifier in the offshore substation for high voltage direct current (HVDC) connection of offshore wind farms (OWFs) has recently received an increasing interest due to its lower cost and losses together with its higher reliability compared with the voltage source converter (VSC) based HVDC link. However, the main drawback of the diode rectifier solution is that the system frequency has to be controlled to guarantee the diode rectifier commutation, given that the isolated offshore AC-grid can not generate the required frequency with the wind turbine generator systems (WTGSs) conventional control systems. This Thesis presents a direct frequency control for such an application whose principles are derived from the AC-side dynamics of a diode rectifier (DR) based HVDC system indicating that the frequency control can be achieved by reactive power balance at the rectifier station without a capacitor bank placed at the diode rectifier station. The proposed control system is implemented by a VSC connected to the rectifier station that is always needed for the centralized frequency control where the WTGSs conventional control systems are not changed. A hybrid HVDC system consisting of the DC-parallel connection of a low-power VSC with the rectifier station is also proposed in this Thesis, which is compared with the traditional VSC-HVDC connected OWFs. Besides the frequency control and diode rectifier harmonic currents compensation, the OWFs start-up is addressed in the centralized hybrid topology, overcoming all the DR-HVDC link drawbacks. Finally, a centralized control strategy of the parallel operation of the DR-HVDC and VSC-HVDC links for OWFs connection is presented. The VSC-HVDC controls the voltage and frequency of the offshore AC-grid needed for the OWFs start-up. The proposed centralized grid forming control is based on controlling the OWFs voltage by controlling the active power balance at the VSC-HVDC AC-bus. This leads to an onpitrized system where the VSC-HVDC can be sized just to support the system energization; while once the OWFs are in operation, the DR-HVDC will be loaded automatically. Stability analysis and simulation results provided throughout this Thesis verify the performance of the proposed control systems.

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Chapter 1

Introduction

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1.1 State of the art and motivation

The increasing demand for electric power has emerged the integration of renewable energy sources and advanced transmission technologies such as high voltage direct current (HVDC) [1]. Wind energy is the most promising and has the huge potential of becoming a major source of renewable energy for this modern world [2] due to its global influence in electricity generation reported in [3]. Recently, as well as the onshore trend, the offshore wind farms (OWFs) are being developed and built increasingly [4]. Offshore wind power refers to the construction of wind farms in bodies of water to generate electricity from wind. Some leading countries in the wind energy arena are focusing more on offshore technology leading to its global growing trend in the last years [3]. The main reasons for adopting offshore are lack of the suitable onshore sites and much better wind conditions of offshore sites (wind is much stronger and more

constant) [1]. In other words, stronger wind speeds are available on offshore compared to on land. Therefore, offshore wind power's contribution in terms of electricity supplied is higher. Offshore wind power can help to reduce energy imports, reduce air pollution and reduce greenhouse gases (by displacing fossil-fuel power generation) while meeting renewable electricity standards.

1.1.1 OWFs connection technology

OWFs are located, in general, far away from the shore. Then, it needs to integrate the offshore wind power plants to the onshore grid by different transmission system schemes. Transmitting the large amount of power generated from the large OWFs to the onshore grid over distances of tens of kilometers has a number of challenges for the developers and system operators. For such applications, the HVDC power transmission system has significant advantages compared with the high voltage alternating current (HVAC) transmission such as no reactive power transmitted, fewer cables required and lower power losses in the cable [5, 6, 7]. Moreover, a synchronization operation of frequency between the OWFs and the onshore grid is not required for the HVDC transmission. Therefore, if the distance is long, the HVDC transmission system might be a more suitable and feasible solution than the HVAC transmission for large OWFs connection [8, 9, 10].

Two different converter technologies needed for the alternating current (AC)-direct current (DC) power conversion can be used in the HVDC transmission systems: line-commutated converters (LCCs) and voltage source converters (VSCs). The LCCs use thyristors which need an stiff AC-grid generating the required frequency for their commutation. However, the isolated offshore AC-grid can not generate the required frequency with the conventional control of the wind turbine generator systems (WTGSs). But, the forced-commutated VSCs which use insulated gate bipolar transistors (IGBTs) in most industrial cases [11] can offer more controllability such as voltage and frequency control for the isolated offshore AC-grid. Therefore, VSC-based HVDC transmission systems have been adopted in current OWF projects, as shown in Figure 1.1 [10, 12]. As observed, the OWF incoming powers from each wind turbine (WT) cluster are collected in the corresponding offshore AC-substation platforms including step-up transformers. Then, all the incoming powers are collected in a central offshore rectifier platform to be transmitted through an HVDC link. However, it needs the construction of a large offshore platform to place a full-rated VSC with its corresponding large-scale transformer, thereby increasing both the OWF installation and maintenance costs.

Recently, different studies have been investigated in order to eliminate the need for the large central VSC offshore platform by proposing new OWF schemes. In [13, 14, 15], OWF schemes based on series and parallel DC collection and transmission are proposed. However, they use additional conversion stages for power conversion via

Figure 1.1: OWFs with VSC-HVDC connection.

multiple stages of transformers and AC-DC and DC-DC stages which need to be placed in one or more offshore platforms, further increasing the losses and cost. Moreover, a DC-collector network is proposed in [16] where the DC-sides of the WTGSs are connected in series. But, such a HVDC connection which is very close to the WTs makes insulation problems for the corresponding power electronics conversion. Therefore, the DC-collector networks for such applications need further investigation to overcome their DC protection drawbacks [17].

In order to eliminate the large offshore platform without the need for using additional conversion stages, a new OWF scheme based on the series connection of diode rectifier units (DRUs) at the intermediate substation level is proposed in [18, 19], as shown in Figure 1.2. The DRU consists of a twelve-pulse diode bridge integrated in the transformer oil insulation, providing a compact solution for OWF applications. Then, by removing the large central DC platform, the volume and weight of offshore platforms and as a result, the total OWF cost can be significantly reduced [20]. Moreover, this technology has lower transmission losses and higher reliability compared with the conventional VSC-HVDC scheme [21]. Therefore, the possibility of using the DRU solution has recently received an increasing interest. However, using the diode rectifier (DR)-based HVDC links for OWFs connection brings some challenges. Due to the unidirectional characteristics of the DRUs, the required start-up power to OWFs can not be supplied through the corresponding HVDC link. For this purpose, [18] proposes an umbilical cable interconnecting the onshore to offshore AC-grid needed for the OWFs start-up, as shown in Figure 1.2. Moreover, as it can be seen in Figure 1.2, passive AC-filters and capacitor banks are placed at the DRU stations needed for the harmonic filtering and reactive power compensation of the DRUs, respectively. But, in order

Figure 1.2: OWFs with DR-HVDC connection.

to guarantee the DRUs commutation, a frequency has to be generated and controlled within the isolated offshore AC-grid. Then, this can be considered as the main challenge in the DRU solution given the missing an AC-voltage source. In the following subsection, the state of the art of the different approaches proposed in the literature to overcome the challenges of the DR-HVDC link is addressed.

1.1.2 Control strategies background for OWFs connected to DR-HVDC link

The studies carried out in the literature prove that the connection of OWFs through the DR-HVDC link is technically feasible using two different control strategies: decentralized and centralized.

1.1.2.1 Decentralized control strategies

Decentralized control strategies refer to the offshore AC-grid forming control without the need for placing any additional element, e.g. static synchronous compensator (STATCOM), at the DRU stations. Then, in decentralized controls, the capability of type-3 and type-4 WTGSs [22] is used to impose the system frequency. But, the corresponding WTGSs available in the market use the well-known voltage-oriented control (VOC) for the conventional converter grid synchronization via phase-locked loop (PLL). Therefore, decentralized controls lead to changing all the conventional WTGS control systems depending on the type of WTGSs used. Note that, in most cases, the type-4 WTGSs are used to implement decentralized controls due to their superior control flexibility.

The decentralized voltage and frequency control proposed in [23, 24, 25] uses only type-4 WTGSs to generate the desired OWFs voltage. The required angular reference is provided from the measurements at the point of common coupling (PCC) bus voltage. However, no communication channel is considered for sending the obtained voltage angular reference to WT's controller. Then, considering a communication delay in the case of a real OWF leads to the lack of robustness in such a control. Using the same control principles, a decentralized control with reduced DRU passive AC-filters is proposed in [26], while the performance of such a control system during fault conditions as well as the efficiency of the solution are studied in [27].

In [28], the standard active power frequency (P/f) as well as reactive power voltage (Q/V) droop controls [29] are implemented in both type-3 and type-4 WTGSs. Then, the WTGSs act as AC-voltage sources controlling their terminal voltages. As a result, the PCC voltage magnitude is controlled to transfer the desired power reference. The concept of using both active power voltage (P/V) and reactive power frequency (Q/f) droop controls is proposed in [30] and [31] resulting in an equal reactive power sharing among WTs for the frequency control, while no communications between WTs are used in [31]. However, the controls still need a remote voltage angle to be received in WT's controller through communication channels, thereby having the same disadvantage addressed before. In [32] and [33], a Q/f droop control without the need for using any communication channels is proposed. The advantage of the control proposed in [33] over the proposal in [32] is that the offshore voltage magnitude is imposed by the DRU stations and as a result, the WTGSs' active power control channel is not affected. However, such grid forming controls based on the droop controls are not implemented in most WTGSs [34].

A decentralized grid forming control is proposed in [35] and [36], where both type-3 and type-4 WTGSs' current injections are synchronized using a common fixed reference frame obtained by the global positioning system (GPS) signal. However, a droop control is used to control the WTGSs voltage angle needed for the WTGSs' reactive power control. Then, this leads to the control of the WTGSs' active and reactive powers being coupled.

It is also worth mentioning that some decentralized controls are proposed for a generic case, where the OWFs connected to the thyristor rectifier (TR) stations are considered. For this purpose, in [37], [38] and [39], a decentralized grid forming control for HVDC connected OWFs using TR is proposed which is implemented in type-3 WTGSs. Then, for objective of the frequency control, the corresponding WTGS terminal voltages are controlled by closing the active power balance at the TR station through adjusting the TR firing angle. Therefore, this control is based on a frequency dependent load. However, it needs the TR firing angle as a degree of freedom and becomes unfeasible with the DR-HVDC connection.

1.1.2.2 Centralized control strategies

Centralized control strategies refer to using a smaller VSC station, compared with the standard VSC-HVDC scheme, connected to the DRU station buses at the PCC for generating and controlling the required frequency within the offshore AC-grid. Then, the conventional VOC needed for the WTGs' converters grid synchronization via PLL becomes feasible, further no changing in the standard WTGS control systems. Therefore, the centralized controls can be implemented in the OWF schemes with WTGSs available in the market. This way, the OWFs active power is injected using the maximum power point tracking (MPPT) control depending on the wind conditions while the OWFs incoming reactive power depends on the reactive power reference or power factor settings in the corresponding WTGS control systems [40].

The possibility of using a STATCOM for centralized controls is proposed in [41], [42], [43], [44] and [45]. In these proposals, the STATCOM behaves as a slack bus setting the offshore voltage magnitude and angle. However, a TR firing angle is always needed in the controls to modify the active power unbalancing detected by the STATCOM DC-link variations, becoming unfeasible with the DRU solution. The principles of a centralized frequency control, which can be implemented by a STATCOM connected to the DRU station bus, are proposed in [46]. The control proves that the frequency control can be achieved by closing the reactive power balance through the STATCOM while the offshore voltage magnitude is imposed by the DRU station through closing the active power balance. However, the proposed control system is derived from an average-value model (AVM) of a DR-HVDC system in which the dynamics of a capacitor bank placed at the DRU station is considered. Then, this is a high-voltage large capacitor bank, demanding larger offshore platforms.

Hybrid HVDC systems needed for the centralized controls in such an application can also be found in the literature. A hybrid HVDC system based on the DC-series connection of a twelve-pulse DRU and a two-level VSC is proposed in [47] and [48]. The VSC is responsible for controlling the offshore voltage magnitude and its DC-link voltage leading to transmitting one third of the OWFs power through the VSC. Besides, the VSC is used to compensate the main low-order harmonics generated by the DRU while reducing the DRU passive AC-filter banks. The same hybrid HVDC converter is used in [49] where the WTGSs are responsible for the controlling the offshore voltage and it can not be considered as a complete centralized control. However, the main contribution of the hybrid HVDC systems in such an application, which is providing the OWFs start-up, is not completely addressed in the previous proposals. Then, [34] uses the same hybrid HVDC converter where the two-level VSC is replaced by a modular multilevel converter (MMC) needed for the OWFs start-up as well as the DC-ripple compensation during the DRU operation. However, again this solution can not be considered as a complete centralized control as the WTGSs are responsible for controlling the frequency within the offshore AC-grid. A new hybrid HVDC system based on

the DC-parallel connection of a twelve-pulse DRU and an MMC is proposed in [50], where an additional DC-DC power conversion stage is used to reduce the MMC rated DC-voltage. The MMC is used for the centralized offshore AC-grid forming control including the OWFs start-up as well as the frequency control during the DRU operation. However, passive AC-filters are still needed to be placed at the DRU station for reducing the total harmonic distortion (THD) of the offshore voltage and as a result, for better performance of the control system.

Moreover, when a new OWF is developed in area beside an existing OWF which is connected to a VSC-HVDC link, both advantages of the VSC and DRU stations can be combined together by connecting a DR-HVDC link to the existing VSC-HVDC link on AC-side. This leads to a large cost reduction as studied in [51] while in the meantime overcoming all the drawbacks of the DR-HVDC link. Then, a control of the parallel operation of the DR-HVDC and VSC-HVDC links for OWFs connection is proposed in [52]. The control system principles are based on the active power voltage (P-V) characteristics of the DR-HVDC link. Therefore, the offshore voltage magnitude is regulated to the required value for transmitting OWFs power through the DR-HVDC link. However, due to the low resistance of the DC-cable, a very slight increase of the offshore voltage magnitude provokes a high increase of the transmitted powers leading to the lack of robustness. Moreover, in this control strategy, the OWFs power must first be transmitted through the DR-HVDC link until it is fully loaded and can not be shared between the VSC-HVDC and DR-HVDC links during this operation mode.

According to the state of the art, the centralized control strategies are the most promising solutions for the industrial companies which are not the WT producer, e.g. ABB HVDC, given that they can be implemented with the conventional WTGS control systems available in the market. Then, the research on the industrially-feasible centralized control strategies for connection of OWFs through the DR-HVDC link has motivated this Thesis work.

1.2 Objectives of the dissertation

The main Thesis objectives can be listed as follows:

- To propose an active power filter (APF) scheme embedded in the centralized frequency control of an OWF connected to the DR-HVDC link leading to the DRU passive AC-filters being completely removed from the offshore rectifier stations.
- To propose a centralized frequency control for connection of OWFs through the DR-HVDC link without the need for placing a capacitor bank at the DRU station.
- To verify the stability of the proposed control scheme through a small-signal analysis.

- To compare the operation and control of two most attractive variants of STATCOM topologies for OWFs connection using the DR-HVDC link during both normal and fault conditions.
- To propose a hybrid HVDC system which allows the OWFs connection through the DRU while overcoming all the drawbacks of the DR-HVDC link.
- To compare the proposed hybrid HVDC system with the traditional VSC-HVDC scheme in terms of relative losses, volume and cost.
- To propose a centralized control strategy for the parallel operation of the DR-HVDC and VSC-HVDC links connected to OWFs which allows a flexible power flow distribution between the two links.

1.3 Structure of the dissertation

This Thesis work is organized as follows. In Chapter 2, the static model of an offshore AC-grid connected to the DR-HVDC link is presented needed for the system harmonic analysis. Then, considering both WT and DRU harmonic currents injection, an APF scheme embedded in the frequency control of the offshore AC-grid is derived to remove the passive AC-filters from the DRU station. This APF scheme is verified and used along the Thesis work for harmonic compensation of the DRU currents. Moreover, the required VSC power demand for the frequency control is analyzed and verified for one specific study case. The principles of a centralized frequency control without a capacitor bank placed at the DRU station are derived in Chapter 3. Then, a new AVM model of the DR-HVDC link is presented where the dynamics of a capacitor bank placed at the DRU station is not needed to produce the outputs. The proposed AVM is verified through a detailed switching model (DSM) simulation. Moreover, the stability of the proposed control system is checked through a small-signal analysis. At the end, a comparison between two variants of STATCOM topologies for the considered OWFs is presented. The dynamic behaviour, fault ride-through (FRT) capability, power losses and relative cost of two approaches are compared during both normal and fault conditions. Chapter 4 presents the performance of the proposed control system in OWFs with a series-connection of DRU stations. It is illustrated that the frequency control can be achieved using only a VSC connected to one of DRU stations. Then, a hybrid HVDC system needed for the centralized control of OWFs connection through the DR-HVDC link is proposed where the OWFs start-up is addressed, overcoming all the DR-HVDC link drawbacks. A comparison between the proposed hybrid HVDC system and the standard VSC-HVDC scheme in terms of relative losses, volume and cost is carried out. Moreover, using the proposed control system principles, a centralized control strategy for parallel operation of the DR-HVDC and VSC-HVDC links is presented. This allows a flexible power flow distribution between the two links needed for developing a

new DR-HVDC connected OWF in area beside an existing OWF connected to the VSC-HVDC link. Finally, Chapter 5 the main contributions of the Thesis work are concluded and the corresponding publications are addressed.

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Chapter 2

Harmonic compensation embedded in frequency control of OWFs with DR-HVDC connection

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2.1 Introduction

Besides the frequency control, compensation of the low-order harmonic voltages and currents generated by the DRU is another challenge of the DR-HVDC connected OWFs. These harmonic components increase the THD of the offshore voltage leading to power quality degradation. In order to compensate the corresponding harmonic

orders, typically huge passive AC-filters are needed to be placed at the DRU stations which increases the size of the offshore platforms. But, by using APFs as another functionality of a VSC connected to the DRU station for the centralized frequency control, the harmonic components can be compensated without the need for changing the WT-GSs conventional control systems. In this chapter, a new APF scheme based on the selective harmonic compensation (SHC) method is proposed which is embedded in a centralized frequency control to completely remove the required passive AC-filters from the DRU stations while reducing the THD of the offshore voltage. In addition, the central VSC power demand for the frequency control in such an application is studied. For this purpose, this chapter is organized as follows. The considered OWF scheme and its simplified model are presented in Section 2.2. In Section 2.3, the harmonic analysis of the system is carried out to justify the harmonic currents to be compensated in the APF scheme. The proposed control scheme needed for the VSC is presented in Section 2.4. The VSC power rating is analysed in Section 2.5. In Section 2.6, the simulation results are presented in both steady-state and transient conditions to verify the proposed control scheme. Finally, the conclusions are addressed in Section 2.7.

2.2 System description and modeling

Figure 2.1a shows the considered OWF consisting of a number of WTs which can be type-3 or type-4. The WTs are connected to the offshore AC-grid through the WT transformers (T_{wt}). The OWF transformers (T_{wf}), interconnecting the WT clusters, are used to step-up the mid-voltage distribution offshore AC-grid. A 12-pulse diode rectifier, representing the offshore HVDC link access, is connected to the offshore AC-grid at the PCC by means of the rectifier transformer (T_r). A required capacitor bank (C) for reactive power compensation of the diode rectifier and its transformer is placed at the rectifier station. A VSC is also placed at the rectifier station which is used for the centralized frequency control of the offshore AC-grid in opposition to the decentralized control [1]. Moreover, the VSC compensates harmonic currents to the system leading to no passive AC-filter banks being placed at the rectifier station. The onshore HVDC inverter can be LCC or VSC based technology, but it has to be operated in constant DC-voltage mode for successful operation of the system.

Note that the conventional WT converters' grid synchronization via PLL is feasible in the proposed system, provided that the system frequency is controlled by the VSC. Considering this assumption, Figure 2.1b shows a simplified model of the system where the WTs are represented by controlled current sources (I_k). Therefore, the WT active and reactive powers (P_k and Q_k) are provided by the desired current injection depending on the corresponding WT bus voltages (V_k). For this purpose, a static model of the offshore AC-grid is considered which uses a grid solution based on the dynamic phasor (DP) theory [2] (described in Subsection 2.3.1). Then, considering the

(a) The considered OWF connected to the grid through the DR-HVDC link.

(b) System simplified model.

Figure 2.1: System under study.

WT currents (I_k) and the capacitor bank voltage (U_c) as inputs, the WT bus voltages (V_k) as well as the OWF current injected to the PCC (I_{wf}) at the fundamental and different harmonic frequencies are defined. As it can be observed from Figure 2.1b, the VSC is modelled as a current-controlled voltage source (V_f) and the inductance of its equivalent line reactor (L_f) whose current is I_f . Furthermore, the inverter station is modelled as a controlled current source (I_{dci}) connected in parallel with a DC capacitor (C_{dci}) whose voltage is V_{di} . Then, the onshore inverter equivalent circuit is controlled in a way that regulates V_{di} , injecting the HVDC link power into the onshore grid.

