Control of multi-terminal VSC-HVDC systems for combined AC/DC systems to improve power system dynamic performance

Qian Zhang
Iowa State University

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Control of multi-terminal VSC-HVDC systems for combined AC/DC systems to improve power system dynamic performance

by

Qian Zhang

A dissertation submitted to the graduate faculty in partial fulfillment of the requirements for the degree of

DOCTOR OF PHILOSOPHY

Major: Electrical Engineering (Electric Power and Energy Systems)

Program of Study Committee:
James D. McCalley, Co-major Professor
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The student author, whose presentation of the scholarship herein was approved by the program of study committee, is solely responsible for the content of this dissertation. The Graduate College will ensure this dissertation is globally accessible and will not permit alterations after a degree is conferred.

Iowa State University
Ames, Iowa
2020

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DEDICATION

I would like to dedicate this dissertation to my parents and to my husband. This work would not have been possible without their unconditional love and support.
<table>
<thead>
<tr>
<th>TABLE OF CONTENTS</th>
</tr>
</thead>
<tbody>
<tr>
<td>LIST OF TABLES</td>
</tr>
<tr>
<td>LIST OF FIGURES</td>
</tr>
<tr>
<td>NOMENCLATURE</td>
</tr>
<tr>
<td>ACKNOWLEDGEMENTS</td>
</tr>
<tr>
<td>ABSTRACT</td>
</tr>
<tr>
<td>CHAPTER 1. INTRODUCTION</td>
</tr>
<tr>
<td>1.1 Background and Motivation</td>
</tr>
<tr>
<td>1.1.1 Why HVDC</td>
</tr>
<tr>
<td>1.1.2 Overview of HVDC technologies</td>
</tr>
<tr>
<td>1.1.3 Practical vision of continental HVDC connections</td>
</tr>
<tr>
<td>1.1.4 Challenges of operating AC-MTDC systems</td>
</tr>
<tr>
<td>1.2 Research Overview</td>
</tr>
<tr>
<td>CHAPTER 2. LITERATURE REVIEW</td>
</tr>
<tr>
<td>2.1 Frequency Support via VSC-MTDC</td>
</tr>
<tr>
<td>2.1.1 Droop based control</td>
</tr>
<tr>
<td>2.1.2 Optimization based control</td>
</tr>
<tr>
<td>2.2 DC Voltage Control in MTDC Grid</td>
</tr>
<tr>
<td>2.2.1 Master slave control</td>
</tr>
<tr>
<td>2.2.2 Voltage margin control</td>
</tr>
<tr>
<td>2.2.3 DC voltage droop control</td>
</tr>
<tr>
<td>2.3 Overview of Approaches in This Dissertation</td>
</tr>
<tr>
<td>CHAPTER 3. MODELING AND CONTROL OF VSC-MTDC SYSTEMS FOR SYSTEM LEVEL STABILITY ANALYSIS</td>
</tr>
<tr>
<td>3.1 Introduction</td>
</tr>
<tr>
<td>3.2 VSC Model Structure</td>
</tr>
<tr>
<td>3.3 AC-MTDC Power Flow Algorithm</td>
</tr>
<tr>
<td>3.4 VSC-MTDC Dynamic Model</td>
</tr>
<tr>
<td>3.4.1 VSC dynamic model</td>
</tr>
<tr>
<td>3.4.2 MTDC grid dynamic model</td>
</tr>
<tr>
<td>3.4.3 $\alpha\beta$ to $dq$ frame transformation and phase locked loop</td>
</tr>
</tbody>
</table>
CHAPTER 4. PRIMARY FREQUENCY SUPPORT AMONG ASYNCHRONOUS AC SYSTEMS VIA A VSC-MTDC SYSTEM

4.1 Introduction ......................................................................................... 43
4.2 VSC Frequency Control ....................................................................... 45
  4.2.1 Frequency control structure ............................................................... 45
  4.2.2 Frequency control with per unit reference (FC-PU).............................. 45
4.3 Proposed Frequency Control with WAF (FC-WAF) .............................. 47
  4.3.1 Methodology ....................................................................................... 47
  4.3.2 On frequency controllers with converter outages ............................... 50
  4.3.3 On the design of frequency controller parameters ............................. 51
4.4 Case Study .......................................................................................... 53
  4.4.1 Test system ......................................................................................... 53
  4.4.2 Simulation results ............................................................................... 59
  4.4.3 Performance of frequency controls with PVD control ....................... 63
  4.4.4 Simulation of frequency controls with a converter outage ................. 65
4.5 Conclusions ......................................................................................... 65

CHAPTER 5. ADAPTIVE DC VOLTAGE DROOP CONTROL OF VSC-MTDC CONSIDERING DC VOLTAGE DEVIATION AND POWER SHARING

5.1 Introduction .......................................................................................... 67
5.2 PVD control .......................................................................................... 68
5.3 Proposed Margin based Adaptive DC Voltage Droop Control (M-ADP-PVD) 70
  5.3.1 Steady state analysis with fixed droop constants ............................... 70
  5.3.2 Methodology of the proposed M-ADP-PVD ....................................... 73
  5.3.3 On the impact of frequency controllers to DC voltage control ......... 77
5.4 Case Study ............................................................................................ 79
  5.4.1 Test system ......................................................................................... 79
  5.4.2 DC voltage droop control schemes simulated for comparison .......... 80
  5.4.3 Simulation results ............................................................................... 83
  5.4.4 Performance of M-ADP-PVD with frequency controls ................. 93
5.5 Conclusions ......................................................................................... 101

CHAPTER 6. STUDY OF NORTH AMERICAN CONTINENTAL HVDC WITH VSC-MTDC SYSTEMS

6.1 Introduction ........................................................................................ 102
6.2 Modeling of a VSC-MTDC System into the North American Power Grid .... 104
6.3 Primary Frequency Support Between WI and EI through Macrogrid-VSC .... 108
  6.3.1 Simulation scenarios .......................................................................... 108
  6.3.2 On frequency filter and droop controller parameters ....................... 109
  6.3.3 Simulation results ............................................................................... 110
  6.3.4 Impact of communication delay ......................................................... 114
  6.3.5 Sensitivity of frequency control to saturation parameters ............... 116
6.4 DC Voltage Control of VSC-MTDC in Macrogrid-VSC ....................... 119
  6.4.1 Control strategies for comparison ...................................................... 119
  6.4.2 Simulation results ............................................................................... 119
6.4.3 Remarks on the effectiveness of the proposed controls under high renewable penetration ........................................ 128
6.5 Conclusions .................................................................................................................. 129

CHAPTER 7. OVERALL CONTRIBUTIONS AND FUTURE WORK ........................................... 131
  7.1 Overall Contributions ................................................................................................. 131
  7.2 Future Work ............................................................................................................... 134

BIBLIOGRAPHY ............................................................................................................... 136
LIST OF TABLES

<table>
<thead>
<tr>
<th>Table</th>
<th>Description</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>Table 1.1</td>
<td>Comparison of LCC and VSC</td>
<td>5</td>
</tr>
<tr>
<td>Table 1.2</td>
<td>Existing MTDC systems around the world</td>
<td>9</td>
</tr>
<tr>
<td>Table 4.1</td>
<td>UFLS settings used for the case study</td>
<td>55</td>
</tr>
<tr>
<td>Table 4.2</td>
<td>Governor model IEEEG1 parameters</td>
<td>56</td>
</tr>
<tr>
<td>Table 4.3</td>
<td>Major parameters of the VSC-MTDC in the AC-MTDC test system</td>
<td>57</td>
</tr>
<tr>
<td>Table 4.4</td>
<td>Power flow solution related to VSCs</td>
<td>58</td>
</tr>
<tr>
<td>Table 4.5</td>
<td>Active power flow on DC branches</td>
<td>58</td>
</tr>
<tr>
<td>Table 5.1</td>
<td>Modified converter operating point and power limits</td>
<td>81</td>
</tr>
<tr>
<td>Table 5.2</td>
<td>Power flow solution related to VSCs with high DC power transfer</td>
<td>81</td>
</tr>
<tr>
<td>Table 5.3</td>
<td>Active power flow on DC branches with high DC power transfer</td>
<td>81</td>
</tr>
<tr>
<td>Table 5.4</td>
<td>DC voltage control strategies</td>
<td>82</td>
</tr>
<tr>
<td>Table 5.5</td>
<td>Converter data at the outage of VSC1 in the test system</td>
<td>85</td>
</tr>
<tr>
<td>Table 5.6</td>
<td>Converter data at the outage of VSC3 in the test system</td>
<td>89</td>
</tr>
<tr>
<td>Table 6.1</td>
<td>Major parameters of the VSC-MTDC in Macrogrid-VSC</td>
<td>106</td>
</tr>
<tr>
<td>Table 6.2</td>
<td>Power flow solution related to the VSCs in Macrogrid-VSC</td>
<td>107</td>
</tr>
<tr>
<td>Table 6.3</td>
<td>Active power flow on DC branches in Macrogrid-VSC</td>
<td>107</td>
</tr>
<tr>
<td>Table 6.4</td>
<td>Simulation scenarios for the study of primary frequency support</td>
<td>108</td>
</tr>
<tr>
<td>Table 6.5</td>
<td>Total estimated communication delays</td>
<td>114</td>
</tr>
</tbody>
</table>
Table 6.6  Converter data at the outage of VSC1 in Macrogrid-VSC . . . . . . . . . . . 120
Table 6.7  Converter data at the outage of VSC3 in Macrogrid-VSC . . . . . . . . . . . 124
# LIST OF FIGURES

<table>
<thead>
<tr>
<th>Figure</th>
<th>Description</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.1</td>
<td>Main components of different HVDC technologies [1]</td>
<td>3</td>
</tr>
<tr>
<td>1.2</td>
<td>DC grid topologies (converters as black dot) (a) Parallel radial connection (b) Multiple point-to-point connections (c) Meshed DC grid [2]</td>
<td>7</td>
</tr>
<tr>
<td>1.3</td>
<td>Topologies of Zhoushan and Zhangbei VSC-MTDC in China [3]</td>
<td>10</td>
</tr>
<tr>
<td>1.4</td>
<td>ABB 40-node Supergrid [4]</td>
<td>11</td>
</tr>
<tr>
<td>1.5</td>
<td>Different Supergrid topologies [5, 6]</td>
<td>12</td>
</tr>
<tr>
<td>1.6</td>
<td>China’s Hybrid AC-DC grids [7]</td>
<td>13</td>
</tr>
<tr>
<td>1.7</td>
<td>Conceptual transmission designs for the US [8]</td>
<td>16</td>
</tr>
<tr>
<td>3.1</td>
<td>VSC modeling [9]</td>
<td>32</td>
</tr>
<tr>
<td>3.2</td>
<td>Sequential AC-MTDC power flow</td>
<td>34</td>
</tr>
<tr>
<td>3.3</td>
<td>Overall structure of the VSC dynamic model</td>
<td>36</td>
</tr>
<tr>
<td>3.4</td>
<td>MTDC grid model</td>
<td>37</td>
</tr>
<tr>
<td>3.5</td>
<td>Space phasor represented in the $\alpha\beta$-frame [10]</td>
<td>40</td>
</tr>
<tr>
<td>3.6</td>
<td>Stationary $\alpha\beta$ and rotating $dq$ frame coordinates [10]</td>
<td>41</td>
</tr>
<tr>
<td>4.1</td>
<td>VSC dynamic model with supplementary frequency control</td>
<td>46</td>
</tr>
<tr>
<td>4.2</td>
<td>Developed AC-MTDC test system and the operating point</td>
<td>54</td>
</tr>
<tr>
<td>4.3</td>
<td>Governor model IEEEG1 [11]</td>
<td>56</td>
</tr>
<tr>
<td>4.4</td>
<td>Frequencies and load shed% (LS) in A1 (top) and A2 (bottom)</td>
<td>59</td>
</tr>
<tr>
<td>4.5</td>
<td>VSC active power injections</td>
<td>60</td>
</tr>
<tr>
<td>4.6</td>
<td>DC voltages</td>
<td>61</td>
</tr>
<tr>
<td>Figure</td>
<td>Description</td>
<td>Page</td>
</tr>
<tr>
<td>--------</td>
<td>-----------------------------------------------------------------------------</td>
<td>------</td>
</tr>
<tr>
<td>Figure 4.7</td>
<td>DC voltage at VSC3 (CV-L vs. PVD)</td>
<td>63</td>
</tr>
<tr>
<td>Figure 4.8</td>
<td>Frequencies with VSC2 out</td>
<td>64</td>
</tr>
<tr>
<td>Figure 4.9</td>
<td>$U_{dc,1}$ (others are similar)</td>
<td>64</td>
</tr>
<tr>
<td>Figure 4.10</td>
<td>VSC active power injections with VSC2 outage</td>
<td>64</td>
</tr>
<tr>
<td>Figure 5.1</td>
<td>Pilot voltage droop</td>
<td>68</td>
</tr>
<tr>
<td>Figure 5.2</td>
<td>Local voltage droop control</td>
<td>70</td>
</tr>
<tr>
<td>Figure 5.3</td>
<td>Conceptual illustration of power margin</td>
<td>75</td>
</tr>
<tr>
<td>Figure 5.4</td>
<td>Conceptual illustration of power headroom</td>
<td>76</td>
</tr>
<tr>
<td>Figure 5.5</td>
<td>AC-MTDC test system with modified steady state operating point</td>
<td>80</td>
</tr>
<tr>
<td>Figure 5.6</td>
<td>VSC active power injections with outage of VSC1 (rectifier)</td>
<td>84</td>
</tr>
<tr>
<td>Figure 5.7</td>
<td>Adaptive droop constants with outage of VSC1 (rectifier)</td>
<td>86</td>
</tr>
<tr>
<td>Figure 5.8</td>
<td>DC voltages with outage of VSC1 (rectifier)</td>
<td>88</td>
</tr>
<tr>
<td>Figure 5.9</td>
<td>VSC active power injections with outage of VSC3 (inverter)</td>
<td>90</td>
</tr>
<tr>
<td>Figure 5.10</td>
<td>Adaptive droop constants with outage of VSC3 (inverter)</td>
<td>91</td>
</tr>
<tr>
<td>Figure 5.11</td>
<td>DC voltages with outage of VSC3 (inverter)</td>
<td>92</td>
</tr>
<tr>
<td>Figure 5.12</td>
<td>System frequencies with M-ADP-PVD (Scenario 1, VSC2 out)</td>
<td>94</td>
</tr>
<tr>
<td>Figure 5.13</td>
<td>$U_{dc,1}$ (others are similar) with M-ADP-PVD (Scenario 1, VSC2 out)</td>
<td>94</td>
</tr>
<tr>
<td>Figure 5.14</td>
<td>VSC active power injections with M-ADP-PVD (Scenario 1, VSC2 out)</td>
<td>95</td>
</tr>
<tr>
<td>Figure 5.15</td>
<td>DC voltage droop constants with M-ADP-PVD (Scenario 1, VSC2 out)</td>
<td>95</td>
</tr>
<tr>
<td>Figure 5.16</td>
<td>System frequencies with M-ADP-PVD (Scenario2, VSC2 out)</td>
<td>96</td>
</tr>
<tr>
<td>Figure 5.17</td>
<td>$U_{dc,1}$ (others are similar) with M-ADP-PVD (Scenario2, VSC2 out)</td>
<td>96</td>
</tr>
<tr>
<td>Figure 5.18</td>
<td>VSC active power injections with M-ADP-PVD (Scenario 2, VSC2 out)</td>
<td>97</td>
</tr>
<tr>
<td>Figure 5.19</td>
<td>DC voltage droop constants with M-ADP-PVD (Scenario 2, VSC2 out)</td>
<td>97</td>
</tr>
</tbody>
</table>
Figure 5.20  Frequencies and load shed% (LS) in A1 (top) and A2 (bottom) with M-ADP-PVD (Gen102 out) ................................................. 98
Figure 5.21  VSC active power injections with M-ADP-PVD (Gen102 out) ............. 99
Figure 5.22  DC voltages with M-ADP-PVD (Gen102 out) ............................... 99
Figure 5.23  Adaptive DC voltage droop constants with M-ADP-PVD (Gen102 out) . . 100
Figure 6.1   Interconnections Seam Study [12] .................................................. 103
Figure 6.2   HVDC Macrogrid network with VSC-MTDC ("Macrogrid-VSC") .......... 105
Figure 6.3   VSC-MTDC system operating condition in Macrogrid-VSC .................. 106
Figure 6.4   The CPAAUT type LCC-HVDC auxiliary signal controller [11] .............. 109
Figure 6.5   System frequencies ................................................................. 110
Figure 6.6   Active power on cross-seam LCC-HVDCs .................................. 111
Figure 6.7   VSC active power injections ....................................................... 112
Figure 6.8   DC terminal voltages ............................................................... 113
Figure 6.9   WI frequency with delay ......................................................... 115
Figure 6.10  $U_{dc}$ at VSC1 with delay ...................................................... 115
Figure 6.11  System frequencies with saturation limits .................................. 116
Figure 6.12  VSC active power injections with saturation limits ......................... 117
Figure 6.13  DC terminal voltages with saturation limits ................................ 118
Figure 6.14  VSC active power injections with the outage of VSC1 (inverter) ......... 121
Figure 6.15  DC voltages with outage of VSC1 (inverter) ................................ 122
Figure 6.16  Adaptive droop constants with outage of VSC1 (inverter) ............... 123
Figure 6.17  VSC active power injections with outage of VSC3 (rectifier) ............ 125
Figure 6.18  DC voltages with outage of VSC3 (rectifier) ................................ 126
Figure 6.19  Adaptive droop constants with outage of VSC3 (rectifier) ............... 127
**NOMENCLATURE**

<table>
<thead>
<tr>
<th>Abbreviation</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>ADP-L</td>
<td>Adaptive local voltage droop control</td>
</tr>
<tr>
<td>ADP-PVD</td>
<td>Adaptive pilot voltage droop control</td>
</tr>
<tr>
<td>CV-L</td>
<td>Conventional local voltage droop control</td>
</tr>
<tr>
<td>CV-PVD</td>
<td>Conventional pilot voltage droop control</td>
</tr>
<tr>
<td>EI</td>
<td>Eastern Interconnection</td>
</tr>
<tr>
<td>FC-PU</td>
<td>Frequency control using per unit frequency as reference</td>
</tr>
<tr>
<td>FC-WAF</td>
<td>Frequency control using weighted average frequency as reference</td>
</tr>
<tr>
<td>HVDC</td>
<td>High voltage direct current</td>
</tr>
<tr>
<td>LCC</td>
<td>Line commutated converter</td>
</tr>
<tr>
<td>M-ADP-PVD</td>
<td>Margin based adaptive pilot voltage droop control</td>
</tr>
<tr>
<td>MTDC</td>
<td>Multi-terminal HVDC</td>
</tr>
<tr>
<td>pu</td>
<td>Per unit</td>
</tr>
<tr>
<td>PVD</td>
<td>Pilot voltage droop</td>
</tr>
<tr>
<td>UFLS</td>
<td>Under frequency load shedding</td>
</tr>
<tr>
<td>VSC</td>
<td>Voltage source converter</td>
</tr>
<tr>
<td>VSC-MTDC</td>
<td>Voltage source converter based multi-terminal HVDC</td>
</tr>
<tr>
<td>WI</td>
<td>Western Interconnection</td>
</tr>
</tbody>
</table>
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ABSTRACT

Renewable energy resources are being integrated into power systems around the world and replacing conventional generators to reduce carbon emission. Voltage source converter based multi-terminal HVDC (VSC-MTDC) is identified to be a promising technology to facilitate the integration and utilization of large amounts of renewable energy resources across large geographic areas. Many challenges exist in operating such a combined AC-MTDC power system under a high renewable future. This dissertation addresses three challenging problems in terms of improving dynamic performance of an AC-MTDC system, which are AC system frequency control, DC voltage control, and implementation of VSC-MTDC controls on practical large scale systems.

The first part of this dissertation focuses on designing frequency control for VSC-MTDC to provide frequency support among asynchronous AC systems for AC side events. A global frequency control scheme is designed and is shown to have superior performance to the traditional local frequency droop scheme with improved frequency nadir and reduced impact to the DC voltage profile. Consequently, the amount of load shedding and the need of total online spinning reserves are reduced. In the second part of this dissertation, an adaptive DC voltage droop control strategy is developed for converter outages taking both DC voltage deviation and power sharing into consideration. The proposed control enables accurate power sharing based on converter operating conditions, with the capability to differentiate the outage of a rectifier from that of an inverter. As a result, converter overloading is avoided and transient DC voltage profiles are improved. Moreover, the proposed control has the flexibility to adjust the strength of DC voltage regulation. In the last part of our work, in order to investigate the effectiveness of the designed frequency and DC voltage controls on practical large scale systems, a six terminal VSC-MTDC system is modeled into a 100k bus North American power system with an HVDC overlay, with which the advantages of the developed controls are demonstrated. With the proposed control strategies for VSC-MTDC
systems, the reliability of the overall AC-MTDC system is improved in terms of both AC and DC side contingencies.
CHAPTER 1. INTRODUCTION

1.1 Background and Motivation

1.1.1 Why HVDC

In order to achieve decarbonization targets for a sustainable future, there has been a global trend to integrate large-scale renewable energy resources into the existing power grid. Rich renewable energy resources are typically located in unpopulated remote areas far from load centers, which needs transmission to make them accessible. HVDC transmission possesses multiple economic, environmental and technical advantages over its counterpart, AC transmission, which is the major transmission backbone currently. Among these benefits, the most important ones are 1) capability to interconnect asynchronous AC systems, 2) lower transmission losses and 3) higher power controllability. Due to these features, HVDC transmission has been a major player in long distance bulk power transmission, and the only viable technical solution to long submarine cable connection and interconnection of asynchronous AC systems \[13, 14\]. Therefore, HVDC is expected to be the key technology to facilitate the integration of large-scale renewable energy over a large-span of geographic area, both onshore and offshore.

1.1.2 Overview of HVDC technologies

1.1.2.1 LCC-HVDC vs VSC-HVDC

HVDC transmission can be classified by the type of converters used at the AC/DC conversion terminals \[10, 15\]. There are conventional line-commutated converters (LCC) and the more recent voltage-source converters (VSC). HVDC lines with LCC and VSC at the AC/DC conversion terminals are herein called LCC-HVDC and VSC-HVDC, respectively. LCC is composed of thyristors which does not have the gate-turn-off capability, meaning that the commutation process is dictated
by the AC system and initiated by the reversal of the AC voltage polarity. On the other hand, VSC uses fully controllable IGBTs which have the gate-turn-off capability. Since the commutation process is fully controllable, VSCs are also called self-commutated converter, or forced commutated converter. Components of typical LCC and VSC converter stations are shown in Figure 1.1. Due to the fundamental difference in the commutation process, VSC-HVDC has the following major benefits over LCC-HVDC [16]:

1) The operation of VSC-HVDC does not require a voltage source on the AC side, thus is possible to connect weak and islanded AC grid, whereas LCC-HVDC relies on a strong AC system to operate stably because its commutation process is dictated by the AC system.

2) A VSC can either absorb or inject reactive power at the converter terminal, providing reactive power support benefits to the AC grid, whereas LCC-HVDC always consumes reactive power at its terminals requiring shunt compensation filters. Moreover, the control of reactive power in VSC is independent from the control of active power whereas an LCC always consumes reactive power about 50% to 60% of its active power [17].

3) VSC can still remain in operation under AC side faults with fast enough control to avoid unacceptable overcurrent caused by the voltage dips in the AC system, whereas LCC-HVDC is vulnerable to AC side faults since it may suffer commutation failure due to low AC side voltages and cause temporary power transmission interruption.

Besides the difference in commutation process, another important distinction between the two types of converters is in terms of DC side voltage and current waveforms. LCC retains the same current direction on the DC side, acting as a current source converter, thus power reversal is realized by reversing the DC voltage polarity. A VSC retains the same voltage polarity on the DC side thus the power reversal is achieved by reversing the current direction. The difference in the source types on the DC side yields the following comparisons of the two technologies [16]:

1) The flexibility in power reversal makes VSC more suitable to multi-terminal configurations as it uses a common DC voltage which makes the parallel connections easy to build and control.
Figure 1.1: Main components of different HVDC technologies [1]
2) For DC side faults, LCC itself can limit the overcurrent easily by its DC current control function and thyristor valve control, thus no circuit breakers are needed. VSC on the other hand will need to open either AC or DC circuit breakers to interrupt the fault since the DC current will continue to flow into the fault due to the diodes in the IGBTs.

LCC-HVDC has been in operation since 1954, whereas the first VSC-HVDC link was not put in operation until 1999 between Gotland and Sweden [18] after fully controllable IGBTs becomes commercially available at high power ratings. Previously, the major factor limiting the application of VSC technology is its relatively lower capacity as compared to the LCC (up to 10GW at ±1100kV to date [19]). However, with the fast developing power electronics technology, and the recently developed modular multi-level converter (MMC) topology, the capacity of the VSC has increased dramatically and is expected to reach higher levels. The basic idea of MMC is to use series and parallel combinations of switching valves to increase the current and voltage ratings to achieve higher power capacity. To date, the commercially available VSC now reaches power capacity of 3000MW and DC voltage level of ±640kV [20].

A side-to-side comparison of LCC and VSC technologies on the aforementioned features, along with some others, is summarized in Table 1.1.

