Advanced Methods for Steady-State and Stability Analyses of Hybrid Power Systems

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ADVANCED METHODS FOR STEADY-STATE AND STABILITY ANALYSES OF HYBRID POWER SYSTEMS

A Dissertation

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in partial fulfillment of the requirements for the degree of Doctor of Philosophy

in

The Department of Electrical and Computer Engineering

by
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This dissertation work is proudly dedicated to:

my beloved parents who did not only raise and nature me but also were a source of motivation and strength during moments of despair and discouragement.

Thank you for your endless love, sacrifices, prayers, supports, and advices.

*Hope I made you proud.*
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ABSTRACT

The term hybrid power grids refer to the combination of two power systems with different intrinsic characteristic. For instance, ac-dc grids and transmission-distribution systems are kinds of hybrid power grids. Challenges in analyzing the hybrid power grids arise since two sets of equations should be solved either simultaneously or sequentially. In the simultaneous (unified) methods, the ac and dc system of equations are solved simultaneously, while, in the sequential approaches, these equations are solved in an error loop.

In this dissertation, a unified method is proposed for steady-state and fault analyses of hybrid ac-dc power grids, while a sequential approach is developed for the steady-state and stability analyses of transmission-distribution grids. The simplicity, speed, and accuracy are among the most important factors in developing the proposed methods. Here, it has been tried to propose methods that are simple in implementation and fast in performing while the accuracy is not compromised. Different scenarios are performed to show the functionality and advantages of the proposed methods over the current approaches.
CHAPTER 1. INTRODUCTION

In general, the term of hybrid grid refers to the inclusion of electrical circuits from different types. Hybrid power grids can be categorized into two different types:

1. Ac-dc hybrid systems: such as inclusion of high voltage direct current (HVDC) and dc distribution network in the ac grids; and

2. Transmission-distribution hybrid systems: such as distribution DER.

1.1 Types of Hybrid System Analyses

The analysis of power grids is becoming more complex due to an increase in the number of power generation from different kinds or expansion of different types of power grids, i.e., transmission and distribution grids. The term hybrid analysis is introduced to address this complexity. In general, the hybrid analysis refers to analyzing the hybrid grids that couple circuits of different kinds.

Several methods were developed to ease the analysis of hybrid power grids. These methods can be mainly divided into two groups as

1. Unified (simultaneous) analysis: in this kind of analysis the system of equations of hybrid grids are solved simultaneously. For instance, in the unified ac-dc circuit analysis, the system of equations of ac grid and dc grid along with the ac-dc converter model are solved simultaneously. Another example of this method is unified transmission-distribution circuit analysis that is used in electromagnetic transients (EMT) and sub-transient analyses;

2. Sequential analysis: here, the system of equations of circuits of different kinds is solved in an error loop. The sequential ac-dc circuit analysis, in which system of equations of ac and dc grids as well as ac-dc converter are solved sequentially, is one good example
of this method. Also, the sequential transmission-distribution circuit analysis, which is mainly used for load flow and stability analyses of transmission and distribution systems, is another example of sequential analysis.

This dissertation aims to develop methods for unified ac-dc circuit analysis and sequential transmission-distribution circuit analysis. The purpose of this work is to develop new models for load flow and fault analyses of hybrid ac-dc grids as a part of unified ac-dc circuit analysis. In addition, this work aims at studying the effects of distribution and transmission systems on each other via co-simulating the transmission and distribution systems to perform steady-state and dynamic analyses in a sequential fashion.

Hybrid ac-dc circuit analysis: Hybrid ac-dc grids were emerged inside of the distribution systems to gain the benefits of both ac and dc grids. Several sequential and unified methods were established to analyze hybrid ac-dc grids. As it will be shown in chapters I and II, there are several challenges in these methods, such as programming challenges, lack of system-level perspective, need to impose certain constraints, time consuming, and HVDC-oriented. The proposed unified ac-dc circuit analysis aims to address challenges that previous works currently face and provide a reliable tool in ac-dc circuit analysis.