The dynamics of the simplified model in Figure 2.1b in the synchronous (dq) reference frame rotating at ω_0 can be written in the following. Note that d and q subscripts refer to the corresponding vector components.

$$C \frac{dU_{d,c}}{dt} = I_{d,wf} + I_{d,f} - I_{d,r} + C\omega_0 U_{q,c} \quad (2.1)$$

$$C \frac{dU_{q,c}}{dt} = I_{q,wf} + I_{q,f} - I_{q,r} - C\omega_0 U_{d,c} \quad (2.2)$$

$$L_f \frac{dI_{d,f}}{dt} = V_{d,f} - U_{d,c} + L_f \omega_0 I_{q,f} \quad (2.3)$$

$$L_f \frac{dI_{q,f}}{dt} = V_{q,f} - U_{q,c} - L_f \omega_0 I_{d,f} \quad (2.4)$$

where $I_{d,r}$ and $I_{q,r}$ are the d- and q-components of the rectifier current, respectively. Due to the missing AC-voltage source, the conventional voltage oriented control via PLL, which is needed for the synchronization of V_f with the controller reference frame, is unfeasible. Therefore, given that no PLL is used, the synchronous axis is not oriented on the capacitor bank voltage vector ($U_{q,c} \neq 0$).

According to (2.2), $I_{q,f}$ can be used to determine the dynamic response of $U_{q,c}$. Then, $I_{q,f}$ is controlled in a way that orients the capacitor bank voltage vector along the d-axis ($U_{q,c} = 0$). As a result, given that the synchronous axis rotates at the desired frequency, the frequency control will be achieved. Furthermore, considering the dynamics of the VSC DC-capacitance, $I_{d,f}$ is used to regulate the VSC DC-voltage. Therefore, according to (2.1), the dynamic response of $U_{d,c}$ defining the offshore voltage magnitude depends on the OWF active power transmitted through the DR-HVDC link. Note that both $I_{d,f}$ and $I_{q,f}$ are controlled to follow their reference values through the conventional proportional-integral (PI) based current controller given by (2.3) and (2.4) (detailed in Subsection 2.4.1).

However, $I_{d,wf}$, $I_{q,wf}$, $I_{d,r}$ and $I_{q,r}$ contain harmonic components because of both the WT converters' operation and the diode rectifier functionality. These harmonic currents flow into the capacitor bank, distorting the offshore voltage. Therefore, desired harmonic currents can be injected through $I_{d,f}$ and $I_{q,f}$ to eliminate the harmonic components of the capacitor bank current. This leads to the THD of the offshore voltage being reduced (shown in Section 2.6).

2.3 System harmonic analysis

In this section, the analytical expressions represented in the dq reference frame are derived for both the offshore AC-grid current and the rectifier AC-current to justify the system harmonic current compensation embedded in the frequency control.

2.3.1 Offshore AC-grid current

The general expression of the WT currents in a balanced three-phase system can be written as follows:

$$I_k^{(abc)}(t) = \sum_{h=1}^H I_k^h \cos(h\omega_0 t + \varphi_k^h + \gamma^h) \quad (2.5)$$

where γ^h equals zero for phase a , $-h\frac{2\pi}{3}$ for phase b , and $h\frac{2\pi}{3}$ for phase c ; I_k^h is the amplitude value of the h^{th} -order harmonic current injected by the WTs, and φ_k^h is its phase angle; and H is the highest considered harmonic order.

The Park transformation shown in (2.6) defines the WT current vectors ($\underline{I}_k(t)$) in the dq reference frame rotating at the fundamental frequency ω_0 .

$$\underline{I}_k(t) = \frac{2}{3}(I_{a,k}(t) + \underline{a}I_{b,k}(t) + \underline{a}^2 I_{c,k}(t)) \cdot e^{-j\omega_0 t} \quad (2.6)$$

where $\underline{a} = e^{j2\pi/3}$ and $\underline{a}^2 = e^{-j2\pi/3}$.

By combining (2.5) and (2.6), $\underline{I}_k(t)$ can be analytically expressed as follows:

$$\underline{I}_k(t) = \underline{I}_k^1 + \sum_{h=2}^H \left[\underline{I}_k^h e^{j(h-1)\omega_0 t} \left(\frac{A}{3}\right) + \underline{I}_k^{h*} e^{-j(h+1)\omega_0 t} \left(\frac{B}{3}\right) \right] \quad (2.7)$$

where $A = 1 + e^{-j(h-1)2\pi/3} + e^{j(h-1)2\pi/3}$ and $B = 1 + e^{-j(h+1)2\pi/3} + e^{j(h+1)2\pi/3}$, $\underline{I}_k^1$ and $\underline{I}_k^h$ are the fundamental and h^{th} -order harmonic current vectors injected by the WTs, respectively, and $*$ denotes the complex conjugate operator.

Hence, the simplified form of (2.7) can be written as

$$\underline{I}_k(t) = \underline{I}_k^1 + \sum_{n=1}^{\infty} \left[\underline{I}_k^{(3n+1)} e^{j3n\omega_0 t} + \underline{I}_k^{(3n-1)*} e^{-j3n\omega_0 t} \right] \quad (2.8)$$

where $(n = 1, 2, 3, \dots)$.

According to (2.8), each WT current vector in the dq reference frame contains triple harmonics $(3n\omega_0)$. These triple harmonics refer to $(3n \pm 1)\omega_0$ harmonics in the stationary $(\alpha\beta)$ reference frame at the fundamental frequency. The harmonic orders $h = 3n + 1$ ($h = 4, 7, 10, \dots$) are the positive sequences, whereas the harmonic orders $h = 3n - 1$ ($h = 2, 5, 8, \dots$) are the negative sequences. In order to take these harmonic currents into account, $\underline{I}_k^{(3n+1)}$ and $\underline{I}_k^{(3n-1)*}$ need to be defined individually for each WT. For this purpose, the ratios of their magnitudes to the magnitude of $\underline{I}_k^1$ given in [3] are used to define the magnitude values of $\underline{I}_k^{(3n+1)}$ and $\underline{I}_k^{(3n-1)*}$. These ratios are listed in Table 2.1. Concerning the phase angle values of $\underline{I}_k^{(3n+1)}$ and $\underline{I}_k^{(3n-1)*}$, they are defined randomly. Note that, both the magnitude and phase angle values of $\underline{I}_k^1$ are determined individually for each WT by

$$\underline{I}_k^1 = \frac{2}{3} \frac{1}{V_k^1} \sqrt{P_k^2 + Q_k^2} \cdot e^{j(\theta_k^1 - \arctan(Q_k/P_k))} \quad (2.9)$$

Table 2.1: Harmonic magnitudes of WTs [3].

Harmonic Order	I_k^h / I_k^1	Harmonic Order	I_k^h / I_k^1
2	0.343 %	22	0.330 %
4	0.201 %	23	0.369 %
5	0.435 %	25	0.235 %
7	0.471 %	26	0.200 %
8	0.401 %	28	0.133 %
10	0.545 %	29	0.271 %
11	1.456 %	31	0.24 %
13	1.854 %	32	0 %
14	0.475 %	34	0 %
16	0.371 %	35	0.350 %
17	0.761 %	37	0.259 %
19	0.420 %	38	0 %
20	0.324 %	40	0 %

where V_k^1 is the magnitude of each WT fundamental bus voltage vector, and θ_k^1 is its phase angle.

Moreover, in order to define all harmonic components of each WT bus voltage vector and the OWF current vector injected to the PCC, a harmonic grid solution is used for the static model of the offshore AC-grid. This solution is carried out separately at each harmonic frequency as well as at the fundamental frequency using the corresponding harmonic components of each WT current vector and the capacitor bank voltage vector as inputs. Therefore, the required matrix expression for this grid solution is as follows:

$$[\underline{V}_1^h \dots \underline{V}_k^h \dots \underline{V}_{N-1}^h \underline{I}_{wf}^h]^\top = [M(h)] [\underline{I}_1^h \dots \underline{I}_k^h \dots \underline{I}_{N-1}^h \underline{U}_c^h]^\top \quad (2.10)$$

where h equals 1, 2, 4, 5, ... H ; N is the number of nodes of the offshore AC-grid; $\underline{V}_k^h$ is the h^{th} -order harmonic voltage vector at node k ; $\underline{I}_{wf}^h$ is the OWF h^{th} -order harmonic current vector; $\underline{I}_k^h$ is the h^{th} -order harmonic current vector injected at node k ; $\underline{U}_c^h$ is the capacitor bank h^{th} -order harmonic voltage vector; and $M(h)$ is defined as a combination of admittance and impedance matrices of the grid at the h^{th} -order harmonic frequency. Note that the impedance matrix of the grid at a frequency corresponding to the harmonic order (h) is $Z(h)$. But, The matrix $M(h)$ is slightly different as there is a node whose input is changed to voltage and whose output becomes current. Specifically, that node is the PCC bus which is placed at position N inside the matrix $Z(h)$. Then, the terms of $M(h)$ can be calculated as follows:.

$$M_{i,j} = Z_{i,j} - Z_{i,N} \frac{Z_{N,j}}{Z_{N,N}} \quad i \neq N, j \neq N \quad , \quad M_{i,j} = -\frac{Z_{N,j}}{Z_{N,N}} \quad i = N, j \neq N$$

$$M_{i,j} = \frac{Z_{i,N}}{Z_{N,N}} \quad i \neq N, j = N \quad , \quad M_{i,j} = \frac{1}{Z_{N,N}} \quad i = N, j = N$$

Finally, the WT bus voltage vectors ($\underline{V}_k(t)$) and OWF current vector ($\underline{I}_{wf}(t)$) are defined by the summation of all of their vector elements at different frequencies given by (2.10) as follows:

$$\underline{V}_k(t) = \underline{V}_k^1 + \sum_{h=2}^H \underline{V}_k^h(t) \quad , \quad \underline{I}_{wf}(t) = \underline{I}_{wf}^1 + \sum_{h=2}^H \underline{I}_{wf}^h(t) \quad (2.11)$$

where $h = 2, 4, 5, 7, \dots, H$, $\underline{V}_k^1$ and $\underline{V}_k^h(t)$ are the fundamental and h^{th} -order harmonic voltage vectors at each WT bus, respectively, and $\underline{I}_{wf}^1$ and $\underline{I}_{wf}^h(t)$ are the fundamental and h^{th} -order harmonic current vectors of the OWF, respectively.

However, $\underline{U}_c^h$ must be provided for the grid solution. For this purpose, a method based on the DP theory is proposed to extract all harmonic components of the capacitor bank voltage vector. The DP theory is based on Fourier series expansion. A time-domain waveform $X(\tau)$ is considered over the interval $\tau \in (t - T, t]$ to be periodic and is represented using the Fourier series as follows:

$$X(\tau) = \sum_{h=-\infty}^{\infty} \langle \mathbf{X} \rangle^h(t) \cdot e^{jh\omega_0\tau} \quad (2.12)$$

where

$$\langle \mathbf{X} \rangle^h(t) = \frac{1}{T} \int_{t-T}^t X(\tau) \cdot e^{-jh\omega_0\tau} d\tau \quad (2.13)$$

is its h^{th} complex Fourier coefficient and is referred to as a DP. The selected number of DPs defines the approximation accuracy of the waveform.

Moreover, the following conjugate property for real $X(\tau)$ proves to be useful:

$$\langle \mathbf{X} \rangle^{-h} = \langle \mathbf{X} \rangle^{h*} \quad (2.14)$$

Figure 2.2 shows the block diagram of the proposed technique to provide $\underline{U}_c^h$. As observed, first, the capacitor bank voltage vector ($\underline{U}_c$) is transformed into the $\alpha\beta$ reference frame to allow the extraction of $\underline{U}_c^h$ individually at all considered positive and negative sequence harmonic frequencies ($h = 3n \pm 1$ with $n = 1, 2, \dots$). Then, the proposed method is implemented similarly on both α - β capacitor bank voltages. For this purpose, the DP calculator based on (2.13) is separately used for both α and β channels to extract the h^{th} -order harmonic component from both α - β capacitor bank voltages. Therefore, the DP calculator output in each channel provides the phasor form of the extracted h^{th} -order harmonic component at its frequency (its reference frame) for the corresponding channel ($\langle U_{\alpha,c} \rangle^h$ and $\langle U_{\beta,c} \rangle^h$). Moreover, both $\langle U_{\alpha,c} \rangle^h$ and $\langle U_{\beta,c} \rangle^h$ need to be transformed from their reference frame into the original reference frame of their DP calculators inputs ($\alpha\beta$ reference frame in this case). According to (2.12),

Figure 2.2: Extraction of the capacitor bank's harmonic voltage vectors.

additional DP calculators at the considered negative frequencies ($-h$) are also needed in each channel for the required transformation. However, according to the conjugate property shown in (2.14), the required transformation can be simplified. This is carried out by multiplying both $\langle U_{\alpha,c} \rangle^h$ and $\langle U_{\beta,c} \rangle^h$ by $e^{jh\theta_0}$ in both channels, and a two-fold multiplication of the real components of their outputs are transformed into the dq reference frame to provide $\underline{U}_c^h$.

2.3.2 Rectifier AC-current

The rectifier phase currents ($I_{a,r}$, $I_{b,r}$, $I_{c,r}$) can be determined by modulation of the rectifier DC-current (I_{dcr}) using the three-phase 12-pulse current switching functions ($S_{a,12p}$, $S_{b,12p}$, $S_{c,12p}$) as follows:

$$[I_{a,r} \ I_{b,r} \ I_{c,r}]^T = I_{dcr} [S_{a,12p} \ S_{b,12p} \ S_{c,12p}]^T \quad (2.15)$$

Assuming balanced operating conditions and diode commutation process, switching functions for wye- and delta-connected windings of the 12-pulse diode rectifier AC-currents ($S_{6p_{Y/Y}}^{(abc)}(t)$ and $S_{6p_{Y/\Delta}}^{(abc)}(t)$) can be analytically expressed in (2.16) and (2.17), respectively.

$$S_{6p_{Y/Y}}^{(abc)}(t) = \frac{2\sqrt{3}}{\pi} \left\{ \sum_{n=0}^{\infty} k_{(6n+1)} \cos \left((6n+1) \left(\omega_0 t - \frac{\mu}{2} + \gamma \right) \right) - \sum_{n=1}^{\infty} k_{(6n-1)} \cos \left((6n-1) \left(\omega_0 t - \frac{\mu}{2} + \gamma \right) \right) \right\} \quad (2.16)$$

$$S_{6p_{Y/\Delta}}^{(abc)}(t) = \frac{2\sqrt{3}}{\pi} \left\{ \sum_{n=0}^{\infty} Dk_{(6n+1)} \cos \left((6n+1) \left(\omega_0 t - \frac{\mu}{2} + \gamma \right) \right) - \sum_{n=1}^{\infty} Dk_{(6n-1)} \cos \left((6n-1) \left(\omega_0 t - \frac{\mu}{2} + \gamma \right) \right) \right\} \quad (2.17)$$

where γ equals zero for phase a , $-\frac{2\pi}{3}$ for phase b , and $\frac{2\pi}{3}$ for phase c ; $k_{(6n\pm1)} = \frac{\sin((6n\pm1)\mu/2)}{(6n\pm1)^2\mu/2}$; μ is the commutation angle of the diodes; and $D = (-1)^n$.

By adding up both (2.16) and (2.17), the frequency-domain expression of $S_{12P}^{(abc)}(t)$ can be analytically obtained as follows:

$$S_{12P}^{(abc)}(t) = \frac{4\sqrt{3}}{\pi} \left\{ \sum_{n=0}^{\infty} k_{(12n+1)} \cos((12n+1)(\omega_0 t - \frac{\mu}{2} + \gamma)) - \sum_{n=1}^{\infty} k_{(12n-1)} \cos((12n-1)(\omega_0 t - \frac{\mu}{2} + \gamma)) \right\} \quad (2.18)$$

where $k_{(12n\pm1)} = \frac{\sin((12n\pm1)\mu/2)}{(12n\pm1)^2\mu/2}$.

According to the modulation theory, the rectifier DC-voltage (V_{dr}) is determined from the distorted offshore voltage in the frequency-domain. Therefore, the Fourier expression of V_{dr} contains the triple harmonic components $h = 3n$ ($n = 1, 2, \dots$) [4]. Since I_{dcr} is determined from V_{dr} , the general expression of $I_{dcr}(t)$ can be written as follows:

$$I_{dcr}(t) = I_{dcr}^1 + \sum_{n=1}^{\infty} I_{dcr}^{3n} \cos(3n\omega_0 t + \varphi_{dcr}^{3n}) \quad (2.19)$$

where I_{dcr}^1 is the DC component of $I_{dcr}(t)$, I_{dcr}^{3n} is the amplitude value of the $3n^{th}$ -order harmonic current of $I_{dcr}(t)$, and φ_{dcr}^{3n} is its phase angle. Note that, the values of these variables depend on the HVDC link model parameters.

Similarly, the rectifier current vector ($\underline{I}_r(t)$) in the dq reference frame can be derived as follows:

$$\underline{I}_r(t) = \frac{2}{3} (I_{a,r}(t) + \underline{a}I_{b,r}(t) + \underline{a}^2 I_{c,r}(t)) \cdot e^{-j\omega_0 t} \quad (2.20)$$

According to (2.18) and (2.20), $\underline{I}_r(t)$ can be generally expressed in the following symbolic form:

$$\underline{I}_r(t) = \underline{I}_r^1 + \sum_{n=1}^{\infty} \left[\underline{I}_r^{(3n+1)} e^{j3n\omega_0 t} + \underline{I}_r^{(3n-1)*} e^{-j3n\omega_0 t} \right] \quad (2.21)$$

where $\underline{I}_r^1$ is the fundamental current vector of the rectifier AC-current, $\underline{I}_r^{(3n+1)}$ and $\underline{I}_r^{(3n-1)}$ are the positive and negative sequence harmonic current vectors of the rectifier AC-current, respectively. As observed, the rectifier AC-current vector in the dq reference frame contains triple harmonics, referring to both corresponding positive and negative sequence harmonics in the $\alpha\beta$ reference frame.

Finally, according to (2.8) and (2.21), the system's harmonic currents flowing into

Figure 2.3: Overall control scheme of the VSC.

the capacitor bank contain triple-order harmonics in the dq reference frame. Therefore, the THD of the offshore voltage can be reduced by elimination of the triple harmonic components of both d-q capacitor bank currents detailed in the following section.

2.4 Control strategy

In this section, the required VSC control scheme for both frequency control and harmonic compensation is presented. It is also worth mentioning that, lowercase notation represents the per-unit values of the variables.

2.4.1 Overall control scheme

The overall scheme of the proposed control strategy is depicted in Figure 2.3. The voltage angular position θ_0 needed for the VSC control implementation is obtained by the integration of a desired fixed angular frequency ω_0 through a reference source. Due to the missing PLL, the reference angular position is entirely independent of measurement noise and grid disturbance. Therefore, the proposed control strategy can be implemented in the dq reference frame rotating at the fundamental frequency (50 Hz) provided by the reference source.

As observed, the VSC q -axis current reference $i_{q,f}^{ref}$ is set by the frequency control scheme where the q -component of the capacitor bank voltage ($u_{q,c}$) is controlled to be zero with a PI controller given the proposed frequency control principles in [5]. Note that, the d -component of the capacitor bank voltage $u_{d,c}$, which defines the magnitude of the offshore AC-grid voltage, remains uncontrolled since the HVDC-diode rectifier acts as a voltage clamp on $u_{d,c}$. On the other hand, the VSC d -axis current reference

Figure 2.4: The SHC diagram.

$i_{d,f}^{ref}$ is set by the DC-voltage regulator scheme where the VSC DC-voltage (v_{df}) is maintained at its reference value using a PI controller. For this purpose, the dynamics of the converter DC capacitance are considered in the VSC model as follows:

$$\frac{1}{\omega_0} \frac{dv_{df}^2}{dt} = \frac{-2p_f}{c_{dcf}} \quad (2.22)$$

where p_f is the VSC active power flowing into the PCC and c_{dcf} is the VSC equivalent DC capacitance.

The VSC d - q currents ($i_{d,f}$ and $i_{q,f}$) are controlled to follow their references with the conventional PI based current controller [6]. Note that x_f used in the cross coupling terms of the current controller is the reactance of the VSC equivalent line reactor. Then, the current controller outputs set the corresponding vector components of the VSC voltage needed for the frequency control ($v_{d,fc}$ and $v_{q,fc}$). Moreover, the required VSC d - q voltages for the harmonic compensation ($v_{d,hc}$ and $v_{q,hc}$) set by an APF scheme (described in the following subsection) are added to $v_{d,fc}$ and $v_{q,fc}$, respectively. This leads to the required VSC d - q voltages for both the frequency control and harmonic compensation ($v_{d,f}$ and $v_{q,f}$) being set. Finally, by transforming back $v_{d,f}$ and $v_{q,f}$ to the three-phase quantities, the required voltage for the modelled current-controlled voltage source in Figure 2.1b (v_f) is determined.

2.4.2 Harmonic compensation

Since the capacitor bank current contains well-known harmonic orders (previously described in Section 2.3), a new SHC method based on the DP theory is used in the APF scheme for the harmonic compensation. As shown in Figure 2.3, the SHC is implemented similarly for both capacitor bank d - q currents ($i_{d,c}$ and $i_{q,c}$) to provide the required VSC d - q voltages for the harmonic compensation ($v_{d,hc}$ and $v_{q,hc}$).