### 1.1.2.2 Point-to-point HVDC vs multi-terminal HVDC

Traditionally, HVDC transmission is two-terminal application where two nodes in either the same AC system or different AC systems are connected via an HVDC link, so called “point-to-point” links. To this date most HVDC transmissions currently in operation are still point-to-point links. However, with the increasing need to integrate large amount of renewable energy resources, it is preferable to incorporate an HVDC system layer with an HVDC network (so called “DC grid”) that interacts with the rest of the system, rather than as mere injections into the existing AC systems [21]. These HVDC systems have three or more connections to the AC systems, which is referred to as multi-terminal HVDC (MTDC) systems. MTDC systems are more favorable due to the following reasons:
Table 1.1: Comparison of LCC and VSC

<table>
<thead>
<tr>
<th>Feature</th>
<th>LCC</th>
<th>VSC</th>
</tr>
</thead>
<tbody>
<tr>
<td>Capacity</td>
<td>High (up to 10GW)</td>
<td>Relatively low (up to 3GW, but has potential to increase thanks to MMC topology)</td>
</tr>
<tr>
<td>Power Reversal</td>
<td>Hard (need to alternate DC voltage polarity)</td>
<td>Easy (via DC current reversal)</td>
</tr>
<tr>
<td>Converter Element</td>
<td>Thyristor based (turn-on capability only, location needs to be electrically stiff)</td>
<td>IGBT based (turn-on/off capability, capable of connecting weak and passive systems)</td>
</tr>
<tr>
<td>Active Power Controllability</td>
<td>Yes</td>
<td>Yes</td>
</tr>
<tr>
<td>Reactive Power Controllability</td>
<td>No (LCC always consumes reactive power)</td>
<td>Yes (reactive power can be supplied at both terminals)</td>
</tr>
<tr>
<td>Harmonic &amp; AC Filters</td>
<td>High harmonics need AC filters</td>
<td>Low harmonics, no AC filters needed</td>
</tr>
<tr>
<td>Black Start Capability</td>
<td>No</td>
<td>Yes</td>
</tr>
<tr>
<td>Short Circuit Ratio</td>
<td>Need $&gt;2$</td>
<td>No requirement</td>
</tr>
<tr>
<td>Renewable Interconnection</td>
<td>No</td>
<td>Yes</td>
</tr>
<tr>
<td>Converter Size</td>
<td>Large footprint</td>
<td>Small footprint</td>
</tr>
<tr>
<td>Multi-terminal Application</td>
<td>Very limited (up to 3 practically)</td>
<td>No limit. MTDC can reduce the number of costly converter stations and allow easier tapping of generation and loads in the middle of the lines</td>
</tr>
<tr>
<td>Converter Losses</td>
<td>Low</td>
<td>High (but can be reduced by improving converter topology)</td>
</tr>
</tbody>
</table>

1) Multi-terminal configuration can cover larger geographic spans to harvest various resources along the routes and balance the variability of renewable energy resources caused by different weather [22, 23];

2) Load diversity across different time zones can be accommodated [22, 24];

3) The MTDC system can effectively reduce the number of receiving converter stations for wind farm integration compared to the point-to-point connections [25];
4) MTDC systems can facilitate the energy trading between various AC systems in a coordinated way by defining the participation of converter stations to ancillary services [26].

5) Geographic and resource diversity provide additional reliability to the system, such as frequency response sharing and AC voltage support [23, 24].

6) Easier to coordinate than multiple point-to-point links to maintain generation and load balance [2].

Moreover, although the current renewable energy resources are not equipped with active power control capabilities, it is expected that they will do so in the future when the penetration level of renewables is much higher, so that they can provide additional reliability support to the grid. A multi-terminal HVDC grid interconnecting renewable resources from diverse spatial locations can combine the network controllability with generation flexibility to maximize the reliability benefits.

There are three major configurations for connecting HVDC terminals in a MTDC system [26, 27]:

1) Series connection, where the converter stations are connected in a loop.

2) Parallel connection with radial network, where the converter stations are connected in parallel forming a radial DC network.

3) Parallel connection with meshed network, where the converter stations are connected in parallel forming a meshed DC network.

In the series connection architecture, all converters share the same DC current and converter active power injections are proportional to their voltage ratings, which makes it difficult for insulation coordination thus complicated for further expansion of the DC grid. It’s therefore not discussed further. On the other hand, in the parallel architecture, all converters operate at the same voltage level. The converter currents are proportional to their power ratings, making it easier to control and flexible for future expansion [26]. A meshed DC grid provides more redundancy thus
is more reliable than the radial parallel architecture. To achieve a meshed DC grid, the power flows in the DC link would have to reverse frequently depending on the real time availability of renewable resources and potential market constraints. As previously mentioned, VSC can achieve power reversal without altering the DC voltage polarity, thus is the only viable solution for a meshed DC grid. This situation is different for the existing LCC based multi-terminal HVDC systems since the power flows are unidirectional [26].

Figure 1.2 demonstrates different DC grid configurations [2]. Configuration (a) and (c) correspond to parallel connection with radial network and meshed network respectively. As explained
above, the parallel radial connection as shown in configuration (a) does not offer redundancy, and is not really a “grid”. This topology is typically useful as an alternative to a single AC line, as a connection between to asynchronous zones with an additional connection, e.g. for an offshore wind farm. Configuration (c) is a meshed DC system with a number of connections to the AC system, which offers redundancy and thus more reliable. In configuration (b), all buses are AC buses, and the DC grid is composed of multiple point-to-point links with two converter stations. All DC lines are fully controllable and can have mix of LCC and VSC HVDC lines operating at different ratings and voltage levels. It is easier to incorporate existing DC lines into the DC grid, however, it needs a complex control coordination between the HVDC lines to keep the main frequency in case of different isolated AC grids. Since it is composed of multiple point-to-point links, it is not really a multi-terminal configuration. However, it is one definition of "DC grids" proposed by the authors in [2] and it is worth mentioning as a comparison to the actual multi-terminal configuration (c) as detailed below.

A critical issue with configuration (b) is the amount of converters needed is very large, as compared to the meshed DC grid in (c). A rule of thumb for normal large grids requires the number of branches to be 1.5 time of the number nodes. This would require \((2 \times 1.5 \times DC\text{nodes})\) converters, whereas the configuration (c) only requires \((DC\text{nodes})\) converters. This is a critical factor to consider since the converters are the most expensive, sensitive and lossy components of the DC grid. If redundancy is needed for the DC grid itself, the meshed DC grid is the only viable option [2].

**Existing MTDC systems around the world** Although not many, there are a few MTDC systems in operation around the world [27, 28], as shown in Table 1.2. As can be seen from Table 1.2, China has made first significant steps towards a practical realization of HVDC grids using the advanced VSC technology - the Nan’ao four terminal MTDC was put in operation in 2013 [25], the Zhoushan five terminal MTDC from 2014 [29, 30], and the most recent four terminal Zhangbei HVDC project [30, 31]. The Zhangbei HVDC grid optimizes the use of renewables while ensuring the reliability of power supply. It is a unique pilot project partnered with ABB utilizing
Table 1.2: Existing MTDC systems around the world

<table>
<thead>
<tr>
<th>Location</th>
<th>Year</th>
<th>Capacity (MW)</th>
<th>Technology</th>
<th>Terminals</th>
<th>DC voltage level (kV)</th>
<th>Length (km)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sardinia - Corsica - Italy (SACOI)</td>
<td>1967, 1988, 1992</td>
<td>300</td>
<td>LCC</td>
<td>3</td>
<td>+200</td>
<td>385</td>
</tr>
<tr>
<td>Hydro Quebec - New England</td>
<td>1990-1992</td>
<td>2000</td>
<td>LCC</td>
<td>3</td>
<td>±450</td>
<td>1480</td>
</tr>
<tr>
<td>Nan’ao</td>
<td>2013</td>
<td>100, 50, 200</td>
<td>VSC</td>
<td>4</td>
<td>±160</td>
<td>40.7</td>
</tr>
<tr>
<td>North-East Agra</td>
<td>2017</td>
<td>6000</td>
<td>LCC</td>
<td>4</td>
<td>±800</td>
<td>1728</td>
</tr>
<tr>
<td>Zhoushan</td>
<td>2014</td>
<td>400, 300, 100, 100</td>
<td>VSC</td>
<td>5</td>
<td>±200</td>
<td>140</td>
</tr>
<tr>
<td>Zhangbei</td>
<td>2019</td>
<td>1500, 1500, 1500, 300</td>
<td>VSC</td>
<td>4</td>
<td>±500</td>
<td>648</td>
</tr>
<tr>
<td>Atlantic Wind Connection</td>
<td>2021</td>
<td>7000</td>
<td>VSC</td>
<td>6</td>
<td>±800</td>
<td>560</td>
</tr>
</tbody>
</table>

the most advanced voltage source converter technology with four interconnected stations in a ring network, delivering up to 4500MW of clean energy. Both Zhoushan and Zhangbei HVDC grid utilizes the most advanced hybrid HVDC breaker that can interrupt the DC side fault, which was a major bottle neck for developing the HVDC grid previously [32]. The topologies of Zhoushan and Zhangbei HVDC grids are shown in Figure 1.3.

1.1.3 Practical vision of continental HVDC connections

In order to achieve aggressive renewable energy integration goals to reduce carbon emissions, research has been conducted around the world to analyze the feasibility and benefits of continental HVDC connections. In the following, the continental HVDC developments in Europe, China, and North America are briefly discussed.

1.1.3.1 The European Supergrid

In order to respond to the Intergovernmental Panel on Climate Change (IPCC) issued in October 2018, the European Union (EU) sets the goal to achieve net-zero greenhouse gas emissions by 2050. This led to increasing deployment of renewable energy resources (RES), mostly wind and
Figure 1.3: Topologies of Zhoushan and Zhangbei VSC-MTDC in China [3]
solar energy. With high penetration of renewables, operating constraints such as transmission congestion can sometimes cause curtailment of the RES. HVDC interconnections between different EU countries can mitigate the transmission congestions by sharing energy surplus across neighboring countries. Several HVDC grid models are proposed in Europe, including the offshore supergrid in Northern Europe mainly to harness the wind from NorthSea, the HVDC grid to integrate CSP and PV from Mediterranean South and East countries, and finally combination of the two to form the so called European “Supergrid” [2, 6, 33].

Several topologies have been proposed for a meshed European Supergrid. Figure 1.4 shows a 40-terminal HVDC grid proposed and studied by ABB, with 30GW infeed of renewable power. The main generation resources are the solar energy from the Sahara desert in the south (19200MW), hydro power in the Northern Europe (2200MW), and wind power from Western Europe (7800MW). All terminals are designed to be VSC in bipolar configuration with metallic returns. Munich is used as the only DC slack bus for DC voltage control, while the rest in power control mode. The study
concludes that such a large DC grid is feasible in terms of load flow, but it is desired to have multiple converter stations participating in DC voltage controls in case power unbalance on the DC side to share the burden of the slack converter in Munich and mitigate the impact to the AC system connected to it. Two other possible configurations of the European Supergrid have also been proposed, which are shown in Figure 1.5.

An open electricity market is required to accommodate these updated transmission networks. Moreover, due to huge financial, political and social differences between the Europe, North African, and Middle East countries, the development of such a project becomes quite complex. It is especially challenging from a political point of view in terms of ensuring energy supply security from RESs from different regions. Therefore, a strict collaboration between involved nations is needed and common HVDC grid codes are essential.

### 1.1.3.2 HVDC in China

In order to move the carbon emission peak to earlier than 2030, and achieve the goal of 2°C temperature rise according to the United Nations report (2014), the CO2 emission intensity of Chinese power sector needs to be reduced by 90% by the year 2050 [34]. The electricity sector
emits more than 40% of China’s CO2 from fossil fuel combustion. Therefore, increasing the share of clean energy or renewables is vital. The fact that the load centers (in the south and east) are far away from the renewable energy resources (in the north and west) favors the development of ultra high voltage (UHV) transmissions due to its long distance bulk power transfer capability and high efficiency with low power losses.

Study in [34] performs quantitative analysis to identify the most effective regional subsidy and inter-regional transmission capacity levels to facilitate integration of clean energy resources. The study finds that even with the most effective clean energy subsidies, the share of coal-fired plants is still over half of the generation capacity because of the insufficient transmission capacity. It concludes that there is strong inter-relationship between the inter-regional transmission capacity and the clean energy penetration level. The study shows that the most effective inter-regional transmission capacity is 2300GW, with which the clean energy share can be increased to 59.6% in the Chinese power sector.
Figure 1.6 shows the existing HVDC lines and those under construction in China. A total of more than 30,000 km of UHV lines have been built across China [7], with the largest link being the Changji-to-Guquan 1100kV HVDC link from the west to the east [35]. However, for stability concerns, this link has carried less than one-quarter of its designed capacity of 12GW on average, which causes curtailment of abundant renewable energies in the west. Other UHV lines have similar situation, which is worsened by the fact that eastern provinces dont have enough incentives to import these clean energies carried on these UHV lines. The ultimate solution to this issue is to increase the redundancy of these HVDC lines, and also strengthening the receiving region with UHV AC lines to help distribute the imported electricity, although some experts argue unifying Chinas grid would make it far more vulnerable to cascading blackout [7].

1.1.3.3 The U.S. Macrogrid

There are three asynchronous interconnections in the North America the Eastern Interconnection (EI), the Western Interconnection (WI), and the Texas Interconnection. The existing seven back to back HVDC facilities enables transfer capability of 1300MW between EI and WI, which is very low as compared to size of these large interconnections (70GW in EI and 25 in WI). Several factors drive the need of developing more cross seam transmission, among which the main one is to facilitate the integration and utilization of renewable energy resources to achieve decarbonization. Recent studies have shown that significant economic benefits can be achieved by developing additional cross-seam HVDC transmission between these asynchronous interconnections [36, 37, 8]. Some examples of the economic benefits include access of load centers to richer renewable resources, interregional sharing of most economic energy resources on a diurnal basis, and interregional sharing of capacity to satisfy each regions annual peak and consequential decrease in capacity investments. The authors in [38] shows that the annual savings of US power consumers with a national HVDC transmission overlay is estimated to be US$47.2 billion, which is almost three times of the cost of the HVDC transmission. Study in [24] shows that one of the HVDC configuration, the so called
“Macrogrid”, could yield a benefit-to-cost ratio of 2.5 under carbon policy scenarios with growing carbon taxes.

Several topologies have been proposed for the design of a North-American HVDC macrogrid overlay [39, 37, 40]. Figure 1.7 shows four conceptual transmission designs. Design 1 is a reference scenario without any upgrades on the existing capacity of B2B HVDC facilities between EI and WI, and new AC transmission and generation are co-optimized to minimize system wide costs. In Design 2a and 2b, the capacity of the existing B2B facilities are co-optimized along with other investments in AC transmission and generation. In addition, Design 2b also builds three HVDC transmission lines. The terminal locations are chosen to maximize cross-seam transmission value. Finally, in Design 3, a continental HVDC overlay, the so called Macrogrid is built, co-optimized with other investments in AC transmission and generation. The four designs are studied under two different renewable penetration levels and policies 50% with carbon policy and 40% with current policy (no carbon emission price).

In terms of converter technology, LCC is utilized for the HVDC lines in Design 2b. In Design 3, a mix of LCCs and VSCs is proposed. The LCC terminals are necessary to enable high capacity transfers between EI and WI during non-coincident peak times. The terminals are selected based on locations with highest annual load diversity as presented in [41]. The VSCs are also included in the design to facilitate ramping capability and dynamic/stability support, and are placed in locations with rich wind and solar resources.

In addition to the economic benefits, network configuration of HVDC lines will result in additional benefits, such as reliability and ancillary service benefits. A study in [24] made the initial effort to develop a 100k bus transient stability model for the Macrogrid interconnecting EI and WI (Design 3), although with the assumption that all HVDC sections are LCC-HVDC links. The study demonstrated preliminary reliability benefits to provide primary frequency support between the interconnections via HVDC. However, as the authors rightly pointed out in the paper, LCC-HVDC may not be the optimal technology to realize such an HVDC overlay; further research is
Figure 1.7: Conceptual transmission designs for the US [8]
necessary to model VSC into the Macrogrid and study its potential reliability benefits, and this has been studied in this dissertation. More details can be found in Chapter 6.

1.1.4 Challenges of operating AC-MTDC systems

From the previous sections, it can be seen that HVDC technology, especially VSC-MTDC would play a major role in future’s power system to facilitate the integration of large amounts of renewable energy resources to achieve the low carbon goal. Similar to the existing AC systems, it is likely that the MTDC systems will be developed in an organic way, emerging from single links to regional systems, inter-regional systems, then continental and eventually inter-continental systems. Operation and control of the future VSC-MTDC systems will have major impact on the connected AC systems, and vice versa. Therefore, the future operation procedure needs to be able to incorporate the MTDC grid as a system layer that interacts with the rest of the system, rather than as mere injections into the existing AC grids [21]. The multi-terminal configuration requires well-designed and coordinated control strategies to make the overall system more reliable and robust. Therefore, the control of MTDC grids will be a challenging but essential task for the reliability of the overall power system. Generally, the challenges associated with operating large AC interconnections also apply to the operation of MTDC grids, for example, with respect to power flow control, dynamic performance improvement, stability study and protection design [28]. In this work, we mainly focus on the aspect of dynamic performance improvement in an AC-MTDC network. To this end, there are three major challenges, as briefly explained below.

Frequency control in AC systems As large amount of renewable energy resource is displacing the conventional generation, the overall system inertia is reducing in AC systems, posing challenges for frequency control under high renewable penetration levels. The reduced system inertia increases the rate of change of frequency in the primary frequency control horizon, which reduces the frequency nadir. Moreover, as conventional generators are retired, the intermittency of renewables requires sufficient online reserves to ensure reliable operation in case of a generator loss event. Ref. [42] shows that the frequency nadir of the Western Interconnection reduces from
59.7Hz to 59.4Hz with wind penetration increasing from 20% to 80% for the same generator loss. Reliable interconnection frequency response requires that the frequency be arrested and stabilized above the highest set-point for UFLS. The recommended UFLS first-step limitations for the three interconnections in the United States are 59.5Hz for WI and EI, and 59.3Hz for ERCOT [43]. It is obvious that maintaining adequate frequency response for large power systems is becoming more important and complex. Therefore, newer and less familiar sources need to be explored to provide frequency support to the system [42]. Although VSC-MTDC is a transmission technology, when using VSC-MTDC to connect asynchronous AC systems, it is a great asset which can be utilized to provide frequency support among asynchronous AC systems utilizing its fast and flexible power control capabilities. VSC-MTDC has been shown to be able to provide both primary and secondary frequency control. In this work, we focus on the primary frequency control time period, which is until about 30s after the initiation of the event since it is the most critical stage to ensure the stability of the grid.

**DC voltage control in VSC-MTDC** Maintaining a stable DC voltage is an essential and the most crucial task for reliable operation of an MTDC grid, since over voltage can cause damage to system equipment, and undervoltage can cause loss of converter control capabilities which could jeopardize the stability of the whole MTDC grid. For a DC side event such as outage of a converter, the transients of DC voltages are much faster than the AC side dynamics because the DC side capacitors only store a fraction of energy that stored in rotating masses of AC systems, which makes the control of DC voltage more challenging. Therefore, an effective and reliable DC voltage control strategy for the VSCs is a must. Many techniques have been proposed in the literature on this aspect as detailed in Section 2.2. Each of them has their own disadvantages, and further research is needed to design more robust and reliable schemes.

**Implementation on practical large systems** Although many controls have been proposed to tackle the aforementioned two challenging problems, very few has been evaluated on practical large scale systems. In order to validate the effectiveness and scalability of any control scheme, it is
preferred to develop VSC-MTDC models and controls that can be utilized in a commercial grade software widely used in the industry, and implement it on practical large systems, so that conclusions and guidelines can be provided for practical power system planning. Compared to synchronous generators, the time responses of VSC-MTDC systems are orders of magnitude faster. To integrate the VSC-MTDC system into large scale power systems, there are two computationally feasible approaches. One is to model the VSC-MTDC system in high-fidelity with details of switching dynamics using electromagnetic transient modeling tools, and the AC systems using the traditional positive sequence analysis tools to capture electromechanical transients. The other approach is to use simplified average models of VSC-MTDC which preserves relevant dynamic behavior and integrate it directly into the existing transient stability analysis tools used for AC systems. The commercial grade software for both of these approaches are still maturing. The latter approach is used in this work with user defined models.

1.2 Research Overview

To address the aforementioned three challenging problems in terms of improving the dynamic performance of an AC-MTDC system (AC system frequency control, DC voltage control and implementation on practical large systems), three major efforts are made in this dissertation, as briefly explained below.

First, in Chapter 4, a frequency droop control strategy is designed for the VSC-MTDC to provide primary frequency support among asynchronous AC systems for AC side contingencies, to tackle the challenge of reducing system inertia within AC systems caused by the displacement of conventional generators with converter interfaced renewable generation. The proposed frequency control utilizes a reference signal calculated from global measurements which reflects the overall frequency dynamics of all connected asynchronous AC systems. The proposed control outperforms the traditional frequency droop scheme by reducing impact on the DC voltage profile and improving frequency nadir. In addition, the frequency control performance is evaluated with different types
of DC voltage control and shows consistent performances. The proposed frequency control can also be well adapted for converter outages.

Second, in Chapter 5, an adaptive DC voltage droop control is designed taking both power sharing and DC voltage deviation into consideration in case of DC side events such as converter outages. The proposed adaptive DC voltage control adapts the DC voltage droop constants based on the current operating conditions of the converters, so that converters with large power margins share more power unbalance after a converter outage. Converters with larger DC voltage deviations tend to prioritize DC voltage control over power sharing, and the strength of DC voltage regulation can be flexibly adjusted. Moreover, the developed control has the capability to differentiate the converter outage direction (i.e. differentiate the outage of a rectifier from that of an inverter), yielding a more accurate assessment of power sharing capabilities of the healthy converters. Consequently, DC voltage transients have less fluctuations and shorter settling times as compared to the existing adaptive DC voltage droop control schemes. Additionally, the impact of the proposed DC voltage control on different frequency control strategies is also investigated.

Lastly, in Chapter 6, the two proposed control strategies are evaluated on a 100k bus practical North American power system with a continental HVDC overlay showing their scalability. A six-terminal VSC-MTDC is modeled into the existing Macrogrid connecting the Western Interconnection and Eastern Interconnection in the US. The proposed primary frequency control is applied and validated on the developed model. The impact of realistic communication delays on the performance of the designed control and the impact of control power limits on different frequency control strategies for future integration with market signals are evaluated. The proposed margin based adaptive DC voltage control is also implemented on the developed Macrogrid model with VSC-MTDC, showing its superior performance to various existing DC voltage control schemes with more accurate power sharing and improved transient DC voltage profiles. With the proposed controls of the VSC-MTDC, the overall reliability of the AC-MTDC system is improved for both AC and DC side contingencies.
CHAPTER 2. LITERATURE REVIEW

2.1 Frequency Support via VSC-MTDC

VSC-MTDC can be used to provide both primary and secondary frequency control. This dissertation focuses on primary frequency control which is till about 30s after the disturbance, as it is the most critical stage to ensure the stability of the grid. The main idea behind primary frequency control with VSC-MTDC is to adjust the power exchanges of VSC with the AC systems at the converter terminals in a coordinated way in response to frequency deviations, sometimes accompanied with an inertia emulation gain to provide some derivative response.

The majority of the work in the current literature utilizes variants of droop based control, in which the basic idea is to apply a supplementary power-frequency proportional droop control into the active power control loop of the VSC. For VSC-MTDC grid with DC voltage droop control, this supplementary frequency droop control is added on top of the DC voltage droop control. In general, the control can be classified into continuous control where the control is active all the time and emergency control in which the control is only activated when frequency deviation or rate of change of frequency (RoCoF) exceeds certain threshold. Besides, the control can either be a local scheme where only local measurements are used, or communication based scheme where signal from remote measurements are utilized.

2.1.1 Droop based control

The frequency droop concept was first proposed in [44], in which the frequency droop control of AC grids is combined with the DC voltage droop control in DC grids for exchange of primary reserves within a MTDC grid. For most existing publications in the literature utilizing frequency droop control [45, 46, 47, 48], the frequency reference is set to be the nominal frequency (1p.u.), either 60Hz or 50Hz. This makes the frequency control dependent on the DC voltage control
to communicate frequency deviation, causing adverse impact to the DC voltage profile while the frequency control is activated. The coupling between the power-frequency \((P - f)\) droop and the power-DC voltage \((P - U_{dc})\) droop not only reduces the efficiency of frequency control, but could cause other issues that are detrimental to the DC grid. For example, excessive low DC voltage due to the coupling may cause issues such as loss of converter control or even break-down of the whole MTDC grid.

The interaction between these two droops is theoretically investigated in [49, 50] and quantified for both AC and DC side events. In [50], the authors propose adjusted frequency droop constants to achieve desired frequency support participation. However, it neglects the frequency deviations in other AC grids. Moreover, the dependence of frequency control to the activation of DC voltage control is not resolved. The “frequency consensus” control is proposed in [51] and applied in [52] along with a pilot voltage droop controller. The design of frequency weights and the interaction with the DC voltage droop are, however, not addressed. In [52], the authors propose a PI type controller instead of the standard droop control to regulate the average DC voltage. Although it has the benefit of bringing the average voltage at the new steady state to nominal after disturbances, the integral term may decrease the stability of the DC voltage control by introducing more oscillations and longer settling times, especially if the integral gain is not well tuned, which is not discussed in the paper. A weighted frequency scheme is proposed in [53] to facilitate inertial support from offshore wind farms, where the weighted frequency is utilized at the converter at the offshore wind farm to emulate the onshore AC system frequencies. The design procedure of the weights is, however, not discussed. Dead-band can be introduced to the frequency droop in which the frequency deviation needs to be greater than a certain threshold for the control to be active. The authors in [54] proposed undead-band droop concept, meaning that the control activity is reduced within the band, rather than setting to zero as in a regular dead-band. In [46], an integral control scheme is proposed to enforce the frequency droop to obtain desired steady-state participation regardless of the DC voltage droop characteristics. This is an emergency control which is activated only as requested (either large frequency deviation or large RoCoF). In another work [47], droop control for
both primary frequency support and inertia support are applied to the MMC-based MTDC system with on-shore wind farms. A droop based emergency control scheme is proposed in [55]. The authors proposed the “selective power routing” concept which provides selective participation from converters to provide frequency support service, which will be important for a market mechanism in the future. An n-th order model is proposed in order to design the frequency droop controllers. However, due to multiple solution issue, the authors select arbitrary set of solution and verified them by small signal analysis. This procedure makes the approach hard to adjust to different operating conditions. The effective inertia of the whole AC system is also needed, which makes the approach difficult to be applied to large scale realistic AC networks.