The diagram of the proposed SHC is depicted in Figure 2.4 where $i_{x,c}$ can be either $i_{d,c}$ or $i_{q,c}$. According to the analytical expressions expanded in Section 2.3, $i_{x,c}$ contains triple harmonics $3n\omega_0$ ($n = 1, 2, \dots$). Then, the proposed SHC is implemented

Figure 2.5: WT's reactive power reference obtainment.

individually for all triple harmonics of $i_{x,c}$. For this purpose, the DP calculators at triple harmonic frequencies extract the phasor form of all harmonics of $i_{x,c}$ at their frequencies ($\langle i_{x,c} \rangle^{3n}$). Therefore, both real and imaginary components of each extracted harmonic phasor are provided in DC-signals. Then, they are regulated separately to zero with PI controllers. This leads to the phasor form of the required voltages for compensation of the corresponding harmonics at their frequencies ($\langle v_{x,hc} \rangle^{3n}$) being provided. After that, $\langle v_{x,hc} \rangle^{3n}$ is transformed back from the corresponding harmonic reference frame into the original reference frame of the DP calculator's inputs (dq reference frame in this case), in accordance with (2.12) and (2.14). Finally, by summation of all the SHC outputs, the required voltage for compensation of all considered harmonic orders ($v_{x,hc}$) is determined which can be either $v_{d,hc}$ or $v_{q,hc}$.

2.5 VSC power rating analysis

According to the proposed control strategy, the VSC is employed as a STATCOM providing the required reactive power for the frequency control while the offshore voltage magnitude is imposed by the diode rectifier. However, the injected or absorbed reactive power by the VSC could be high depending on the different operation points, further increasing the required power demand for the VSC. This can be avoided through sending a reactive power command to the WTs which sets their reactive power references. Then, the required VSC reactive power for the frequency control can be provided by the WTs without the need for changing their conventional control systems. For this purpose, the required reactive power command is set by a control scheme depicted in Figure 2.5. As observed, the VSC reactive power (q_f) is regulated to zero with a PI controller. Then, in order to provide an equal reactive power sharing among the WTs, the output of the controller is divided by the number of WTs (k). This results in the obtainment of the required reactive power command (q_{com}) for the WTs. Finally, q_{com} is sent through a communication channel to the WTs, defining their reactive power references (q_k). Note that the communication channel is modelled as a time delay of 0.1 s in this study case to postpone the signal reception. Moreover, the VSC reactive power is calculated as $q_f = u_{q,c} i_{d,f} - u_{d,c} i_{q,f}$.

However, the required reactive power capability of the VSC depends on the transients in the system and the control and system parameters, e.g., the OWF active and

Figure 2.6: VSC power rating analysis.

reactive power changes and the communication time delay. For this purpose, a simplification of the considered OWF is proposed, as shown in Figure 2.6a, which is used to analyse and select the power rating of the VSC. In this simplified model, one aggregated WT is considered, which is represented by its active and reactive powers (p_{wt} and q_{wt} , respectively) injected into the offshore AC-grid. The offshore AC-grid is represented by a lumped π -model (x_l and b_{cl}) including the susceptance of the capacitor bank C. Also, the rectifier transformer is represented by its short-circuit reactance (x_t) and the active and reactive powers demanded by the diode rectifier are depicted by p_r and q_r , respectively. Finally, the VSC reactive power (q_f) defining the VSC power rating is also injected to the offshore AC-grid for frequency control. Note that, to select the power rating of the VSC, only the effect of reactive power compensation needed for the frequency control is taken into account, whereas the harmonic compensation affecting the power rating of the VSC is negligible [7].

Given that the frequency control is achieved by satisfying the reactive power balance at the rectifier station [5], the equation defining the value of q_f can be written as follows:

$$q_f = -q_{wt} + q_{x_l} + q_{x_t} - q_{b_{cl}} + q_r \quad (2.23)$$

where q_{x_l} , q_{x_t} and $q_{b_{cl}}$ are the reactance and susceptance reactive powers, respectively. Apart from this simplified form of the OWF, the analysis of the VSC power rating to calculate the value of q_f is carried out under the following assumptions: power losses are neglected in the system ($p_{wt} = p_r$); the offshore AC-grid voltage magnitude remains at one per-unit, independently of the value of p_{wt} ($q_{b_{cl}} = b_{cl}$); q_{x_l} and q_{x_t} depend on the square of p_{wt} ($q_{x_l} = x_l p_{wt}^2$ and $q_{x_t} = x_t p_{wt}^2$, respectively); and there is a linear relationship between p_r and q_r with the rate of change k_r ($q_r = k_r p_r$).

After considering the aforementioned assumptions as well as defining the rate of change k_{wt} for p_{wt} ($p_{wt}(t) = k_{wt}t$), (2.23) can be rewritten as follows:

$$q_f(t) = -q_{wt}(t) + (x_l + x_t)k_{wt}^2t^2 - b_{cl} + k_r k_{wt}t. \quad (2.24)$$

According to (2.24), once the expression of $q_{wt}(t)$ is defined, the value of $q_f(t)$ can be calculated. For this purpose, Figure 2.6b shows the block diagram of the WT reactive power reference obtainment based on sending the reactive power command to the WTs. This is used to obtain the analytical expression of the $q_{wt}(t)$, further calculating the value of $q_f(t)$. In this block diagram, k_p and k_i are the control parameters of the proportional and integral parts of the corresponding PI controller, respectively. Moreover, the communication time delay for the reactive power command q_{com} is modelled by a first-order low-pass filter with a time constant τ .

For this purpose, the equations of the control scheme depicted in Figure 2.6b in the time-domain are written as follows:

$$\frac{dq_{com}(t)}{dt} = k_p \frac{dq_f(t)}{dt} + k_i q_f(t) \quad (2.25)$$

$$\frac{dq_{wt}(t)}{dt} = \frac{1}{\tau} (q_{com}(t) - q_{wt}(t)) \quad (2.26)$$

By combining (2.25) and (2.26), and substituting the expression of $q_f(t)$ using (2.24), the $q_{wt}(t)$ can be analytically expressed as follows:

$$\tau \frac{d^2 q_{wt}(t)}{dt^2} + (1 + k_p) \frac{dq_{wt}(t)}{dt} + k_i q_{wt}(t) = K_1 t^2 + K_2 t + K_3 \quad (2.27)$$

where $K_1 = k_i(x_l + x_t)k_{wt}^2$, $K_2 = 2k_p(x_l + x_t)k_{wt}^2 + k_i k_r k_{wt}$ and $K_3 = k_p k_r k_{wt} - b_{cl} k_i$.

The aforementioned equation is a second-order linear inhomogeneous equation, whose general solution ($q_{wt}(t)$) consists of complementary function solution ($q_{wt}^{cf}(t)$) plus particular integral solution ($q_{wt}^p(t)$).

First of all, $q_{wt}^{cf}(t)$ has to be defined while the right-hand side of (2.27) is made zero, as written in (2.28):

$$\tau \frac{d^2 q_{wt}(t)}{dt^2} + (1 + k_p) \frac{dq_{wt}(t)}{dt} + k_i q_{wt}(t) = 0. \quad (2.28)$$

For this purpose, a trial solution with an unknown coefficient c ($q_{wt}(t) = e^{ct}$) is considered for Equation (2.28). By solving c for different conditions, the following results for $q_{wt}^{cf}(t)$ are obtained:

1. If $(K_4^2 - K_5) > 0$:

$$q_{wt}^{cf}(t) = Ae^{\frac{-K_4 + \sqrt{K_4^2 - K_5}}{2\tau}t} + Be^{\frac{-K_4 - \sqrt{K_4^2 - K_5}}{2\tau}t}; \quad (2.29)$$

2. If $(K_4^2 - K_5) < 0$:

$$q_{wt}^{cf}(t) = e^{\frac{-K_4}{2\tau}t} \left(A \cos \frac{\sqrt{K_5 - K_4^2}}{2\tau}t + B \sin \frac{\sqrt{K_5 - K_4^2}}{2\tau}t \right); \quad (2.30)$$

3. If $(K_4^2 - K_5) = 0$:

$$q_{wt}^{cf}(t) = (A + Bt)e^{\frac{-K_4}{2\tau}t} \quad (2.31)$$

where $K_4 = 1 + k_p$, $K_5 = 4\tau k_i$, and A and B are arbitrary constants whose values are calculated in a way that satisfies the initial conditions of $q_{wt}(0) = -b_{cl}$ and $\frac{dq_{wt}(0)}{dt} = 0$ (because of the considered time delay). This leads to the following results for the values of A and B .

1. If $(K_4^2 - K_5) > 0$:

$$A = \frac{\sqrt{K_4^2 - K_5}(-2\tau K_7 - ((b_{cl} + K_8)(K_4 + \sqrt{K_4^2 - K_5})))}{2(K_4^2 - K_5)} \\ B = -b_{cl} - K_8 - A; \quad (2.32)$$

2. If $(K_4^2 - K_5) < 0$:

$$A = -b_{cl} - K_8, \quad B = \frac{\sqrt{K_5 - K_4^2}(-2\tau K_7 - b_{cl}K_4 - K_8K_4)}{K_5 - K_4^2}; \quad (2.33)$$

3. If $(K_4^2 - K_5) = 0$:

$$A = -b_{cl} - K_8, \quad B = \frac{-2\tau K_7 - b_{cl}K_4 - K_8K_4}{2\tau}. \quad (2.34)$$

Then, $q_{wt}^p(t)$ has to be defined, which has the same form as the right-hand side of (2.27). Therefore, the expression of $q_{wt}^p(t)$ with the unknown coefficients K_6 , K_7 , and K_8 can be written in (2.35):

$$q_{wt}^p(t) = K_6t^2 + K_7t + K_8. \quad (2.35)$$

By substituting (2.35) in (2.27) for $q_{wt}(t)$, the aforementioned unknown coefficients are defined as follows:

Figure 2.7: WT active power and dynamic responses of the WT and the VSC reactive powers in the VSC power rating analysis.

$$\begin{aligned}
 K_6 &= \frac{K_1}{k_i} \quad , \quad K_7 = \frac{k_i K_2 - 2K_4 K_1}{k_i^2} \\
 K_8 &= \frac{K_3 k_i^2 - 2\tau K_1 k_i - K_4 K_2 k_i + 2K_4^2 K_1}{k_i^3} .
 \end{aligned} \tag{2.36}$$

Finally, as stated before, the general solution of (2.27) is defined by summation of $q_{wt}^{cf}(t)$ and $q_{wt}^p(t)$, as written in (2.37):

$$q_{wt}(t) = q_{wt}^{cf}(t) + q_{wt}^p(t). \tag{2.37}$$

In this way, the required reactive power capability of the VSC for the system under study can be determined using the obtained VSC power rating analysis. For this purpose, Figure 2.7 shows the dynamic response of the developed analytical expressions of $q_{wt}(t)$ and $q_f(t)$ under the transient of an scheduled p_{wt} change. p_{wt} changes from 0 to 1 pu with a rate of change of $k_{wt} = 0.5$ pu/s which is used to simulate the whole system under study presented in Section 2.6. Moreover, the required input parameters values for this dynamic response are defined in Table 2.2 which fit the parameters of the detailed system simulated in Section 2.6.

As observed, during the scheduled active power change, q_{wt} varies from its initial value ($q_{wt}(0) = -b_{cl}$) to a value of 0.23 pu, thanks to q_{com} being sent to the WT. As a result, q_f reaches its maximum value ($q_f^{max} = 0.076$ pu) at the end of this transient in order to satisfy the reactive power balancing at the diode rectifier station needed for the frequency control. It should be pointed out that the obtained VSC power rating value is verified by the simulation of the whole system under the study presented in the following section.

2.6 Simulation results

The proposed control strategy has been validated using MATLAB/Simulink. Figure 2.1b shows the system under consideration. Note that only the resistances of the

Table 2.2: System and control parameters

Description	Value
$S_{base,ac}$	450 MVA
$V_{base,ac}$	220 kV
$P_{base,dc}$	450 MW
$V_{base,dc}$	594.209 kV
T_r	$x = 0.12$ pu $x/r = 80$ (66 kV/220 kV)
T_{wf}	$x = 0.1$ pu $x/r = 80$ (33 kV/66 kV)
T_{wt}	$x = 0.07$ pu $x/r = 80$ (0.69 kV/33 kV)
b_c (capacitor bank C)	0.05 pu
AC-cable1 (π -model)	$R = 68.4$ m Ω $L = 0.234$ mH $C = 3.96$ μ F
AC-cable2 (π -model)	$R = 67.2$ m Ω $L = 0.606$ mH $C = 6.888$ μ F
AC-cable3 (π -model)	$R = 18.2$ m Ω $L = 0.34$ mH $C = 21.11$ μ F
DC-cable model	according to [8] and 200 km
onshore grid equivalent	$x = 0.05$ pu $x/r = 80$ $v = 1$ pu
VSC equivalent dc-capacitance	3.33 μ F
communication time delay	0.1 s
x_f	0.15 pu
C_{dcf}	0.2 μ F
f_0	50 Hz
k_p	1
k_i	5
k_r	0.23
x_l	0.192 pu
x_t	0.12 pu
b_{cl}	0.237 pu
τ	0.1 s

AC- and DC-line impedances are taken into account as power losses of the system, whereas the power losses of the converters and the WTGSs' services are neglected. Moreover, an accurate DC-cable model for the DC transmission line given in [8] is used in the simulation including the DC smoothing reactor. The DC-voltage reference of the onshore inverter station is set to 0.964 pu. The OWF uses a total of six WT aggregated equivalents of the same rated power ($S_k = 75$ MVA), and its parameters are detailed in Table 2.2. It should be pointed out that the reactive power limits of the WTs are set to ± 0.25 pu. The capacitor bank (C) is designed in order to keep the steady-state reactive power value of the VSC equal to zero without exceeding the WTs' reactive power limits at any operation point. Finally, a 0.05 pu capacitor bank is placed at the rectifier station which is smaller than the equivalent capacitance placed in the HVDC benchmark model [9] using a diode rectifier instead of a thyristor rectifier [10] and [11]. The following simulation results show the performance of the proposed control strategy during both steady-state and transient conditions.

2.6.1 Steady-state APF performance

The steady-state performance of the proposed APF scheme for the harmonic compensation has been tested with the OWF operating at the rated power where the considered harmonic currents have their highest values. Figure 2.8a shows the steady-state response of phase a of the capacitor bank voltage ($u_{a,c}$) waveform to the proposed harmonic compensation scheme at the rated power. Initially, the VSC operates only in centralised frequency control mode and harmonic compensation is disabled. During this period, the steady-state waveform of $u_{a,c}$ is distorted and its THD is 6.15 %. At $t = 0.04$ s, the proposed APF is enabled. After a transient, the system returns to the steady-state where $u_{a,c}$ has a THD equal to 0.23 %. The APF steady-state response for the waveform of phase a of the VSC current ($i_{a,f}$) is also represented in Figure 2.8a. The good transient response to such control variation proves the good stability and capability of the proposed harmonic compensation scheme. It is worth mentioning that the time needed for the APF to achieve the steady-state varies at different operation points, since the harmonic currents values are different at the corresponding operation points.

Moreover, the effect of the proposed harmonic compensation scheme on WTs' active and reactive powers as well as the DC-system voltages at the rated power is depicted in Figure 2.8b. As can be observed, the APF implementation significantly reduces the large ripples of the WTs' active and reactive powers (p_k and q_k) created by the harmonic currents of the system. This leads to a power quality improvement in the OWF system which validates the proposed method of APF implementation in such applications. Besides, the rectifier DC-voltage (v_{dr}) shows a noticeable harmonic distortion before enabling the APF. These DC harmonic voltages are removed by the proposed control, and as a result, an almost pure 12-pulse per period signal remains for v_{dr} during APF operation. Therefore, enabling the APF also causes a good impact on the HVDC link voltage ripple which is reduced compared to in the APF disabled mode, as can be observed from the onshore inverter DC-voltage (v_{di}) represented in Figure 2.8b.

2.6.2 Transient control performance

Figure 2.9 shows the transient performance of the proposed control strategy during scheduled OWF active power changes. Note that, from the beginning, the proposed APF scheme is enabled. Initially, given that the system is in steady-state, the VSC reactive power (q_f) is zero, and the required steady-state reactive power for the frequency control is provided by the WTs' reactive powers (q_k) thanks to the reactive power command being sent to the WTs. From $t = 1$ s to $t = 3$ s, the WTs' active powers (p_k) decrease from 100% to 1% of their rated powers. After a transient, q_k reaches a different value as it provides the required steady-state reactive power, while q_f returns to zero. Then, from $t = 6$ s to $t = 8$ s, p_k increases from 1 % to 100 % of its rated value. After this transient, both q_k and q_f return to their initial values. Finally, at

Figure 2.8: Steady-state APF performance at the rated power.

$t = 11$ s, an active power reduction is scheduled. Specifically, the individual WTs' active powers decrease and reach different values, providing, in total, 50 % of the OWF active power. As shown, at steady-state, the same conclusions as before are obtained for both q_k and q_f because the reactive power command is sent to the WTs. As can be observed, although the reactive power capability of the WTs is used to provide the required steady-state reactive power, q_f varies within approximately ± 8 % of the OWF

(a) WTs' active and reactive powers, VSC reactive power, offshore AC-grid frequency and THD of phase a of the capacitor bank voltage

(b) OWF active and reactive powers, capacitor bank voltage magnitude, and the average values of the rectifier and onshore inverter DC-voltages

Figure 2.9: Transient control performance during the OWF active power changes.

rated power during the applied transients. As stated before, this variation depends on the OWF active power changes, the communication time delay, and the control and system parameters. This result validates the VSC power rating analysis presented in Section 2.5. Moreover, the offshore AC-grid frequency (f) is controlled during this scenario with negligible disturbances during the scheduled OWF active power changes. Finally, the THD of phase a of the capacitor bank voltage ($u_{a,c}$) is kept below 1 % by the

proposed harmonic compensation scheme during this scenario.

The aforementioned p_k and q_k changes lead to the variation in the active and reactive powers flowing from the OWF to the PCC bus (p_{wf} and q_{wf}) which are the result of the offshore AC-grid power flow. According to the corresponding changes in p_{wf} and q_{wf} , the offshore AC-grid voltage magnitude ($u_{d,c}$) varies within an acceptable limit of less than ± 4 % of its rated value. As the diode rectifier acts as a voltage clamp on the offshore AC-grid voltage, the average value of the rectifier DC-voltage (v_{dr}^{avg}) has the same trend as $u_{d,c}$ with variations of less than ± 2 % of its rated value. However, the average value of the onshore inverter DC-voltage (v_{di}^{avg}) is regulated at its reference value with negligible variation during the applied transients.

2.7 Conclusions

This chapter has presented a new APF scheme embedded in a frequency control of an OWF connected to the DR-HVDC link. The APF has been accomplished by a VSC connected to the rectifier station, that is always needed for the centralized frequency control of the isolated offshore AC-grid in such an application. The proposed APF scheme uses a PI-based SHC technique to eliminate harmonic currents of a capacitor bank placed at the rectifier station. This has resulted in a significant reduction in the THD of the offshore voltage while removing the required passive AC-filters from the rectifier station. Moreover, by using a static model of the offshore AC-grid based on the DP theory, the WTs' harmonic current injections have been considered in the required system harmonic analysis for the APF. The simulation results have demonstrated that the reactive power capability of the WTs can be used to provide the required steady-state reactive power for the frequency control. This is performed by sending the reactive power command to the WTs' reactive power control channels while in the meantime no changing in their conventional control systems. Finally, it has been demonstrated that the required VSC rated power is reduced. The simulation results have verified the performance of the proposed control system during both steady-state and transient conditions.

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Chapter 3

Proposed frequency control of OWFs with DR-HVDC connection

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3.1 Introduction

The principles of the centralized frequency control used in the previous chapter are based on the dynamics of a capacitor bank placed at the DRU station. Then, besides a VSC, a high-voltage large capacitor bank is needed to be placed at the DRU station. This demands larger offshore platforms for the DRU stations, further increasing the OWF installation cost. Therefore, in this chapter, a centralized control strategy for OWFs connected to the DR-HVDC link is proposed where the frequency control is guaranteed through reactive power balance at the DRU station bus without the need for placing a capacitor bank at the DRU station. Furthermore, using the same proposed frequency control, a comparison between two variants of STATCOM topologies

for DR-HVDC connected OWFs is presented. The considered STATCOM topologies for this study case are: The two-level (2L) STATCOM and the delta-connected cascaded H-bridge (CHB) STATCOM. Then, these converter topologies are compared during three functions: the OWF frequency control, APF and negative-sequence voltage compensation. The comparison includes the system operation and control during both normal and fault conditions. The dynamic behavior, FRT capability, power losses and relative cost of both approaches are evaluated through simulation model of the system during a specific study case. For this purpose, this chapter is organized as follows. In Section 3.2, the considered OWF scheme is presented. The required VSC control scheme for the proposed centralized control strategy is presented in Section 3.3. A new AVM of the DRU system is derived in Section 3.4 to test the proposed frequency control principles while the stability of the system with embedded frequency control is evaluated through a small-signal analysis in Section 3.5. In Section 3.6, the proposed control system performance and the accuracy of the proposed AVM are verified through a DSM simulation of the system. The corresponding comparison of those STATCOM topologies for such an application is presented in Section 3.7. Finally, the conclusions are addressed in Section 3.8.

3.2 System description

Figure 3.1 shows an OWF connected to the onshore grid through the DR-based HVDC link. The DR station consists of a twelve-pulse diode bridge connected to the PCC through a transformer T_r . A VSC is connected to the DR station bus, that is always needed for the centralized frequency control of the isolated offshore AC-grid in opposition to the decentralized control [1]. But, the high voltage large capacitor bank C , which was originally needed for the objective of derivation of the frequency control principles, is removed from the DR station, given the proposed control system (described in Subsection 3.3.2). In addition, the VSC is used to compensate harmonic currents generated by the DR, thereby removing passive AC-filters from the DR station as well. The VSC current injected to the DR station bus is I_f , while I_g is the offshore AC-grid current flowing into the PCC, the PCC voltage being V . Note that the onshore inverter can be LCC or VSC, but it has to be operated in constant DC-voltage mode for successful operation of the system.

3.3 Control strategy

In this section, the required VSC control scheme implemented in the synchronous dq reference frame is presented. Note that d and q subscripts refer to the corresponding vector components. It is also worth mentioning that, lowercase notation represents the per-unit values of the variables and parameters.

Figure 3.1: Proposed system: Using a VSC for connection of an OWF through the DR-based HVDC link without a capacitor bank placed at the DR station.

3.3.1 Overall control scheme

Figure 3.2 shows the overall control scheme in which the required VSC reference voltage (v_f^{ref}) for both the frequency control and the harmonic compensation is generated. As observed, d- and q-components of the VSC reference current ($i_{d,f}^{ref}$ and $i_{q,f}^{ref}$) are given by the DC-link voltage control and the proposed frequency control (described in Subsection 3.3.2), respectively. Using the conventional PI based current controller, d- and q-components of the VSC current ($i_{d,f}$ and $i_{q,f}$) are controlled to follow their reference values. This leads to the corresponding vector components of the VSC reference voltage ($v_{d,f}^{ref}$ and $v_{q,f}^{ref}$) being determined. Note that x_f used in the cross-coupling terms of the controller is the reactance of the VSC line reactor. However, the reference voltages $v_{d,f}^{ref}$ and $v_{q,f}^{ref}$ are needed only for the frequency control and successful operation of the VSC.

Therefore, an APF scheme embedded in the frequency control is also needed for the harmonic current compensation, as shown in Figure 3.2. For this purpose, the PI based SHC technique proposed in Chapter 2 is used in the APF scheme. The SHC is separately carried out on both d- and q-components of the offshore AC-grid current ($i_{d,g}$ and $i_{q,g}$) to eliminate their harmonic orders $h = 12k$ ($k = 1, 2, \dots$), thereby removing the corresponding harmonic orders from the PCC phase voltages (shown in Subsection 3.6.2). This leads to d- and q-components of the APF reference voltage being obtained which are added to $v_{d,f}^{ref}$ and $v_{q,f}^{ref}$, respectively. Then, their outputs are transformed back to three-phase quantities to generate v_f^{ref} . Finally, by passing v_f^{ref} through the modulator, the required gate signals of the VSC are provided.

Note that the DC-link voltage control scheme and the type of modulation depend on the VSC topology used, whereas the frequency control scheme can be used with any VSC topology. Moreover, the conventional voltage oriented control using PLL, that is needed for the synchronization of the VSC output voltage with the controller reference

Figure 3.2: Overall control scheme of the VSC.

frame, becomes unfeasible due to the missing AC-voltage source [2]. Therefore, the reference voltage angular position (θ_0) used in the overall control scheme is obtained internally by integrating a desired angular frequency (ω_0), as shown in Figure 3.2. As a result, given that no PLL is used, θ_0 is not subject to any grid disturbance.

In the following subsection, the obtainment of the reference $i_{q,f}^{ref}$ needed for the frequency control is described, which is within the scope of this work.

3.3.2 Frequency control

For objective of the derivation of the frequency control principles, Figure 3.3 shows a simplified model of the proposed system depicted in Figure 3.1. The OWF is represented by the offshore AC-grid active and reactive powers injected to the PCC (P_g and Q_g), which are independent of the WT technology (type-3, type-4, etc.). In addition, a controlled reactive power (Q_{ct}) given by the VSC is injected in the DR station bus. The rectifier transformer is represented by its short-circuit reactance (X_t) whose current is I_r . This provides the rectifier AC-side voltage (V_r) determining active and reactive powers drawn by the DR (P_r and Q_r).

The dynamics of the simplified model in Figure 3.3 represented in the dq reference frame rotating at ω_0 can be written as follows:

$$\underline{v} - \underline{v}_r = \frac{x_t}{\omega_0} \frac{d\underline{i}_r}{dt} + j x_t \underline{i}_r \quad (3.1)$$

where $\underline{i}_r$ is the rectifier current vector, and $\underline{v}$ and $\underline{v}_r$ are the PCC and rectifier voltage vectors, respectively. Note that since no PLL is used, the synchronous axis is not oriented on $\underline{v}$ ($v_q \neq 0$), as shown in Figure 3.4.

Figure 3.3: Simplified model of the system.

Figure 3.4: Vector diagram in both the stationary and synchronous reference frames.

Multiplying both left and right-hand sides of (3.1) by the conjugate of $\underline{i}_r$, (3.2) is given.

$$\underline{v} \underline{i}_r^* - \underline{v}_r \underline{i}_r^* = \frac{x_t}{\omega_0} \frac{d\underline{i}_r}{dt} \underline{i}_r^* + j x_t i_r^2 \quad (3.2)$$

where i_r is the magnitude of $\underline{i}_r$ and $*$ denotes the complex conjugate operator.

Then, the term $\frac{d\underline{i}_r}{dt} \underline{i}_r^*$ in (3.2) can be analytically expressed in its dq components as

$$\frac{d\underline{i}_r}{dt} \underline{i}_r^* = \frac{di_{d,r}}{dt} (i_{d,r} - j i_{q,r}) + \frac{di_{q,r}}{dt} (i_{q,r} + j i_{d,r}) \quad (3.3)$$

Using this expression, (3.2) can be split in its real and imaginary terms in the following set of equations:

$$p_g - p_r = \frac{x_t}{\omega_0} (i_{d,r} \frac{di_{d,r}}{dt} + i_{q,r} \frac{di_{q,r}}{dt}) \quad (3.4)$$

$$q_g + q_{ct} - q_r - q_t = \frac{x_t}{\omega_0} (-i_{q,r} \frac{di_{d,r}}{dt} + i_{d,r} \frac{di_{q,r}}{dt}) \quad (3.5)$$

where $q_t = x_t i_r^2$ is the reactive power of the rectifier transformer.

The right-hand sides of (3.4) and (3.5) are modified using the transformation from Cartesian to polar coordinates system. This leads to the dynamic equations whose state

variables are magnitude and angle of $\underline{i}_r$. For this purpose, a set of equations defining the polar derivatives as a function of the Cartesian derivatives is presented in (3.6).

$$\begin{aligned} i_r \frac{di_r}{dt} &= i_{d,r} \frac{di_{d,r}}{dt} + i_{q,r} \frac{di_{q,r}}{dt} \\ i_r^2 \frac{d\delta_i}{dt} &= -i_{q,r} \frac{di_{d,r}}{dt} + i_{d,r} \frac{di_{q,r}}{dt} \end{aligned} \quad (3.6)$$

where $\delta_i = \delta_v - \varphi$ stands for the angle of $\underline{i}_r$, while δ_v stands for the angle of $\underline{v}$ and φ is the angle between $\underline{v}$ and $\underline{i}_r$, as shown in Figure 3.4.

Using the expressions in (3.6), the dynamic equations (3.4) and (3.5) can be rewritten as follows:

$$\frac{1}{\omega_0} \frac{di_r}{dt} = \frac{i_r}{q_t} (p_g - p_r) \quad (3.7)$$

$$\frac{1}{\omega_0} \frac{d\delta_i}{dt} = \frac{q_g + q_{ct} - q_r}{q_t} - 1 \quad (3.8)$$

According to (3.8), the reactive power balance at the PCC determines the dynamic of δ_i , further driving the dynamic response of δ_v . Then, the frequency control can be guaranteed by maintaining δ_v equal to zero in order to align $\underline{v}$ along the d-axis ($v_q = 0$) as the synchronous axis rotates at the desired frequency (e.g. 50 Hz). This is used to derive the frequency control principles based on the control of the q-component of the PCC voltage v_q in the dq reference frame which is generated internally. Furthermore, given that the system frequency can be expressed as $\omega = \omega_0 + \frac{d\delta_v}{dt}$, (3.8) can be transformed into a certain expression of frequency depending on the reactive power balance at the PCC as follows:

$$\frac{\omega}{\omega_0} q_t = q_g + q_{ct} - q_r \quad (3.9)$$

However, in the reactive power balance stated in (3.9), both q_r and q_t are the reactive powers drawn by the DR station depending on the active power transmitted which can not be controlled. Moreover, the incoming OWF reactive power q_g depends on the power factor settings at the WTGSs and the length of the AC-cables. Therefore, the additional q_{ct} generated by the VSC can be used to achieve the reactive power balance and as a result, to guarantee the frequency control. Note that the maximum instantaneous q_{ct} depends of the system and control parameters and the transients in the system. Finally, according to the aforementioned explanations, the proposed frequency control scheme is derived where v_q is controlled to be zero ($\delta_v = 0$) using a PI controller resulting in the q-component of the VSC reference current $i_{q,f}^{ref}$ being set, as shown in Figure 3.5.

Figure 3.5: Proposed frequency control scheme.

Furthermore, according to (3.7), the active power balance in the DR station bus determines the dynamic of i_r . Given that i_r is proportional to the DR DC-current in the per-unit system [3], the dynamic of the DR DC-voltage is also determined by the active power balance based on the HVDC link model. As a result, due to the relationship between AC- and DC-voltages of the DR [4] and [5], the active power balance in the DR station bus drives the dynamic response of the PCC voltage magnitude. Therefore, as can be seen in Figure 3.5, the d-component of the PCC voltage v_d defining the PCC voltage magnitude remains uncontrolled and it automatically accommodates the voltage required for transmitting the power through the DR (shown in Section 3.6). This way the system frequency is controlled by regulation of v_q while the PCC voltage magnitude v_d is clamped by the DR operation, provided that the HVDC link voltage is imposed by the onshore inverter. This makes the difference between the proposed control system and the conventional VSC-based grid forming using inner current control in which VSC behaves as a slack bus through regulation of the AC-voltage magnitude [6]. Moreover, the operation and control of the VSC used in the proposed system are different from the conventional STATCOM in which reactive power is generated through regulation of the AC-voltage magnitude.

3.4 DRU station modeling

In this section, the AVM of the DR station and the HVDC link is derived which is used to test the proposed frequency control principles. For this purpose, the HVDC link is represented by a lumped T-model given in [7]. In addition, given the onshore inverter operation in constant DC-voltage mode, the inverter station is modelled as a DC-voltage source.

According to the aforementioned assumptions, Figure 3.6a shows the considered DR-based HVDC system to be modelled with its corresponding input and output signals. As observed, the DC-cable is represented by its parallel capacitor c_c whose voltage is v_c and two series resistor-inductor branches depicted by r_{dc1} , l_{dc1} and r_{dc2} , l_{dc2} . This provides two different DC-currents: The rectifier DC-current i_{dc1} and the inverter DC-current i_{dc2} , respectively. Moreover, the DC-voltage input at the inverter station is v_{di} , while the rectifier DC-voltage is v_{dr} . Finally, v is the magnitude of the PCC voltage vector. Note that, in this study case, the rectifier losses and system harmonics are neglected, and the rectifier station consists of a twelve-pulse diode bridge.

Figure 3.6: Overall scheme of the proposed AVM.

Considering the model in Figure 3.6a, the dynamic equations regarding the DC-side of the system are given in (3.10)-(3.12).

$$v_{dr} - v_c = r_{dc1} i_{dc1} + \frac{l_{dc1}}{\omega_0} \frac{di_{dc1}}{dt} \quad (3.10)$$

$$i_{dc1} - i_{dc2} = \frac{c_c}{\omega_0} \frac{dv_c}{dt} \quad (3.11)$$

$$v_c - v_{di} = r_{dc2} i_{dc2} + \frac{l_{dc2}}{\omega_0} \frac{di_{dc2}}{dt} \quad (3.12)$$

Then, using the expression in (3.10), the rectifier power balance is assumed given by:

$$p_r = p_{dr} = r_{dc1} i_{dc1}^2 + \frac{l_{dc1}}{\omega_0} i_{dc1} \frac{di_{dc1}}{dt} + v_c i_{dc1} \quad (3.13)$$

where $p_{dr} = i_{dc1} v_{dr}$ is the rectifier DC-power.

In a per-unit system, the rectifier AC-current magnitude i_r is equal to $k_\mu i_{dc1}$ ($i_r = k_\mu i_{dc1}$) [3] where k_μ depends on the commutation angle μ detailed as follows:

$$k_\mu = \frac{1}{2} (1 + \cos \mu) \sqrt{1 + (\mu \csc^2 \mu - \cot \mu)^2} \quad (3.14)$$

$$r_\mu i_{dc1} = \frac{v}{2} (1 - \cos \mu) \quad (3.15)$$

$$v_{dr} = v - r_\mu i_{dc1} = k_\mu v \cos \varphi \quad (3.16)$$

where $r_\mu = \frac{\pi}{6} \frac{x_t}{n_b}$ is the commutation resistance and n_b is the number of six-pulse diode bridges.

By substituting the aforementioned expressions of p_r and i_r in (3.7), the final dynamic equations of the proposed model (3.17)-(3.20) are derived.

$$f_1 = \frac{1}{\omega_0} \frac{d\delta_i}{dt} = \frac{q_g + q_{ct} - q_r}{q_t} - 1 \quad (3.17)$$

$$f_2 = \frac{1}{\omega_0} \frac{di_{dc1}}{dt} = \frac{i_{dc1} (p_g - r_{dc1} i_{dc1}^2 - v_c i_{dc1})}{q_t + l_{dc1} i_{dc1}^2} \quad (3.18)$$

$$f_3 = \frac{1}{\omega_0} \frac{dv_c}{dt} = \frac{i_{dc1} - i_{dc2}}{c_c} \quad (3.19)$$

$$f_4 = \frac{1}{\omega_0} \frac{di_{dc2}}{dt} = \frac{v_c - v_{di} - r_{dc2} i_{dc2}}{l_{dc2}} \quad (3.20)$$

However, besides these dynamic equations, some algebraic expressions are needed for the model to determine the undefined variables. For this purpose, by substituting (3.18) in (3.10), the algebraic expression defining the value of v_{dr} is given by:

$$v_{dr} = r_{dc1} i_{dc1} + \frac{l_{dc1} i_{dc1} (p_g - r_{dc1} i_{dc1}^2 - v_c i_{dc1})}{q_t + l_{dc1} i_{dc1}^2} + v_c \quad (3.21)$$

This expression, along with the algebraic equation (3.16), yield the values of v and φ . Then, the value of δ_v can be defined as $\delta_v = \delta_i + \varphi$ (Figure 3.4). In addition, the total reactive power value of the rectifier plus its transformer ($q_r + q_t$) is determined as follows:

$$q_r + q_t = p_g \tan \varphi \quad (3.22)$$

where $q_t = x_t (k_\mu i_{dc1})^2$, resulting in the value of q_r being defined.

Finally, the dynamic equations (3.17)-(3.20), along with the above-mentioned algebraic expressions and extra algebraic equations (3.14)-(3.16), constitute the proposed AVM of the DR system depicted in Figure 3.6a. Therefore, the required input signals for the proposed model are the inverter DC-voltage (v_{di}), the incoming offshore AC-grid active and reactive powers (p_g and q_g) and the controlled reactive power (q_{ct}); while the outputs are the system states, as can be seen in Figure 3.6a. Note that the proposed model is a totally new AVM of the DR system as it does not require the dynamics of a capacitor placed at the DR station to produce the outputs [3]. Moreover, Figure 3.6b shows the frequency control scheme for the proposed AVM to determine the value of q_{ct} based on the previously proposed frequency control principles. As observed, the q -component of the PCC voltage v_q is defined as $v_q = v \sin \delta_v$ using the output signals

of the model. Then, v_q is regulated to be zero through a PI controller, resulting in the value of q_{ct} being set.

3.5 Control system stability analysis

In this section, the stability of the system with embedded frequency control is evaluated using the small-signal studies. For this purpose, the expression for the corresponding PI frequency controller in the small-signal is presented in (3.23). Note that subscript 0 refers to the steady-state value of the variables.

$$\Delta q_{ct} = -(k_p \Delta v_q + k_i \omega_0 \int \Delta v_q dt) \quad (3.23)$$

where $\Delta v_q = v_0 \Delta \delta_v = v_0 (\Delta \delta_i + \Delta \varphi)$, provided that δ_v equals zero in steady-state. k_p and k_i are the proportional and integral control parameters of the PI controller, respectively.

Substituting the expression of Δv_q in (3.23) defines a new state Δx_5 , further adding a new dynamic equation to the system given in (3.24).

$$f_5 = \frac{1}{\omega_0} \frac{d\Delta x_5}{dt} = \Delta \delta_i + \Delta \varphi \quad (3.24)$$

In addition, the system's dynamic model given in (3.17)-(3.20) is linearized around a steady-state operating point to analyze the stability of the control system through its eigenvalues. Then, the corresponding small-signal equations are as follows:

$$\begin{aligned} \frac{1}{\omega_0} \frac{d\Delta x}{dt} &= A \Delta x + B \Delta u \\ \Delta y &= C \Delta x + D \Delta u \end{aligned} \quad (3.25)$$

where $\Delta x = [\Delta \delta_i, \Delta i_{dc1}, \Delta v_c, \Delta i_{dc2}, \Delta x_5]^\top$, $\Delta y = [\Delta \delta_v, \Delta v, \Delta i_{dc1}, \Delta v_c, \Delta i_{dc2}, \Delta q_{ct}]^\top$ and $\Delta u = [\Delta p_g, \Delta q_g, \Delta v_{di}]^\top$.

However, in order to derive the linearized system, the algebraic equations detailed in Section 3.4 are needed for the obtainment of the matrices A - D . Note that, in this study, variable k_μ used in the proposed model is assumed constant with a value equal to steady-state [3].

Therefore, A is a 5×5 matrix whose non-zero elements are as follows:

$$a_{11} = \frac{\partial f_1}{\partial \delta_i} = \frac{-k_p v_0}{q_{t0}}$$

$$\begin{aligned}
a_{12} &= \frac{\partial f_1}{\partial i_{dc1}} = \frac{-(v_0 k_p + p_{g0} \sec^2 \varphi_0) \left(\frac{\partial \varphi}{\partial i_{dc1}} \right)_0}{q_{t0}} - \frac{(-q_{t0} + q_{g0} + q_{ct0} - q_{r0}) \left(\frac{\partial q_t}{\partial i_{dc1}} \right)_0}{q_{t0}^2} \\
a_{13} &= \frac{\partial f_1}{\partial v_c} = \frac{-(v_0 k_p + p_{g0} \sec^2 \varphi_0) \left(\frac{\partial \varphi}{\partial v_c} \right)_0}{q_{t0}} \\
a_{15} &= \frac{\partial f_1}{\partial x_5} = \frac{-k_i v_0}{q_{t0}} \\
a_{22} &= \frac{\partial f_2}{\partial i_{dc1}} = \frac{p_{g0} - r_{dc1} i_{dc10}^2 - v_{c0} i_{dc10}}{(q_{t0} + l_{dc1} i_{dc10}^2)^2} (q_{t0} + l_{dc1} i_{dc10}^2 - i_{dc10} \left(\frac{\partial q_t}{\partial i_{dc1}} \right)_0 - 2 l_{dc1} i_{dc10}) \\
&\quad - \frac{i_{dc10}}{q_{t0} + l_{dc1} i_{dc10}^2} (2 r_{dc1} i_{dc10} + v_{c0}) \\
a_{23} &= \frac{\partial f_2}{\partial v_c} = \frac{-i_{dc10}^2}{q_{t0} + l_{dc1} i_{dc10}^2} \\
a_{32} &= \frac{\partial f_3}{\partial i_{dc1}} = -\frac{\partial f_3}{\partial i_{dc2}} = -a_{34} = \frac{1}{c_c} \\
a_{43} &= \frac{\partial f_4}{\partial v_c} = -\frac{1}{r_{dc2}} \frac{\partial f_4}{\partial i_{dc2}} = -\frac{a_{44}}{r_{dc2}} = \frac{1}{l_{dc2}} \\
a_{51} &= \frac{\partial f_5}{\partial \delta_i} = 1 \\
a_{52} &= \frac{\partial f_5}{\partial i_{dc1}} = \left(\frac{\partial \varphi}{\partial i_{dc1}} \right)_0 \\
a_{53} &= \frac{\partial f_5}{\partial v_c} = \left(\frac{\partial \varphi}{\partial v_c} \right)_0
\end{aligned} \tag{3.26}$$

where $\left(\frac{\partial q_t}{\partial i_{dc1}} \right)_0 = 2 x_t k_\mu^2 i_{dc10}$, $\left(\frac{\partial \varphi}{\partial i_{dc1}} \right)_0 = \frac{r_\mu}{k_\mu v_0^2 \sin \varphi_0} (v_0 - i_{dc10} \left(\left(\frac{\partial v_{dr}}{\partial i_{dc1}} \right)_0 + r_\mu \right))$ and $\left(\frac{\partial \varphi}{\partial v_c} \right)_0 = \frac{-r_\mu i_{dc10}}{k_\mu v_0^2 \sin \varphi_0} \left(\frac{\partial v_{dr}}{\partial v_c} \right)_0$ while $\left(\frac{\partial v_{dr}}{\partial i_{dc1}} \right)_0$ and $\left(\frac{\partial v_{dr}}{\partial v_c} \right)_0$ are expressed as follows:

$$\begin{aligned} \left(\frac{\partial v_{dr}}{\partial i_{dc1}} \right)_0 &= r_{dc1} + \frac{l_{dc1}(p_{g0} - r_{dc1} i_{dc10}^2 - 2 v_{c0} i_{dc10}) - 2 l_{dc1} i_{dc10}(r_{dc1} i_{dc10} + v_{c0})}{q_{t0} + l_{dc1} i_{dc10}^2} \\ &+ \frac{v_{c0} \left(\frac{\partial q_t}{\partial i_{dc1}} \right)_0}{q_{t0} + l_{dc1} i_{dc10}^2} - \frac{2 l_{dc1} i_{dc10} + \left(\frac{\partial q_t}{\partial i_{dc1}} \right)_0}{(q_{t0} + l_{dc1} i_{dc10}^2)^2} \left(l_{dc1} i_{dc10}(p_{g0} - r_{dc1} i_{dc10}^2 \right. \\ &\left. - 2 v_{c0} i_{dc10}) + v_{c0} q_{t0} \right) \end{aligned}$$

$$\left(\frac{\partial v_{dr}}{\partial v_c} \right)_0 = \frac{q_{t0} - 2 l_{dc1} i_{dc10}^2}{q_{t0} + l_{dc1} i_{dc10}^2}$$