2.1.2 Optimization based control

To tackle the major drawback of frequency droop control, which is the coupling between \((P - f)\) and \((P - U_{dc})\) droops, several optimization based approaches for primary frequency control are also proposed, either using centralized scheme [56] or distributed scheme [57, 58]. They take advantage of model predictive control (MPC) which is able to handle different constraints at the same time. However, dynamic state estimation required in the centralized MPC and estimation of real-time system inertia in the de-centralized MPC make it difficult to apply on large-scale realistic power systems. More details are provided in the following.

In [56], kalman filter is used to estimate the states of the system after which a state-space model is derived for the MPC problem to minimize the frequency deviations and the losses in the dc grid, and maintaining the dc voltages within desired bounds. The major drawback of this scheme is the need of dynamic state estimation which is difficult for large scale realistic power grids. In [57] and [58], the authors propose a emergency frequency support control using only local measurements in a distributed MPC scheme. The motivation of using MPC scheme was to tackle the coupling between the voltage droop and frequency droop. Simplified models of AC and DC sides of the converters are used where each converter can identify emergency situations that could potentially lead to unacceptable frequency values. Various constraints are considered in the problem
formulation such as voltage limits and converter power limits. However, the major drawbacks as follows. First, the simplified AC model used in the work requires the estimate of effective inertia of the AC system, which is hard to obtain for large scale power systems, thus resulting in inaccurate AC frequency prediction. Second, the distributed MPC structure requires the assumption that only one VSC in each AC area contributes to the frequency support, which is hardly the case since multiple converters can be connected in one AC area. The authors claim to use a sensitivity factor $s_f$ to address this issue but did not detail the setting procedure for this factor. Third, the authors proposed a simplified DC grid model with a sensitivity matrix used for the distributed MPC control. It assumes the the control action calculated by each controller at each time step is proportional to the corresponding frequency droops. This assumption requires that no converter is hitting their rating limits in the area providing the control service, which could be violated in the case of emergency control since the required power set point change could be large to support the other AC areas. And the sensitivity matrices need to be updated whenever there the topology changes. Lastly, the MPC based controllers does not take control actions further if all values are within limits, i.e., the steady state selling values are not nominal and require a centralized control in a slower time frame to correct them.

\subsection{DC Voltage Control in MTDC Grid}

DC voltage control is an essential and critical task yet a challenging one for stable operation of VSC-MTDC systems. DC voltage has to be maintained close to its nominal value before and after contingencies to maintain the stability of VSC-MTDC systems, since overvoltage can cause damage to the converter equipment whereas undervoltage can result in loss of converter control capability \cite{52}. The role of DC voltage to VSC-MTDC systems is somehow similar to that of frequency to AC systems. For example, a power unbalance in the AC system would cause frequency to vary. Analogously, an unbalance of power in the DC grid would cause the DC voltage to vary. Two important distinctions, however, exist between the two quantities - 1) The DC voltage is not the same across the MTDC grid due to power flows in the DC grid thus voltage drops across DC lines,
whereas frequency is a universal quantity for an AC system. 2) The DC voltage transients after a contingency is orders of magnitude faster than that of AC voltage because the DC side capacitors only store a fraction of energy of those stored in the rotating masses in AC systems \[59\]. These distinctions make the control of DC voltage more challenging. Therefore, coordinated and fast controls are necessary from the healthy converters in VSC-MTDC systems in case of contingencies on the DC side.

Many techniques have been proposed for the control of DC voltage in VSC-MTDC systems. They can be categorized into three main categories: 1) master-slave control, 2) voltage margin control and 3) voltage droop control. Each of these is briefly explained as follows.

2.2.1 Master slave control

For point-to-point VSC-HVDC links, typically, one converter controls the active power and the other controls the DC voltage as a slack converter station. Similarly, for an MTDC grid with more than two converter stations, the simplest way to control the DC voltage is to use one converter to control the DC voltage (as slack converter) and the rest controls active power. This is sometimes referred to as master-slave (M-S) control. With M-S control, the DC voltage controllability is lost in case of slack converter failure, and thus suffers poor reliability performance as it relies on fast inter-station communication to transfer the DC voltage responsibility to another healthy converter \[60, 61\]. Moreover, the MTDC system also loses DC voltage controllability if the slack converter reaches its voltage or current limits, especially during contingencies.

2.2.2 Voltage margin control

In order to remove the need of fast communication and maintain normal operation during the outage of the slack converter, voltage margin control \[62, 63\] is proposed to ensure another converter station takes over the responsibility of DC voltage control when the slack converter station is out of service. The basic idea of voltage margin control is that by assigning margins between the DC voltage references of the converters, it automatically ensures that there is one and only one
converter controlling the DC voltage at a time, with the rest controls active power, given that each converter is within its pre-designed operating upper and lower power limits. Once the converter reaches these power limits, it will act as a constant power terminal. This scheme can automatically assign a new converter based on voltage margins in case of slack converter failure without the need of fast communication as in M-S control. However, the upper and lower operating power limits and voltage references need to be carefully coordinated to avoid interactions between the converters during DC voltage disturbances, which becomes challenging when the number of converters in the MTDC system is large. Moreover, the transition between the reference voltages of old and new master converters place great stress on the new master converter, which raises the risk of DC voltage oscillations [64].

2.2.3 DC voltage droop control

A major drawback of the aforementioned master slave and voltage margin control strategies is that following the outage of one converter station, the slack converter would have to pick up the whole power imbalance with a sudden power change since it is the only one controlling the DC voltage. Two bad consequences could follow - 1) this could overload the slack converter which would result in lost of DC voltage controllability of the MTDC grid and 2) the sudden power change in the slack converter would cause sudden frequency variation in the connected AC system, which might be unacceptable if the amount of power imbalance is significant compared to the AC system loading. To overcome these two issues, DC voltage droop control is proposed [65, 66, 67, 63], which distributes the power imbalance among all the rest converter stations after a converter outage, making it a great candidate for operation of large DC grids. The values of droop constants determine how the power imbalance is distributed in the remaining converters following a converter outage. A proper control design should ensure autonomous power sharing after a converter outage.

DC voltage droop control can be further categorized in terms of adaptability of the DC voltage droop constant (fixed vs. adaptive), and the selection of feedback signal (local vs. pilot), as briefly explained below.
2.2.3.1 Fixed droop

In a fixed DC voltage droop control strategy, the droop constants are fixed for all converter stations. Similar to the generator’s power-frequency droop in AC systems, it is desired that the converters with higher (less) ratings share more (less) power imbalances after a converter outage. Considering this, different droop values can be assigned to different converters according to their ratings. Moreover, the droop values can be designed to achieve different goals, such as minimizing losses in MTDC systems [68], minimizing AC and DC system interactions [69, 70] and differentiating different converter outages [59].

DC voltage droop can be combined with two other modes - constant power and constant voltage to provide more flexibility to system operators, as shown in [71, 72]. A combination of Master-Slave(M-S) and droop control is also possible, as proposed in [73] and named as priority control, which periodically ranks and select the converter with highest priority as slack converter and the rest as constant power converters. In case the slack converter fails, a minimum number of converters are selected based on their priority to participate in voltage droop control, which are switched back to constant power control after the disturbance ends. However, this scheme requires careful coordination and becomes more difficult as the number of converters increases in the system. Moreover, only DC voltage deviation is used to guide operation, which makes the scheme not adaptive to power margins of the converter.

2.2.3.2 Adaptive droop

The fixed droop control strategy does not consider the operating condition of the converters, and thus is not able to capture the level of control capability of converters to provide support. This could overload and stress the converters that are already very close to their operating limits. The adaptive droop control addresses this shortcoming by taking into consideration the operating condition of the converters. In [74], the selection of DC voltage droop constants based on the available headroom of converters is proposed, with which the converters with more headroom tend to have a lower droop constant, and thus higher gain to share more power. Modal analysis is
performed to show that both upper and lower bounds of the DC voltage droop constants are necessary to ensure stability. In [64], an adaptive DC voltage droop control is proposed considering the DC voltage deviation in addition to the available headroom of the converters. Both of these schemes will be discussed in more detail in Chapter 5 and compared with the proposed adaptive DC voltage control approach. A trajectory sensitivity analysis based method is proposed in [75] to determine the stability constraints of the adaptive droop gains. However, the requirement of complete system model and the computation complexity of solving the system trajectories make this approach hard to be applied on practical large scale power systems.

Nonlinear droop control techniques are proposed and combined with linear droop to tackle the trade off between DC voltage regulation and the accuracy of current sharing. In [76], three decentralized nonlinear droop methods are proposed to improve current sharing accuracy in DC microgrids. These methods are more difficult to implement as compared to the linear droops, as the design of nonlinear coefficients and tuning of voltage compensation are complicated. A similar idea is applied in [77] with a cubic accelerated term added on top of the linear droop, enhanced by a self-correction algorithm to drive the droop curve to the new operating point to ensure the same performance and regulation speed at the new operating condition. The performance improvement is, however, limited and power references have larger fluctuations as compared to the linear droops.

Several optimization based methods are proposed to determine the adaptive droop constants. For example, in [78], a two layer hierarchical optimization approach is proposed, droop constants are optimized by maximizing the least negative real part of system eigenvalues, and DC voltage references are optimized based on optimal power flow (OPF) to enhance DC voltage dynamics and accurate power sharing. In [79], the authors propose MPC based control to minimize the DC voltage deviations and control mode changes. In [80], the authors propose another optimization based design of droop coefficients with integral square error of converter DC voltages used as an index, with which the DC system stability is guaranteed after disturbances. However, these optimization based approaches require detailed state space model of the overall system, which is hard to obtain for practical large scale systems. Moreover, the optimization needs to be resolved
whenever system operating condition or topology changes, which is computationally intensive for real-time applications.

2.2.3.3 Feedback signal of DC voltage droop control

Local signal For either fixed or adaptive droop, local DC voltage measured at the terminal of each converter can be used as feedback signal, and compared with its own DC voltage reference, which is typically from power flow solution. This enables a decentralized local scheme without the need of communication. However, due to the existence of line resistances and power flows in the MTDC grid, local voltage based droop control suffers from poor reference tracking due to different DC voltages across the DC grid and the lack of a common feedback signal. Because of this, power sharing of converters after a contingency not only depends on droop constants but also the DC grid topology and line parameters [81], which makes the planning of power sharing difficult. This dependence is analytically illustrated further in Section 5.2.

Pilot signal To overcome this issue, pilot voltage droop (PVD) control is proposed in [82, 83] and later used in [74]. In a PVD control scheme, a common voltage signal measured at an arbitrary station is communicated to all converter stations as feedback control signal. Therefore, all converters control the same voltage with the same reference. It will be shown in Section 5.2 that this scheme provides precise power sharing purely determined by the value of droop constants and independent of DC grid topology and line parameters. This scheme is further enhanced in [52] where average DC voltage of all converter stations is regulated to the nominal value so that precise power regulation can be achieved without the risk of large steady state deviations in the DC voltages.

Since the common voltage is needed to the converters, the PVD scheme relies on communication to operate. The impact of communication latency can be mitigated [84], with fallback control based on local voltage measurements. Alternatively, another communication based approach is proposed in [85] by communicating the power sharing index between neighboring converters with better robustness to communication latencies, although it requires prior knowledge of communication latencies to better tune the correction term in the DC voltage reference calculation. Besides, a
communication-free strategy is proposed [86] taking advantage of the DC grid itself and completely removes the need of communication to transfer the information of the common voltage. The basic idea is that by injecting a small AC signal whose frequency is known globally to the DC voltage, each converter is able to estimate the pilot voltage magnitude based on the locally measured frequency.

2.3 Overview of Approaches in This Dissertation

For frequency support, droop control structure is adopted due to its simplicity to design and implement for large scale realistic power systems as compared to the optimization based approaches. In order to mitigate the impact of frequency control on DC voltage droop, a common global reference is proposed which can reflect the overall frequency dynamics of all AC systems. The designed global frequency control is robust and works well with different types of DC voltage control, as discussed in Sections 4.4.3 and 5.4.4.

For DC voltage control, adaptive droop control is adopted in this work, due to its simple structure, as well as the adaptability to changing operating conditions. Pilot voltage droop (PVD) framework is chosen instead of local voltage based droop because of its advantage of accurate power sharing purely determined by the value of droop constants. The adaptive droop constants are designed to incorporate the power sharing capability of each converter, as well as the DC voltage deviation. Moreover, the designed approach has the capability to differentiate the converter outage direction (i.e. rectifier outage vs. inverter outage).
CHAPTER 3. MODELING AND CONTROL OF VSC-MTDC SYSTEMS FOR SYSTEM LEVEL STABILITY ANALYSIS

3.1 Introduction

A valid VSC-MTDC model is needed in order to perform studies for combined AC and MTDC systems. Depending on the goal of the study and the time scale of interest, various levels of details can be incorporated into the converter models [87, 88], from full physics based models that capture the switching dynamics and electromagnetic transients, to simplified models which represent the AC and DC side characteristics as controlled current and voltage sources for phasor domain analysis. For system level dynamic studies, the latter is sufficient since the AC system dynamics of interests are much slower than the switching dynamics in the DC system. There have been many recent efforts in the literature focusing on the development of power flow and dynamic VSC-MTDC models suitable for electromechanical transient stability study [63, 89, 90, 91, 92, 9]. These models are developed for different transient stability software such as MatDyn [89, 90], PSAT [63] and PSS/E [91, 92, 9]. Since PSS/E is a widely used commercial software in the United States, it has been chosen to be used in this work due to its capability to handle realistic large scale power systems.

To date there is still no standard dynamic model for VSC-MTDC in PSS/E, and thus user defined models have to be used for the study. Three VSC-MTDC PSS/E models have been developed in the literature [91, 92, 9], each of which presents some differences with the others. The main distinct feature of the model developed in [92] is its detailed modeling of the inner current controllers, which is assumed to be instantaneous in [91] and approximated by first order systems in [9]. Another major difference among the three is with respect to the DC grid modeling. In [91] and [9], the DC grid is modeled with full details including capacitors, resistances and inductances whereas the inductances are ignored in [92] because it only contributes to fast transients. Besides,
the converter losses are also handled differently. The losses are fully ignored in [91], approximated with fixed losses and variable losses proportional to the square of the converter current in [92], and represented by a generalized losses formula in which the losses are quadratically dependent on the converter current in [9]. These models have been verified against the detailed electromagnetic model developed in Matlab or PSCAD. This work adopts the user defined model developed in [9] due to its capability to handle the converter losses more realistically as compared to [92], and its moderate representation of the inner current loop as compared to [91]. The following sections will briefly describe the structure and the major components of the VSC-MTDC model used in this study and its integration with PSS/E.

3.2 VSC Model Structure

A conceptual illustration of the converter model is shown in Figure 3.1 [9]. Each converter bridges an AC bus and a DC bus. The AC side of the converter is modeled as a controllable voltage source $\bar{e}_c = e_c \angle \delta_c$ coupled to the AC bus $\bar{u}_s = u_s \angle \delta_s$ through a phase reactor and a transformer, with a total series impedance of $Z_s = R_s + j \omega L_s$. The DC side of the converter is modeled by a current injection $i_{dc}$ into the DC grid. The AC side and the DC side are related by the energy conservation principle with converter losses quadratically dependent on the rms converter AC current $i_s$ as in (3.1) [93].

$$P_c + P_{dc} + P_{loss} = 0 \quad , \quad P_{loss} = a + bi_s + ci_s^2$$ (3.1)
**VSC operating mode** By transforming AC quantities into the dq frame (see Section 3.4.3, each VSC is able to independently control the d and q axis currents [10]. The d-axis current can be used to either control active power injected into the AC bus $P_s$, or the DC bus voltage $U_{dc}$. The q-axis current can be used to either control reactive power injected into the AC bus $Q_s$ or the AC bus voltage magnitude $U_s$. Therefore, each VSC has four possible operating modes: 1) $P_s - Q_s$ mode; 2) $P_s - U_s$ mode; 3) $U_{dc} - Q_s$ mode; and 4) $U_{dc} - U_s$ mode. As explained in Section 2.2 the DC voltage control can either be achieved with voltage margin control in which one converter controls the DC voltage and the rest control the active power, or with distributed droop control in which all converters are equipped with a $P_s - U_{dc}$ droop.

### 3.3 AC-MTDC Power Flow Algorithm

There are two types of approaches to solve the combined AC-MTDC power flow: unified approach [94, 95] and sequential approach [90, 9]. The unified approach solves the AC and DC power flow equations together whereas the sequential approaches solves the AC and DC side power flow equations iteratively in a sequential manner. For the ease of integration with the existing AC power flow algorithm in PSS/E, the sequential approach proposed is adopted in this work.

In the AC power flow, there are three type of buses - slack, PQ and PV. For the DC grid power flow, there are two type of DC buses - DC slack bus whose DC voltage $U_{dc, slack}$ is known, and constant power bus whose active power exchange with the AC system $P_{slack}$ is known. Since there is no power flow model of VSC-MTDC in PSS/E, the VSC is represented as a generator for the purpose of power flow analysis. Depending on the operating mode of the converter, in the power flow formulation, one converter is assumed to be the slack converter which controls the DC voltage, whereas the rest converters control the active power $P_s$. Depending on the q-axis current operating mode, the AC terminal of P-controlling converters could either be a PQ or PV bus in the AC system. A flow chart showing the overall procedure is summarized from [9] and shown in Figure 3.2. The values with subscript “$dc, slack$” represents those associated with the DC slack converter, whereas those with subscript “$P$” are associated with those converters controlling the
Figure 3.2: Sequential AC-MTDC power flow
active power output. More detailed explanation of this sequential AC-MTDC power flow can be found in [9] and [90].

3.4 VSC-MTDC Dynamic Model

The dynamic models for VSC-MTDC systems consist of the VSC model and the MTDC grid model. These models are simplified phasor domain dynamic models which are suitable for system level dynamic analysis. They are briefly explained in the next sections.

3.4.1 VSC dynamic model

The VSC model is a simplified model with cascaded control loops in the $dq$ frame (see Section 3.4.3). The overall structure of the VSC model is shown in Figure 3.3. The $d$-axis current can be used to either control active power injected into the AC bus $P_s$, or the DC bus voltage $U_{dc}$. The $q$-axis current can be used to either control reactive power injected into the AC bus $Q_s$ or the AC bus voltage magnitude $u_s$. The outer controllers generate converter current references in the $dq$ axis $i_{sd}^{ref}$, $i_{sq}^{ref}$ for the inner current controllers. DC voltage droop control [63, 74] can be enabled to have all converters collaborate on the DC voltage control, in which an additional signal $\Delta P_{s,U_{dc}}$ is added to the $P$ control reference $P_s^{ref}$. The inner current controllers are approximated by first order transfer functions since their dynamics are much faster than the generator controllers in the AC system. The time constants $\tau_d$, $\tau_q$ are typically selected in the range of 0.5-5ms [10]. The inner current limits can be utilized as P-priority, Q-priority or P-Q equal priority [9, 63]. Finally, the currents are transformed back to the $\alpha\beta$ frame and used to compute the converter inner voltage $\bar{e}_c$ and the equivalent Norton current $I_{SOURCE}$ of the VSC model.

The outer controllers in Figure 4.1 can be realized in different configurations, typically composed of PI controllers [9]. Therefore, there are six state variables in total (not active at the same time but depends on the VSC control modes): the $d$ axis current $i_{sd}$ and the $q$ axis current $i_{sq}$ for the
Figure 3.3: Overall structure of the VSC dynamic model
inner current loops, and four state variables associated with the four outer PI controllers. In this work, the P and Q outer controllers used are:

\[ i_{sd}^{ref} = \frac{P^{ref}}{u_s} \quad (3.2) \]

\[ i_{sq}^{ref} = \frac{-Q^{ref}}{u_s} \quad (3.3) \]

, meaning that the PI controller gains for the P and Q controllers are set to zeros. This is enabled by the ideal phase locked loop [89, 63, 91] assumed in this work aligning the converter AC voltage with the \( d \) axis (see Section 3.4.3). This is a reasonable assumption for studying interconnection wide dynamics.

### 3.4.2 MTDC grid dynamic model

Most existing literature approximates the DC grid with resistive networks since the inductances and capacitances are only effective during transients. In this work, we adopt a more accurate equivalent \( \pi \) model to represent the MTDC grid as shown in Figure 3.4 using a four terminal ring MTDC network as an example.
For an MTDC grid composed of \( n \) converters and \( n_L \) DC lines, the dynamics of the MTDC grid model are described by (3.4) and (3.5) [89, 90, 9]:

\[
\begin{align*}
C_{dc} \frac{dU_{dc}}{dt} &= -G_{dc} U_{dc} - A_c I_{cc} + I_{dc} \\
L_{dc} \frac{dI_{cc}}{dt} &= A_T U_{dc} - R_{dc} I_{cc}
\end{align*}
\] (3.4) (3.5)

where \( U_{dc} \in \mathbb{R}^{n \times 1} \) and \( I_{cc} \in \mathbb{R}^{n_L \times 1} \) are state variable vectors containing \( n \) converter DC terminal voltages, and \( n_L \) converter-to-converter DC branch currents. \( I_{dc} \in \mathbb{R}^{n \times 1} \) are DC current injections from each converter to the MTDC grid, which are also the inputs of the MTDC network model. \( C_{dc} \in \mathbb{R}^{n \times n} \) and \( G_{dc} \in \mathbb{R}^{n \times n} \) are diagonal matrices with shunt admittances at the \( n \) VSCs including any resistive loads on the DC bus, \( R_{dc} \in \mathbb{R}^{n_L \times n_L} \) and \( L_{dc} \in \mathbb{R}^{n_L \times n_L} \), are diagonal matrices with series impedances for the \( n_L \) DC branches. \( A_c \in \mathbb{R}^{n \times n_L} \) is the incidence matrix of the DC grid, with each element \( a_{c,ij} \) defined as follows:

\[
a_{c,ij} = \begin{cases} 
+1, & \text{if line } j \text{ is defined leaving node } i. \\
0, & \text{if line } j \text{ is defined entering node } i. \\
-1, & \text{if line } j \text{ is not connected to node } i. 
\end{cases}
\] (3.6)

### 3.4.3 \( \alpha\beta \) to \( dq \) frame transformation and phase locked loop

The key enabler for the decoupled current control in the \( dq \) frame is the \( \alpha\beta \) to \( dq \) transformation of the space phasor. The concept of space phasor and representations of space phasor in \( \alpha\beta \) to \( dq \) frame are briefly explained below. These are summarized from [10].

**Definition of space phasor** Consider the following balanced, three-phase, sinusoidal function:

\[
\begin{align*}
f_a(t) &= \hat{f} \cos(\omega t + \theta_0), \\
f_b(t) &= \hat{f} \cos(\omega t + \theta_0 - \frac{2\pi}{3}), \\
f_c(t) &= \hat{f} \cos(\omega t + \theta_0 - \frac{4\pi}{3})
\end{align*}
\] (3.7)
where \( \hat{f} \), \( \theta_0 \) and \( \omega \) are the amplitude, the initial phase angle and the angular frequency of the function, respectively. The space phasor is defined as:

\[
\hat{f}(t) = \frac{2}{3} [e^{j\theta_0} f_a(t) + e^{j\frac{2\pi}{3}} f_b(t) + e^{j\frac{4\pi}{3}} f_c(t)]
\]  

(3.8)

Substituting for \( f_{abc} \) from (3.7) and using \( \cos \theta = \frac{1}{2} (e^{j\theta} + e^{-j\theta}) \) and \( e^{j\frac{2\pi}{3}} + e^{j\frac{4\pi}{3}} = 0 \), we get:

\[
\hat{f}(t) = (\hat{f}e^{j\theta_0})e^{j\omega t} = \bar{f}e^{j\omega t} = \hat{f}e^{j(\theta_0+\omega t)}
\]  

(3.9)

where \( \bar{f} = \hat{f}e^{j\theta_0} \) is a complex value which can be represented by a vector in the complex plane. With a constant \( \hat{f} \), the vector is analogous to the conventional phasor used in steady state analysis. The space phasor \( \hat{f}(t) \) is a rotating vector with amplitude \( \hat{f} \) in the complex plane at the speed of \( \omega \) with the initial position at \( \theta_0 \).

**\( \alpha\beta \)-frame representation of a space phasor** The space phasor can be decomposed into its real and imaginary components as:

\[
\hat{f}(t) = f_\alpha(t) + j f_\beta(t)
\]  

(3.10)

where \( f_\alpha \) and \( f_\beta \) are the \( \alpha \) and \( \beta \) frame components of \( \hat{f}(t) \). The real and imaginary axis in the complex plane can thus be renamed as \( \alpha \) and \( \beta \) axis, as illustrated in Figure 3.5. As can be seen from Figure 3.5, the \( \alpha \) and \( \beta \) frame components of the space phasor can be written as:

\[
f_\alpha(t) = \hat{f}(t)\cos[\theta(t)], \]
\[
f_\beta(t) = \hat{f}(t)\sin[\theta(t)]
\]  

(3.11)

, which means that they are time varying as \( \hat{f}(t) \) rotates in the counter-clock direction.