Then, B is a 5×3 matrix whose non-zero elements are as follows:

$$\begin{aligned} b_{11} &= \frac{\partial f_1}{\partial p_g} = \frac{-(v_0 k_p + p_{g0} \sec^2 \varphi_0) \left(\frac{\partial \varphi}{\partial p_g} \right)_0 - \tan \varphi_0}{q_{t0}} \\ b_{12} &= \frac{\partial f_1}{\partial q_g} = \frac{1}{q_{t0}} \\ b_{21} &= \frac{\partial f_2}{\partial p_g} = \frac{i_{dc10}}{q_{t0} + l_{dc1} i_{dc10}^2} \\ b_{43} &= \frac{\partial f_4}{\partial v_{di}} = \frac{-1}{l_{dc2}} \\ b_{51} &= \frac{\partial f_5}{\partial p_g} = \left(\frac{\partial \varphi}{\partial p_g} \right)_0 \end{aligned} \tag{3.27}$$

where $\left(\frac{\partial \varphi}{\partial p_g} \right)_0 = \frac{-r_{dc1} i_{dc10}}{k_{\mu} v_0^2 \sin \varphi_0} \left(\frac{\partial v_{dr}}{\partial p_g} \right)_0$ while $\left(\frac{\partial v_{dr}}{\partial p_g} \right)_0$ is expressed as follows:

$$\left(\frac{\partial v_{dr}}{\partial p_g} \right)_0 = \frac{l_{dc1} i_{dc10}}{q_{t0} + l_{dc1} i_{dc10}^2}$$

Matrix C is a 6×5 matrix whose non-zero elements are as follows:

$$c_{11} = c_{32} = c_{43} = c_{54} = 1$$

$$c_{12} = -\frac{c_{62}}{v_0 k_p} = \left(\frac{\partial \varphi}{\partial i_{dc1}} \right)_0$$

$$c_{13} = -\frac{c_{63}}{v_0 k_p} = \left(\frac{\partial \varphi}{\partial v_c} \right)_0$$

$$c_{22} = \left(\frac{\partial v_{dr}}{\partial i_{dc1}} \right)_0 + r_\mu$$

$$c_{23} = \left(\frac{\partial v_{dr}}{\partial v_c} \right)_0$$

$$c_{61} = -v_0 k_p$$

$$c_{65} = -v_0 k_i \quad (3.28)$$

Finally, D is a 6×3 matrix whose non-zero elements are $d_{11} = -\frac{d_{61}}{v_0 k_p} = \left(\frac{\partial \varphi}{\partial p_g} \right)_0$ and $d_{21} = \left(\frac{\partial v_{dr}}{\partial p_g} \right)_0$.

Figure 3.7a shows the eigenvalues of the system at different active power operating points. The eigenvalues are obtained under a hundred incoming active power operating points between $p_{g0} = 0.01$ p.u. and $p_{g0} = 1$ p.u. with the required input parameters detailed in Table 3.1. Note that, for the sake of clarity, the least relevant eigenvalues which are the ones with the non-positive real part far away from the imaginary axis are not shown here. Furthermore, the arrows in Figure 3.7a indicate the movement of the system eigenvalues based on the active power increment and dominant state variables are shown for each of the eigenvalues. As observed, the real part of the obtained eigenvalues is negative which proves the control system stability in all operating points. The zoomed view of the complex eigenvalues whose dominant state is Δv_c is depicted in Figure 3.7b. These eigenvalues are damped by increasing the incoming active power, with the movement of the eigenvalues towards more stable points. In addition, Δx_5 in Figure 3.7b is the dominant state variable of the eigenvalues which does not depend on the operating points.

3.6 Simulation results

In this section, the performance of the proposed control system has been tested through two different simulations: The AVM simulation and the DSM simulation. In the first, the proposed model with embedded frequency control derived in Section 3.4 (Figure 3.6) has been simulated for different study cases in MATLAB/Simulink using the system and control parameters detailed in Table 3.1. In the second, one of those study cases has been implemented in the DSM of the proposed system (described in Subsection 3.6.2), while the accuracy of the proposed AVM has been verified.

Figure 3.7: Small-signal studies: Eigenvalues of the control system at different active power operating points (arrows indicate increasing active power).

3.6.1 AVM simulation results

Two different initial OWF incoming active powers $p_{g01} = 0.1$ p.u. and $p_{g02} = 0.8$ p.u. have been considered for the simulation of the proposed AVM. Furthermore, the initial incoming reactive power for both cases is zero. Then, the performance of the proposed model has been tested by applying two scheduled input changes. In the first, the incoming active power p_g has increased by 0.2 p.u. at $t = 0.01$ s. In the second, the incoming reactive power q_g has increased by 0.1 p.u. at $t = 0.3$ s.

Finally, Figure 3.8 shows the simulation results of the proposed AVM for both cases where the dynamic response of controlled reactive power q_{ct} , frequency f and PCC voltage magnitude v are presented. It is worth mentioning that the reactive power drawn by the DR station increases with the increment of the active power transmitted. Therefore, as observed, the initial value of q_{ct} set by the frequency control scheme depicted in Figure 3.6b has increased for the higher p_{g0} to satisfy the reactive power balance at the PCC needed for the frequency control. The initial value of v has also

Table 3.1: System and control parameters

Description	Value
$S_{base,ac}$	100 MVA
$V_{base,ac}$	33 kV
$P_{base,dc}$	100 MW
$V_{base,dc}$	89.131 kV
n_b	2 six-pulse bridges
T_r	$x_t/n_b = 0.12$ p.u. (33 kV/33 kV)
x_{wf}	0.1 p.u.
$r_{dc1} = r_{dc2}$	0.00765 p.u.
$l_{dc1} = l_{dc2}$	0.57367 p.u.
c_c	2.66347 p.u.
v_{di}	0.9609 p.u.
f_0	50 Hz
k_p	2 p.u.
k_i	$10/\omega_0 = 0.0318$ p.u.
H-Bridges capacitance	8 mF
H-Bridges capacitor voltage	2.7 kV
Number of H-Bridges per phase	23
Cluster inductance	0.15 p.u.
Equivalent switching frequency	2.5 kHz

Figure 3.8: Controlled reactive power, frequency and PCC voltage magnitude during incoming active and reactive power changes for the AVM.

increased for p_{g02} as a consequence of higher initial active power which is clamped by the DR, provided that the HVDC link is imposed by the onshore inverter.

According to the aforementioned explanations, both q_{ct} and v increase with the active power increment at $t = 0.01$ s. However, v has not been affected by the change of q_g at $t = 0.3$ s while q_{ct} decreases to maintain the frequency at its reference value (50 Hz). Note that the system's dynamic response to the active power increment in the case with p_{g01} oscillates more compared to the one with p_{g02} . This result can be proved by the small-signal studies carried out in Section 3.5 where the complex eigenvalues

Figure 3.9: The DSM simulation model.

are damped with the active power increment, as shown in Figure 3.7b. Moreover, the steady-state response of v during the scheduled active power changes proves that the DR acts as a voltage clamp on v in this system.

3.6.2 DSM simulation results

Figure 3.9a shows the considered OWF for the DSM simulation of the proposed control system, which has been fully built in MATLAB/Simulink with the system and control parameters detailed in Table 3.1. The DR station consists of the nonlinear model of a 12-pulse DR given by the Simulink Library Browser (Simscape/Power Systems). A CHB VSC has been considered in this study case for both the frequency control and the harmonic compensation through generating a controlled reactive power (Q_{ct}). For this purpose, a full DSM of the CHB VSC has also been built in MATLAB/Simulink consisting of three identical clusters with a delta connection, as shown in Figure 3.9b. Each cluster consists of a number of series-connected H-bridges to reach the AC-voltage level depending on the H-bridges DC-link rated voltages and is connected as the delta branch through the reactance X_f . The IGBTs used in the H-bridges have also been given by the Simscape/Power Systems and their corresponding gate signals have been provided through the overall control scheme previously described in Subsection 3.3.1 (Figure 3.2), leading to the modulated cluster voltages $V_{ab,f}$, $V_{bc,f}$ and $V_{ca,f}$ being generated. Note that the model predictive control (MPC) technique [8] has been used in the modulator block to obtain the CHB VSC switching pattern, while the d-component

(a) Incoming active and reactive powers, controlled reactive power and frequency

(b) Rectifier AC-current magnitude, rectifier DC-current, PCC voltage magnitude and rectifier DC-voltage

Figure 3.10: Control system performance during active and reactive power changes for both the DSM and the AVM.

of the CHB VSC reference current ($i_{d,f}^{ref}$) has been determined by the DC-link voltage control given in [9].

Furthermore, as can be seen in Figure 3.9a, The OWF has been represented by an aggregated model consisting of a *current-controlled AC-voltage source* V_g and a reactance X_g given that the proposed control system can be implemented irrespective of the type of WTs used. Then, the AC-voltage V_g is generated through the conventional power controller where d- and q-components of the offshore AC-grid currents are controlled to transfer the desired OWF incoming active and reactive power references. Note that V_g is synchronized with the controller reference frame by using the conventional voltage oriented control via PLL, provided that the system frequency is controlled by the CHB VSC. This way the offshore AC-grid current control becomes the same as the WTGS control systems used in the conventional VSC-based HVDC grid forming [10]. Therefore, using the proposed control system, no changes are needed to be applied in the conventional WT control systems.

Figure 3.10 shows the performance of the proposed control system for both the DSM

Figure 3.11: Rectifier, CHB VSC and offshore AC-grid phase currents, and PCC phase voltages for the DSM during the rated incoming active power when the incoming reactive power is equal to 0.1 p.u.

and the AVM during the case with the higher initial incoming active power considered in Subsection 3.6.1. But, the scheduled input changes have been simulated here for 4 s where the incoming active and reactive powers have increased at $t = 0.5$ s and $t = 2.5$ s, respectively. Finally, an appropriate performance of the proposed control system can also be concluded from the DSM simulation with the observations obtained by the AVM simulation, while the accuracy of the proposed AVM is verified. However, due to the implementation of the offshore AC-grid current controller in the OWF aggregated model for the DSM, the incoming active power change affects the dynamic response of the incoming reactive power and vice versa. This results in a slight difference between the dynamic responses of the DSM and the AVM during the corresponding transients. Note that, from the system studies of Figure 3.3 and the simulations, it can be concluded that the proposed control system provides comparative advantages in terms of the DR station volume and the OWF installation cost with any other centralized control in which besides a VSC, a high voltage large capacitor bank is also needed to be placed at the DR station [3].

Figure 3.11 shows rectifier, CHB VSC and offshore AC-grid phase currents (i_r , i_f and i_g), and PCC phase voltages (v) for the DSM during the rated incoming active power when q_g equals 0.1 p.u. As observed, the DR harmonic currents do not exist in i_g because of the APF scheme (Figure 3.2) implemented by the CHB VSC. But, i_f contains the corresponding harmonic components for the SHC. Therefore, the CHB VSC needs an increased power rating of less than 2 % for the harmonic current compensation at OWF rated power, although the required reactive power for the frequency control accounts for the largest part of the total CHB VSC rated power [11]. Finally, as can be seen in Figure 3.11, v does not have low-order harmonics generated by the DR, thereby

Figure 3.12: Considered offshore wind farm for the system operation and control

verifying the performance of the proposed overall control scheme.

3.7 STATCOM operation and control for OWFs connected to DR-HVDC link

In this section, the STATCOM operation and control for OWFs connected to the DR-HVDC link during the three functions of the OWF frequency control, APF and negative-sequence voltage compensation is proposed. Then, two variants of attractive STATCOM topologies are used to implement the proposed control system: the 2L-STATCOM and delta-connected CHB-STATCOM.

For this purpose, Figure 3.12 shows the considered OWF used for the operation of the control system. As observed, a *current controlled AC-voltage source* V_{wf} and a reactance X_{wf} are used as the OWF aggregated model providing the OWF incoming active and reactive powers (P_{wf} and Q_{wf}). Note that, given the offshore AC-grid frequency control provided by the VSC-STATCOM, the conventional voltage oriented control via PLL in the OWF aggregated model is feasible. Moreover, the inverter station is modelled as a DC-voltage source V_{di} as it operates in constant DC-voltage control mode. Finally, the lumped π - and T-models [1], [7] are used for the AC- and DC-cables, respectively. Then, the DRU DC-voltage and DC-current are V_{dr} and I_{dr} , respectively. Moreover, the VSC-STATCOM current injected to the PCC is I_s .

As mentioned before, the central VSC-STATCOM in such application is used for the frequency control and the harmonic compensation. Besides, the FRT capability is included in the control of the VSC-STATCOM carried out in fault conditions where the PCC negative-sequence voltage is regulated to zero. Therefore, a fault is scheduled in the OWF aggregated model shown in Figure 3.12. Then, the corresponding overall control scheme for the generic case (either 2L- or CHB-STATCOM) is depicted in Figure 3.13. As observed, the VSC-STATCOM positive-sequence q-current reference $i_{q,s}^{+ref}$ is set by the previously proposed frequency control scheme (see Figure 3.5). Both VSC-STATCOM negative-sequence d-q current references ($i_{d,s}^{-ref}$ and $i_{q,s}^{-ref}$) are

Figure 3.13: Overall control scheme

provided by the negative-sequence voltage compensation [12] whose purpose is to implement the FRT in fault conditions. Finally, the DC-link voltage control sets the VSC-STATCOM positive-sequence d-current reference $i_{d,s}^{+ref}$ [13], [14]. Both VSC-STATCOM positive- and negative-sequence current vectors ($\underline{i}_s^+$ and $\underline{i}_s^-$) are independently controlled to follow their references with the conventional PI based positive- and negative-sequence current controllers, respectively. Note that the corresponding current controllers are based on the following equations, providing the VSC-STATCOM positive- and negative-sequence voltage vector references ($\underline{v}_s^{+ref}$ and $\underline{v}_s^{-ref}$).

$$\underline{v}_s^{+ref} = \underline{v}^+ + j x_s \underline{i}_s^+ + \frac{x_s}{\omega_0} \frac{d\underline{i}_s^+}{dt} \quad (3.29)$$

$$\underline{v}_s^{-ref} = \underline{v}^- - j x_s \underline{i}_s^- + \frac{x_s}{\omega_0} \frac{d\underline{i}_s^-}{dt} \quad (3.30)$$

where x_s represents the reactance of the VSC-STATCOM equivalent line reactor; while $\underline{v}^+$ and $\underline{v}^-$ are the PCC positive- and negative-sequence voltage vectors, respectively.

Therefore, the outputs of the current controllers result in the VSC-STATCOM positive and negative-sequence d-q voltage references ($v_{d,s}^{+ref}$, $v_{q,s}^{+ref}$ and $v_{d,s}^{-ref}$, $v_{q,s}^{-ref}$) being obtained which are needed for the frequency control and the negative-sequence voltage compensation, respectively. In addition, the required positive-sequence d-q voltage references for the DR harmonic current compensation ($v_{d,hc}^{+ref}$ and $v_{q,hc}^{+ref}$) are obtained by an APF scheme where the previously proposed PI based SHC technique is individually carried out for both corresponding components of the offshore AC-grid current ($i_{d,g}^+$ and $i_{q,g}^+$). Then, the APF is embedded in the frequency control by adding $v_{d,hc}^{+ref}$ and $v_{q,hc}^{+ref}$ to $v_{d,s}^{+ref}$ and $v_{q,s}^{+ref}$, respectively. After that, the obtained reference voltages are

Table 3.2: System parameters

Description	Value
$P_{base,dc}$	100 MW
$V_{base,dc}$	89.131 kV
n_b	2 six-pulse bridges
T_r	$x_t/n_b = 0.12$ p.u. (33 kV/33 kV)
x_{wf}	0.1 p.u.
AC-cable (π -model)	$r_c = 0.0024$ p.u. , $x_c = 0.192$ p.u. , $b_c = 0.187$ p.u.
DC-cable (T-model)	$r_{dc} = 0.00765$ p.u. , $l_{dc} = 0.57367$ p.u. , $c_c = 2.66347$ p.u.
v_{di}	0.9609 p.u.

added to $v_{d,s}^{-ref}$ and $v_{q,s}^{-ref}$ in the stationary ($\alpha\beta$) reference frame and their outputs are transformed back to three-phase quantities to obtain the final VSC-STATCOM reference voltage v_s^{ref} . However, in the case of delta-connected CHB-STATCOM, the modified DC-link voltage control output is added to v_s^{ref} which is responsible to inject the appropriate zero-sequence current inside the delta branches needed for re-balancing the CHB-STATCOM DC-link voltages during the fault condition [14]. Finally, the converter switching pattern is obtained in the modulator block depending on the VSC-STATCOM topologies (i.e. 2L- and CHB-STATCOM). Note that the positive and negative-sequence components of the measured signals are provided by the delayed signal cancellation (DSC) technique given in [15]. It is also worth mentioning that, given that no PLL is used, the reference angular position θ_0 used in the overall control scheme is internally obtained by integrating the desired angular frequency ω_0 .

In the following subsections, the performance of the control system for a specific study case has been investigated using the detailed switching model of two variants of the VSC-STATCOM topologies implemented in Matlab/Simulink with the system parameters detailed in Table 3.2.

3.7.1 System operation with 2L-STATCOM

A conventional three-phase two-level converter, shown in Figure 3.14, is considered as the 2L-STATCOM topology in which $V_{a,s}$, $V_{b,s}$, $V_{c,s}$ are the phase output voltages of the converter while the converter phase currents are $I_{a,s}$, $I_{b,s}$, $I_{c,s}$. The reactance X_l represents the line reactor of the converter. Moreover, AC-filters are placed at the converter bus to remove high-frequency harmonics from its outputs. Measured from the mid-point of DC-capacitors C_{dc} (fictitious ground point), each phase-leg can generate any DC-voltage between 0 to rated DC-voltage V_{dc} . As it can be seen, a 2 p.u. DC-voltage is required to generate a 1 p.u. AC-voltage magnitude.

A study case has been considered to implement the control system where the OWF incoming active power p_{wf} has the initial value equal to 1 p.u. Before the fault condition, two scheduled p_{wf} changes have been applied at $t = 0.5$ s and $t = 1.5$ s. In the

Figure 3.14: 2L-STATCOM topology

first, p_{wf} has decreased by 0.9 p.u. In the second, p_{wf} has increased back to its rated power. Then, a fault has been applied at $t = 2.5$ s in such a way that p_{wf} has suddenly reduced by 0.3 p.u. with a 0.05 p.u. negative-sequence voltage magnitude at the PCC in steady-state. The fault has lasted for 80 ms and has been cleared at $t = 2.58$ s. Finally, after the recovery time, p_{wf} has increased to the pre-fault power at $t = 3.5$ s. It has to be mentioned that the OWF incoming reactive power q_{wf} at all the operating points is zero, thereby no contribution from the WTs in the frequency control (the worst-case scenario).

Figure 3.15 shows the performance of the control system with the 2L-STATCOM operation during this study case. Note that the required 2L-STATCOM reference voltage is generated using the overall control scheme described before and the conventional sinusoidal pulse width modulation (SPWM) has been used to provide the corresponding gate signals. Furthermore, the steady-state values of p_g and q_g at different operating points are defined based on the results of the power flow at the corresponding operating points. As observed, since q_g increases with the reduction of p_g , q_{ct} injected by the 2L-STATCOM has decreased at both $t = 0.5$ s and $t = 2.5$ s to satisfy the required reactive power balance for the frequency control.

Moreover, q_{ct} has increased with the reduction of q_g at both $t = 1.5$ s and $t = 3.5$ s to maintain the frequency at its reference value (50 Hz). As it can be observed from Figure 3.15b, i_r is proportional to the rectifier DC-current i_{dr} . Therefore, the steady-state values of v and as a result, the rectifier DC-voltage v_{dr} at different operating points have been affected by the active power balance in the DR station bus at the corresponding operating points, which, are clamped by the DR. In addition, the dynamic response of PCC negative-sequence voltage magnitude (v^-), 2L-STATCOM negative-sequence and total current magnitudes (i_s^- and i_s), and 2L-STATCOM DC-link voltage (v_{dc}) during the fault condition are presented in Figure 3.15c. Right after the fault, i_s^- has increased from zero for the compensation of v^- due to the FRT performance. During this transient, according to the dynamic response of i_s^- and q_{ct} , the maximum value of i_s is 0.32 p.u. A double-frequency ripple is observed on v_{dc} due to the interaction between positive- and negative-sequence components during the fault condition. Figure 3.16 shows rectifier and offshore grid phase currents, and PCC phase voltages (i_r , i_g and

(a) Incoming active and reactive powers, controlled reactive power and frequency

(b) Rectifier AC-current magnitude, rectifier DC-current, PCC voltage magnitude and rectifier DC-voltage

(c) PCC negative-sequence voltage magnitude, 2L-STATCOM negative-sequence and total current magnitudes, and 2L-STATCOM DC-link voltage during the fault condition

Figure 3.15: Control system performance with 2L-STATCOM during active and reactive power changes.

v) during the rated incoming active power. As observed, the DR harmonic currents do not exist in i_g waveforms due to the APF scheme and filtering the high-frequency

Figure 3.16: Rectifier and offshore grid phase currents, and PCC phase voltages during the rated incoming active power.

Figure 3.17: Delta-connected CHB-STATCOM topology

harmonics. This results in less than 1 % THD for v .

3.7.2 System operation with CHB-STATCOM

Figure 3.17 shows a CHB-STATCOM in delta configuration. Each cluster of the CHB-STATCOM consists of series-connected H-bridge cells, generating the voltages $V_{ab,s}$, $V_{bc,s}$, $V_{ca,s}$ while X_{cl} represents the reactance of the cluster reactor. During the fault condition, a zero-sequence current I_{cl}^0 is circulated inside the delta branches to remove the power disturbance in each cluster caused by the interaction between sequence components, thereby re-balancing the clusters capacitor voltages.

Figure 3.18 and Figure 3.19 show the performance of the control system with the CHB-STATCOM operation during the considered study case. Note that the required CHB-STATCOM reference voltage is generated using the overall control scheme described before and the corresponding gate signals are provided by the MPC technique [8]. From these simulation results, an appropriate performance of the control system with the CHB-STATCOM operation can also be concluded based on the corresponding

(a) Incoming active and reactive powers, controlled reactive power and frequency

(b) Rectifier AC-current magnitude, rectifier DC-current, PCC voltage magnitude and rectifier DC-voltage

(c) PCC negative-sequence voltage magnitude, cluster negative-sequence, zero-sequence and total current magnitudes, and cluster voltages during the fault condition

Figure 3.18: Control system performance with CHB-STATCOM during active and reactive power changes.

Figure 3.19: Rectifier and offshore grid phase currents, and PCC phase voltages during the rated incoming active power.

observations obtained by the 2L-STATCOM. However, for successful operation of the CHB-STATCOM in the fault condition, the cluster zero-sequence current magnitude (i_{cl}^0) has increased from zero right after the fault, as shown in Figure 3.18c. Therefore, according to the dynamic response of the cluster negative-sequence current magnitude (i_{cl}^-) and q_{ct} , the maximum value of the cluster total current magnitude (i_{cl}) is 0.52 p.u. during the FRT performance. Furthermore, as it can be observed from the balanced clusters capacitor voltages (v_{cl}), the power disturbance in each cluster during the fault condition has been removed using i_{cl}^0 .

3.7.3 Comparison

For a fair comparison of both studied topologies, the converters design specification should be based on the same criteria. The minimum size of the converter depends on the system and control parameters, the rated power of the DR, the length of the AC-cables and the transients in the system. For the simulated study case, a 30 MVA converter (0.3 p.u.) is required as analyzed in Chapter 2. For a nominal voltage of 33 kV, the converter transformer is not required. However, it could be included for galvanic isolation. In this comparative analysis the transformer is not considered.

As can be seen in Table 3.3, Figure 3.15c, and Figure 3.18c, the maximum instantaneous current for a fault with around 5 % negative-sequence voltage is greater in CHB-STATCOM than in 2L-STATCOM, due to the zero-sequence component of the delta connected CHB-STATCOM (0.52 p.u. and 0.32 p.u., respectively). In both cases the over-current factor should be considered in the converter design specification. The per-unit value of the required total IGBTs rating s_{si} (total silicon power) is defined as:

$$s_{si} = \frac{V_{sw} \times I_{sw} \times n_{sw}}{S_{b,ac}} \quad (3.31)$$

Table 3.3: Comparison of 2L-STATCOM and CHB-STATCOM for OWF with DR-HVDC connection

Parameters	Values	
	2L-STATCOM	CHB-STATCOM
Base AC-power	100 MVA, 1 p.u.	100 MVA, 1 p.u.
Base AC-voltage	33 kV, 1 p.u.	33 kV, 1 p.u.
Nominal frequency	50 Hz	50 Hz
Converter AC-power*	30 MVA, 0.3 p.u.	30 MVA, 0.3 p.u.
Converter peak current**	0.79 kA, 0.32 p.u.	0.74 kA (inside delta), 0.52 p.u.
AC-filters	5 MVAR, 0.05 p.u.	-
DC-link voltage	65 kV	62 kV (equivalent cluster)
H-bridge DC-link voltage	-	2.7 kV
Number of H-bridges	-	23
Number of IGBTs	186	276
Total IGBTs rating	4.86 p.u.	6.77 p.u.
Switching frequency	2.5 kHz	2.5 kHz (equivalent cluster)
DC-link capacitor	144 μ F	174 μ F (equivalent cluster)
Total capacitor energy	300 kJ	641 kJ
Line reactor	5.2 mH, 0.15 p.u.	3.47 mH, 0.1 p.u.
Cluster reactor	-	5.2 mH (inside delta), 0.05 p.u.

* The converter power demand during normal operation.

** The per-unit values are based on the peak values.

where V_{sw} and I_{sw} are the device voltage and current, respectively. n_{sw} is the total number of devices while $S_{b,ac}$ is the base AC-power of the system. This results in 6.77 p.u. for the CHB-STATCOM against 4.86 p.u. for the 2L-STATCOM, as shown in Table 3.3, Figure 3.15c and Figure 3.18c. The total IGBTs rating are linked with the devices cost that is greater for the CHB-STATCOM converter in delta connection due to the maximum current after fault.

The required total capacitance in the CHB-STATCOM is greater than the capacitance of the 2L-STATCOM, which imply a disadvantage of the CHB-STATCOM with respect to the 2L-STATCOM. A 4 mF capacitor per cell is required in CHB-STATCOM (174 μ F per cluster) and 144 μ F in the 2L-STATCOM as shown in Table 3.3. As is known, the size of the capacitor of the CHB-STATCOM converter can be substantially reduced by an optimized control system, which is beyond the scope of this work. To achieve a similar behavior of 2L-STATCOM and CHB-STATCOM in terms of low frequency harmonics injected, the switching frequency of the 2L-STATCOM converter must be considerably higher than that of the CHB-STATCOM (2.5 kHz vs. 108.5 Hz, respectively), which significantly increases the switching losses and consequently the cooling requirements and costs of the 2L-STATCOM. The total losses of the 2L-STATCOM at the nominal power is around 60 % grater than the CHB-STATCOM losses. On the other hand, 2L-STATCOM converter needs passive AC-filters tuned around the switching frequency (2.5 Khz) to keep the THD voltage in the PCC within the recommended

values (less than 1 %). In this case 5 MVAR AC-filter is required for 2L-STATCOM, which implies an additional cost and more complexity of this solution compared to the CHB-STATCOM. As it can be seen from the results in Figure 3.15 and Figure 3.18, the dynamic behavior of the system is satisfactory in both cases: after the applied power changes at $t = 0.5$ s and $t = 1.5$ s, the frequency keeps controlled (its variation is less than 0.9 Hz). Figure 3.15 and Figure 3.18 shows also the operation of the system during the fault condition (at $t = 2.5$ s) where the converters inject negative-sequence current in order to compensate the negative-sequence voltage during the fault, thereby keeping the operation after the fault.

3.8 Conclusions

This chapter has presented a direct frequency control for an OWF connected to the onshore through a DR-HVDC link. A VSC has been placed at the DRU station to guarantee the frequency control through satisfying reactive power balance at the DRU station bus without a capacitor bank placed at the DRU station. Then, considering the OWF active and reactive powers as inputs, the control system has been derived from a simplified model representing the AC-side dynamics of the DRU whose state variables are the polar components of the rectifier current vector (i.e. magnitude and angle of the current vector). In addition, given an APF scheme implemented by the VSC, passive AC-filters have also been removed from the DRU station. Therefore, the required DRU station volume and as a result, the OWF installation cost can be relatively reduced compared to other centralized controls in which besides a VSC, a high-voltage large capacitor bank is also needed to be placed at the DRU station. A new AVM of the DRU system for such applications has also been derived to test the performance of the proposed frequency control principles, in which the the dynamics of a capacitor bank is not needed to produce the outputs. The stability of the proposed control system has also been proved by a small-signal analysis. The performance of the proposed control system has been demonstrated using the operation and control of a delta-connected CHB VSC in a DSM simulation of the system, while the accuracy of the proposed AVM has been verified. Both the AVM and the DSM simulations have verified a proper performance of the proposed control system. Furthermore, the control functions incorporated to both the 2L-STATCOM and the CHB-STATCOM: OWF frequency control, APF and negative-sequence voltage compensation, have contributed effectively to the proper system operation in both steady-state and transients. It can be concluded that, from the point of view of the functionalities, both types of converter technologies can be used while obtaining a similar behavior. Therefore, the most relevant factors to consider should be the size of the converter, the weight of the converter and the footprint. Both solutions have advantages and disadvantages. However, the solution through the CHB-STATCOM, despite requiring a bit larger initial investment, is more robust, does

not require harmonic filters and has fewer losses. Therefore, considering the system life-time, the use of CHB-STATCOM is more attractive for such an application.

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Chapter 4

Hybrid HVDC systems for OWFs connection using series-connected DRU stations

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4.1 Introduction

In order to address the OWFs start-up in the previously proposed centralized control strategy of the DR-HVDC connected OWFs, a battery energy storage system is needed to be integrated in the central VSC-STATCOM which increases the OWF installation cost. But, the VSC needed for the centralized frequency control in such an application can be used as hybrid HVDC systems in DC-series and -parallel connections with the DRUs to provide the required start-up power to OWFs. Then, hybrid HVDC systems can be used as the centralized solutions to overcome all drawbacks of the DRUs. Therefore, in this chapter, a new hybrid HVDC system for connection of OWFs using a series-connection of DRUs is proposed. The proposed approach overcomes all the drawbacks of the DRUs in such an application using a low-power VSC connected in parallel with all the DRUs. The distributed version of the proposal is also presented where a number of VSCs are connected in parallel with individual DRUs. A comparison in terms of relative losses, cost and volume is carried out between the proposed approach and the traditional VSC-HVDC connection for OWFs. In addition, when a new OWF is developed in area beside an existing VSC-HVDC connected OWFs, all the drawbacks of the DRUs can be addressed by connecting a DR-HVDC link to the existing VSC-HVDC link on AC-side. However, a centralized control strategy for such a parallel operation of the DR-HVDC and VSC-HVDC links for OWFs connection is needed to be implemented by the VSC-HVDC station. For this purpose, a flexible

power flow distribution between these two HVDC links is proposed in this chapter leading to an optimized system for such an application. The proposed grid forming control is based on the proposed frequency control principles in Chapter 3 which allows, for example, sizing the VSC-HVDC just to support the energization of the OWFs during start-up where a high amount of reactive power has to be drawn; while during operation using that power capability to complement the DR-HVDC rating. As a result, this chapter is organized as follows. In Section 4.2, the previously proposed frequency control scheme is tested for OWFs connected to an HVDC link through a series-connection of DRUs where only one central VSC-STATCOM is used. Moreover, the required APF scheme for harmonic current compensation of all the DRUs is proposed while removing passive AC-filters from the DRU stations. The proposed hybrid HVDC system and its control scheme are described in Section 4.3. In Section 4.4, the corresponding control of the parallel operation of the DR-HVDC and VSC-HVDC links for OWFs connection is proposed. Finally, the conclusions are addressed in Section 4.5.

4.2 Frequency control of OWFs with series-connected DRU stations

In this section, the performance of the proposed frequency control scheme in Chapter 3 is tested for OWFs connected to a series connection of DRU stations. For this purpose, Figure 4.1 shows the proposed OWF scheme in which the HVDC link connection is provided by a number of series connected DRUs consisting of 12-pulse diode bridges with their corresponding transformers T_{rk} , which connect them to the PCC. The DRUs are also connected to each other within the offshore AC-grid through AC-bus cables. A VSC-STATCOM placed at the k^{th} DRU bus is used for both the OWF frequency control and all the DRUs harmonic current compensation. The VSC-STATCOM controlled reactive power and current injected to the k^{th} DRU bus are Q_{ct} and I_s , respectively. Then, the DRU AC-currents are I_{uk} ; while the PCC voltages at the DRU station buses are V_k . Note that k subscript can be $1, 2, \dots, n$ where n is the number of DRU stations used. Moreover, the onshore inverter can be LCC or VSC. But, for successful operation of the system, it has to be operated in constant DC-voltage control mode.

In the following subsection, the required VSC-STATCOM overall control scheme needed for the centralized control strategy of the proposed OWF scheme is presented. Note that the lowercase notation represents the per-unit values of the variables and parameters.

4.2.1 Centralized control strategy

Figure 4.2a shows the overall control scheme where the VSC-STATCOM reference voltage needed for both the frequency control and the harmonic compensation (v_s^{ref})

Figure 4.1: Using VSC-STATCOM in OWFs connected to a series-connection of DRUs.

is generated. As observed, the DC-link voltage control [1] sets the VSC-STATCOM reference d-current $i_{d,s}^{ref}$; while the VSC-STATCOM reference q-current $i_{q,s}^{ref}$ is set by the frequency control scheme based on the previously proposed frequency control principles in Subsection 3.3.2. Therefore, the q-component of the PCC voltage at the k^{th} DRU station $v_{q,k}$, where the VSC-STATCOM is connected, is regulated to zero by a PI controller defining the required VSC-STATCOM reactive power for the frequency control. Then, the VSC-STATCOM d-q currents ($i_{d,s}$ and $i_{q,s}$) can be controlled to follow their references with the conventional PI based current controller. This results in the VSC-STATCOM d-q voltage references needed only for the frequency control ($v_{d,s}^{ref}$ and $v_{q,s}^{ref}$) being obtained. Furthermore, an APF scheme is used for all the DRUs harmonic current compensation resulting in the reference d-q voltages needed for the harmonic compensation ($v_{d,hc}^{ref}$ and $v_{q,hc}^{ref}$) being obtained which are added to $v_{d,s}^{ref}$ and $v_{q,s}^{ref}$, respectively. Then, their outputs are transformed into the abc coordinate system to generate v_s^{ref} . Finally, v_s^{ref} is passed through the modulator to generate the required gate signals. Note that the reference voltage angular position θ_0 needed for the overall control scheme is generated internally by integrating the desired angular frequency ω_0 , as no PLL is used.

Fig. 4.2b shows the APF scheme which is separately carried out on both d- and q-components of all the DRU currents ($i_{d,uk}$ and $i_{q,uk}$) to compensate their harmonic orders $h = 12n_h$ ($n_h = 1, 2, \dots$). This results in the considered harmonic orders being removed from the offshore phase voltages (shown in Subsection 4.2.2). Therefore, x subscript in Fig. 4.2b can refer to either d- or q-vector component. First, the phasor form

Figure 4.2: Proposed control of VSC-STATCOM.

of the h^{th} -order harmonics of all $i_{x,uk}$ ($k = 1, 2, \dots, n$) and $i_{x,s}$ at the corresponding harmonic frequency ($\langle i_{x,uk} \rangle^h$ and $\langle i_{x,s} \rangle^h$) are extracted by the DP technique based on the expression in 2.13. By separately adding up all the real and imaginary components of $\langle i_{x,uk} \rangle^h$ together, the corresponding components of the VSC-STATCOM h^{th} -order harmonic current reference are obtained, respectively. Then, both real and imaginary components of $\langle i_{x,s} \rangle^h$ are controlled to follow their reference values using the PI based SHC scheme proposed in Chapter 2. Note that the number of SHC outputs used in the APF scheme depends on the number of considered harmonic orders (n_h). Finally, by adding up all the SHC outputs together, the corresponding vector component of the required reference voltage for harmonic compensation of all the DRU AC-currents ($v_{x,hc}^{ref}$) is provided.

4.2.2 Simulation results

In this subsection, the performance of the proposed control system has been tested by a detailed switching model simulation implemented in MATLAB/Simulink.

Figure 4.3: Considered OWF to be simulated.

Figure 4.3 shows the considered OWF used for the simulation, consisting of three series connected 12-pulse DRUs which provide the HVDC access to the OWF aggregated model. The *current controlled AC-voltage sources* (V_{wf1} , V_{wf2} , V_{wf3}) and the reactance (X_{wf1} , X_{wf2} , X_{wf3}) constitute the OWF aggregated models, while X_{11} and X_{12} represent the AC-bus cables' reactance. A delta-connected CHB-STATCOM, is connected to the 2nd-DRU bus used for both the OWF frequency control and the harmonic compensation whose reference voltage is provided by the overall control scheme described in Subsection 4.2.1. Note that the conventional voltage oriented control via PLL in the OWF aggregated models is feasible, provided that the system frequency is controlled by the CHB-STATCOM. Furthermore, the DC-cable is represented by a T-model given in [2] and the inverter station is modelled as a DC-voltage source V_{di} given that it operates in constant DC-voltage control mode. The system and control parameters are detailed in Table 4.1.

Figure 4.4 shows the simulation results for a study case where all the OWF incoming active and reactive powers (p_{wf} and q_{wf}) have initial values equal to 0.165 p.u. and zero, respectively. Two scheduled p_{wf} changes have been applied at $t = 0.5$ s and $t = 2.5$ s, respectively. In the first, the 1st-OWF active power (p_{wf1}) has increased by 0.165 p.u., the 2nd-OWF active power (p_{wf2}) has remained unchanged and the 3rd-OWF active power (p_{wf3}) has decreased by 0.135 p.u. In the second, p_{wf1} has remained unchanged while p_{wf2} and p_{wf3} have increased by 0.165 p.u. and 0.3 p.u., respectively, to provide the rated OWF incoming active power. Furthermore, an scheduled q_{wf} change has been considered at $t = 1.5$ s where the 1st- and 2nd-OWF reactive powers (q_{wf1} and q_{wf2}) have increased by 0.065 p.u. and 0.035 p.u., respectively, while the 3rd-OWF reactive power (q_{wf3}) has remained unchanged.

Table 4.1: System and control parameters

Description	Value
$S_{base,ac}$	150 MVA
$V_{base,ac}$	33 kV
$P_{base,dc}$	150 MW
$V_{base,dc}$	267.39 kV
n	3 series-connected 12-pulse DRUs
$T_{r1} = T_{r2} = T_{r3}$	33 kV/33 kV
$x_{t1}/(2n), x_{t2}/(2n), x_{t3}/(2n)$	0.15 p.u. , 0.12 p.u. , 0.1 p.u.
$x_{wf1}/n, x_{wf2}/n, x_{wf3}/n$	0.1 p.u. , 0.07 p.u. , 0.08 p.u.
x_{l1}, x_{l2}	0.02 p.u. , 0.01 p.u.
r_{dc} (DC-cable)	0.00765 p.u.
l_{dc} (DC-cable)	0.57367 p.u.
c_c (DC-cable)	2.66347 p.u.
v_{di}	0.9609 p.u.
f_0	50 Hz
H-Bridges capacitance	8 mF
H-Bridges capacitor voltage	2.7 kV
Number of H-Bridges per phase	23
Cluster inductance	0.15 p.u.
Equivalent switching frequency	2.5 kHz

As observed, p_{wf} has been equally shared between the DRU buses at all the operating points given that the DRU active powers (p_{u1}, p_{u2}, p_{u3}) have the same values at the corresponding operating points. This is because of the interconnection of the DRUs within the offshore AC-grid which makes equal active power balances in all the DRU buses based on the expression in 3.7. Moreover, during the simulation, the DRU reactive powers (q_{u1}, q_{u2}, q_{u3}) have been changed proportionally to the DRU active powers with different values depending on the parameters of the DRUs. Since q_u increases with the increment of p_u , the controlled reactive power (q_{ct}) injected by the CHB-STATCOM has increased at both $t = 0.5$ s and $t = 2.5$ s to satisfy the required reactive power balance for the frequency control. Furthermore, q_{ct} has decreased with the increment of q_{wf} at $t = 1.5$ s to maintain the frequency at its reference value (50 Hz). From these simulation results, it can also be concluded that the DRU AC-current magnitudes (i_{u1}, i_{u2}, i_{u3}) are proportional to the rectifier DC-current (i_{dr}). Moreover, the PCC voltage magnitudes at the DRU buses (v_1, v_2, v_3) have increased with the increment of p_u at both $t = 0.5$ s and $t = 2.5$ s which are clamped by the corresponding DRUs. However, their steady-state values have not been affected by q_{wf} change at $t = 1.5$ s. Note that the difference between the DRU DC-voltages ($v_{dr1}, v_{dr2}, v_{dr3}$) is affected by the parameters of the DRUs which is negligible in this study case.