**\( dq \)-frame representation of a space phasor** The space phasor in the \( dq \) frame is defined by the following transformation from \( \alpha\beta \) frame:

\[
f_d + j f_q = (f_\alpha + j f_\beta)e^{-j\rho(t)}
\]  

(3.12)

, which is equivalent to rotating \( \hat{f}(t) \) by an angle of \( -\rho(t) \). The angle \( \rho(t) \) can be chosen arbitrarily. However, to decouple the \( d \) and \( q \) frame components for the purpose of control design, \( \rho(t) \) is chosen...
to be equal to $\omega t$, which is the nominal frequency of the power system. With $\rho(t) = \omega t$, the space phasor in the $dq$ frame becomes:

$$\begin{align*}
 f_d + jf_q &= (f_\alpha + jf_\beta)e^{-j\omega t} = (\hat{f}e^{j(\omega t + \theta_0)})e^{-j\omega t} = \hat{f}e^{j\theta_0} \\
 &= \text{constant (3.13)}
\end{align*}$$

, which is no longer time varying. Therefore, $f_d$ and $f_q$ are decoupled DC quantities which can be easily controlled independently. This is the key motivation behind the $\alpha\beta$ to $dq$ transformation.

The $dq$ frame can be viewed as a rotating reference frame in the speed of $\omega$, as illustrated in Figure 3.6.

**Transformation matrices between $dq$ and $\alpha\beta$ frames** Substituting $e^{-j\omega t} = \cos(\omega t) - jsin(\omega t)$ into (3.13), we get:

$$\begin{align*}
 f_d + jf_q &= [f_\alpha \cos(\omega t) + f_\beta \sin(\omega t)] + j[f_\beta \cos(\omega t) - f_\alpha \sin(\omega t)] \\
 &= [f_\alpha \cos(\omega t) - f_\beta \sin(\omega t)] + j[f_\beta \cos(\omega t) + f_\alpha \sin(\omega t)] \\
 &= \begin{bmatrix}
 1 & j \\
 -j & 1
\end{bmatrix}
\begin{bmatrix}
 f_\alpha \\
 f_\beta
\end{bmatrix} = T_{\alpha\beta\to \alpha\beta} T_{\alpha\beta\to dq} T_{dq\to \alpha\beta}
\end{align*}$$

, which in the matrix form can be written as:

$$\begin{align*}
 \begin{bmatrix}
 f_d \\
 f_q
\end{bmatrix} &= \begin{bmatrix}
 \cos(\omega t) & \sin(\omega t) \\
 -\sin(\omega t) & \cos(\omega t)
\end{bmatrix}
\begin{bmatrix}
 f_\alpha \\
 f_\beta
\end{bmatrix} = T_{\alpha\beta\to \alpha\beta} T_{\alpha\beta\to dq} T_{dq\to \alpha\beta}
\end{align*}$$

(3.15)
Figure 3.6: Stationary $\alpha\beta$ and rotating $dq$ frame coordinates \[10\], where $T_{\alpha\beta\rightarrow dq}$ is the transformation matrix from $\alpha\beta$ frame to $dq$ frame. From (3.12), we can also write the inverse transformation from $dq$ to $\alpha\beta$ as:

$$\hat{f}(t) = f_\alpha + jf_\beta = (fd + jf_q)e^{j\rho(t)} = (fd + jf_q)e^{j\omega t} \quad (3.16)$$

Similarly, the transformation in matrix form from $dq$ frame to $\alpha\beta$ frame can be obtained as the following:

$$\begin{bmatrix} f_\alpha \\ f_\beta \end{bmatrix} = \begin{bmatrix} \cos(\omega t) & -\sin(\omega t) \\ \sin(\omega t) & \cos(\omega t) \end{bmatrix} \begin{bmatrix} fd \\ f_q \end{bmatrix} = T_{dq\rightarrow \alpha\beta} \begin{bmatrix} fd \\ f_q \end{bmatrix} \quad (3.17)$$

**Phase locked loop**  Phase locked loop is a close loop controller used to achieve the above mentioned $\rho(t) = \omega t$ to synchronize the converter to the three-phase AC grid. Its implementation is not unique and a good review of various structures is given in \[96\]. In the simplified model used in this work, an ideal phase locked loop controller is assumed \[89, 63, 91\], meaning that the converter can track the ac signals perfectly without any noise and delay. This is a reasonable assumption for studying interconnection wide dynamics. For each converter, the AC voltage is aligned with the $d$
axis: $\bar{u}_s = u_s + j0$. The active and reactive power injections from the VSC to the AC system can be obtained as $P_s + jQ_s = (u_s + j0)(i_{sd} + ji_{sq})^* = u_s i_{sd} - j u_s i_{sq}$, where $^*$ denotes conjugate of the phasor. Therefore, the active power and reactive power can be controlled by the $d$ axis and $q$ axis current independently as shown in Section 3.4.1.
CHAPTER 4. PRIMARY FREQUENCY SUPPORT AMONG ASYNCHRONOUS AC SYSTEMS VIA A VSC-MTDC SYSTEM

4.1 Introduction

One of the greatest challenges faced by power system operators is the reduction of system inertia caused by the displacement of conventional generators with inverter based renewable generation. It is expected that frequency issues in the AC systems will become more prominent and increasingly threaten the stability of the power systems in the coming years. Therefore, new sources of frequency support need to be explored. VSC-MTDC can facilitate primary frequency reserves among different AC systems connected through the DC network by utilizing its fast and flexible power control capabilities at the converters, thus providing frequency support from each AC system to the others. This chapter investigates the frequency support among asynchronous AC systems enabled by the VSC-MTDC system. The main idea is to equip the converters with supplementary frequency control which adds an additional signal to the active power reference of the VSC active power controller. This additional signal is proportional to some frequency error signal depending on the control scheme used as explained in the next two paragraphs. With the frequency control enabled, the VSC-MTDC is able to respond in a coordinated way to improve the dynamic frequency performance of the AC area which experiences large generation loss. The benefits resulting from the improved dynamic frequency performance are mainly reduced load shedding and reduced need of total online spinning reserves, thus yielding both stability and economic benefits to the overall system.

As indicated in Section 2.1, droop based control is adopted in this work due to its simplicity to design and implement on large scale realistic power systems. The majority of the existing works utilize a local scheme, in which each converter compares its measured frequency at the AC terminal with the nominal frequency (a constant, either 60Hz or 50Hz) and generates a frequency error which is then multiplied by a proportional droop before adding to the active power reference. No
communication is required among the converters to implement such schemes. The major drawback of the local scheme is that it severely degrades the DC voltage profile while the frequency control is activated due to the strong coupling between the frequency and DC voltage droop controllers, posing great threat to the reliability of the MTDC grid.

To overcome the above mentioned issue, a frequency control scheme based on a global signal is proposed in this Chapter. In the proposed global scheme, the reference signal of each converter is set to be the measured weighted average frequency from the AC terminals of all the converters that participate in the frequency control, and thus each converter possesses a global view of the overall system state. The frequency error of each converter is the difference between the local measured frequency and this time-varying global reference signal. The main idea behind this control strategy is that the converters with terminal frequencies higher than the weighted average frequency tend to draw more power from their connected AC systems, whereas those with terminal frequencies lower than the weighted average frequency tend to inject more power into their connected AC systems. This control scheme can successfully provide frequency support among different asynchronous AC systems, resulting in improved frequency nadir (and thus less load shedding if there is any) than the local schemes. More importantly, it significantly improves the DC voltage profile by reducing DC voltage deviations while the frequency control is activated. The performance of the designed frequency control is evaluated with different DC voltage droop controls, showing consistent effectiveness. The adaptation of the proposed frequency control to converter outages is also analyzed and validated through simulation.

The VSC-MTDC model and the proposed frequency control are implemented in the commercial grade software PSS/E and thus is suitable to study large-scale realistic systems and can be incorporated into the power system planning process at utilities and ISOs. The effectiveness of the proposed control is illustrated on a developed AC-MTDC test system in this Chapter. It will be shown in Chapter 6 that its effectiveness and superiority over the local scheme remains on a large-scale realistic model combining the North American Western Interconnection (WI) and Eastern Interconnection (EI) with continental HVDC interconnections.
4.2 VSC Frequency Control

4.2.1 Frequency control structure

The VSC frequency control is a supplementary control that modulates the set point of the active power controller $P_s^0$ with an additional signal $\Delta P_{s,f}$, as shown in the dashed box in Figure 4.1. For each converter $i$, the value $\Delta P_{s,f,i}$ is proportional to the filtered frequency error $\Delta f_i^f$ with a droop. The design of the low pass filter and the washout filter time constants are discussed in Section 6.3.2. For the frequency deviation within the designed bandwidth, $\Delta f_i^f = \Delta f_i = f_i^{ref} - f_i$ can be assumed. Combined with the DC voltage droop control [63, 74], the overall active power reference for each converter $i$ is given by:

$$P_{s,i}^{ref} = P_{s,i}^0 - \frac{1}{k_{dc,i}} \Delta U_{dc,i} + \frac{1}{k_{f,i}} \Delta f_i$$

$$= P_{s,i}^0 + \Delta P_{s,U_{dc}} + \Delta P_{s,f}$$

(4.1)

where $k_{dc,i} > 0$ and $k_{f,i} > 0$ are called the DC voltage droop constant and the AC frequency droop constant, respectively. $P_{s,i}^0$ is the constant active power set point for each converter $i$ obtained from the steady state power flow solution. $\Delta P_{s,U_{dc}}$ and $\Delta P_{s,f}$ are the power reference deviations from the DC voltage and the AC frequency droops, respectively. A positive power reference deviation indicates the need of power injection from the DC grid to the AC grid. In this chapter, we design and evaluate frequency controls on the basis of the conventional local (CV-L) voltage droop control. However, the designed frequency control remains effective for other types of DC voltage droop controls such as the well-known pilot voltage droop (PVD) control [82], which is further discussed in Section 4.4.3.

The major difference between the traditional frequency control in the literature and the proposed global scheme is the selection of the reference value $f_i^{ref}$, as detailed in the following sections.

4.2.2 Frequency control with per unit reference (FC-PU)

For the traditional frequency control (FC) scheme in the literature [44, 45, 49, 46, 50, 55, 47], the frequency reference for each VSC is set to be equal to the nominal frequency, which is a constant
Figure 4.1: VSC dynamic model with supplementary frequency control
value of 1.0 in per unit (PU): $f_{i}^{ref} = 1.0\text{p.u.}$ It is herein referred as “FC-PU”. FC-PU is a local scheme which does not require communication among converters. However, the major drawback of FC-PU is that it severely degrades the DC voltage profile while the frequency control is activated due to the strong coupling between the AC frequency and DC voltage droop controls, as will be shown later in the simulation results. DC voltage control is critical for the operation of MTDC systems, since overvoltage can cause damage to the converter equipment whereas undervoltage can result in loss of converter control capability [52]. Therefore, it is necessary to develop a frequency control scheme that can mitigate the impact to the DC voltage profile with equivalent or better frequency control performance.

Another thing that is worth mentioning here is that if DC voltage droop is not enabled (i.e. only one converter controls the DC voltage as in the voltage margin control described in Section 2.2.2), FC-PU will not function properly if the generation loss takes place in the AC system which is connected to the slack VSC. This is because the power imbalance in the DC network caused by the modulated active power reference from FC-PU as response to generation loss in an AC system will be compensated by only the slack converter which is in the same AC system, and thus will not be seen by the other AC systems. If the generator loss is large enough, the local frequency control could even have adverse impact to the AC system that experiences generation loss. This is another advantage of DC voltage droop control as compared to voltage margin control. To this end, the study of frequency support for different strategies in this dissertation assumes that DC voltage droop control is used so that the performances of different frequency control schemes are comparable.

### 4.3 Proposed Frequency Control with WAF (FC-WAF)

#### 4.3.1 Methodology

To overcome the drawback of FC-PU, we propose a global frequency control scheme by setting the frequency reference of each converter $f_{i}^{ref}$ to the weighted average of frequencies (WAF) measured at all converter AC terminals that participate in FC-WAF control [97]:

\[
\text{WAF} = \frac{\sum_{i} w_{i} f_{i}}{\sum_{i} w_{i}}
\]
\[ f_{i}^{\text{ref}} = \sum_{i=1}^{k} \alpha_i f_i \] (4.2)

, where \( \alpha_i \in [0, 1] \), \( \sum_{i=1}^{k} \alpha_i = 1 \) are the weights of the measured frequencies of the converters, and \( k \) is the number of converters that participate in FC-WAF in the VSC-MTDC system with \( n \) converters \( (k \leq n) \). We call this scheme “FC-WAF”. The frequency reference \( f_{i}^{\text{ref}} \) is a common time varying quantity which is updated for each converter at every time step, unless communication delays are considered as will be discussed in Section 6.3.4 when evaluating the proposed control on a realistic large system.

Another control scheme called “WA-F” proposed in [98] also utilizes a similar idea but for a different application to improve the transient stability of a single AC system with embedded VSC-MTDC systems. Here in the context of frequency support among asynchronous AC systems, the main idea of utilizing weighted average frequency as frequency control reference is because the nominal frequency for each AC system is the same (1p.u.), thus the variation is comparative among different AC systems. Therefore, with FC-WAF, converters with terminal frequencies higher than the WAF tend to draw more power from its connected AC system, whereas those with terminal frequencies lower than the WAF tend to inject more power into its connected AC system. This effectively achieves the goal of exchanging primary reserves among asynchronous AC systems. The proposed FC-WAF scheme is a centralized supplementary control to the VSC-MTDC system. It is robust to communication delays as shown in Section 6.3.4. In case of any communication link failure, each VSC could switch to the decentralized scheme FC-PU as a backup.

In order to mitigate the interaction between the AC frequency control and DC voltage control, the weights \( \alpha_i \)’s in FC-WAF are selected strategically [50, 98], which is explained as follows. For a VSC-MTDC system with \( n \) converters, assume the first \( k \) converters participate in the frequency droop control, the first \( m \) converters participate in only the voltage droop control, and the rest \( (n - m) \) converters are constant power converters, such as those connected with wind plants \( (k \leq \)
$m \leq n$), the total active power of each converter $i$ at steady state follows:

$$P_{s,i} = \begin{cases} 
    P_{s,i}^{ref} = \text{repeat (4.1)}, & \text{for } i = 1, \ldots, k \\
    P_{s,i}^0 - \frac{1}{k_{dc,i}} \Delta U_{dc,i}, & \text{for } i = k + 1, \ldots, m \\
    P_{s,i}^0, & \text{for } i = m + 1, \ldots, n 
\end{cases} \tag{4.3}$$

Sum over all $n$ converters in the MTDC grid gives:

$$\sum_{i=1}^{n} P_{s,i} = \sum_{i=1}^{n} P_{s,i}^0 + \sum_{i=1}^{m} \left(- \frac{1}{k_{dc,i}} \Delta U_{dc,i}\right) + \sum_{i=1}^{k} \left( \frac{1}{k_{f,i}} \Delta f_i \right) \tag{4.4}$$

To simplify the analysis, a lossless MTDC grid is assumed, from which we can get from power balance within the MTDC grid in steady state (performance under lossy systems will be examined in the case studies):

$$\sum_{i=1}^{n} P_{s,i} = \sum_{i=1}^{n} P_{s,i}^0 = 0 \tag{4.5}$$

Moreover, this assumption makes the DC voltage variations identical at all nodes both in steady state and transients:

$$\Delta U_{dc,i} = \Delta U_{dc} \tag{4.6}$$

Substituting (4.5) and (4.6) into (4.4), with $\Delta U_{dc}$ calculated in terms of $\Delta f_i$ results in:

$$\Delta U_{dc} = \frac{\sum_{i=1}^{k} \frac{1}{k_{f,i}} \Delta f_i}{\sum_{i=1}^{m} \frac{1}{k_{dc,i}}} \tag{4.7}$$

To minimize the impact to the DC voltage profile for any frequency variation, set the right hand side of (4.7) to 0. Using $\Delta f_i = f_{i}^{ref} - f_i$, the frequency reference $f_{i}^{ref}$ can be calculated as follows (note that $f_{i}^{ref} = f^{ref}$ $\forall i \in \{1, \ldots, k\}$):

$$f_{i}^{ref} = \frac{\sum_{i=1}^{k} \frac{1}{k_{f,i}} f_i}{\sum_{i=1}^{k} \frac{1}{k_{f,i}}} = \frac{1}{\sum_{i=1}^{k} \frac{1}{k_{f,i}}} \sum_{i=1}^{k} \frac{1}{k_{f,i}} f_i \tag{4.8}$$

Comparing (4.8) with (4.2) yields the following:

$$\alpha_i = \frac{1}{\sum_{i=1}^{k} \frac{1}{k_{f,i}}} \tag{4.9}$$
Use of (4.9) in (4.2) eliminates the impact of frequency control on the DC voltage profile, and reduces the coupling between the two control loops. In this way, FC-WAF achieves coordination between converters, improves the frequency control effectiveness, and minimizes DC voltage deviation.

Note that the derivation of $f^{ref}$ for FC-WAF assumes the system is at steady state, so that (4.5) is true. However, it will be shown in the case studies with non-linear time domain simulations that the resulting DC voltage nadir in transient responses is also significantly improved.

4.3.2 On frequency controllers with converter outages

In case of converter outages, the MTDC grid relies on DC voltage control to regulate the DC voltage and maintain the stability of the DC grid. A converter outage also causes frequency variation in the underlying AC system, with which the frequency controller would counteract the effort of the DC voltage control. For converters connected to strong AC systems, this counteraction is negligible since the frequency variation is small. However, if the converter is connected to a weak AC system, this counteraction could be large and problematic to the MTDC grid. Therefore, it is recommended that weak AC systems should limit their participation to provide frequency support to other AC systems. This can be done by increasing the saturation parameter $\Delta P_{s,f,min}$ (a negative value) as shown in the dashed box of Figure 4.1.

With a converter outage, the DC voltage variates orders of magnitude faster than the quantities in AC systems due to the small energy stored in DC capacitors. Therefore, for converters equipped with both controls, DC voltage control will first stabilize the DC voltage (defined as stage 1), followed by frequency control (defined stage 2) to mitigate the impact on AC frequency variations. Due to the difference in response time scales, the two control loops are naturally decoupled. At stage 2, to minimize the impact of frequency control to DC voltage profile, FC-WAF can be designed using a similar procedure as described in Section 4.3, as briefly explained below. Assume the $j$th
converter is tripped, at the new steady state, we get from (4.4):

\[
\sum_{i=1}^{n} P'_{s,i} = \sum_{i=1}^{n} P^0_{s,i} + \sum_{i=1}^{m} \left( -\frac{1}{k_{dc,i}} \Delta U'_{dc,i} \right) + \sum_{i=1}^{k} \left( \frac{1}{k_{f,i}} \Delta f'_{i} \right)
\]

(4.10)

where \( P^0_{s,i} \) are the original power references of the converters before the outage of the \( j \)th converter, and primes indicate the new steady state after the converter outage.

From power balance within the MTDC grid, the term on the left side of (4.10) is zero at the new steady state (with the assumption of a lossless MTDC grid). The first term on the right side of (4.10) is equal to \(- P^0_{j} \), since \( \sum_{i=1}^{n} P^0_{s,i} + P^0_{j} = 0 \) as indicated in (4.5). Solve for \( \Delta U'_{dc,i} \) from (4.10):

\[
\Delta U'_{dc} = \frac{-P^0_{s,j} + \sum_{i=1}^{k} \frac{1}{k_{f,i}} \Delta f'_{i}}{\sum_{i=1}^{m} \frac{1}{k_{dc,i}}}
\]

(4.11)

In order to mitigate the DC voltage deviation, set (4.11) to zero and solve for \( f'_{i}^{ref} \) gives:

\[
f'_{i}^{ref} = \sum_{i=1}^{k} \left( \frac{1}{k_{f,i}} \right) f'_{i} + \left( !ST_j \right) P^0_{s,j} \sum_{i=1}^{m} \frac{1}{k_{dc,i}}
\]

(4.12)

where \( !ST_j \) is the inverse of the Boolean variable \( ST_j \) (the status of the \( j \)th converter), to generalize (4.12) for cases without converter outages. The first term in (4.12) is the same as that in (4.8), except that the contribution from the tripped \( j \)th VSC is removed. Note that (4.12) also accounts for the case when converter \( j \) does not participate in frequency control before it is tripped (i.e. \( j > k \)). In this case, the first term in (4.12) is exactly the same as that in (4.8). The second term in (4.12) accounts for the effect of the \( j \)th converter outage. Therefore, besides the frequencies at the converter terminals, the status and the active power of each VSC should also be communicated between converters, so that the frequency reference can be adapted from (4.8) to (4.12) upon any converter outages.

4.3.3 On the design of frequency controller parameters

The contribution of the VSC-MTDC system to frequency support mainly depend on the droop constant \( k_f \) and the saturation parameters \( (\Delta P_{s,f,\text{max}}, \Delta P_{s,f,\text{min}}) \) of each VSC (see Figure 4.1).
The values of those parameters depend on the amount of effort needed from the VSC-MTDC system to provide frequency control, according to the requirements of Transmission System Operators (TSOs). For example, similar values to those used in the frequency droop controllers of synchronous generators could be used \((k_f = 4 - 5\%)\) [44]. Nevertheless, lower values of \(k_f\) could be used \((k_f = 0.2 - 1\%)\) [45] to increase the contribution of the VSC-MTDC to frequency control. The latter is relevant for emergency frequency control and would be the approach used in this work. Furthermore, more complex schemes could also be used depending on the frequency deviation. For example, a deadband could be used to disable the controller action during small transients in steady state; a value of \(k_f = 4 - 5\%\) could be used in a certain range of frequency deviations and values of \(k_f = 0.2 - 1\%\) could be used for frequency deviations above a certain threshold. From the view of AC systems that provide frequency support, the resulted VSC power changes act as disturbances. The subsequent frequency variations depend on its system parameters, especially those affecting its frequency response; for example, the inertia constants of generators, parameters of generator governors and any other frequency responsive controls. When selecting the frequency droop constant \(k_f\), it is important to make sure that the selected value will not cause any transient instability or load shedding in the assisting AC system. One principle that can be used at the reliability planning stage is that the value of \(k_f\) should be selected such that for the largest credible contingencies in other AC systems, the assisting AC system should not experience any undamped power or voltage oscillations, or load shedding. An alternative principle is that the support is provided only insofar as no load shedding occurs in the assisting system. This can be achieved by monitoring the local frequency to make sure it is above the highest UFLS tripping point. Besides, the local frequency should be higher than the frequency reference of FC-WAF as an indication that the AC system with this VSC is assisting others.

Saturation parameters \((\Delta P_{s,f,max}, \Delta P_{s,f,min})\) determine the available power changes from each VSC for frequency support. For example, \((\Delta P_{s,f,max}, \Delta P_{s,f,min}) = \pm 1\text{p.u.}\) allows each VSC to change its active power injection by a maximum of its base MVA for frequency support, while obeying the operating limits of each converter. Lower values of saturation parameters would further
bound the contribution of each VSC to frequency support, as will be studied on the realistic large scale system in Section 6.3.5.

4.4 Case Study

4.4.1 Test system

In order to study the proposed the control strategy, an AC-MTDC test system is developed as shown in Figure 4.2. This test system is designed to emulate the case where two large AC interconnections such as the US Western Interconnection (WI) and Eastern Interconnection (EI) are connected via a VSC-MTDC system. Each AC system is a modified IEEE benchmark 2-area test system [99], with a governor model added to each generator using the IEEEG1 model whose parameters are chosen based on the typical values for steam turbines proposed in [100]. The loads in both areas are modeled as 100% constant current for active power and 100% constant impedance for reactive power, respectively. A five block under frequency load shedding (UFLS) scheme is adopted for all loads with a highest tripping frequency of 59.3Hz according to the WECC off-nominal frequency load shedding plan [101], as shown in Table 4.1. When the frequency at the load terminal stays under the corresponding frequency set-points for the under-frequency pick-up time (14 cycles), 5.6% of original load will be shed in 3 cycles. This UFLS scheme is realized by the UFLS model “LDS3BL” in PSS/E. Detailed power flow and dynamic data of the AC systems can be found in [99], with additional modifications described in Section 4.4.1.1.

As for the VSC-MTDC network, the nominal DC voltage is ±320kV. VSC ratings are 500MVA. The dynamic model structure corresponds to those described in Section 3.4. The VSC model, the MTDC grid model and the droop controllers (for both AC frequency and DC voltage) are written in FORTRAN as PSS/E user defined models and consists of a “generator-type” model for each VSC, one “governor-type” model for the MTDC grid, and an “exciter-type” model for each set of droop controllers. All VSCs are equipped with both frequency droop and DC voltage droop controllers. The DC voltage droop constant \( k_{dc} \) and frequency droop constant \( k_f \) are set to 0.1p.u. and 0.005p.u. on converter MVA base for each VSC, respectively. All VSCs are in \( P_s \) and \( Q_s \)
control mode, with $Q_{s}^{\text{ref}} = 0$ and $P_{s}^{\text{ref}}$ determined by (4.1). The power flow and dynamic model parameters of the VSC-MTDC system are summarized in Table 4.3.

4.4.1.1 Dynamic data of the AC-MTDC test system

The dynamic data for the generators, exciters and PSSs of the AC systems in the AC-MTDC test system can be found in [99]. The generators are all modeled by the 6th order GENROU model in PSS/E. Each generator is equipped with a 5th order exciter model ESST1A, and a 3rd order PSS model IEEEST. These models are inherited from the IEEE benchmark system in [99]. Besides, in order to properly simulate frequency response of power systems, a governor model is added to each generator using a 4th order IEEEG1 model, whose parameters are chosen based on the typical
values for steam turbines proposed in [100]. The model structure of IEEEG1 is shown in Figure 4.3, and the parameter values are shown in Table 4.2.