Finally, Figure 4.5 shows all the DRU, CHB-STATCOM and all the OWF phase currents (i_u, i_s and i_{wf}) as well as all the PCC phase voltages at the DRU buses (v) during the full OWF incoming active powers. As observed, the DRU harmonic orders do not

(a) OWF incoming active powers, DRU active powers, OWF incoming reactive powers, DRU reactive powers, controlled reactive power and system frequency

(b) DRU AC-current magnitudes, rectifier DC-current, PCC voltage magnitudes at the DRU buses and DRU DC-voltages

Figure 4.4: Control system performance during OWF incoming active and reactive power changes.

exist in individual i_{wf1} , i_{wf2} and i_{wf3} due to the APF scheme while i_s contains the corresponding harmonic orders for the harmonic compensation. As a result, v does not have low-order harmonics generated by the DRUs which validates the performance of the proposed control system.

4.3 Proposed hybrid HVDC system for OWFs connection

In order to address all the drawbacks of the DR-HVDC link, a hybrid HVDC system is proposed in this section. Figure 4.6a shows the proposed hybrid HVDC system for

Figure 4.5: DRU, CHB-STATCOM and OWF phase currents, and PCC phase voltages at the DRU buses during the full OWF incoming active powers.

OWFs connection, where the HVDC link is provided by a number of series connected DRUs which are connected to the PCC through their corresponding transformers. A low-power VSC is connected in parallel with all the DRUs which is used to implement three functions: OWFs start-up, offshore AC-grid frequency control and DRUs harmonic current compensation. This approach is considered as a centralized parallel hybrid topology; while the distributed one shown in Figure 4.6b consists of a number of low-power VSCs connected in parallel with individual DRUs. Therefore, the total required VSC rated power and DC-voltage for the centralized topology is equally shared among the VSCs used in the distributed one. However, such a grid forming control using the distributed parallel hybrid topology requires a droop control, i.e. Q/f [3], to synchronize all the VSCs together providing the frequency control which is not implemented in the conventional VSC-HVDC based grid forming [4]. Then, in order to have a fair comparison between the proposed hybrid HVDC scheme and the conventional VSC-HVDC scheme depicted in Figure 1.1, in the following, the study is carried out with focus on the proposed centralized parallel hybrid topology.

Note that, for successful operation of the proposed system, the onshore inverter has to be operated in constant DC-voltage control mode, which can be LCC or VSC. Then, during the start-up, the required active power for energization of the OWF is extracted from the HVDC link by the central VSC while the amount of provided or absorbed reactive power by the VSC depends on the inductive or capacitive offshore AC-grid, respectively. After energization, the OWF incoming active power is fully transmitted by the DRUs while the VSC is used to inject the required reactive power for both the OWF frequency control and the harmonic compensation. In the following subsection, the required VSC overall control scheme for the aforementioned centralized control

Figure 4.6: Proposed hybrid HVDC systems for OWFs connection using series connected DRUs.

strategy is presented.

4.3.1 Overall control scheme

Figure 4.7 shows the considered OWF used for the system operation and control, consisting of two series connected 12-pulse DRUs connected to the PCC through their

Figure 4.7: Considered OWF for the system operation and control.

corresponding transformers (T_{r1} and T_{r2}) which provide the HVDC connection for two OWF aggregated models. As observed, the *current controlled AC-voltage sources* (V_{wf1} , V_{wf2}) and the reactance (X_{wf1} , X_{wf2}) constitute the OWF aggregated models. A low-power VSC with its corresponding transformer T_f are connected in parallel with all the DRUs as a centralized parallel hybrid topology. In addition, the inverter station is modelled as a DC-voltage source V_{di} given that it operates in constant DC-voltage control mode. Finally, the reactance X_1 represents the AC-bus cable interconnecting DRUs within the offshore AC-grid; while the lumped π - and T-models [3], [2] are used for the AC-array and DC-cables, respectively. Note that the conventional voltage oriented control via PLL in the OWF aggregated models is feasible, provided that the system frequency is controlled by the VSC.

Figure 4.8 shows the overall control scheme used to generate the required VSC reference voltage v_f^{ref} for implementation of three functions: OWF start-up, frequency control and harmonic compensation. During the energization, the VSC d-current reference $i_{d,f}^{ref}$ is set by the start-up control scheme in which the PCC d-voltage at the 2nd-DRU bus (where the VSC is placed) $v_{d,2}$ is ramped up by following its reference ($v_{d,2}^{ref}$) using a PI controller. Note that $v_{d,2}^{ref}$ is set less than the rated AC-voltage to prevent DRUs from conducting during start-up. But, after producing power by the OWFs, $v_{d,2}$ changes automatically based on the active power balance at the DRU buses, as described before. Therefore, during the DRUs operation mode, $i_{d,f}^{ref}$ is set by the conventional power control with the zero reference value for the VSC active power ($p_f^{ref} = 0$). Furthermore, the VSC q-current reference $i_{q,f}^{ref}$ during both energization and DRUs operation modes is set by the frequency control scheme, where the PCC q-voltage at the 2nd-DRU bus $v_{q,2}$ is regulated to zero through a PI controller based on the previously proposed frequency control principles. Then, the VSC d-q currents ($i_{d,f}$ and $i_{q,f}$) are controlled to follow their desired references with the conventional PI-based current

Figure 4.8: Overall control scheme of the VSC.

controller leading to the VSC d-q voltage references ($v_{d,f}^{ref}$ and $v_{q,f}^{ref}$) being obtained, respectively. In addition, the reference d-q voltages needed for all the DRUs harmonic current compensation ($v_{d,hc}^{ref}$ and $v_{q,hc}^{ref}$) are generated by the harmonic compensation scheme proposed in Subsection 4.2.1 which are added to $v_{d,f}^{ref}$ and $v_{q,f}^{ref}$, respectively. Then, their outputs are transformed back to three-phase quantities to obtain v_f^{ref} . Finally, the converter switching pattern is obtained in the modulator block. Note that, since no PLL is used, the reference voltage angular position θ_0 needed for the overall control scheme is generated internally by integrating the desired angular frequency ω_0 .

4.3.2 Simulation results

In this subsection, the performance of the control system has been tested through a detailed switching model simulation of the considered OWF in Figure 4.7 as the focus is on the proposed centralized topology. Note that an MMC with half-bridge cells is used in this study case, although any converter topology can be considered as a low-power VSC. The simulation has been implemented in MATLAB/Simulink using the system and control parameters detailed in Table 4.2.

The simulation starts with the HVDC link already started by the onshore inverter. Note that the OWFs are started-up sequentially to reduce the amount of provided or absorbed reactive power by the MMC during the energization depending on the inductive or capacitive offshore AC-grid, respectively. This leads to the reduction of the required MMC rated power. Besides, during the DRUs operation mode, the additional control technique described in Section 2.5 is used where a reactive power command is sent to the OWFs to take advantage of their incoming reactive power capability needed for the frequency control. This avoids the MMC exceeding its maximum rated power as well. Therefore, the 1st-OWF is started-up by the MMC from $t = 0.1$ s to $t = 0.5$ s, being 0.9 p.u. the PCC voltage magnitude target. At this point, the 1st-OWF starts

Table 4.2: System and control parameters

Description	Value
$S_{base,ac}$	100 MVA
$V_{base,ac}$	33 kV
$P_{base,dc}$	100 MW
$V_{base,dc}$	178.26 kV
n	2 series-connected 12-pulse DRUs
$T_{r1} = T_{r2}$	$x_t/(2n) = 0.12$ p.u. (33 kV/33 kV)
T_f	$x = 0.05$ p.u. (33 kV/109.16 kV)
$x_{wf1}/n, x_{wf2}/n$	0.15 p.u. , 0.1 p.u.
x_l	0.01 p.u.
r_c (AC-cable)	0.0048 p.u.
x_c (AC-cable)	0.384 p.u.
b_c (AC-cable)	0.0935 p.u.
r_{dc} (DC-cable)	0.00765 p.u.
l_{dc} (DC-cable)	0.57367 p.u.
c_c (DC-cable)	2.66347 p.u.
v_{di}	0.9609 p.u.
f_0	50 Hz
SM capacitance	83 μ F
SM capacitor voltage	3.69 kV
SM number per arm	53
Arm inductance	0.05 p.u.
Equivalent switching frequency	5.5 kHz

producing power till its rated value, following a reactive power command received through a communication channel with a 0.1 s time delay. Then, at $t = 3$ s, the 2nd-OWF is connected and started-up but it starts producing power at $t = 4$ s in the same condition as the first OWF.

Figure 4.9 shows the performance of the control system with the proposed centralized topology during this study case. Note that the required MMC reference voltage is generated using the overall control scheme described in the previous subsection and the phase disposition pulse width modulation (PD-PWM) has been used to provide the corresponding gate signals. As observed, the PCC voltage magnitudes at both 1st- and 2nd-DRUs (v_1 and v_2) have been driven till 0.9 p.u. during the start-up. During this period of time, the MMC needs to provide the active power losses of the OWFs (less than 1 % of the OWF rated power) and to absorb the required reactive power due to the capacitive offshore AC-grid as shown in the dynamic response of its active and reactive powers (p_f and q_f), respectively. After producing the 1st-OWF active power p_{wf1} at $t = 0.5$ s, v_1 and v_2 have changed automatically due to active power balancing at the DRUs. But, they are clamped by the corresponding DRUs, provided that the HVDC link voltage is imposed by the onshore inverter. At this point, p_f has been driven to zero resulting in the OWF incoming active powers (p_{wf}) being fully transmitted by the DRUs during all the operating points.

(a) PCC voltage magnitudes, incoming active powers, DRU active powers and MMC active power

(b) DRU reactive powers, OWF incoming reactive powers, MMC reactive power and frequency

(c) rectifier AC-current magnitudes, rectifier and MMC DC-currents, and rectifier DC-voltages

Figure 4.9: Control system performance with proposed centralized topology.

Furthermore, as it can be observed from the dynamic response of the DRU active

Figure 4.10: Rectifier, offshore grid phase currents and PCC phase voltages during the full incoming active powers.

powers (p_u), p_{wf} has been equally shared between the DRU buses during all the operating points due to the interconnection of DRUs within the offshore AC-grid. Therefore, as the DRUs are identical, the DRU reactive powers (q_u) are the same during all the operating points as well. Finally, the dynamic response of the OWF incoming reactive powers (q_{wf}) during the DRUs operation mode have been defined based on the reactive power command sent to the OWFs needed for the frequency control, thereby defining the dynamic response of q_f which is within 10 % of the OWF rated power. By satisfying the reactive power balance at DRU buses, it can be seen that the system frequency f has been maintained at its reference value (50 Hz). As it can be observed from Figure 4.9c, the DRU AC-current magnitudes (i_u) and the MMC DC-current (i_{dcf}) are proportional to the rectifier DC-current (i_{dcu}) and p_f , respectively. Furthermore, the DRU DC-voltages (v_{dr}) are the same during all the operating points as well.

Finally, Figure 4.10 shows all the rectifier and OWF phase currents, and all the PCC phase voltages (i_u , i_{wf} and v) during the full OWF incoming active powers. As observed, the DRU harmonic currents do not exist in i_{wf} waveforms due to the harmonic compensation scheme. This results in less than 1 % THD for the offshore phase voltages (v).

4.3.3 Comparison

By means of the study carried out and the analysis of the simulation results, it can be concluded that the proposed centralized topology (Figure 4.6a) provides some comparative advantages with the traditional topology (Figure 1.1). Therefore, in this section, a fair comparison in terms of relative losses, cost and volume is carried out between the proposed centralized topology and the full-rated VSC-HVDC system. Note that an MMC with half-bridge cells is considered in both topologies for this comparison.

Table 4.3: Comparison of proposed centralized topology and full-rated MMC-HVDC system for OWFs connection

Parameters	Values	
	Full-rated MMC	Proposed centralized topology
Base DC-power	600 MVA, 1 p.u.	600 MVA, 1 p.u.
Base DC-voltage	200 kV, 1 p.u.	200 kV, 1 p.u.
Total power in transformers	2 p.u.	1.1 p.u.
Total power in DRU	-	1 p.u.
Total power in MMC	1 p.u.	0.1 p.u.
Total power**	3 p.u.	2.2 p.u.
Total Diode rating	-	9.55 p.u.
Total IGBTs rating	13.93 p.u.	1.39 p.u.
Total devices rating***	13.93 p.u.	10.94 p.u.
Total cell capacitors energy	10.8 kJ	1.08 kJ
Total relative losses**	1 p.u.	0.57 p.u.

* The per-unit values are based on the peak values.

** (transformers + converter)

*** (Diodes + IGBTs)

The system studies of Figure 4.6a and the simulations verify that the proposed system can be operated through the series-connection of the DRUs without any significant problem: the OWF powers are collected and distributed uniformly through the DRUs. The total DC-voltage is divided equally between the DRUs. Table 4.3 shows a comparison between the full-rated MMC-HVDC and the proposed centralized topology for collecting three strings of 200 MW and connecting them to the onshore through an HVDC link based on the OWF configurations shown in Figure 1.1 and Figure 4.6a, respectively. The total power of transformers needed in each case, MMC and DRU rated powers, total devices (Diodes and IGBTs) power rating, capacitor energy of the MMCs and relative losses of both cases have been calculated. The values are expressed in per-unit and/or in relative values for a better comparison.

As observed, based on the results shown in Chapter 2 and the simulation results, the MMC rated power for the proposed centralized topology is 10 % of the total power compared to the full-rated MMC-HVDC. From this comparison, it can be concluded that the proposed centralized topology requires a lower power in transformers and converters (2.2 p.u. vs. 3 p.u.). In general, the proposed centralized topology has lower total losses (57 % of the traditional topology) since the total power is transmitted through the diode bridges, with less losses than the full-rated MMC-HVDC, and the system needs a smaller number of transformers. Moreover, platforms are smaller and lighter in the proposed centralized topology compared to the full-rated MMC-HVDC (10.94 p.u. vs. 13.93 p.u. for the total devices rating).

Figure 4.11: Parallel DR-HVDC and VSC-HVDC systems for OWF connection.

4.4 Control of the parallel operation of DR-HVDC and VSC-HVDC for OWFs connection

In this section, the required centralized control strategy for the DR-HVDC connection of a new developed OWF in area beside an existing OWF which is connected to the VSC-HVDC link is presented. Then, the DR-HVDC link is connected to the existing VSC-HVDC link on AC-side, overcoming all the DR-HVDC link drawbacks. The considered parallelly connected DR-HVDC and VSC-HVDC links for OWFs connection is shown in Figure 4.11. The DR-HVDC link consists of a number of series connected 12-pulse DRU stations with their corresponding transformers (T_{rk}) interconnected to each other through AC-bus cables. The VSC-HVDC link is composed of a VSC station with its corresponding transformer (T_f) connected to one of the DRU stations through an AC-bus cable. The VSC station is used to implement a centralized control strategy needed for the OWFs start-up and operation of the parallel system, guaranteeing the DRUs commutation by controlling the system frequency. As observed, the individual VSC and DRU station buses are connected to an aggregated OWF at the PCC through AC-array cables. Then, the PCC voltages at the VSC and DRU stations are V and V_{uk} , respectively. The active power flowing into the VSC station is P_f ; while the VSC station

AC-current and reactive power injected to the PCC are I_f and Q_f , respectively. Finally, I_{uk} is the DRU AC-current. Note that the onshore inverters can be LCCs or VSCs, but they have to be operated in constant DC-voltage control mode for successful operation of the parallel system.

In the following subsection, the required VSC-HVDC overall control scheme based on the previously proposed control system principles in Chapter 3 is presented. Note that the lowercase notation represents the per-unit values of the variables and parameters.

4.4.1 Control of VSC-HVDC station

Figure 4.12a shows the overall control scheme where the required VSC reference voltage for the implementation of the proposed centralized control strategy (v_f^{ref}) is generated. As observed, similar to the conventional VSC-HVDC grid forming control, the q-component of the VSC reference current $i_{q,f}^{ref}$ is set by regulating the q-component of the PCC voltage at the VSC station (v_q) to zero using a PI controller. However, this is considered as the frequency control scheme for such a parallel system which is based on the expression in 3.9 as $i_{q,f}^{ref}$ is proportional to the desired q_f needed for satisfying the reactive power balance in all the DRU station buses. This also results in the d-component of the VSC reference current $i_{d,f}^{ref}$, which is set by an active power flow control scheme, being proportional to the desired VSC station active power (p_f) needed for the proposed grid forming control. Both d- and q-components of the VSC current ($i_{d,f}$ and $i_{q,f}$) are controlled to follow their reference values using the conventional cascaded inner PI based current controller. This leads to the required corresponding vector components of the VSC reference voltage for the operation of the parallel system ($v_{d,f}^{ref}$ and $v_{q,f}^{ref}$) being provided. In addition, the required d- and q-components of the reference voltage for harmonic compensation of all the DRU AC-currents ($v_{d,hc}^{ref}$ and $v_{q,hc}^{ref}$) are provided by the harmonic compensation scheme proposed in Subsection 4.2.1 which are added to $v_{d,f}^{ref}$ and $v_{q,f}^{ref}$, respectively. Finally, their outputs are transformed back to the three-phase quantities to generate v_f^{ref} . Note that the required VSC gate signals are provided by the modulation of v_f^{ref} depending on the type of VSC topology used. Moreover, as no PLL is used, the reference voltage angular position (θ_0) used in the overall control scheme is obtained internally by integrating the desired angular frequency (ω_0).

Figure 4.12b shows the proposed active power flow control scheme needed for both the OWFs start-up procedure as well as the parallel operation of the DR-HVDC and VSC-HVDC links in such an application. As observed, the d-component of the PCC voltage at the VSC station (v_d) defining the offshore voltage magnitude is controlled to follow its reference (v_d^{ref}) through a PI controller. Therefore, during the OWFs start-up, the offshore voltage magnitude is ramped up by providing the required start-up

Figure 4.12: Proposed control of VSC station.

power to OWFs through $i_{d,f}^{ref}$. Note that the steady-state value of v_d^{ref} is set less than the DR-HVDC link voltage to prevent the DRU stations from conducting during the OWFs start-up. But, given the expression in 3.7 and the simulation results, the offshore voltage magnitude has to automatically accommodate the required value for transmitting the OWF powers through the DR-HVDC link, provided that the DR-HVDC link voltage is imposed by the onshore inverter. Therefore, the proposed grid forming control during the DR-HVDC link operation is based on controlling the offshore voltage magnitude through the injection of OWF active powers, deriving the active power flow control principles of the parallel system. Then, in order to implement the proposed active power flow control principles of the parallel system when the OWFs start injecting active powers, a saturation level is set for $i_{d,f}^{ref}$ by defining its minimum instantaneous value ($i_{d,f,min}^{ref}$) as follows:

$$i_{d,f,min}^{ref} = \frac{-1}{v_d^2 + v_q^2} (p_f^{ref} v_d - q_f v_q) \quad (4.1)$$

where p_f^{ref} is the VSC station active power reference which can vary between zero till the VSC-HVDC link rated power depending on the offshore wind conditions. Therefore, when the injected OWF active powers are less or equal to p_f^{ref} , the offshore voltage magnitude is controlled to be maintained at its reference value by the VSC-HVDC through satisfying the active power balance. But, by increasing the injected OWF active powers till more than p_f^{ref} , $i_{d,f}^{ref}$ reaches its saturation level and the offshore voltage magnitude starts increasing. By increasing the offshore voltage magnitude, the DRUs start conducting and as a result, the offshore voltage magnitude automatically clamps to the required value for transmitting the rest of the OWF powers through the DR-HVDC link. Therefore, the proposed grid forming control leads to the power flow distribution between the VSC-HVDC and DR-HVDC links without the need for any control mode switching process. This makes a difference between the proposed control system and the conventional VSC-HVDC based grid forming control. Note that the block diagram of the expression of $i_{d,fmin}^{ref}$ in (4.1) is also depicted in Figure 4.12b where the VSC station reactive power is calculated as $q_f = v_q i_{d,f} - v_d i_{q,f}$.

4.4.2 Simulation results

In this subsection, the performance of the proposed centralized control strategy has been tested by a DSM simulation implemented in MATLAB/Simulink.

Figure 4.13 shows the considered parallel system for the simulation where the DR-HVDC link consists of two series connected 12-pulse DRU stations. In this study case, the half-bridge submodule (SM) based MMC has been used as a VSC-HVDC to implement the proposed control system. Then, the IGBT gate signals have been provided by the MMC modulator block depicted in Figure 4.13b. As observed, first, the required reference voltages of the MMC upper and lower arms (V_{uarm}^{ref} and V_{larm}^{ref}) have been generated as follows:

$$V_{uarm}^{ref} = \frac{1}{2} V_{dcf} - \frac{1}{2} m_i V_{dcf} v_f^{ref} \quad (4.2)$$

$$V_{larm}^{ref} = \frac{1}{2} V_{dcf} + \frac{1}{2} m_i V_{dcf} v_f^{ref} \quad (4.3)$$

where m_i is the modulation index and V_{dcf} is the MMC-HVDC link voltage. Note that v_f^{ref} used in the aforementioned expressions has been given by the overall control scheme described in Subsection 4.4.1 (Figure 4.12). Then, the MMC circulating current controller [5] output has been added to both V_{uarm}^{ref} and V_{larm}^{ref} . After that, their outputs have been modulated using the PD-PWM to obtain the required MMC switching pattern. Finally, by passing the switching pattern through the SM capacitor voltage balancing [6], the required IGBT gate signals have been provided.

(a) The parallel system to be simulated

(b) Block diagram of the modulator for MMC

Figure 4.13: Detailed switching model of the parallel system.

Three OWF aggregated models of same rated power have been used in the considered parallel system represented by the *current-controlled voltage sources* (V_{wf1} , V_{wf2} , V_{wf3}) and reactance (X_{wf1} , X_{wf2} , X_{wf3}). Then, V_{wf1} , V_{wf2} and V_{wf3} are generated by the conventional power controllers where the OWF active and reactive power references are set. It is also worth mentioning that the conventional grid synchronization via PLL can be implemented in the OWF aggregated models, provided that the system frequency is controlled by the MMC station. Moreover, the AC-bus cables have been represented by the reactance (X_{l1} , X_{l2}); while the lumped π - and T-models given in [3] have been used for the AC-array cables and DC-cables, respectively. Finally, the inverter stations have been modelled by the DC-voltage sources (V_{dci1} , V_{dci2}) as they operate in constant DC-voltage control mode. Note that the required system parameters for the simulation are detailed in Table 4.4.

Figure 4.14 shows the dynamic response of the PCC phase voltages (v , v_{u1} , v_{u2}), MMC station active power (p_f), MMC station reactive power (q_f) and frequency (f) during the OWFs start-up procedure. As observed, v , v_{u1} and v_{u2} have been driven till 0.9 p.u. by the MMC station, preventing the DRU stations from conducting. Then,

Table 4.4: Parameters of the modelled System

Parameter		Value
OWF	$S_{base,ac}$	1800 MVA
	$V_{base,ac}$	66 kV
	f_0	50 Hz
	$x_{wf1}/3, x_{wf2}/3, x_{wf3}/3$	0.15 p.u., 0.1 p.u., 0.12 p.u.
	x_{l1}, x_{l2}	0.015 p.u., 0.01 p.u.
	r_c (AC-cable)	0.0072 p.u.
	x_c (AC-cable)	0.576 p.u.
	b_c (AC-cable)	0.0623 p.u.
MMC-HVDC	$P_{base,dc}$	600 MW
	$V_{base,dc}$	640 kV
	m_i	0.8
	T_f	66 kV/313.54 kV
	x_{Tf}	0.1 p.u. ($x_{Tf}/r_{Tf}=80$)
	SM capacitance	7 mF
	SM capacitor voltage	3.69 kV
	SM number per arm	174
	Arm inductance	0.05 p.u.
	Switching frequency	8 kHz (equivalent arm)
	r_{dc} (DC-cable)	0.00765 p.u.
	l_{dc} (DC-cable)	0.57367 p.u.
	c_c (DC-cable)	2.66347 p.u.
DR-HVDC	v_{dci1}	0.9609 p.u.
	$P_{base,dc}$	1200 MW
	$V_{base,dc}$	640 kV
	n	2 12-pulse DRUs
	T_{r1}	66 kV/118.47 kV
	T_{r2}	66 kV/118.47 kV
	$x_{T_{r1}}/2n$	0.12 p.u. ($x_{T_{r1}}/r_{T_{r1}}=80$)
	$x_{T_{r2}}/2n$	0.1 p.u. ($x_{T_{r1}}/r_{T_{r1}}=80$)
	r_{dc} (DC-cable)	0.00765 p.u.
	l_{dc} (DC-cable)	0.57367 p.u.
	c_c (DC-cable)	2.66347 p.u.
	v_{dci2}	0.9609 p.u.

the MMC station provides the power losses of the OWFs (less than 1 % of their rated powers) extracted from the MMC-HVDC link. Moreover, due to the capacitive off-shore AC-grid at no load condition, the MMC station has to absorb the reactive power needed for the grid forming control. As the system frequency is measured by a PLL, this measurement is affected by the PLL dynamics.

Figure 4.15 shows the parallel system's dynamic response to the injections of OWF active and reactive powers. After the start-up procedure, the OWFs have started injecting active powers (p_{wf1} , p_{wf2} , p_{wf3}) with a 0.1 s delay between them until they have

Figure 4.14: PCC phase voltages, MMC active power, MMC reactive power and frequency during the OWFs start-up.

(a) PCC voltage magnitudes, OWF active powers, MMC active power and DRU active powers.

(b) OWF reactive powers, DRU reactive powers, MMC reactive power and frequency.

Figure 4.15: Control system performance during the injections of OWF active and reactive powers.

reached different active power operation points from 0.33 p.u. to 0.13 p.u. of the parallel system rated power. Then, right after the injection of p_{wf1} , p_f has increased to transmit the OWF active power through the MMC-HVDC link needed for maintaining the PCC voltage magnitudes (v , v_{u1} , v_{u2}) at their reference values (v^{ref}). However, at t

= 0.3 s, the MMC AC-voltage controller has reached its saturation level defined by the MMC active power reference (p_f^{ref}) value which has been set equal to 17 % of the parallel system rated power in this simulation. Therefore, after $t = 0.3$ s, the PCC voltage magnitudes have automatically increased and the DRU stations have started conducting to transmit the rest of the OWF active powers through the DR-HVDC link, as shown in the dynamic response of the DRU station active powers (p_{u1}, p_{u2}). Note that, during the DR-HVDC link operation, the offshore voltage magnitudes have been imposed by the DRUs through satisfying the active power balance in the parallel system. As a result, due to the interconnection of DRU stations within the offshore AC-grid and equal active power balances at their buses, the rest of the injected OWF active powers have been equally shared among p_{u1} and p_{u2} . Moreover, the reactive powers drawn by the DRU stations (q_{u1} and q_{u2}) have increased with the increment of p_{u1} and p_{u2} depending on the corresponding DRU station parameters. Therefore, q_f has increased to satisfy the reactive power balance in the parallel system needed for the frequency control. At $t = 1$ s, the OWFs have also started injecting reactive powers ($q_{wf1}, q_{wf2}, q_{wf3}$) with a 0.01 s delay between them until they have reached the same reactive power operation points equal to 0.017 p.u. of the parallel system rated power. However, the offshore voltage magnitudes have not been affected by the injection of OWF reactive powers; while q_f has decreased after $t = 1$ s to maintain the frequency at its reference value (50 Hz).

Figure 4.16 shows the parallel system's dynamic response to the scheduled changes of MMC active power reference and injected OWF active powers. As observed, during the parallel operation of the MMC-HVDC and DR-HVDC links, the MMC active power reference p_f^{ref} has increased till the MMC-HVDC link rated power. Then, the power transmitted through the MMC-HVDC link has increased until it has been fully loaded, further decreasing the power transmitted through the DR-HVDC link. This allows to state that the proposed active power flow control of the parallel system is flexible as the power transmitted through one HVDC link can be changed when the other HVDC link is not fully loaded. At $t = 0.75$ s, p_{wf2} and p_{wf3} have increased till their rated values, injecting full OWF active powers in steady-state. Therefore, after $t = 0.75$ s, p_{u1} and p_{u2} have increased till their rated values which has led to the DR-HVDC link being fully loaded as well. During the simulation, the PCC voltage magnitudes have automatically accommodated the required values for transmitting the OWF powers through the DR-HVDC link which have been clamped within acceptable operation limits. Moreover, according to the dynamic response of q_{u1} and q_{u2} , q_f has changed to control the system frequency. From these simulation results, it can be concluded that the dynamic response of the DRU AC-current magnitudes (i_{u1}, i_{u2}) are proportional to the dynamic response of the DRU DC-current (i_{dcu}); while the dynamic response of the MMC DC-current (i_{dcf}) is defined by the dynamic response of p_f . Note that, the slight difference between the DRU DC-voltages (v_{dcu1}, v_{dcu2}) during the full OWF

(a) PCC voltage magnitudes, OWF active powers, MMC active power and DRU active powers.

(b) OWF reactive powers, DRU reactive powers, MMC reactive power and frequency.

(c) MMC DC-current, DRU AC-current magnitudes, DRU DC-current and DRU DC-voltages.

Figure 4.16: Control system performance during the changes of MMC active power reference and injected OWF active powers.

active powers depends on the corresponding DRU station parameters which is negligible in this study case. Moreover, the dynamic responses in the simulation results have verified the stability of the operation of the parallel system during both transient and steady-state conditions.

Finally, Figure 4.17 shows the DRU phase currents (i_{u1} , i_{u2}), MMC phase currents

Figure 4.17: DRU phase currents, MMC phase currents, OWF phase currents and PCC phase voltages during the full OWF active powers.

(i_f), OWF phase currents (i_{wf1} , i_{wf2} , i_{wf3}) and PCC phase voltages during the full OWF active powers. As observed, due to the implementation of the APF scheme, i_{wf1} , i_{wf2} and i_{wf3} do not contain the low-order harmonics generated by the DRU stations. This has also led to the considered harmonic orders being removed from v , v_{u1} and v_{u2} . However, the DRU harmonic currents exist in i_f for the harmonic compensation affecting the power demand of the MMC station by less than 1 % when the DR-HVDC link is fully loaded [7]. Therefore, these simulation results verify the performance of the proposed overall control scheme of the MMC station.

4.5 Conclusions

This chapter has presented a hybrid HVDC system for connection of OWFs using series-connected DRUs. The proposed centralized topology allows lighter and smaller platforms which can be built closer to WTs to collect the wind energy. Several transformers and AC-cables can be avoided, compared with the traditional topology. The start-up, the frequency control and the harmonic compensation can be implemented by a centralized VSC of around 10 % of the total power, connected in parallel with all the DRUs. Distributed low-power VSCs can be also an alternative hybrid topology. The simulation results show a robust performance of the system in steady state and also during transients. Furthermore, a control of the parallel operation of the DR-HVDC and VSC-HVDC links for OWFs connection has been proposed in this chapter. The VSC-HVDC is used during start-up for energizing the OWFs grid. Then, during operation, the grid forming just controls the system frequency through satisfying the reactive power balance in the system. The proposed control system allows a flexible power flow distribution between the DR-HVDC and VSC-HVDC links without the need for any

control mode switching procedure given that the DR-HVDC will automatically close the active power balance while the active power transmitted through the VSC-HVDC can be set independently. The performance of the proposed control system has been demonstrated using the operation and control of an MMC-HVDC for a parallel system with a series-connection of DRUs. Simulation results have verified the performance of the proposed control system during both for transient and steady-state conditions.

4.6 Bibliography

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Chapter 5

Conclusions

In this chapter, first, the overall conclusions throughout this Thesis are addressed in Section 5.1. Then, the main contributions of the this Thesis are described in Section 5.2. Finally, Section 5.3 addresses the sources which have financed this Thesis.

5.1 Overall conclusions

This Thesis has presented centralized control strategies which allow the connection of OWFs to the onshore through the DR-HVDC link. The proposed centralized control systems have been implemented by a VSC connected to the DRU station. In this way, the offshore AC-grid frequency control needed for the DRU commutation is guaranteed without the need for changing the WTGSs conventional control systems. The principles of a direct frequency control using reactive power balance at the DRU station bus without a capacitor bank placed at the DRU station have been proposed in Chapter 3. Then, the corresponding frequency control scheme has been derived from the AC-side dynamics a DRU model using the OWF incoming active and reactive powers as inputs. This has also led to derive a new AVM of the DRU system where the dynamics of a capacitor bank placed at the DRU station is not needed to produce the outputs given that the state variables of the DRU model used are the magnitude and angle of the DRU current vector. The stability of the proposed frequency control has been proved through a small-signal analysis while the accuracy of the proposed AVM with embedded frequency control has been verified by a DSM simulation of the system. In the corresponding DSM simulation, a CHB VSC has been used as STATCOM providing a controlled reactive power to satisfy the reactive power balance needed for the frequency control. In addition, the DRU harmonic currents compensation has been implemented by the CHB VSC through taking advantage of an APF scheme embedded in the frequency control proposed in Chapter 2. This has resulted in less than 1 % THD for the offshore voltage without passive AC-filters placed at the DRU station. Then, the proposed centralized control strategy provides comparative advantages in terms of the offshore platforms footprint and OWF installation cost with other centralized controls where, besides a VSC, a high-voltage large capacitor bank is needed to be placed at the

DRU station.

Besides the frequency control and harmonic compensation, the FRT capability of the central VSC-STATCOM has been tested using negative-sequence offshore voltage compensation during the fault conditions in such an application. The control functions have been incorporated to two variants of STATCOM topologies: The 2L-STATCOM and the delta-connected CHB-STATCOM. The 2L-STATCOM can easily handle the fault conditions while a zero-sequence current is needed to be injected inside the delta branch of the CHB-STATCOM to re-balance the clusters' capacitor voltages during the fault conditions. But, similar behaviors have obtained for both converter topologies in point of view of the functionalities. Therefore, the volume/weight and size of the converters are the most relevant factors to be considered. Although using the CHB-STATCOM for the proposed centralized control strategy requires a bit larger initial investment, the CHB-STATCOM is more robust due to its modular structure and does not require passive filters and has lower losses. Therefore, considering the system life-time, the CHB-STATCOM can be the most attractive topology for such an application.

The connection of OWFs to the onshore through a number of series-connected DRU stations has been considered in Chapter 4. Then, the performance of the previously proposed frequency control has been verified for such OWFs scheme by using only one VSC-STATCOM connected to one the DRU stations while compensating all the DRUs' harmonic currents. But, the OWFs start-up has been addressed by proposing a hybrid HVDC system consisting of a low-power VSC (analyzed in Chapter 2) connected in parallel with all the DRUs on DC-side. Then, besides the frequency control and harmonic compensation during the DRUs operation, the start-up power can be provided to the OWFs through the HVDC link while overcoming all the DRU drawbacks. Therefore, given the comparison presented in Chapter 4, the proposed centralized hybrid topology allows smaller offshore platforms in comparison with the traditional VSC-HVDC connected OWFs while in the meantime avoiding several required step-up transformers and AC-array cables in an OWF scheme.

Finally, using the same control system principles, a centralized control strategy of the parallel operation of the DR-HVDC and VSC-HVDC links for OWFs connection has been presented in Chapter 4. During the OWFs start-up procedure, the VSC-HVDC controls the voltage and frequency of the isolated offshore AC-grid. Then, as the proposed grid forming control is based on controlling the offshore voltage through the injection of OWF active powers, when the OWFs start injecting active powers and the VSC-HVDC active current controller reaches its saturation level, the offshore voltage automatically starts increasing and the DR-HVDC starts conducting. During the DR-HVDC link operation, the VSC-HVDC controls the system frequency through satisfying the reactive power balance while the offshore voltage is imposed by the DR-HVDC through satisfying the active power balance. Moreover, by increasing the saturation level of the VSC-HVDC active current controller, the power transmitted through the

VSC-HVDC link can be increased until the maximum power capability of the VSC-HVDC is reached. In any case, the power transmitted through the DR-HVDC link will close the power balance within the OWFs grid. This leads to an optimized system as the total apparent power required for OWFs start-up can be used later during operation. The performance of the proposed control system has been verified through the operation and control of a half-bridge SM based MMC-HVDC for such a parallel system with series-connected DRUs.

5.2 Main contributions

The main contributions of this Thesis are as follows:

- An APF scheme embedded in the centralized frequency control which improves the system performance without the need for placing passive AC-filters at the DRU stations.
- The central VSC power rating analysis which defines its required power demand for the frequency control depending on the system and control parameters and the transients in the system.
- A direct frequency control using reactive power balance at the DRU station bus implemented by the central VSC without a capacitor bank placed at the DRU station.
- An AVM of the DRU system in OWF applications in which the dynamics of a capacitor bank is not needed to produce the outputs.
- Stability analysis which illustrates that operation of the proposed control system is stable.
- A comparison between the 2L-STATCOM and CHB-STATCOM while incorporating the three control functions to them: OWF frequency control, DRU harmonic compensation and negative-sequence offshore voltage compensation, which demonstrates that both converter topologies can effectively contribute to a proper performance of the system during both steady-state and fault conditions.
- A centralized control strategy needed for connection of OWFs to the onshore through a series connection of DRU stations in which only one VSC-STATCOM is needed to be placed at one of the DRU stations.
- A hybrid HVDC system which consists of a low-power VSC connected in parallel with series-connected DRU stations on DC-side which overcomes all the DR-HVDC link drawbacks in OWF applications.

- A comparison between the proposed centralized hybrid topology and the conventional scheme of the VSC-HVDC connected OWFs which demonstrates that several components ,i.e. step-up transformers, and AC-array cables can be removed from the OWFs scheme using the proposed hybrid topology while in the meantime installing lighter/smaller offshore platforms.
- A centralized grid forming control for parallel operation of the VSC-HVDC and DR-HVDC links based on controlling the OWFs voltage by controlling the active power balance at the VSC-HVDC AC-bus which results in an optimized system.

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Acronyms

2L two-level. xiv, xviii, xxi, 15, 38, 55, 57–59, 62–64, 94, 95

AC alternating current. xiii, xvii, 1–8, 16, 18, 20, 22–24, 27, 31, 34, 35, 38–40, 42–45, 52–57, 59, 61–64, 67, 68, 70–74, 76–80, 82, 83, 86, 87, 89–91, 93–96

APF active power filter. xiv, xvii, 1, 7, 8, 16, 25, 32, 33, 35, 38, 39, 54–56, 59, 64, 68–70, 73, 91, 93, 95

AVM average-value model. xiv, xvii, 6, 8, 15, 38, 43–45, 49–51, 53, 54, 64, 93, 95

CHB cascaded H-bridge. xiv, xvii, xviii, xxi, 15, 38, 52–55, 57, 60–65, 71, 72, 74, 93–95

DC direct current. 2, 3, 6, 7, 16–18, 22, 23, 25, 26, 31–35, 38, 39, 43–45, 52, 53, 55–59, 61, 63, 67–69, 71–74, 76, 79–81, 83, 86, 89, 90, 94, 95

DP dynamic phasor. 16, 21, 22, 25, 26, 35, 70

DR diode rectifier. xi, xiii–xv, xvii, xviii, xxi, 3–9, 15–68, 73, 82–85, 89, 91–96

DRU diode rectifier unit. xiv, xviii, xix, 3–8, 15, 16, 37, 38, 43, 55, 64, 67–95

DSC delayed signal cancellation. 57

DSM detailed switching model. xiv, xvii, 8, 15, 38, 49, 52–54, 64, 85, 93

FFT fast Fourier transform. 33

FRT fault ride-through. 8, 38, 55, 56, 58, 62, 94

GPS global positioning system. 5

HVAC high voltage alternating current. 2

HVDC high voltage direct current. xi, xiii–xv, xvii, xviii, xxi, 1–9, 15–96

IGBT insulated gate bipolar transistor. 2, 62, 63, 81, 85

LCC line-commutated converter. 2, 16, 38, 68, 74, 83

MMC modular multilevel converter. xviii, xix, xxi, 6, 7, 77–81, 85–92, 95

MPC model predictive control. 52, 60

MPPT maximum power point tracking. 6

- OWF** offshore wind farm. xi, xiii–xv, xvii–xix, xxi, 1–9, 15–96
- P/f** active power frequency. 5
- P/V** active power voltage. 5
- PCC** point of common coupling. xvii–xix, 5, 6, 16, 17, 20, 25, 35, 38–40, 42, 43, 45, 50, 51, 53–56, 58–63, 68, 69, 72–80, 82, 83, 86, 88–91
- PD-PWM** phase disposition pulse width modulation. 78, 85
- PI** proportional-integral. 18, 24–26, 28, 35, 39, 42, 46, 56, 69, 70, 76, 83
- PLL** phase-locked loop. 4, 6, 16, 18, 24, 39, 40, 53, 55, 57, 69, 71, 76, 77, 83, 86, 87
- Q/f** reactive power frequency. 5, 74
- Q/V** reactive power voltage. 5
- SHC** selective harmonic compensation. xvii, 16, 25, 26, 35, 39, 56, 70
- SM** submodule. 85, 95
- SPWM** sinusoidal pulse width modulation. 58
- STATCOM** static synchronous compensator. xiv, xviii, xxi, 4, 6, 8, 15, 26, 37, 38, 43, 55–65, 67–72, 74, 93–95
- THD** total harmonic distortion. 7, 15, 16, 18, 24, 32, 34, 35, 60, 63, 80, 93
- TR** thyristor rectifier. 5, 6
- VOC** voltage-oriented control. 4, 6
- VSC** voltage source converter. xi, xiv, xv, xvii, xviii, 1–3, 6–9, 16–18, 24–27, 30–35, 37–40, 42, 43, 52–57, 64, 67–70, 74, 76, 77, 80, 82–85, 91–96
- WT** wind turbine. xvii, xxi, 2, 3, 5–8, 16–21, 26–28, 30–35, 40, 53, 58, 91
- WTGS** wind turbine generator system. xi, 2–7, 16, 31, 42, 53, 93