To emulate the impact of renewable integration, which are reduced system inertia and reduced online spinning reserves due to the de-commitment of conventional generators, the inertia constant of each synchronous generator is reduced by approximately a half. The inertia constants of generators 101, 102, 201 and 202 are reduced from 6.5s to 3.5s. Those of generator of 103, 104, 203, 204 are reduced from 6.175 to 3.175. The effective inertia for each AC system which is the summation of all inertia constants of all generators is thus reduced accordingly. Besides, to emulate the de-commitment of conventional machines, the nameplate MVA for each generator is reduced from 900MVA to 600MVA, and the maximum active power output for each generator is reduced from 765MW to 550MW to simulate less online spinning reserves. These modifications are simplified ways to emulate renewable integration to demonstrate more clearly the advantages of the proposed VSC frequency control.

4.4.1.2 Design of the operating point

The steady state operating point of the test case is modified to proportionally emulate the percentage of load transmitted between the WI to EI in the practical case developed in [24]. In [24], to capture the load diversity benefits, the load in WI is reduced by 14GW (about 7.5% of the total load in WI) to simulate a slightly off-peak condition in WI, and the peaking generation

<table>
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<tr>
<th>Load Shedding Block</th>
<th>Load Dropped (% of original)</th>
<th>Frequency Set-point (Hz)</th>
<th>Under-frequency Pickup Time (cycle)</th>
<th>Breaker Time (cycle)</th>
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<td>3</td>
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<td>5.6</td>
<td>59.2</td>
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<td>3</td>
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</tr>
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<td>5.6</td>
<td>58.6</td>
<td>14</td>
<td>3</td>
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Table 4.2: Governor model IEEEG1 parameters

<table>
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<th>Parameter</th>
<th>Value</th>
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</tr>
<tr>
<td>T1 (sec)</td>
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</tr>
<tr>
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<tr>
<td>T3 (&gt;0) (sec)</td>
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</tr>
<tr>
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</tr>
<tr>
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<tr>
<td>$P_{MAX}$ (pu on machine MVA rating)</td>
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</tr>
<tr>
<td>$P_{MIN}$ (pu on machine MVA rating)</td>
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</tr>
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<td>K2</td>
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<tr>
<td>K3</td>
<td>0.4</td>
</tr>
<tr>
<td>K4</td>
<td>0</td>
</tr>
<tr>
<td>T6 (sec)</td>
<td>0.5</td>
</tr>
<tr>
<td>K5</td>
<td>0.3</td>
</tr>
<tr>
<td>K6</td>
<td>0</td>
</tr>
<tr>
<td>T7 (sec)</td>
<td>0</td>
</tr>
<tr>
<td>K7</td>
<td>0</td>
</tr>
<tr>
<td>K8</td>
<td>0</td>
</tr>
</tbody>
</table>

Figure 4.3: Governor model IEEEG1 [11]
Table 4.3: Major parameters of the VSC-MTDC in the AC-MTDC test system

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>HVDC configuration</td>
<td>symmetrical monopole</td>
</tr>
<tr>
<td>Converter rating</td>
<td>500 MVA</td>
</tr>
<tr>
<td>Nominal DC voltage</td>
<td>±320 kV</td>
</tr>
<tr>
<td>Converter DC voltage limits</td>
<td>1.1p.u., 0.9p.u.</td>
</tr>
<tr>
<td>VSC loss coefficients $a/b/c_{rec}/c_{inv}$ (on converter MVA base)</td>
<td>1.05, 1.65, 10.5, 15.7 ($\times 10^{-3}$p.u.)</td>
</tr>
<tr>
<td>DC line resistance $R_{dc_{i-j}}$</td>
<td>0.81Ω (for all 4 lines)</td>
</tr>
<tr>
<td>DC line inductance $L_{dc_{i-j}}$</td>
<td>51.84mH (for all 4 lines)</td>
</tr>
<tr>
<td>DC bus shunt capacitance $C_{dc_{i}}$</td>
<td>195 µF (for all 4 buses)</td>
</tr>
<tr>
<td>Converter time constants $\tau_d$, $\tau_q$</td>
<td>5ms, 5ms</td>
</tr>
<tr>
<td>Converter active power limits</td>
<td>±500MW</td>
</tr>
<tr>
<td>Converter reactive power limits</td>
<td>±200Mvar</td>
</tr>
<tr>
<td>Converter current limit</td>
<td>1 p.u. (P priority)</td>
</tr>
<tr>
<td>Filter time constants $T_f$, $T_w$</td>
<td>0.1s, 1000s</td>
</tr>
<tr>
<td>Droop constants $k_{dc}$, $k_f$ (on converter MVA base)</td>
<td>0.1p.u., 0.005p.u.</td>
</tr>
<tr>
<td>FC-WAF weights $\alpha_i$</td>
<td>1/4 (for all 4 converters)</td>
</tr>
</tbody>
</table>
in EI is turned off by the same amount. The excessive generation in WI is then transferred to EI via the HVDC network. This procedure is emulated in the developed AC-MTDC test system to create an operating point. Before interconnecting the west AC system (A1) and the east one (A2), there is no power interchange between the two. Each AC system is serving its own load which is 1800MW in total. Afterwards, the load in A1 is decreased by 135MW (7.5% of its total load), and the generation in A2 is decreased by the same amount to model the outage of peaking generation. The excessive generation of 135MW in the west AC system is then transferred to the east AC system via the VSC-MTDC network. The steady state active power of each generator and each load are shown in Figure 4.2. The power flow solution related to the VSCs (notations correspond to those in Figure 3.1) and DC branch active power flows are shown in Table 4.4 and Table 4.5, respectively.

### Table 4.4: Power flow solution related to VSCs

<table>
<thead>
<tr>
<th></th>
<th>VSC1</th>
<th>VSC2</th>
<th>VSC3</th>
<th>VSC4 (slack)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P_s$ (MW)</td>
<td>-65.00</td>
<td>-70.00</td>
<td>65.00</td>
<td>66.35</td>
</tr>
<tr>
<td>$Q_s$ (MVAR)</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>$u_s$ (p.u.)</td>
<td>1.019</td>
<td>1.033</td>
<td>1.019</td>
<td>1.032</td>
</tr>
<tr>
<td>$\delta_s$ (deg)</td>
<td>-16.19</td>
<td>-17.69</td>
<td>-14.09</td>
<td>-16.78</td>
</tr>
<tr>
<td>$P_c$ (MW)</td>
<td>-64.83</td>
<td>-69.81</td>
<td>65.17</td>
<td>66.52</td>
</tr>
<tr>
<td>$Q_c$ (MVar)</td>
<td>1.63</td>
<td>1.84</td>
<td>1.63</td>
<td>1.66</td>
</tr>
<tr>
<td>$e_c$ (p.u.)</td>
<td>1.017</td>
<td>1.030</td>
<td>1.022</td>
<td>1.035</td>
</tr>
<tr>
<td>$\delta_c$ (deg)</td>
<td>-17.63</td>
<td>-19.20</td>
<td>-12.66</td>
<td>-15.35</td>
</tr>
<tr>
<td>$P_{loss}$ (MW)</td>
<td>0.72</td>
<td>0.73</td>
<td>0.72</td>
<td>0.72</td>
</tr>
<tr>
<td>$U_{dc}$ (p.u.)</td>
<td>1.00025981</td>
<td>1.00026647</td>
<td>0.99999962</td>
<td>1.00000000</td>
</tr>
<tr>
<td>$P_{dc}$ (MW)</td>
<td>64.12</td>
<td>69.08</td>
<td>-65.88</td>
<td>-67.28</td>
</tr>
</tbody>
</table>

### Table 4.5: Active power flow on DC branches

<table>
<thead>
<tr>
<th></th>
<th>1-2</th>
<th>1-3</th>
<th>2-4</th>
<th>3-4</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P_{cci,j}$ (MW)</td>
<td>-1.68552</td>
<td>65.80317</td>
<td>67.39265</td>
<td>-0.96462</td>
</tr>
<tr>
<td>$P_{ccj,i}$ (MW)</td>
<td>1.68553</td>
<td>-65.78605</td>
<td>-67.37470</td>
<td>0.96462</td>
</tr>
</tbody>
</table>
4.4.2 Simulation results

Time domain simulation is performed in PSS/E with a time step of 0.0001s. The contingency used is the outage of Gen102 (450MW) in AC system 1 (A1). The resulting AC system frequencies with load shed percentage in each AC system are shown in Figure 4.4. The VSC active power injections and DC terminal voltages are shown in Figures 4.5 and 4.6, respectively.

As can be seen from Figure 4.4, FC-WAF provides more effective frequency support to A1 with a higher frequency nadir (59.18Hz) and lower load shedding amount (6%) than the traditional FC-PU scheme. This improved effectiveness of FC-WAF is a result of reduced coupling between the frequency droop and the DC voltage droop, as briefly explained below. With FC-PU, frequency controllers are activated for VSCs in A1 because of the frequency drop due to the generation loss. This increases power injections from VSC1 and VSC2 to A1 since the third term in (4.1) becomes positive. The resulted power mismatch in the MTDC grid produces drop in the DC voltage, which is propagated in the whole MTDC grid. The DC voltage droop controls at all VSC stations then
Figure 4.5: VSC active power injections
Figure 4.6: DC voltages
react to decrease the active power injections into their connected AC systems [50]. Consequently, active power is transferred from A2 to A1, using DC voltage as intermediate. Therefore, DC voltage deviation with FC-PU is unavoidable, and it is the reason why FC-PU works. On the other hand, with FC-WAF, the contribution from DC voltage droops of all converters are minimized by setting (4.7) to zero using the designed weights $\alpha_i$’s as shown in (4.9). Therefore, the counteraction from DC voltage droop controllers is minimized, resulting in improved frequency control effectiveness and thus improved frequency nadir in A1, with which the load shedding in A1 is reduced. Note that since more power is extracted from A1 with FC-WAF, the frequency nadir in A2 is lower than that with FC-PU. However, it is still above the highest UFLS tripping frequency and therefore results in no load shedding. Notice that the steady-state frequency deviation in A1 obtained with FC-WAF is greater than the ones obtained with no control and with FC-PU (Figure 4.4). This is because the load shed is much more without control and with FC-PU than with FC-WAF. Performance improvement is also verified in Figure 4.5 which shows that FC-WAF is able to extract more power from A2.

On the DC side, we observe from Figure 4.6 that FC-PU causes severe impact to the DC voltage profile reaching a very low DC voltage nadir of about 0.91p.u., whereas FC-WAF introduces unnoticeable impact to the DC voltage profile. This is as expected since the weights in FC-WAF are specifically designed to achieve this. The reason why it is not exactly zero is because of the existence of the resistances, which are assumed to be zeros when deriving the weights. Since the resistances are very small, DC voltages are impacted but very slightly. It can also be noticed that the DC voltages of VSC1 and VSC2 decrease whereas those for VSC3 and VSC4 increase. This is because the frequency in A1 is lower than the frequency reference $f^{ref}$ (the weighted average frequency) and the frequency in A2 is higher than $f^{ref}$.

The simulation results and the above analysis indicate that the proposed FC-WAF is superior to the traditional FC-PU in terms of both AC and DC side performances.
4.4.3 Performance of frequency controls with PVD control

As mentioned in Section 4.2.1, in addition to the conventional local (CV-L) voltage based droop control, the designed frequency control FC-WAF remains effective with other DC voltage droop control methods. To show this, we repeat the simulations in Section 4.4.2 using the well-known PVD (PVD) control [82] as basis. Average voltage of all converter DC terminals is used as feedback signal in the standard droop structure. If desired, an integral controller can be further added to the loop to bring steady state voltage back to its nominal value as those in [52]. It turns out that all quantities are almost the same as those with the CV-L voltage droop control (see Figure 4.4 through Figure 4.6). DC voltages have very slight differences, as shown in Figure 4.7 using VSC3 as an example. It can be seen that both FC-WAF and FC-PU perform almost the same with either voltage control method. FC-WAF remains effective by keeping the DC voltage deviation very close to its pre-disturbance value, whereas FC-PU results in severe DC voltage deviations with a low DC voltage nadir of 0.91p.u.

Figure 4.7: DC voltage at VSC3 (CV-L vs. PVD)
Figure 4.8: Frequencies with VSC2 out

Figure 4.9: $U_{dc,1}$ (others are similar)

Figure 4.10: VSC active power injections with VSC2 outage
4.4.4 Simulation of frequency controls with a converter outage

As mentioned in Section 4.3.2, with some adjustments, FC-WAF is still effective upon converter outages. To show this, we simulate the outage of VSC2 (rectifier, $P_{s,2} = -70$MW). The AC system frequencies, the DC voltage and VSC active power injections are shown in Figure 4.8 through Figure 4.10. Since A1 loses a “load”, its frequency increases. The maximum deviation is about 0.1Hz without frequency controls (small since converter power is small as compared to A1 loading). Even if no frequency control is enabled, the frequency in A2 is still impacted because all VSCs participate in power sharing with DC voltage droop. With frequency controls FC-WAF and FC-PU enabled, the system frequencies in A1 and A2 are both improved, which is expected since the frequency control intends to reduce the frequency deviations from the nominal value of 60Hz. FC-WAF performs slightly better as can be seen from the zoom-in plot in Figure 4.8. The DC voltage deviation with FC-WAF is also smaller than with FC-PU because of the designed global frequency reference (4.12), as shown in Figure 4.9.

4.5 Conclusions

A new frequency control scheme (FC-WAF) based on weighted average frequencies at converter AC terminals is proposed for VSC-MTDC to provide frequency support among asynchronous AC systems. As compared to the traditional frequency droop control scheme (FC-PU), the major benefit of FC-WAF is reduction of the interaction between the AC frequency control and the DC voltage control loops, resulting in a better DC voltage profile while the frequency control is activated. Consequently, FC-WAF can achieve higher frequency nadir in the system in need of frequency support, reducing the risk of load shedding and indirectly reducing the need of total online spinning reserves. The proposed controller is generic and could be applied in arbitrary systems. Time domain simulation is performed to illustrate these advantages of FC-WAF on an AC-MTDC test system. Moreover, the proposed FC-WAF has consistent performances with different DC voltage droop controls, and adapts to converter outages well.
The proposed control is implemented in the commercial grade software PSS/E, and thus is suitable to study large-scale realistic power systems and can be incorporated into the power system planning process at ISOs and utilities. In Chapter 6, the developed FC-WAF is validated on a new North American continental model with an HVDC Macrogrid, in which the robustness of FC-WAF to realistic communication delays and power saturation limits is also shown.
CHAPTER 5. ADAPTIVE DC VOLTAGE DROOP CONTROL OF VSC-MTDC CONSIDERING DC VOLTAGE DEVIATION AND POWER SHARING

5.1 Introduction

Besides providing ancillary services to AC systems as described in Chapter 4, the reliable operation of VSC-MTDC grid itself is an essential but challenging task. A major mission to ensure stable operation of an MTDC grid is to maintain a stable DC voltage in case of DC side contingencies such as a converter outage, since overvoltage can cause damage to the converter equipment whereas undervoltage can result in loss of converter control capability and even breaks down the whole MTDC system. This chapter proposes a new adaptive DC voltage droop control strategy taking both power sharing and DC voltage deviation into consideration. The main idea is to adaptively modify the DC voltage droop constants based on the operating condition of converters, so that the converters with larger power margins (as defined in Section 5.3) tend to share more power after a converter outage, so that the risk of overloading can be reduced for the heavily loaded converters. Moreover, DC voltage deviations are also considered when calculating the adaptive droop constants. Converters with large DC voltage deviations tend to prioritize DC voltage control. The DC voltage control effort can be adjusted flexibly depending on the DC voltage regulation requirement. Pilot voltage droop (PVD) control is adopted as basis of the proposed adaptive control to ensure precise power sharing based on droop constants as explained in more details in Section 5.2. The proposed control assesses power sharing capabilities more accurately than the existing adaptive droop control methods, can differentiate the converter outage direction, and yields better transient DC voltage profiles with less oscillations and shorter settling times.

The proposed control approach in this Chapter is similar to the ideas proposed in [74, 64] but possesses certain advantages over them. In [74], although the authors consider converter active
power headroom to avoid overloading, the calculation of headroom is not accurate and cannot reflect the actual power sharing capability of converters under certain circumstances. Because of this, it does not have the capability to differentiate a rectifier outage from an inverter outage, which is essential since it directly affects the calculation of adaptive droop coefficients and determines converter power sharing. Moreover, it does not consider DC voltage deviations when calculating the adaptive droops. The authors in [64] adds the consideration of DC voltage deviations to the formulation but the calculation of converter headroom suffers the same issue of not being able to differentiate the converter outage direction. Besides, it uses local voltage as feedback control signal, which reduces the accuracy of power sharing based on the adaptively designed droop constants. The proposed approach overcomes these issues by using power margin instead of headroom as an indication of converter power sharing capabilities, with which the converter outage direction can be differentiated (i.e. rectifier outage vs. inverter outage). Moreover, the designed approach takes DC voltage deviation into consideration when calculating the adaptive droops, whose contribution to droop constant calculations can be adjusted flexibly depending on the DC voltage regulation requirement.

5.2 PVD control

This section briefly introduces the pilot voltage droop (PVD) control technique [82] since it is essential to ensure precise power sharing among the rest converters in the event of a converter outage.

Figure 5.1: Pilot voltage droop
The control block of a basic PVD control is illustrated in Figure 5.1. The core difference with the conventional local voltage based droop control is that instead of using the local DC voltage for each converter, PVD control uses a common DC voltage $U_{dc,pilot}$, named as “pilot voltage”, as the feedback signal to all converters. Therefore, all converters receive the same DC voltage information. Mathematically, the active power reference of a converter $i$ with PVD control can be written as:

$$P_{s,i}^{ref} = P_{s,i}^0 - \frac{1}{k_{dc,i}}(U_{dc,pilot}^{ref} - U_{dc,pilot}) \tag{5.1}$$

, where $P_{s,i}^0$ is the initial active power reference, $k_{dc,i}$ is the DC voltage droop constant, $U_{dc,pilot}^{ref}$ and $U_{dc,pilot}$ are the reference and measured pilot DC voltage, respectively, which are the same for all converters. All converters are controlled to follow their active power references, therefore, the converter active power outputs are:

$$P_{s,i} = P_{s,i}^{ref} = \text{repeat (5.1)} \tag{5.2}$$

From (5.2), it is not difficult to see that the active power deviation ($P_{s,i} - P_{s,i}^0$), and thus the power sharing of each converter after a converter outage, is precisely determined by the droop constant $k_{dc,i}$, since $(U_{dc,pilot}^{ref} - U_{dc,pilot})$ is the same for all converters.

**Comparison with local voltage based droop control**  On the other hand, if local voltage based droop control is used as shown in Figure 5.2, power sharing after a converter outage will not only depend on the droop constant, but also the DC voltage deviations at each converter, which are different across the DC grid due to power flows and voltage drops in the DC grid. Therefore, for local voltage based droop control, there exists a trade off between the steady state DC voltage deviation and the accuracy of power sharing, which is analyzed in [67]. The authors proposed an optimization based approach to optimize the droop constants to tackle this trade off, with the conclusion that increasing the gains (i.e. reducing droop constants) reduces the steady state DC voltage deviation but sacrifices power distribution accuracy after a converter outage. The PVD scheme introduced in the previous section completely removes the dependence of power sharing on DC grid topology, and thus always ensures accurate power sharing after a contingency determined
by the designed droop constants. This is why it is used as basis to design the proposed adaptive control.

![Diagram](image)

Figure 5.2: Local voltage droop control

Methods of obtaining the pilot voltage at each converter The pilot voltage in the PVD scheme can be selected as any specific point in the DC grid, or alternatively as the average of the DC voltages of the converters [82], both of which have similar control performances. At each converter, the pilot voltage can be obtained through either communication between the converters, or local measurements with compensation [84, 85] for better robustness to communication latencies, or even a communication-free scheme [86].

5.3 Proposed Margin based Adaptive DC Voltage Droop Control (M-ADP-PVD)

5.3.1 Steady state analysis with fixed droop constants

For an MTDC grid with \( n \) converters, assume the first \( m \) converters participate in the PVD control, and the rest \( (n - m) \) converters are constant power converters, such as those connected with wind plants. For the first \( m \) converters, their active power outputs follow their references determined in (5.1), and for the rest \( (n - m) \) converters, their active power outputs are equal to their power references:

\[
P_{s,i} = \begin{cases} 
  P_{s,i}^0 - \frac{1}{k_{dc,i}} (U_{dc,pilot}^{ref} - U_{dc,pilot}) = P_{s,i}^0 & \text{at steady state, for } i = 1, \ldots, m \\
  P_{s,i}^0 & \text{, for } i = m + 1, \ldots, n
\end{cases}
\]  

(5.3)
Sum over the first \( m \) converters with PVD control:

\[
\sum_{i=1}^{m} P_{s,i} = \sum_{i=1}^{m} P_{s,i}^0 - (U_{dc,pilot}^{ref} - U_{dc,pilot}^0) \sum_{i=1}^{m} \frac{1}{k_{dc,i}}
\] (5.4)

From power balance within the MTDC grid (note that the positive direction of \( P_{s,i} \) is defined to be from the DC side to AC):

\[
\sum_{i=1}^{m} P_{s,i} + \sum_{i=m+1}^{n} P_{s,i} + P_{loss} = 0
\] (5.5)

### 5.3.1.1 Trip of a converter with PVD control

Assume the \( m \)th converter with PVD control is tripped, at the new steady state operating point after the converter outage, (5.5) becomes:

\[
\sum_{i=1}^{m-1} P'_{s,i} + \sum_{i=m+1}^{n} P'_{s,i} + P'_{loss} = 0
\] (5.6)

where the prime notation indicates the new operating point. Similarly as in (5.4), sum over the active power of the rest \((m-1)\) converters with PVD control:

\[
\sum_{i=1}^{m-1} P'_{s,i} = \sum_{i=1}^{m-1} P_{s,i}^0 - (U_{dc,pilot}^{ref} - U_{dc,pilot}^0) \sum_{i=1}^{m-1} \frac{1}{k_{dc,i}}
\] (5.7)

Substitute (5.7) into (5.6):

\[
\sum_{i=1}^{m-1} P_{s,i}^0 + \sum_{i=m+1}^{n} P_{s,i} + P_{loss} = (U_{dc,pilot}^{ref} - U_{dc,pilot}^0) \sum_{i=1}^{m-1} \frac{1}{k_{dc,i}}
\] (5.8)

Since the converters from \( m+1 \) to \( n \) are constant power converters, their active power outputs remain the same as those before the \( m \)th converter outage:

\[
P'_{s,i} = P_{s,i} = P_{s,i}^0, \text{ for } i = m+1, \ldots, n
\] (5.9)

Further notice that the initial power balance at steady state before the \( m \)th converter outage:

\[
\sum_{i=1}^{m-1} P_{s,i}^0 + P_{s,m}^0 + \sum_{i=m+1}^{n} P_{s,i}^0 + P_{loss} = 0
\] (5.10)

Substitute (5.9) and (5.10) to (5.8), we get:

\[
-P_{s,m}^0 - P_{loss} + P_{loss} = (U_{dc,pilot}^{ref} - U_{dc,pilot}^0) \sum_{i=1}^{m-1} \frac{1}{k_{dc,i}}
\] (5.11)
The total loss before and after the converter outage are approximately the same, therefore, it is reasonable to assume \( P_{\text{loss}} \approx P'_{\text{loss}} \), with which we can get the following from (5.11):

\[
(U_{\text{dc,pilot}}^{\text{ref}} - U'_{\text{dc,pilot}}) \approx -\frac{P_{s,m}^0}{\sum_{i=1}^{m-1} \frac{1}{k_{dc,i}}}
\] (5.12)

Finally, substitute (5.12) to (5.3), we can obtain the active power outputs of the converters at the new steady state point after the outage of the \( m \)th converter as:

\[
P'_{s,i} \approx \begin{cases} 
  P_{s,i}^0 + \frac{P_{s,m}^0}{\sum_{i=1}^{m-1} \frac{1}{k_{dc,i}}} & , \text{for } i = 1, \ldots, m - 1 \\
  P_{s,i}^0 & , \text{for } i = m + 1, \ldots, n
\end{cases}
\] (5.13)

5.3.1.2 Trip of a converter with constant power control

Assume the \( n \)th converter with constant control is tripped, at the new steady state operating point after the converter outage, (5.5) becomes:

\[
\sum_{i=1}^{m} P''_{s,i} + \sum_{i=m+1}^{n-1} P''_{s,i} + P''_{\text{loss}} = 0
\] (5.14)

, where the double prime notation indicates the new operating point after the constant power converter outage. Sum over the active power of the first \( m \) converters with PVD control:

\[
\sum_{i=1}^{m} P''_{s,i} = \sum_{i=1}^{m} P_{s,i}^0 - (U_{\text{dc,pilot}}^{\text{ref}} - U''_{\text{dc,pilot}}) \sum_{i=1}^{m} \frac{1}{k_{dc,i}}
\] (5.15)

Substitute (5.15) into (5.14):

\[
\sum_{i=1}^{m} P_{s,i}^0 + \sum_{i=m+1}^{n-1} P''_{s,i} + P''_{\text{loss}} = (U_{\text{dc,pilot}}^{\text{ref}} - U''_{\text{dc,pilot}}) \sum_{i=1}^{m} \frac{1}{k_{dc,i}}
\] (5.16)

The active power outputs for the rest constant power converter from \( m + 1 \) to \( n - 1 \) remain the same as those before the \( n \)th converter outage:

\[
P''_{s,i} = P_{s,i} = P_{s,i}^0, \text{ for } i = m + 1, \ldots, n - 1
\] (5.17)

Further notice that the initial power balance at steady state before the converter outage:

\[
\sum_{i=1}^{m} P_{s,i}^0 + \sum_{i=m+1}^{n-1} P_{s,i}^0 + P_{\text{loss}} = 0
\] (5.18)
Substitute (5.17) and (5.18) to (5.16), we get:

\[-P_{0s,n} - P_{loss} + P''_{loss} = (U_{dc,pilot}^{ref} - U_{dc,pilot}^\prime) \sum_{i=1}^{m} \frac{1}{k_{dc,i}}\]  
(5.19)

The total loss before and after the converter outage are approximately the same, therefore, it is reasonable to assume \( P_{loss} \approx P''_{loss} \), with which we can get the following from (5.19):

\[(U_{dc,pilot}^{ref} - U_{dc,pilot}^\prime) \approx \frac{-P_{0s,n}}{\sum_{i=1}^{m} \frac{1}{k_{dc,i}}}\]
(5.20)

Finally, substitute (5.20) to (5.3), we can obtain the active power outputs of the converters at the new steady state point after the outage of the \( n \)th converter as:

\[P''_{s,i} \approx \begin{cases} 
P_{0s,i} + \frac{P_{0s,n}}{k_{dc,i}} \sum_{i=1}^{m} \frac{1}{k_{dc,i}}, & \text{for } i = 1, \ldots, m \\
1 \times P_{0s,n}, & \text{for } i = m + 1, \ldots, n - 1 
\end{cases}\]
(5.21)

### 5.3.1.3 Conclusions from steady state analysis

From (5.13) and (5.21), it can be concluded that for any converter outage, either a PVD controlled converter or a converter with constant power control, the power mismatch will be shared within the rest converters with PVD control based on their relative droop constants: \( (m-1) \) converters in case of an outage of PVD controlled converter based on (5.13) and \( m \) converters for a constant power converter outage based on (5.21).

### 5.3.2 Methodology of the proposed M-ADP-PVD

The droop constants of fixed droop control are usually determined by the ratings of converters, similarly as those power-frequency droops in the generator governors in the AC systems. An analytical method to determine the fixed droop constants based on desired power sharing, and also with limits of pilot voltage deviation considered, is presented in [83] with post disturbance steady state analysis. However, a critical issue related with fixed droop voltage control is that it does not consider the loading of the converters, and their availability to share the power unbalance caused by converter outages at the current operating condition, which may cause certain converters to hit
their limits while providing voltage support. This is the motivation of designing a control with adaptive droops.

A desired adaptive droop should take the actual capability of a converter to share the power unbalance into consideration, so that converters close to their power limits will share less power. Moreover, it is desired that it can also consider the limits of the local DC voltage deviation at its terminal, so that those converters with large DC voltage deviation (i.e. more stressed) tend to prioritize DC voltage control, while keeping the power within operational limits. To this end, an adaptive DC voltage droop control is proposed for converter \( i \) with the adaptive droop constant defined as:

\[
k_{adp,dc,i} = \beta_i k_{dc,i} \left( \xi - |\Delta U_{dc,i}| \right) / P_{margin,i}
\]

(5.22)

where \( k_{dc,i} \) is the initial DC voltage droop coefficient in steady state, typically determined by converter ratings. \( \xi \) is the user defined maximum DC voltage deviation, typically 5% or 10%. \( |\Delta U_{dc,i}| \) is the absolute value of the local DC voltage deviation from its pre-disturbance operating point. \( P_{margin,i} \) is the power margin of each converter to indicate the actual capability of the converter for power sharing, as detailed in the next paragraph. \( \gamma_i > 0 \) is an adjustable odd integer to determine the contribution of DC voltage control effort. A larger (smaller) \( \gamma_i \) puts more (less) emphasis on the regulation of DC voltage, when providing power sharing based on power margins. \( \beta_i \) is a positive constant to ensure \( k_{adp,dc,i} = k_{dc,i} \) in steady state. Since the designed control uses power margin as indicator of converter sharing capability, and utilizes PVD control as the basic control structure, it is named as margin based adaptive PVD control (M-ADP-PVD).

**Power margin and differentiation of converter outage direction** For each converter \( i \), the power margin \( P_{margin,i} \) in (5.22) is defined as [80]:

\[
P_{margin,i} = P_{max,i} + \text{msgn}(\Delta P_{step})P_{s,i}
\]

(5.23)
where $P_{\text{max},i}$ and $P_{s,i}$ are the maximum and steady state active power of each converter $i$, respectively, and $msgn()$ denotes a modified sign function:

$$
msgn(x) = \begin{cases} 
+1, & \text{if } x \geq 0 \\
-1, & \text{if } x < 0 
\end{cases}
$$ (5.24)

$\Delta P_{\text{step}}$ represents a step change of power in the DC grid, for example, a converter outage. A positive $\Delta P_{\text{step}}$ indicates a rectifier loss, causing power deficiency in the MTDC grid, and a negative $\Delta P_{\text{step}}$ indicates an inverter loss, causing power surplus in the MTDC grid. A conceptual illustration of the power margin is shown in Figure 5.3, where the shaded bar indicates the power margin in the rest of converters for a rectifier outage, and the blank bar indicates that for an inverter outage.

As a comparison, the conceptual illustration of power headroom as used in the related works [74, 64] is shown in Figure 5.4, in which the power headroom is defined to be the difference between the maximum power and the absolute value of the converter active power output. Comparing Figure 5.4 to Figure 5.3, it can be easily observed that power headroom is not an accurate indicator of the actual capability of converter to provide power sharing, in case of a rectifier outage for an inverter, and in case of an inverter outage for a rectifier.
It is now clear that the proposed adaptive droop using power margin has the capability to
differentiate the converter outage direction, yielding a more accurate assessment of power sharing
capabilities within the rest of converters. This advantage comes at an additional cost of communi-
cating the sign of $\Delta P_{step}$ once a converter outage is detected. After a converter outage event, the
failed converter station will send the value of $msgn(\Delta P_{step})$ based on its operating mode (either
a rectifier or an inverter) to the rest of converter stations. Unlike that in [80] which utilizes a
local voltage droop based control, the proposed adaptive droop control is based on a PVD control
framework. Therefore, this binary-like value can be obtained in a similar way of obtaining the in-
formation of the pilot voltage. Therefore, the communication aspect for PVD control also applies,
as those discussed in Section 2.2.3.3.

As a summary, the proposed M-ADP-PVD control modifies the droop constants considering
the loading of the converters, and can differentiate the direction of converter outages (rectifier vs.
inverter), and thus it can distribute power sharing more accurately according to the “actual” capa-
bility of each converter, i.e. the power margin. Besides, the adaptive droop calculation also takes
the DC voltage deviation into consideration, with additional flexibility to adjust its contribution
according to the need of regulating DC voltage. These benefits will be verified by case studies in the following section.

### 5.3.3 On the impact of frequency controllers to DC voltage control

If no frequency controllers are enabled in the converters, VSCs will only respond to DC side power unbalances caused by, for example, converter outages. For AC side disturbances, the VSC-MTDC acts as a firewall, meaning that a disturbance that happens in one of the AC systems cannot be seen by others. As discussed in Chapter 4, VSCs can be equipped with supplementary frequency controllers on top of the DC voltage droop control to provide ancillary services to underlying AC systems, such as primary frequency support. In this case, DC voltage will be impacted by frequency controllers since VSCs also respond to AC side disturbances, but the impact can be mitigated with certain frequency control strategies such as FC-WAF developed in Chapter 4. In that Chapter, conventional decentralized local DC voltage droop method is used as basis of DC voltage control, with which two major frequency controls are compared: the traditional local frequency droop FC-PU and the developed global frequency droop control FC-WAF. It is shown that FC-PU causes severe DC voltage deviation in order to provide primary frequency support between different AC systems for generation outage in AC systems, and FC-WAF overcomes this major drawback by utilizing a global reference signal, which can reflect the overall frequency dynamics of all AC systems. It is also shown that the proposed frequency control adapts well to converter outages.

In this Chapter, instead of the conventional local (CV-L) voltage droop control, PVD control is used as basis for DC voltage control, and adaptation is added to the DC voltage droop constants in the proposed margin based adaptive PVD control (M-ADP-PVD). However, similar conclusions can be drawn that for generation outages in AC systems, the traditional local frequency droop control FC-PU will also cause more DC voltage deviation than with FC-WAF. The main reason behind this is that in the design process of FC-WAF, a lossless MTDC grid is assumed, which makes the DC voltage the same all across the MTDC grid. Therefore, the DC voltage deviation expression $\Delta U_{dc}$ would be the same regardless of the DC voltage droop control scheme, either CV-L or M-ADP-PVD.
The detailed analysis can be found in Section 4.3.1. The difference, however, is that M-ADP-PVD also takes DC voltage deviation into consideration while adapting the DC voltage droop constants (more efforts with larger deviation). Moreover, the strength of DC voltage regulation is adjustable with a parameter $\gamma_i$ as shown in (5.22). Therefore, the DC voltage deviation with FC-PU will be less with M-ADP-PVD than that with CV-L under the same amount of generation outage, especially when stronger regulation of DC voltage is imposed. This is an advantage of M-ADP-PVD.

In case of DC side disturbances, analysis procedure in Section 4.3.2 applies. Similarly, due to the assumption of a lossless DC grid, the expression of DC voltage deviation $\Delta U_{dc}$ would be the same for both CV-L (which uses $\Delta U_{dc,i}$ but assumes to be a common number $\Delta U_{dc}$ with the assumption of a lossless DC grid) and M-ADP-PVD (which uses $\Delta U_{dc,pilot}$ and by itself is a common signal to all converters). The analysis is briefly outlined below highlighting the difference of using a common pilot voltage $U_{dc,pilot}$ in this Chapter instead of local DC voltages $U_{dc,i}$ in Chapter 4. For a VSC-MTDC system $n$ converters, assume the first $k$ converters participate in the frequency droop control, the first $m$ converters participate in only the PVD control, and the rest ($n - m$) converters are constant power converters, such as those connected with wind plants ($k \leq m \leq n$), the total active power of each converter $i$ at steady state follows:

$$P_{s,i} = \begin{cases} \frac{P^0_{s,i}}{k_{dc,i}} - \frac{1}{k_{dc,i}}(U_{dc,pilot}^{ref} - U_{dc,pilot}) + \frac{1}{k_{f,i}}\Delta f_i & \text{at steady state, for } i = 1, \ldots, k \\ \frac{P^0_{s,i}}{k_{dc,i}} & \text{at steady state, for } i = k + 1, \ldots, m \\ \frac{P^0_{s,i}}{k_{dc,i}} & \text{for } i = m + 1, \ldots, n \end{cases}$$

(5.25)

Suppose the $j$th converter is tripped, and sum over all $n$ converters:

$$\sum_{i=1}^{n} P'_{s,i} = \sum_{i=1}^{n} P^0_{s,i} + \sum_{i=1}^{m} (-\frac{1}{k_{dc,i}}\Delta U'_{dc,pilot}) + \sum_{i=1}^{k} (\frac{1}{k_{f,i}}\Delta f'_i)$$

(5.26)

where $P^0_{s,i}$ are the original power references of the converters before the outage of the $j$th converter, and primes indicate the new steady state after the converter outage.

From power balance within the MTDC grid, the term on the left side of (5.26) is zero at the new steady state (with the assumption of a lossless MTDC grid). The first term on the right side...
of (5.26) is equal to \(-P^0_j\), since \(\sum_{i=1\atop i \neq j}^n P^0_{s,i} + P^0_j = 0\) as indicated in (4.5). Solve for \(\Delta U'_{dc,pilot}\) from (5.26):

\[
\Delta U'_{dc,pilot} = \frac{-P^0_{s,j} + \sum_{i=1\atop i \neq j}^k \frac{1}{k_{f,i}} \Delta f'_i}{\sum_{i=1\atop i \neq j}^n \frac{1}{k_{dc,i}}} = (4.11)
\]

(5.27)

The VSC power injections at the new steady state can be obtained by substituting (5.27) to (5.25):

\[
P'_{s,i} \approx \begin{cases} 
P^0_{s,i} + \frac{-P^0_{s,j} + \sum_{i=1\atop i \neq j}^k \frac{1}{k_{f,i}} \Delta f'_i}{k_{dc,i} \sum_{i=1\atop i \neq j}^n \frac{1}{k_{dc,i}}} + \frac{1}{k_{f,i}} \Delta f_i, & \text{for } i = 1, \ldots, k \\
P^0_{s,i} + \frac{-P^0_{s,j} + \sum_{i=1\atop i \neq j}^k \frac{1}{k_{f,i}} \Delta f'_i}{k_{dc,i} \sum_{i=1\atop i \neq j}^n \frac{1}{k_{dc,i}}}, & \text{for } i = k + 1, \ldots, m \\
P^0_{s,i}, & \text{for } i = m + 1, \ldots, n 
\end{cases}
\]

(5.28)

Comparing (5.27) to (5.12) and (5.20), it can be observed that if no frequency controllers are enabled (i.e. \(k = 0\)), (5.27) is a general form which exactly describes both (5.12) and (5.20). Similarly, comparing (5.28) to (5.13) and (5.21), it can also be observed that without frequency control (i.e. \(k = 0\)), (5.28) is a general form which describes exactly (5.13) and (5.21).

If frequency controllers are enabled (i.e. \(k > 0\)), they will have impact to the DC voltage profiles, whose impact is reflected in the second term in the numerator of (5.27). As discussed in Section 4.3.2, the impact of frequency controllers to DC voltage profile can be mitigated by setting the numerator of (5.27) to zero, as done in the developed frequency control scheme FC-WAF. On the other hand, if local frequency control FC-PU is used, its impact to DC voltage under converter outages will be more than that with FC-WAF since the numerator of (5.27) is not minimized.

### 5.4 Case Study

#### 5.4.1 Test system

The same test system developed in Chapter 4 is used to study the adaptive DC voltage control strategy, with modified operating condition, as shown in Figure 5.5. The DC power transfer from A1 to A2 is increased to further stress the converters to demonstrate the effectiveness of the proposed DC voltage control. The ratings of the converters and their maximum power ratings are reduced.
(see Table 5.1) to further stress the case, making converters operate closer to their power limits. Other parameters of the AC-MTDC test system are the same as those described in Section 4.4.1.

The power flow solution related to the VSCs (notations correspond to those in Figure 3.1) and DC branch active power flows are shown in Table 5.2 and Table 5.3, respectively.

5.4.2 DC voltage droop control schemes simulated for comparison

Depending on the DC voltage reference used (local or pilot), and the feature of the DC voltage droop coefficients (fixed or adaptive), four types of different DC voltage control strategies in the existing literature are compared with the one proposed in this dissertation, as shown in Table 5.4.
Table 5.1: Modified converter operating point and power limits

<table>
<thead>
<tr>
<th>Converter</th>
<th>$P_s$ (MW)</th>
<th>$P_{\text{max}}$ (MW)</th>
<th>$P_{\text{min}}$ (MW)</th>
<th>Rating (MVA)</th>
</tr>
</thead>
<tbody>
<tr>
<td>VSC1</td>
<td>-85</td>
<td>100</td>
<td>-100</td>
<td>100</td>
</tr>
<tr>
<td>VSC2</td>
<td>-105</td>
<td>120</td>
<td>-120</td>
<td>120</td>
</tr>
<tr>
<td>VSC3</td>
<td>110</td>
<td>120</td>
<td>-120</td>
<td>120</td>
</tr>
<tr>
<td>VSC4 (slack)</td>
<td>$\approx$ 75</td>
<td>100</td>
<td>-100</td>
<td>100</td>
</tr>
</tbody>
</table>

Table 5.2: Power flow solution related to VSCs with high DC power transfer

<table>
<thead>
<tr>
<th></th>
<th>VSC1</th>
<th>VSC2</th>
<th>VSC3</th>
<th>VSC4 (slack)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P_s$ (MW)</td>
<td>-85.00</td>
<td>-105.00</td>
<td>110.00</td>
<td>75.05</td>
</tr>
<tr>
<td>$Q_s$ (MVAR)</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>$u_s$ (p.u.)</td>
<td>1.020</td>
<td>1.033</td>
<td>1.019</td>
<td>1.033</td>
</tr>
<tr>
<td>$\delta_s$ (deg)</td>
<td>-15.77</td>
<td>-17.66</td>
<td>-12.84</td>
<td>-16.57</td>
</tr>
<tr>
<td>$P_c$ (MW)</td>
<td>-84.72</td>
<td>-104.58</td>
<td>110.48</td>
<td>75.27</td>
</tr>
<tr>
<td>$Q_c$ (MVAR)</td>
<td>2.79</td>
<td>4.14</td>
<td>4.67</td>
<td>2.12</td>
</tr>
<tr>
<td>$e_c$ (p.u.)</td>
<td>1.017</td>
<td>1.030</td>
<td>1.025</td>
<td>1.036</td>
</tr>
<tr>
<td>$\delta_c$ (deg)</td>
<td>-17.66</td>
<td>-19.94</td>
<td>-10.42</td>
<td>-14.96</td>
</tr>
<tr>
<td>$P_{\text{loss}}$ (MW)</td>
<td>0.81</td>
<td>0.91</td>
<td>0.95</td>
<td>0.76</td>
</tr>
<tr>
<td>$U_{dc}$ (p.u.)</td>
<td>1.00031632</td>
<td>1.00036309</td>
<td>0.99993780</td>
<td>1.00000000</td>
</tr>
<tr>
<td>$P_{dc}$ (MW)</td>
<td>83.91</td>
<td>103.67</td>
<td>-111.43</td>
<td>-76.08</td>
</tr>
</tbody>
</table>

Table 5.3: Active power flow on DC branches with high DC power transfer

<table>
<thead>
<tr>
<th></th>
<th>1-2</th>
<th>1-3</th>
<th>2-4</th>
<th>3-4</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P_{cc,i,j}$ (MW)</td>
<td>-11.8290</td>
<td>95.7357</td>
<td>91.8374</td>
<td>-15.7256</td>
</tr>
<tr>
<td>$P_{ccj,i}$ (MW)</td>
<td>11.8295</td>
<td>-95.6995</td>
<td>-91.8041</td>
<td>15.7266</td>
</tr>
</tbody>
</table>
Table 5.4: DC voltage control strategies

<table>
<thead>
<tr>
<th>Control Strategy</th>
<th>$U_{dc}$</th>
<th>$k_{dc}$</th>
<th>$\Delta U_{dc}$</th>
<th>Power Limit</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Local</td>
<td>PVD</td>
<td>Fixed</td>
<td>Adaptive</td>
</tr>
<tr>
<td>Conventional local (CV-L) [67]</td>
<td>✓</td>
<td>✓</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Conventional PVD (CV-PVD) [82]</td>
<td>✓</td>
<td>✓</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Adaptive local (ADP-L) [64]</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>Adaptive PVD (ADP-PVD) [74]</td>
<td>✓</td>
<td>✓</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Proposed margin based adaptive PVD (M-ADP-PVD, $\gamma_i = 1, 3$)</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
</tbody>
</table>

**CV-L and CV-PVD**  The conventional local voltage based droop control (CV-L) and the conventional PVD control (CV-PVD) utilize fixed droop constants, and are those shown in Figure 5.2 and Figure 5.1, respectively.

**ADP-L**  The local voltage based adaptive droop (ADP-L) [64] is a scheme which uses local voltage as feedback signal (i.e. based on CV-L), but with adaptive droop constant defined as:

$$k_{dc,i}^{adp} = \beta_i k_{dc,i} \frac{\xi - |\Delta U_{dc,i}|}{P_{max,i} - |P_{s,i}|}$$

(5.29)

where $P_{max,i} - |P_{s,i}|$ is the headroom of the converter as illustrated in Figure 5.4. The meaning of the other variables are the same as defined in the proposed droop control in (5.22).

**ADP-PVD**  Another adaptive droop control scheme [74] in the literature is simulated, which uses PVD control as basis (i.e. based on CV-PVD), and the adaptive droop constant is defined as:

$$k_{dc,i}^{adp} = k_{dc,i} \left( \frac{1}{P_{max,i} - |P_{s,i}|} \right)^\lambda$$

(5.30)

where $P_{max,i} - |P_{s,i}|$ is the headroom of the converter as illustrated in Figure 5.4. $k_{dc,i}$ is the initial droop constant determined by converter ratings. $\lambda$ is user-defined positive constant, which
is set to be $1$ for the case study to equalize the contribution of power headroom from the ADP-L and power margin from the proposed M-ADP-PVD strategy.

**M-ADP-PVD** The developed margin based adaptive PVD scheme introduced in Section 5.3.2 is simulated, whose adaptive droop constants are defined as in (5.22). Two scenarios of M-ADP-PVD are simulated with different values of $\gamma_i$ ($\gamma_i = 1$ and $\gamma_i = 3$). The scenario with $\gamma_i = 3$ is supposed to have stronger regulation of the DC voltage.

As a summary, six different control scenarios are studied and results are shown in the following section.

### 5.4.3 Simulation results

Time domain simulation is performed in PSS/E with a time step of 0.0001s. For the control strategies using PVD control, DC voltage at the terminal of VSC4 is used as the pilot voltage. Steady state DC voltage droop constants are all set to 0.1p.u. on converter MVA base. All VSCs are in $P_s$ and $Q_s$ control mode with $Q_s^{ref} = 0$ and $P_s^{ref}$ determined by the type of DC voltage control strategy used. Only DC voltage droop controls are implemented and compared in this Section, and both rectifier outage and inverter outage are studied. The performance of the proposed control with frequency control enabled will be studied separately in Section 5.4.4.

#### 5.4.3.1 Rectifier outage

The performance of different control strategies is first compared with a rectifier outage. The contingency used is the outage of VSC1 at 1s, with power loss of $P_{s,1} = -85$MW.

**VSC active power outputs** The active power outputs of all converters are shown in Figure 5.6. From 5.6b, it shows that with the conventional droop controls with fixed droop contants (either CV-L or CV-PVD), the remaining rectifier in the system (VSC2) hits its lower limit of -120MW. Actually, the power reference of VSC2 reaches beyond -120MW, but is capped at -120MW because of the dc current limiter (max 1p.u.) of VSC to ensure the converter is not overloaded and
Figure 5.6: VSC active power injections with outage of VSC1 (rectifier)

damaged. On the other hand, the other four adaptive controls are all able to consider the operating condition of VSC2 so that it does not reach its limit, and distribute more power sharing to the other two remaining converters. Regarding the adaptive droop control schemes, as explained previously in Section 5.3.2, a critical issue with the existing schemes (ADP-L and ADP-PVD) is that they are not able to identify the “actual” power sharing capabilities of the remaining converters because of the usage of an inaccurate indicator - power headroom (see Figure 5.4). On the other hand, the proposed M-ADP-PVD scheme can accurately estimate the power sharing capabilities of the healthy converters by utilizing another indicator, which is the power margin (see Figure 5.3). The initial values of power margin and power headroom of each VSC upon the tripping of VSC1...
Table 5.5: Converter data at the outage of VSC1 in the test system

<table>
<thead>
<tr>
<th>Converter</th>
<th>$P_s$ (MW)</th>
<th>$P_{max}$ (MW)</th>
<th>$\Delta P_{step}$</th>
<th>Initial margin (MW, used in M-ADP-PVD)</th>
<th>Initial headroom (MW, used in ADP-L &amp; ADP-PVD)</th>
</tr>
</thead>
<tbody>
<tr>
<td>VSC1 (inv)</td>
<td>-85</td>
<td>100</td>
<td>+1</td>
<td>NA</td>
<td>NA</td>
</tr>
<tr>
<td>VSC2 (inv)</td>
<td>-105</td>
<td>120</td>
<td>+1</td>
<td>15</td>
<td>15</td>
</tr>
<tr>
<td>VSC3 (rec)</td>
<td>110</td>
<td>120</td>
<td>+1</td>
<td>230</td>
<td>10</td>
</tr>
<tr>
<td>VSC4 (inv)</td>
<td>$\approx 75$</td>
<td>100</td>
<td>+1</td>
<td>175</td>
<td>25</td>
</tr>
</tbody>
</table>

are shown in Table 5.5. It can be seen that ADP-L and ADP-PVD estimate the power sharing capabilities of VSC3 and VSC4 to be 10MW and 25MW, respectively. However, since the tripped converter VSC1 is a rectifier, as inverters, VSC3 and VSC4 have much more capability to provide power sharing (230MW for VSC3 and 175MW for VSC4, as conceptually illustrated in the shaded bar on the right in Figure 5.3). From Figures 5.6c and 5.6d, it can be seen that with ADP-L and ADP-PVD, VSC3 shares 68MW of power, which is almost 7 times of that shared by VSC4 (only 10MW), despite the fact that they both have high power margins to provide power sharing. The large discrepancy between the power sharing between VSC3 and VSC4 is because of the incorrect indicator - the power headroom, instead of the power margin used in M-ADP-PVD, when calculating the adaptive droop constants. On the other hand, the proposed M-APD-PVD is able to deploy the available active power of VSC3 and VSC4 more reasonably according to their power margins, with VSC3 sharing 40MW and VSC4 sharing 30MW. One other observation from Figure 5.6b is that the existing ADP-L and ADP-PVD schemes are more conservative to utilize VSC2 to provide power sharing (6MW), whereas the proposed M-PVD can exhaust more capability from the remaining rectifier VSC2 (11MW), while keeping it within its power limit.

Adaptive droop constants  The adaptive droop constants of all adaptive droop schemes are shown in Figure 5.7. The droop constant of VSC2 increases to reduce the control gain, so that it shares less power, and the droop constants of the other two converters (VSC3 and VSC4) decrease to share more power. Again, the advantage of the proposed M-ADP-PVD can be observed
Figure 5.7: Adaptive droop constants with outage of VSC1 (rectifier)
with more equal variation of $k_{dc,3}$ and $k_{dc,4}$, whereas ADP-L and ADP-PVD result in excessive low value of $k_{dc,3}$, which may lead to stability issues. The reducing stability level can be observed from the large oscillations in the active power outputs of VSC3 in Figure 5.6c with ADP-L and ADP-PVD. Note that a minimum droop value of 0.01 p.u. (on converter MVA base) is imposed in all adaptive control schemes to ensure stability, without which ADP-L and ADP-PVD would yield even lower droop constant for VSC3, which could cause instability. A more rigorous small signal analysis can be performed to identify the minimum droop constants to guarantee stability for each operating condition. One other thing can be noticed from Figure 5.7a is that for the proposed M-ADP-PVD with $\gamma = 3$, $k_{dc,2}$ initially decreases. This is because the control puts more emphasis on regulating the DC voltage deviation with a larger $\gamma$, indicating that initially the contribution from the numerator is larger than that of the denominator in (5.22). After a short time, the power margin of VSC2 further reduces and takes over the priority over DC voltage regulation, which causes $k_{dc,2}$ start to increase.

**DC voltages** The DC voltages of all converters are shown in Figure 5.8. Note that it is assumed the DC circuit of VSC1 remains connected after the converter is taken out, which requires DC circuit breakers to be available at this voltage level. The patterns of all DC voltages are very similar, with similar initial voltages due to small DC line resistances. From Figure 5.8, it can be seen that the conventional fixed droop controls CV-L and CV-PVD yield the largest steady state DC voltage deviation, whereas the existing adaptive DC voltage controls ADP-L and ADP-PVD result in the smallest DC voltage deviations, but with more transient fluctuations. The proposed M-ADP-PVD method lie in between. A trade off actually exist between the steady state DC voltage deviation and its transient fluctuations and setting time, which is discussed further in Section 5.4.3.3. Regardless, since the proposed M-ADP-PVD method has the flexibility to adjust the regulation of DC voltage using the parameter $\gamma$, it can be seen that by increasing $\gamma$ from 1 to 3, it is able to reduce the steady state DC voltage deviation. This can also be verified from Figure 5.7a that the variation of $k_{dc,2}$ with $\gamma = 3$ is less, indicating that it tends to enforce more regulation on
DC voltage deviation. Moreover, the proposed M-ADP-PVD control has less transient fluctuation and shorter settling time than the existing adaptive droop controls schemes.

5.4.3.2 Inverter outage

The performance of different control strategies is also compared with an inverter outage. The contingency used is the outage of VSC3 at 1s, with power loss of $P_{s3} = 110$MW. The initial values of power margin and power headroom of each VSC upon the tripping of VSC3 are shown in Table 5.6.
Table 5.6: Converter data at the outage of VSC3 in the test system

<table>
<thead>
<tr>
<th>Converter</th>
<th>$P_s$ (MW)</th>
<th>$P_{max}$ (MW)</th>
<th>$\Delta P_{step}$</th>
<th>Initial margin (MW, used in M-ADP-PVD)</th>
<th>Initial headroom (MW, used in ADP-L &amp; ADP-PVD)</th>
</tr>
</thead>
<tbody>
<tr>
<td>VSC1 (inv)</td>
<td>-85</td>
<td>100</td>
<td>-1</td>
<td>185</td>
<td>15</td>
</tr>
<tr>
<td>VSC2 (inv)</td>
<td>-105</td>
<td>120</td>
<td>-1</td>
<td>225</td>
<td>15</td>
</tr>
<tr>
<td>VSC3 (rec)</td>
<td>110</td>
<td>120</td>
<td>-1</td>
<td>NA</td>
<td>NA</td>
</tr>
<tr>
<td>VSC4 (inv)</td>
<td>$\approx$75</td>
<td>100</td>
<td>-1</td>
<td>25</td>
<td>25</td>
</tr>
</tbody>
</table>

In general, similar conclusions can be drawn as those from the rectifier outage described in Section 5.4.3.1, proving the advantages of the proposed M-ADP-PVD method in terms of 1) more accurate power sharing, 2) improved DC voltage transient performance and 3) capable of differentiating converter outage directions. The VSC active power outputs, and the adaptive droop constants of the four scenarios with adaptive control are shown in Figure 5.9 and Figure 5.10, respectively. The DC voltages of all converters are shown in Figure 5.11. The detailed result analysis is similar to those presented in Section 5.4.3.1 and thus is not repeated here for simplicity.

### 5.4.3.3 A trade off

From Figures 5.8 and 5.11, a trade off can be observed between the steady state DC voltage deviation and transient overshoot and settling time [83]. This phenomenon is intrinsic to the droop control structure, as analyzed in detail in [80]. Briefly speaking, from $\Delta P_s U_{dc} = -\frac{1}{k_{dc}} \Delta U_{dc}$, in which $\Delta U_{dc}$ could either be local voltage based or pilot voltage based, the steady state DC voltage deviation $\Delta U_{dc} = -k_{dc} \Delta P_s U_{dc}$. This indicates a smaller (larger) DC voltage deviation with a smaller (larger) droop constant. However, with a smaller droop constant (i.e. a larger gain), the transient overshoot is larger and it takes longer for the DC voltage to settle to a new steady state [80]. With the proposed M-ADP-PVD scheme, although it yields slightly larger steady state DC voltage deviations than those with the existing adaptive droop controls ADP-L and ADP-PVD, it has less transient fluctuations and faster settling time. The magnitude of the DC
Figure 5.9: VSC active power injections with outage of VSC3 (inverter)
Figure 5.10: Adaptive droop constants with outage of VSC3 (inverter)
Figure 5.11: DC voltages with outage of VSC3 (inverter)
voltage deviation is still a lot better than the conventional droop controls Conv-L and Conv-PVD. Moreover, the strength of DC voltage regulation in the proposed method M-ADP-PVD can be adjusted by modifying the parameter $\gamma$.

5.4.4 Performance of M-ADP-PVD with frequency controls

In Section 5.3.3, the impact of supplementary frequency controls on M-ADP-PVD is discussed for both DC and AC side disturbances. In this section, simulations are performed to verify the theoretical analysis. Parameter $\gamma$ is set to be 1 in the M-ADP-PVD scheme.

5.4.4.1 DC side disturbance

Similar to Section 4.4.4, the outage of VSC2 (rectifier, $P_{s,2} = -105$MW) is used as the contingency to evaluate the impact of different frequency controls on the developed M-ADP-PVD scheme. Two case scenarios with different VSC ratings are simulated.

**Scenario 1** The first scenario is with a VSC rating of 500MVA for all VSCs, similar to those used in Chapter 4. The AC system frequencies are shown in Figure 5.12. The DC voltage is shown in Figure 5.13. The VSC active power outputs and adaptive droop constants are shown in Figure 5.14 and Figure 5.15, respectively.

**Scenario 2** The second scenario is with smaller VSC ratings (i.e. less power headroom) as those shown in Table 5.1, which is also the scenario used in the previous case studies in this Chapter. The AC system frequencies are shown in Figure 5.16. The DC voltage is shown in Figure 5.17. The VSC active power outputs and adaptive droop constants are shown in Figure 5.18 and Figure 5.19, respectively.

Results in Scenario 1 generally match those in Section 4.4.4 where frequency controls are utilized on the basis of conventional voltage droop control (CV-L) rather than the proposed M-ADP-PVD method used here, which is not surprising since from Figure 5.15 it can be seen that the DC voltage droop constants are close to their original values of 0.1pu at steady state. Both frequency controls
can successfully provide primary frequency support from A2 to A2 and reduce frequency deviations in both AC systems. This shows that the designed frequency control FC-WAF in Chapter 4 also works well with the developed DC voltage control strategy M-ADP-PVD in this Chapter, demonstrating the robustness of FC-WAF, and the compatibility of M-ADP-PVD with additional supplementary controls. FC-WAF outperforms FC-PU with less DC voltage deviation as shown in Figure 5.13. In this scenario, frequency control dominates as can be seen from the modulation on VSC power injections with frequency controls as in Figure 5.14).

Results in Scenario 2 shows the performance of the combined DC voltage and frequency controls with limited converter headroom. In this case, the headroom of VSC1 (15MW) is very small as compared to the lost VSC2 (105MW), therefore, the power mismatch is mainly shared by VSC3 and VSC4, similar to the case in Section 5.4.3.1. As a result, frequencies in both A1 and A2 are impacted due to the converter outage of VSC2, even with frequency controls enabled, as shown in Figure 5.16. However, since the size of VSC2 is relatively small as compared to the AC system loading, neither AC system experiences any load shedding. Figure 5.17 shows that the DC voltage is slightly impacted due to the existence of frequency controls, with FC-WAF having less DC voltage deviation than FC-PU. The impact of frequency controls can be seen more clearly from
Figure 5.14: VSC active power injections with M-ADP-PVD (Scenario 1, VSC2 out)

Figure 5.15: DC voltage droop constants with M-ADP-PVD (Scenario 1, VSC2 out)
Figure 5.16: System frequencies with M-ADP-PVD (Scenario2, VSC2 out)

Figure 5.17: $U_{dc,1}$ (others are similar) with M-ADP-PVD (Scenario2, VSC2 out)

Figure 5.18a. Without frequency controls, VSC1 does not reach its power limit of -100MW because of the adaptive droop. However, with frequency controls enabled, the additional headroom of VSC1 is extracted to help mitigate frequency deviation, which can also be seen from the slight improvement of frequencies shown in Figure 5.16. Consequent, the power margin of VSC1 is close to zero, which would cause its adaptive droop to be close to be infinity. For simulation purpose (to avoid bouncing issues), a maximum value of 10 is imposed to the DC voltage droop constant, meaning that a minimum gain ($1/k_{dc}$) of 0.1pu is imposed. From Figure 5.19a we can see that $k_{dc,1}$ reaches this limit, equivalent to changing to a constant power converter since its power limit is reached. On the other hand, DC voltage droop constants of VSC3 and VSC4 reduce to provide more power sharing. The results in Scenario 2 show that the two controls work as expected with limited converter headroom and frequency controls have slight impact to DC voltage control in this condition.

5.4.4.2 AC side disturbance

Similar to Section 4.4.2, the outage of Gen102 (450MW) in A1 of the AC-MTDC test system is used as the contingency to compare different frequency control performances with the proposed
Figure 5.18: VSC active power injections with M-ADP-PVD (Scenario 2, VSC2 out)

Figure 5.19: DC voltage droop constants with M-ADP-PVD (Scenario 2, VSC2 out)
M-ADP-PVD, and also evaluate the impact of M-ADP-PVD on different frequency controls. The resulting AC system frequencies along with load shedding amount in percentage in each AC system are shown in Figure 5.20. The VSC active power injections and DC terminal voltages are shown in Figures 5.21 and 5.22, respectively. The adaptive droop constants are shown in Figure 5.23.

The general patterns of these results match with those from Section 4.4.2, showing the superiority of the developed frequency control FC-WAF with improved frequency nadir (thus less load shedding) and reduced DC voltage deviations, and also the compatibility of M-ADP-PVD to work with different frequency controls for AC side disturbances. However, the following differences can be observed. Comparing Figure 5.20 to Figure 4.4, it can be seen that both FC-PU and FC-WAF are less effective when working with M-ADP-PVD, especially for FC-PU. As a result, both controls yield a lower frequency nadir and more load shedding in A1 than those in Figure 4.4 which uses the conventional local (CV-L) DC voltage droop control as basis. The reduced effectiveness can also be seen by comparing Figure 5.21 and Figure 4.5, which shows that the active power changes are less
Figure 5.21: VSC active power injections with M-ADP-PVD (Gen102 out)

(a) VSC1 (VSC2 has similar pattern)  
(b) VSC3 (VSC4 has similar pattern)

Figure 5.22: DC voltages with M-ADP-PVD (Gen102 out)

(a) VSC1 (VSC2 has similar pattern)  
(b) VSC3 (VSC4 has similar pattern)
Figure 5.23: Adaptive DC voltage droop constants with M-ADP-PVD (Gen102 out)

with M-ADP-PVD based frequency controls than with CV-L based ones. The reason behind the reduced effectiveness, especially for FC-PU, which is impacted more, is that M-ADP-PVD takes DC voltage deviation into consideration while adapting the DC voltage droop constants (i.e. more DC voltage regulation with larger DC voltage deviation). Since FC-PU relies DC voltage deviation to function, it is adversely impacted by the DC voltage regulation in M-ADP-PVD. This can be also be verified by comparing Figure 5.22 to Figure 4.6. As can be seen from Figure 4.6, the DC voltage reaches a nadir of 0.91pu with CV-L based FC-PU, whereas the nadir with M-ADP-PVD based FC-PU is much higher, which is about 0.97pu. On the other hand, FC-WAF is impacted much less because of the fact that it does not rely on DC voltage deviation as intermediate for frequency control to function.

As a summary from Sections 5.4.4.1 and 5.4.4.2, M-ADP-PVD works better with FC-WAF for both DC and AC side disturbances. M-ADP-PVD prioritizes more the control performance on the DC side as compared to CV-L, and this constraints the functionality of FC-PU which relies on DC voltage deviation to be effective, especially in stressed system condition with large frequency deviations.
5.5 Conclusions

A new power margin based adaptive PVD control (M-ADP-PVD) is proposed for DC voltage control in VSC-MTDC systems. The main feature of the proposed control is that by utilizing the power margin, it is able to capture accurate power sharing capabilities of each converter in case of a converter outage. As a result, it can differentiate the outage of a rectifier with that of an inverter, and distributes the power unbalance more accurately according to the actual power sharing capabilities of the converters. Moreover, the DC voltage transient fluctuation is also reduced, with less settling time than the existing adaptive droop control schemes. The designed M-ADP-PVD has flexibility to adjust the strength of DC voltage regulation while providing power sharing, depending on the control requirement. The impact of supplementary frequency controls to the developed M-ADP-PVD is investigated for both AC and DC side disturbances, and it is shown that M-ADP-PVD works well with the frequency control FC-WAF proposed in Chapter 4. The advantages of the developed M-ADP-PVD are verified through time domain simulation on a developed AC-MTDC test system, and compared with several existing DC voltage control schemes, illustrating the superior performances of the proposed control in terms of both power sharing and DC voltage regulation.

The proposed control is implemented in the commercial grade software PSS/E, and thus is suitable to study large-scale realistic power systems and can be incorporated into the power system planning process at ISOs and utilities. In Chapter 6, the developed M-ADP-PVD is validated on a new North American continental model with an HVDC Macrogrid.
CHAPTER 6. STUDY OF NORTH AMERICAN CONTINENTAL HVDC WITH VSC-MTDC SYSTEMS

6.1 Introduction

This chapter studies the practical North American power system model with a continental HVDC overlay. A new HVDC Macrogrid model is developed including a six terminal VSC-MTDC system and several point to point LCC-HVDC links. The proposed primary frequency control FC-WAF in Chapter 4 and the margin based adaptive DC voltage control M-ADP-PVD designed in Chapter 5 are applied and compared with the existing methods on the developed 100k bus large scale planning model, demonstrating the effectiveness and scalability of the proposed control algorithms.

As briefly introduced in Section 1.1.3.3, in order to reduce the cost of developing and utilizing the renewable energy resources in the US, such as wind in the Midwest and solar in the southwest, the “Interconnection Seam Study” [36, 37, 102] proposes to interconnect the asynchronous AC systems via a continental HVDC overlay. Figure 6.1 shows the seams between the three asynchronous AC interconnections in the US, namely the Western Interconnection (WI), the Eastern Interconnection (EI) and the Electric Reliability Council of Texas (ERCOT); it also shows and the geographic distribution of different types of renewable resources [12] and the major load centers, which can be seen are typically far away from each other. Major economic benefits of such continental HVDC interconnection include access to richer renewable resources, inter-regional sharing of most economic resources on a diurnal bases taking advantage of time differences, and inter-regional sharing of capacity to satisfy each regions annual peak with consequential decrease in capacity investment. Several topologies have been proposed for a continental HVDC overlay, as previously shown in Figure 1.7, among which the design 3 is called “Macrogrid”. Economic analysis shows that the
Macrogrid could yield a benefit-to-cost ratio of 2.5 under carbon policy scenarios with growing carbon taxes [8].

Besides the above economic benefits, such continental HVDC connections also possess potential reliability benefits. In [24], the authors developed a transient stability model for an HVDC Macrogrid across the North American power grid, and illustrated the frequency support benefit of the Macrogrid, assuming all converters are line commutated converters (LCC) and all HVDC lines are point-to-point links. However, as the authors rightly pointed out in the paper, LCC may not be the best converter technology to implement the Macrogrid. Compared to LCC-point-to-point HVDC links, a VSC-MTDC grid possesses many advantages as follows (more details can be found in Section 1.1.2.2):

- It can significantly reduce the number of costly converter stations;
- It allows for tapping of load and generation (including renewables) in the middle of a DC line, which reduces the need for AC transmission needed to interconnect new renewable resources;
- It is easier to control and coordinate as a single entity to maintain generation and load balance [2].
• VSC has flexible and independent control of active power and reactive power control capabilities, whereas LCC always consumes reactive power and relies on a strong AC grid at its terminals to function.

These benefits of VSC-MTDC motivate our study to develop a continental HVDC model including VSC-MTDC, and quantify its potential reliability benefits. One thing to note is that although VSC-MTDC possess many advantages, currently the converter rating of VSC is still relatively low as compared to LCC. Therefore, only part of the Macrogrid-Base [24] is replaced by a VSC-MTDC system. As a result, we can take advantage of both the high capacity of LCC and the flexible power control capabilities of VSC along with those benefits mentioned above in a MTDC configuration. As the capacity of VSC increases with the technology, it is possible to design other configurations of an HVDC overlay composing of more VSCs to support more power transfer across the AC systems with more power control capabilities.

6.2 Modeling of a VSC-MTDC System into the North American Power Grid

A new HVDC Macrogrid model is developed [97], with the southern part of the Macrogrid (herein referred as “Macrogrid-Base”) proposed in [24] replaced by a six terminal VSC-MTDC system, based on the proposed configuration in [103], as shown in Figure 6.2. This configuration takes the maximum capacity of VSC (3000MW [20]) into consideration. The rest of the Macrogrid sections remain as LCC-HVDC links. We call this newly developed model “Macrogrid-VSC”. The asynchronous interconnections WI and EI are connected through two LCC-HVDC lines and a VSC-MTDC network. There are seven other HVDC back-to-back interties with relatively low capacities located at the WI-EI Seam, which are not shown here for simplicity. The power transfer directions in the HVDC network can either be from WI to EI, or vice versa, taking advantage of the time difference in each time zone with different load peaking times. The operating scenario used in this study is from EI to WI, where WI loading is at its peak and EI is supplying 14.4GW of load diversity capacity to the west. The steady state power system operating condition follows the “EI2WECC” case developed in [24]. The system operating condition including the power transfers on DC lines,
DC line losses, and the power exchanges with the underlying AC systems at the converter terminals are shown in Figure 6.2. Since there are mixed terminals of both LCC and VSC at Victorville and St. Louis, the operating condition of the VSC-MTDC network showing the active power injection of each VSC is detailed separately in Figure 6.3, along with the numbering of VSCs from 1 to 6. VSC1, VSC2 and VSC4 operate as inverters, and the other three as rectifiers. The reason of why VSC4 is an inverter is because we keep the same power scheduling on the two LCC-HVDC lines out of St. Louis, MO as those in the “EI2WECC” Macrogrid-Base case. The major parameters of the VSC-MTDC system is shown in Table 6.1. The total number of AC buses in the developed Macrogrid-VSC is 100,222; and the total number of generators is 10,631, supplying a total system load of 860GW. The rest of power flow and dynamic data summaries can be found in [24]. The power flow solution related to the VSCs (notations correspond to those in Figure 3.1) and DC branch active power flows are shown in Table 6.2 and Table 6.3, respectively.
Table 6.1: Major parameters of the VSC-MTDC in Macrogrid-VSC

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>HVDC configuration</td>
<td>symmetrical monopole</td>
</tr>
<tr>
<td>Converter Rating</td>
<td>3000MVA</td>
</tr>
<tr>
<td>Nominal DC Voltage</td>
<td>±640kV</td>
</tr>
<tr>
<td>Converter DC voltage limits</td>
<td>1.1p.u., 0.9p.u.</td>
</tr>
<tr>
<td>VSC loss coefficients, (a, b, c_{rec}, c_{inv}) (on converter MVA base)</td>
<td>0.875, 1.65, 12.6, 18.9 ((\times 10^{-3}\text{p.u.}))</td>
</tr>
<tr>
<td>DC line resistance, (R_{dc_{i-j}})</td>
<td>(R_{dc_{1-2}} = 1.43\Omega, R_{dc_{2-3}} = 6.31\Omega,)</td>
</tr>
<tr>
<td></td>
<td>(R_{dc_{3-4}} = 5.92\Omega, R_{dc_{4-5}} = 6.98\Omega,)</td>
</tr>
<tr>
<td></td>
<td>(R_{dc_{5-6}} = 2.82\Omega)</td>
</tr>
<tr>
<td>DC line inductance, (L_{dc_{i-j}})</td>
<td>(L_{dc_{1-2}} = 91.39\text{mH}, L_{dc_{2-3}} = 401.69\text{mH},)</td>
</tr>
<tr>
<td></td>
<td>(L_{dc_{3-4}} = 377.68\text{mH}, L_{dc_{4-5}} = 448.94\text{mH},)</td>
</tr>
<tr>
<td></td>
<td>(L_{dc_{5-6}} = 180.99\text{mH})</td>
</tr>
<tr>
<td>DC bus shunt capacitance, (C_{dc_{i}})</td>
<td>195(\mu)F (for all 6 buses)</td>
</tr>
<tr>
<td>Converter time constants, (\tau_d, \tau_q)</td>
<td>5ms, 5ms</td>
</tr>
<tr>
<td>Converter active power limits</td>
<td>±3000MW</td>
</tr>
<tr>
<td>Converter reactive power limits</td>
<td>±1000Mvar</td>
</tr>
<tr>
<td>Converter current limit</td>
<td>1 p.u. (P priority)</td>
</tr>
<tr>
<td>Filter time constants, (T_f, T_w)</td>
<td>0.1s, 1000s</td>
</tr>
<tr>
<td>Droop constants, (k_{dc}, k_f) (on converter MVA base)</td>
<td>0.1p.u., 0.002p.u.</td>
</tr>
<tr>
<td>FC-WAF weights, (\alpha_i)</td>
<td>(1/6 \ (i = 1, 2, ..., 6))</td>
</tr>
</tbody>
</table>
### Table 6.2: Power flow solution related to the VSCs in Macrogrid-VSC

<table>
<thead>
<tr>
<th></th>
<th>VSC1</th>
<th>VSC2</th>
<th>VSC3</th>
<th>VSC4 (slack)</th>
<th>VSC5</th>
<th>VSC6</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P_s$ (MW)</td>
<td>2543</td>
<td>2025</td>
<td>-2511</td>
<td>490</td>
<td>-2370</td>
<td>-826</td>
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<tr>
<td>$Q_s$ (MVAR)</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
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<tr>
<td>$u_s$ (p.u.)</td>
<td>1.00064</td>
<td>1.06400</td>
<td>1.04158</td>
<td>0.99938</td>
<td>1.01919</td>
<td>1.05115</td>
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<tr>
<td>$\delta_s$ (deg)</td>
<td>31.69</td>
<td>38.30</td>
<td>-7.59</td>
<td>-70.81</td>
<td>-33.89</td>
<td>-32.27</td>
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<td>$P_c$ (MW)</td>
<td>2614</td>
<td>2065</td>
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<td>493</td>
<td>-2311</td>
<td>-819</td>
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<td>$Q_c$ (MVar)</td>
<td>710</td>
<td>398</td>
<td>639</td>
<td>26</td>
<td>595</td>
<td>68</td>
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<td>$\epsilon_c$ (p.u.)</td>
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<td>1.006</td>
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<td>$\delta_c$ (deg)</td>
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<td>-67.73</td>
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<td>$P_{loss}$ (MW)</td>
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<td>$U_{dc}$ (p.u.)</td>
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<td>0.966</td>
<td>1.004</td>
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<td>$P_{dc}$ (MW)</td>
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<td>-2086</td>
<td>2416</td>
<td>-498</td>
<td>2282</td>
<td>813</td>
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### Table 6.3: Active power flow on DC branches in Macrogrid-VSC

<table>
<thead>
<tr>
<th></th>
<th>1-2</th>
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<th>3-5</th>
<th>5-6</th>
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<tr>
<td>$P_{cci,j}$ (MW)</td>
<td>-2648</td>
<td>-4746</td>
<td>-498</td>
<td>-3015</td>
<td>-811</td>
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<tr>
<td>$P_{ccj,i}$ (MW)</td>
<td>2661</td>
<td>4931</td>
<td>500</td>
<td>3092</td>
<td>813</td>
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Table 6.4: Simulation scenarios for the study of primary frequency support

<table>
<thead>
<tr>
<th>Case</th>
<th>Scenario</th>
<th>Frequency Control Strategy on the Southern Part of Macrogrid</th>
</tr>
</thead>
<tbody>
<tr>
<td>Macrogrid-VSC</td>
<td>S1</td>
<td>No control</td>
</tr>
<tr>
<td></td>
<td>S2</td>
<td>VSC FC-PU</td>
</tr>
<tr>
<td></td>
<td>S3</td>
<td>VSC FC-WAF</td>
</tr>
<tr>
<td>Macrogrid-Base [24]</td>
<td>S4</td>
<td>LCC-AUX on PALV-ELD</td>
</tr>
</tbody>
</table>

6.3 Primary Frequency Support Between WI and EI through Macrogrid-VSC

In this section, frequency control of VSC-MTDC is studied for primary frequency support between WI and EI in the developed continental Macrogrid-VSC model. The global frequency control strategy FC-WAF proposed in Chapter 4 is implemented and compared with the traditional decentralized local frequency droop control. The impact of realistic communication latencies on the performance of the proposed frequency control FC-WAF and the sensitivities of the frequency controls to power saturation limits are studied.

6.3.1 Simulation scenarios

Four scenarios are developed to compare different frequency control strategies as shown in Table 6.4, distinguished by the choice of the HVDC model and the control utilized on the southern part of the Macrogrid. S1 through S3 compare different VSC control strategies, whereas S4 is a reference scenario to compare different VSC controls with the LCC control in the case Macrogrid-Base.

In all scenarios, the two parallel LCC-HVDC lines across EI and WI, Colstrip to Minneapolis (COL-MSP), and Ault to St.Louis (AULT-STL) are assumed to be equipped with the auxiliary (AUX) frequency control proposed in [24]; they participate in the frequency support along with the VSC-MTDC in the south. The block diagram of the AUX controllers is shown in Figure 6.4. The controller settings are the same as those in [24]: $T_A = 0.1\text{s}$, $T_B = 1000\text{s}$, $C_M = -100,000\text{MW/60Hz.}$
6.3.2 On frequency filter and droop controller parameters

In order to coordinate with the parallel LCC-HVDC frequency control, the filter time constants of the VSC frequency controllers are designed to be consistent with those on the LCC-HVDC lines COL-MSP and AULT-STL. This coordination is simple and communication free, but is critical. If these filter time constants do not match, especially the low pass filter time constant $T_f$, the HVDC system with smaller $T_f$ will respond to AC system contingencies first, which may cause opposite response on the other HVDC systems that have slower responses (with larger $T_f$) and reduce the overall frequency control effectiveness. The coordination here is simply to make sure that the HVDC systems do not counteract each other. If other control objectives are desired, more advanced coordination techniques can be studied further. The filter time constant settings used in this work are: $T_f = 0.1s$, $T_w = 1000s$. Their values are designed as a compromise between fast response to improve frequency nadir and lasting response to maintain the support in the frequency recovery stage as explained in [24]. Generally, for primary frequency controllers, no washout filters are necessary. We include it in the model in order to have a generalized control scheme which can be extended for other control applications such as transient stability support. Using $T_w = 1000s$ is equivalent to removing the washout filter impact. The DC voltage droop constant $k_{dc}$ is set to a typical value of 0.1p.u [80]. The frequency control time constant $k_f$ is set to 0.002p.u. on converter MVA base for each VSC. The value is chosen such that for the largest credible contingency in the receiving end AC system (WI), the VSCs can provide the maximum frequency support while staying within operation limits. Moreover, since this value is designed under the largest credible contingency in the receiving end, for other less severe contingencies, the VSC power variations will
be less and VSCs will not be overloaded when providing frequency support. Similar procedure can be used to design $k_f$ for other operating conditions. All VSCs are equipped with both frequency droop and DC voltage droop controllers, and are in $P_s$ and $Q_s$ control mode, with $Q_s^{ref} = 0$ and $P_s^{ref}$ determined by (4.1).

### 6.3.3 Simulation results

Time domain simulation is performed in PSS/E with a time step of 0.0002s. The contingency used to test the performances of different frequency control algorithms is loss of the two largest generation units in the WI at 2.0s, with a total generation loss of 2766MW. The frequencies in WI and EI for the four scenarios are shown in Figure 6.5. The active power of the cross-seam LCC-HVDCs are shown in Figure 6.6. The VSC active power injections and DC terminal voltages are shown in Figures 6.7 and 6.8, respectively.

From Figure 6.5, it shows that both FC-PU (S2) and FC-WAF (S3) can provide effective frequency support to WI, yielding a higher frequency nadir than the scenario without any VSC frequency control (S1). Moreover, both VSC controls (S2 and S3) provide better frequency support
Figure 6.6: Active power on cross-seam LCC-HVDCs

capability than the scenario with LCC control on PALV-ELD (S4). Note that the system is designed to be able to withstand the selected (N-2) contingency, thus there is no load shedding in all scenarios. In this case, the frequency support performance of FC-WAF and FC-PU are similar, as can also be seen from Figure 6.7 that each VSC has similar power variations with the two control strategies. However, in case of a more severe generation loss, FC-WAF would outperform FC-PU with a higher frequency nadir in WI thus less risk of load shedding. This superiority is illustrated in the simulation results of AC-MTDC test system with 25% of generation loss in Section 4.4.2.

In terms of DC voltage profiles, as expected, FC-WAF outperforms FC-PU with closer values to the pre-disturbance steady state voltages as shown in Figure 6.8. The slight deviations of $U_{dc}$ from its pre-disturbance values are caused by the resistances in the MTDC network, which are assumed to be zeros when designing the weights for the FC-WAF scheme. This can also be verified by comparing the DC voltage deviations with those in the AC-MTDC test system, where the resistances are much smaller, and thus the DC voltage deviations with FC-WAF are also much smaller. Lastly, from Figure 6.6 and Figure 6.7, we can see that LCC-HVDC and VSC-MTDC
Figure 6.7: VSC active power injections
Figure 6.8: DC terminal voltages
Table 6.5: Total estimated communication delays

<table>
<thead>
<tr>
<th>Time delay (ms)</th>
<th>VSC1</th>
<th>VSC2</th>
<th>VSC3</th>
<th>VSC4</th>
<th>VSC5</th>
<th>VSC6</th>
</tr>
</thead>
<tbody>
<tr>
<td>VSC1</td>
<td>99</td>
<td>101</td>
<td>113</td>
<td>117</td>
<td>117</td>
<td>121</td>
</tr>
<tr>
<td>VSC2</td>
<td>101</td>
<td>99</td>
<td>111</td>
<td>115</td>
<td>116</td>
<td>119</td>
</tr>
<tr>
<td>VSC3</td>
<td>113</td>
<td>111</td>
<td>99</td>
<td>103</td>
<td>104</td>
<td>107</td>
</tr>
<tr>
<td>VSC4</td>
<td>117</td>
<td>115</td>
<td>103</td>
<td>99</td>
<td>107</td>
<td>111</td>
</tr>
<tr>
<td>VSC5</td>
<td>117</td>
<td>116</td>
<td>104</td>
<td>107</td>
<td>99</td>
<td>103</td>
</tr>
<tr>
<td>VSC6</td>
<td>121</td>
<td>119</td>
<td>107</td>
<td>111</td>
<td>103</td>
<td>99</td>
</tr>
</tbody>
</table>

coordinate well with each other, both extracting power from EI to provide frequency support to WI.

6.3.4 Impact of communication delay

Since the proposed FC-WAF is a global scheme which requires remote frequency measurements, communication is necessary. The robustness of FC-WAF to communication latency is, therefore, investigated. In this work, it is assumed the communication is achieved by peer-to-peer communication among converters via optical fiber. The estimated time delays between different VSCs are calculated based on a fixed local PMU processing time (99ms) and the transmitting times among different VSCs based on their relative distances [104]. The final estimated communication delays for the VSC-MTDC system are shown in Table 6.5. These realistic time delays are built into FC-WAF in PSS/E using a second-order Padé’s approximation [105], as shown in (6.1).

\[
e^{-s\tau} \approx \frac{1 - \frac{s}{2} + \frac{s^2}{12}}{1 + \frac{s}{2} + \frac{s^2}{12} + \frac{s^3}{12}}
\] (6.1)

We find that the impact of communication delay to the FC-WAF control is very minor, resulting in almost the same frequency performances in WI and EI as compared to the cases without communication delays. The frequency in WI is shown in Figure 6.9. The EI frequency has similar responses, and thus is not shown. In terms of the DC voltage profile, the impact of communication delay is also very minor, as shown in Figure 6.10 using VSC1 as an example.
Figure 6.9: WI frequency with delay

Figure 6.10: $U_{dc}$ at VSC1 with delay
6.3.5 Sensitivity of frequency control to saturation parameters

As discussed in Section 4.3.3, the frequency control contribution of VSC-MTDC depend on the saturation parameters ($\Delta P_{s,f,max}$, $\Delta P_{s,f,min}$). These saturation parameters give flexibility to system planners to adjust the supporting capability from their own interconnection based on the current system operating condition. To illustrate the sensitivity of FC-PU and FC-WAF to the saturation parameters, $\pm 500$ MW per interconnection are chosen to test their performances. Dividing it equally among the VSCs in each interconnection, the power limits for the converters are $\pm 250$ MW for VSC1 and VSC2 in WI, and $\pm 125$ MW for VSC3 through VSC5 in EI.

The four scenarios defined in Table 6.4 are simulated. The frequencies in WI and EI are shown in Figure 6.11. The VSC active power injections and DC terminal voltages are shown in Figures 6.12 and 6.13, respectively. From Figure 6.11, it can be seen that FC-WAF provides the best frequency support, better than LCC-AUX, whereas the performance of FC-PU degrades significantly and becomes worse than LCC-AUX. Figure 6.12 shows that the change in $P_s$ is much lower than its pre-defined limit (i.e. less effective), but much closer with FC-WAF, indicating more effective frequency support with FC-WAF. The degraded performance of FC-PU is due to the interactions
Figure 6.12: VSC active power injections with saturation limits
Figure 6.13: DC terminal voltages with saturation limits
between the frequency and DC voltage droop controllers. The reason why FC-WAF also cannot fully reach the exact limit is because of the resistances in the VSC-MTDC system, which are assumed to be zeros when designing the weights for FC-WAF. In conclusion, FC-PU is more sensitive to the saturation parameters, making the benefit of FC-WAF more obvious. From Figure 6.13, it can be seen that FC-WAF outperforms FC-PU, with less deviations from the pre-disturbance DC voltages, similar to the previous results without saturation limits.

6.4 DC Voltage Control of VSC-MTDC in Macrogrid-VSC

In this section, DC voltage control of VSC-MTDC is investigated on the developed North American continental HVDC overlay model Macrogrid-VSC. The margin based adaptive DC voltage control M-ADP-PVD developed in Chapter 5 is implemented and compared with the several other DC voltage control strategies to illustrate its superiority.

6.4.1 Control strategies for comparison

A total of six DC voltage control strategies, the same as those listed in Table 5.4 in Chapter 5, are simulated and compared on the developed Macrogrid-VSC case.

6.4.2 Simulation results

Time domain simulation is performed in PSS/E with a time step of 0.0002s. Two contingencies are simulated for all six control scenarios, one with an inverter outage (VSC1 at Victorville), and the other with a rectifier outage (VSC3 at El Dorado). These two converters are selected because they are the most heavily loaded inverter and rectifier, receptively, as shown in Figure 6.3.

Inverter outage The performances of different control strategies are first compared with an inverter outage of VSC1 at Victorville, with a power loss of $P_{s1} = 2543$MW. The initial values of power margin and power headroom of each VSC upon the tripping of VSC1 are shown in Table 6.6.
Table 6.6: Converter data at the outage of VSC1 in Macrogrid-VSC

<table>
<thead>
<tr>
<th>Converter</th>
<th>$P_s$ (MW)</th>
<th>$P_{max}$ (MW)</th>
<th>$\Delta P_{step}$</th>
<th>Init. margin (MW)</th>
<th>Init. headroom (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>VSC1 (inv)</td>
<td>2541</td>
<td>3000</td>
<td>-1</td>
<td>NA</td>
<td>NA</td>
</tr>
<tr>
<td>VSC2 (inv)</td>
<td>2025</td>
<td>3000</td>
<td>-1</td>
<td>975</td>
<td>975</td>
</tr>
<tr>
<td>VSC3 (rec)</td>
<td>-2511</td>
<td>3000</td>
<td>-1</td>
<td>5511</td>
<td>489</td>
</tr>
<tr>
<td>VSC4 (inv)</td>
<td>$\approx$490</td>
<td>3000</td>
<td>-1</td>
<td>2510</td>
<td>2510</td>
</tr>
<tr>
<td>VSC5 (rec)</td>
<td>-2370</td>
<td>3000</td>
<td>-1</td>
<td>5370</td>
<td>630</td>
</tr>
<tr>
<td>VSC6 (rec)</td>
<td>-826</td>
<td>3000</td>
<td>-1</td>
<td>3826</td>
<td>2174</td>
</tr>
</tbody>
</table>

Figures 6.14 through 6.16 present the VSC active power injections, DC terminal voltages, and the adaptive droop constants, respectively. In general, similar conclusions can be drawn as those for the AC-MTDC test system described in Section 5.4.3.2, illustrating the scalability and consistent superiority of the proposed M-ADP-PVD scheme in terms of 1) more accurate power sharing based on power margin 2) less DC voltage transient fluctuations and shorter settling time. However, there is an additional important distinction, as described below.

Unlike that in Figure 5.9d, in which both CV-L and CV-PVD schemes cause the remaining inverter hit its upper limit, Figure 6.14b shows that in this case only the CV-L scheme results in the converter hitting its upper limit of 3000MW. The reason for this is that the pilot voltage bus used in the Macrogrid-VSC case is VSC4 in St. Louis, whose DC voltage is less impacted by the outage of VSC1 because of the large electrical distance between them due to the existence of DC line resistances. Therefore, although the local voltage of VSC2 in Palo Verde is impacted more due to the outage of VSC1, VSC2 uses a less impacted remote voltage to participate in DC voltage droop control. This further illustrates the benefit of utilizing the PVD control architecture as compared to that of local voltage based control.

**Rectifier outage** The performances of different control strategies are then compared with a rectifier outage of VSC3 at El Dorado, with a power loss of $P_{s1} = -2511$MW. The initial values of power margin and power headroom of each VSC upon the tripping of VSC3 are shown in Table 6.7.
Figure 6.14: VSC active power injections with the outage of VSC1 (inverter)
Figure 6.15: DC voltages with outage of VSC1 (inverter)
Figure 6.16: Adaptive droop constants with outage of VSC1 (inverter)
Table 6.7: Converter data at the outage of VSC3 in Macrogrid-VSC

<table>
<thead>
<tr>
<th>Converter</th>
<th>$P_s$ (MW)</th>
<th>$P_{max}$ (MW)</th>
<th>$\Delta P_{step}$</th>
<th>Init. margin (MW)</th>
<th>Init. headroom (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>VSC1 (inv)</td>
<td>2541</td>
<td>3000</td>
<td>+1</td>
<td>5541</td>
<td>459</td>
</tr>
<tr>
<td>VSC2 (inv)</td>
<td>2025</td>
<td>3000</td>
<td>+1</td>
<td>5025</td>
<td>975</td>
</tr>
<tr>
<td>VSC3 (rec)</td>
<td>-2511</td>
<td>3000</td>
<td>+1</td>
<td>NA</td>
<td>NA</td>
</tr>
<tr>
<td>VSC4 (inv)</td>
<td>490</td>
<td>3000</td>
<td>+1</td>
<td>3490</td>
<td>2510</td>
</tr>
<tr>
<td>VSC5 (rec)</td>
<td>-2370</td>
<td>3000</td>
<td>+1</td>
<td>630</td>
<td>630</td>
</tr>
<tr>
<td>VSC6 (rec)</td>
<td>-826</td>
<td>3000</td>
<td>+1</td>
<td>2174</td>
<td>2174</td>
</tr>
</tbody>
</table>

Figures 6.17, 6.18, and 6.19 present the VSC active power injections, DC terminal voltages, and the adaptive droop constants, respectively. In general, similar conclusions can be drawn as those for the AC-MTDC test system described in Section 5.4.3.1, illustrating the scalability and consistent superiority of the proposed M-ADP-PVD scheme in terms of 1) more accurate power sharing based on power margin 2) less DC voltage transient fluctuations and shorter settling time. Actually, in this case, the proposed M-ADP-PVD scheme not only reduces the DC voltage settling time, but also the DC voltage steady state deviation, as compared to other control schemes as shown in Figure 6.18, demonstrating further its superiority in terms of DC voltage control.

An obvious observation in this case is that the ADP-PVD scheme in the existing literature results in DC voltage oscillations and instability, whereas the rest controls including the proposed M-ADP-PVD scheme can survive the rectifier outage and provide DC voltage droop control as designed. Since all adaptive control strategies use the same steady state DC voltage droop constants ($k_{dc} = 0.1$ p.u. on converter MVA base), this means that the stability region of the ADP-PVD scheme in this case is smaller than the others. As a result, in order to avoid the instability, the ADP-PVD scheme would require a larger steady state value of $k_{dc}$, with which the steady state DC voltage deviation would be sacrificed as compared to using $k_{dc} = 0.1$ in the same scheme. This is because of the trade off explained in Section 5.4.3.3.
Figure 6.17: VSC active power injections with outage of VSC3 (rectifier)
Figure 6.18: DC voltages with outage of VSC3 (rectifier)
Figure 6.19: Adaptive droop constants with outage of VSC3 (rectifier)
6.4.3 Remarks on the effectiveness of the proposed controls under high renewable penetration

Although the three major interconnections in the United States are still dominated by synchronous machines nowadays, it is anticipated that the penetration of converter interfaced renewable generation will increase significantly in the future. This will, as a result, alter the dynamic behavior of the AC systems. One of the major changes, as mentioned previously, is the decreasing system inertia, which would cause much faster frequency transients in case of disturbances such as loss of generation. Therefore, it is important to understand how the proposed controls on the VSC-MTDC work under circumstances of high renewable penetration with low inertia. Good news is that the VSC itself is a technology based on power electronics, which can be controlled to respond rapidly to the disturbances, to provide either active or reactive power support. It is, however, important to note that HVDC is a transmission technology, which does not “produce” generation itself. It acts more like a controllable valve, which can flexibly control power flows between different points in the AC systems. Therefore, the active power support from VSC-HVDC has to come from the AC system, either from other asynchronous AC systems (including wind farms) like in the context of this dissertation, or from the same AC system in case of an embedded application. The effectiveness of the VSC-MTDC controls is thus largely dependent on the dynamic behavior of the underlying AC systems. More specifically, the effectiveness of the two types of controls discussed in this dissertation (primary frequency control and DC voltage control) is briefly discussed below in case of high renewable penetration.

In terms of using VSC-MTDC to provide primary frequency support between asynchronous AC systems, the control effectiveness largely depends on the frequency response characteristics of the underlying AC system. The active power references ordered from VSCs act as generation and load variations from the perspective of their underlying AC systems. Currently, it is mainly the governors in the synchronous generators which would respond to these power variations. In the future with high penetration of renewables, other controls, for example, inertia emulation from wind or solar plants, or frequency responsive loads, could all participate or even dominate the frequency
response in the AC systems. Therefore, how those controls respond to the generation variation (VSC power demand changes) determines how effectively one AC system can provide frequency support to other AC systems, and vice versa.

For DC voltage control, since the time constants of the underlying AC system is reduced due to increased penetration of converter based renewable generation, the AC side transients are no longer in the time scale of electromechanical transients, but much faster and closer to the speed of DC side transients. As a result, controller interactions between the ones in the DC system and those in the AC system could potentially be a problem. System operators should perform proper planning studies which can accurately capture the dynamics of both AC and DC side components.

6.5 Conclusions

A new HVDC Macrogrid model with a six terminal VSC-MTDC network is developed for the North American power grid. The primary frequency support benefit between the Western Interconnection (WI) and the Eastern Interconnection (EI) via the VSC-MTDC system is demonstrated with several control strategies, including the FC-WAF scheme proposed in Chapter 4. The major benefits of FC-WAF are improved frequency nadir and reduced interaction with the DC voltage control loop, as validated via time domain simulation. Besides its own advantages, the VSC-MTDC frequency control can be coordinated with the frequency control on the two parallel LCC-HVDC lines across WI and EI, by properly designing the frequency filter time constants. The proposed FC-WAF is robust against communication delays and power saturation limits. Several DC voltage control techniques including the M-ADP-PVD strategy proposed in Chapter 5 are applied and compared in terms of power sharing and DC voltage profile in case of an converter outage. The proposed M-ADP-PVD is demonstrated to outperform the existing methods with more accurate power sharing based on the actual availability of the converters, and the capability to differentiate the converter outage direction (a rectifier vs. an inverter). Moreover, the DC voltage profile resulted from the developed M-ADP-PVD scheme has less transient fluctuations and shorter setting times, and it has flexibility to adjust the strength of DC voltage regulation as desired. Therefore,
it can provide more reliability in case of a converter loss. As a summary, with the proposed control strategies for VSC-MTDC systems, the reliability of the overall AC-MTDC system is improved in terms of both AC and DC side events. The VSC-MTDC model and all controls are implemented in the commercial grade software PSS/E, and thus is suitable to study large scale realistic power systems and can be incorporated into the power system planning process at ISOs and utilities.
CHAPTER 7. OVERALL CONTRIBUTIONS AND FUTURE WORK

7.1 Overall Contributions

VSC-MTDC is a promising and cost-effective technology to facilitate the integration of large amount of renewable generation across large geographic span and multiple AC systems. In this work, we address two important issues related with the control of VSC-MTDC systems with two new control strategies, and we demonstrate the effectiveness of these proposed controls on a developed North American continental power system model with an HVDC overlay.

Firstly, the decreasing system inertia caused by increasing renewable penetration poses great challenges to AC system frequency control for generation outages in AC systems. We address this challenge by designing frequency controls for the converters to provide frequency support among asynchronous AC systems. Secondly, in order to improve the dynamic performance of DC voltage in case of a converter outage, and also to ensure accurate power sharing, an adaptive DC voltage control method is proposed taking into consideration both converter operating condition as well as the real-time DC voltage deviation. Finally, both of these controls are investigated and validated on a newly developed North American power system model with an HVDC overlay, showing their scalability and effectiveness on large scale practical power systems. The contributions of this work are summarized as follows:

**Primary frequency support among asynchronous AC systems via VSC-MTDC**

1) A global supplementary frequency droop control scheme is developed for primary frequency support among asynchronous AC grids via a VSC-MTDC system. The developed control solves the major problem of the existing local frequency control, which is its reliance on the DC voltage control to function when activated. The following are resulting advantages.
(a) The developed control outperforms the existing local frequency control with a improved frequency nadir, and thus less load shedding in the AC system experiencing generation loss.

(b) The improved frequency nadir increases the capability of sharing primary frequency reserves from each AC system, and thus reduces the amount of online spinning reserves required.

(c) The developed control exhibits consistent effectiveness with different types of DC voltage control and significantly improves the DC voltage profile while the control is activated, as compared to the traditional local droop control.

2) The developed control not only controls frequency but also indirectly contributes to the control of rate of change of frequency to provide inertia support to the AC systems.

3) The developed control is scalable to large scale realistic power systems, as illustrated on the continental North American power system model.

4) The developed control is robust against realistic communication time delays even though it is a communication based scheme.

5) The developed control is robust against power saturation limits, making it more suitable for future integration with market operation.

6) The developed control is easy to implement into the actual converters due to its simple droop structure.

7) The developed control is generic and could be applied to arbitrary systems.

8) The developed control is implemented in the commercial grade software and thus is suitable to study large scale realistic power systems and can be incorporated into the power system planning process at utilities and ISOs.
Adaptive DC voltage control of VSC-MTDC considering power sharing and DC voltage deviation

1) A new power margin based adaptive DC voltage control (M-ADP-PVD) for VSC-MTDC systems is designed. The main feature of the developed control is its capability to distribute the DC power unbalance according to the actual power sharing capabilities of the converters. Moreover, it has flexibility in adjusting the strength of DC voltage regulation with adaptive droop constants. The resulting advantages are as follows:

(a) It takes converter operating point into consideration and avoids overloading for the converters operating close to their power limits.

(b) It can differentiate the converter outage direction owing to the usage of power margin as indicator of the converter power sharing capabilities.

(c) The post-disturbance DC voltage profile has less transient fluctuations and shorter settling times.

2) The developed DC voltage control can cooperate with frequency control strategies (especially the developed FC-WAF) with improved frequency performance for both AC and DC side events, enhancing the overall reliability of the AC-MTDC system.

3) The proposed control is scalable and effective on large scale realistic power systems, as validated on the continental North American power system with an HVDC overlay.

4) The developed control is easy to implement into the actual converters due to its simple droop structure.

5) The developed control is generic and could be applied to arbitrary systems.

6) The developed control is implemented in the commercial grade software and thus is suitable to study large scale realistic power systems and can be incorporated into the power system planning process at utilities and ISOs.
Study of North American continental HVDC with VSC-MTDC systems

1) A VSC-MTDC system is integrated into the continental North American power system planning model with an HVDC overlay for the first time, in the widely used commercial grade software PSS/E.

2) The benefit of primary frequency support between the Western Interconnection and Easter Interconnection is demonstrated, showing the advantages of the developed frequency control scheme FC-WAF, especially with reduced impact to DC voltage profile.

3) The coordination between the six-terminal VSC-MTDC system and the two parallel LCC-HVDC lines are shown with collaborative effort to provide primary frequency support across the EI-WI seam.

4) The robustness of FC-WAF is investigated with realistic communication delays and power saturation limits.

5) The benefits of improved of DC voltage dynamics and improved power sharing accuracy are demonstrated with the developed margin based adaptive droop control (M-ADP-PVD) for converter outages.

7.2 Future Work

Communication The two controls proposed in this work both rely on communication to some extent. Although communication delays are studied and discussed briefly in this dissertation, detailed communication network is not modeled. Future work can include such communication network into the control loop and study the impact of communication link failure. It should also be noted that the proposed frequency control can be realized in a distributed manner, with which the requirement of the total length of communication links and associated investment cost can be reduced. Therefore, designing topologies and strategies with distributed control is another research direction.
**AC-DC interaction**  A control designed solely for the DC (AC) system may not be optimal for AC (DC) side performances. For example, a DC voltage control with excessive focus on DC voltage regulation may cause large power variations in the AC system, whose stability could be jeopardized. Therefore, it would be valuable to investigate controls that would consider such a trade off and co-optimize the AC and DC side dynamic performances, especially in a real time manner.

**Hybrid simulation**  Because of the different time constants in the DC and AC systems, modeling and simulating them simultaneously would require a small time step to satisfy the need of DC system components, which reduces the computation efficiency of the overall system. Therefore, hybrid simulations with the capability to model and solve the AC and DC systems separately using different time steps are desired, and worth more research investigation.


137


[40] MISO, “MISO transmission expansion planning (MTEP),” 2014.