Hybrid transmission-distribution systems: Analyzing grids using available tools does not provide information about the effects of transmission and distribution parts on one another. Thus, a sequential transmission-distribution (T&D) co-simulator is developed here to provide detailed interactions between the transmission and distribution networks. Unlike the past T&D co-simulator that did not model all equipment of the power system in their co-simulation algorithm or considered a loose or semi-tight coupling between transmission and distribution networks, the proposed method aims to completely model all equipment of the power system in the algorithm.
Also, the tight coupling between transmission and distribution grids is considered in the proposed approach, which makes the algorithm more robust and accurate.

1.2 Organization of the Dissertation

The hybrid circuit analysis, which was mentioned earlier, is discussed in four chapters in this dissertation work and can be found in Figure 1.1. The first two chapters look into hybrid ac-dc grid analysis, while the last two focus on analyzing transmission-distribution hybrid systems. In the first chapter, the focus is on the load flow analysis of hybrid ac-dc grids. A unified ac-dc load flow analysis via an improved model for the ac-dc coupling through which the ac and dc load flow analyses can be simultaneously conducted is explained. The ac-dc coupling is modeled by an admittance building block, and thus, the dc grid is converted to an ac-equivalent circuit within the hybrid network. However, unlike past works, state variables beyond nodal voltages and angles are avoided, leading to a simpler algorithm, and reactive power injection of the coupling mechanism, a requirement for the IEEE 1547, is explicitly modeled. Furthermore, the proposed model can be applied to both voltage-source and line-commutated converters (VSCs & LLCs). Accordingly, the conventional ac power flow algorithms can be applied to the hybrid circuit paving the path for seamless adoption of hybrid circuits and significantly saving time and effort in programming, troubleshooting, and upgrading techniques currently used in research and industry as well as a high-level analysis tool regarding the hybrid ac-dc grid is achieved. While this chapter provides a tool to study steady-state analysis of the hybrid ac-dc grid, it does not provide any information about the short circuit current inside hybrid ac-dc grids, which plays an important role in designing a protection scheme for hybrid ac-dc grids. Thus, the next chapter is developed to address this issue.
Fault analysis of a hybrid ac-dc grid is conducted in the second chapter. This chapter aims to extend the results of the classical ac fault analysis to the ac-dc hybrid grids. This will make available the well-developed ac fault analysis methods to the hybrid circuit with accuracy and simplicity. The proposed method finds a Thevenin equivalent of the dc laterals applied to the ac circuit; thus, avoids multiple analyses currently used in ac-dc circuits. Classical fault analysis is a powerful tool for the ac power system fault analysis, which provides the short circuit currents for an effective protection system design. With the rise of dc circuits within the ac grid and the
presence of dc loads and power sources, new fault analysis that includes the hybrid system has
become an immediate need. Faults at the ac side of the circuit are affected by the dc loads and
sources. Thus, a fast and accurate scheme that can analyze the effects of the dc circuit on the ac
grid is required. The existing methods for fault analysis of the ac-dc hybrid grids are complex and
require different modeling for the ac and dc circuits.

Unlike the first two chapters where distribution systems were considered as lumped loads,
in the subsequent chapters a coupled load flow algorithm is developed to address the effects of the
transmission and distribution (T&D) grids on each other. In Chapter III, a co-simulation algorithm
is developed to study these effects under the steady-state condition. Subsequently, in Chapter IV,
the effects of distribution-connected renewable energy sources via DER_A mechanisms on the
dynamics of power systems are studied by extending the T&D load flow algorithm to T&D
dynamic co-simulation. The detailed distribution and transmission systems are modeled separately
and are coupled at the substation nodes at each time step. Dynamics of the DER_A mechanism are
modeled separately downstream of the distribution substation using the North American Electric
Reliability Corporation model (NERC-2018). The effects of the DER_A control settings are
examined on the dynamics of the transmission system and the DER operation. In addition, lumped
and distributed induction motor load models are compared at the distribution level.
CHAPTER 2. UNIFIED AC-DC LOAD FLOW VIA AN ALTERNATE AC-EQUIVALENT CIRCUIT

2.1 Introduction

Classical three-phase ac analysis has been widely used and effective methods have been developed to calculate power transfer in complex grids. The penetration of the emerging distributed generations (DGs) in the form of renewable power, ac [1] and dc [2] microgrids, and dc distribution systems [3] make the power management of the modern larger grid challenging [4]. Due to the abundance of ac generation, transformation, and consumption, ac grids will still be the dominant type of circuits in the near future; nevertheless, the share of the dc power is expected to be significant making hybrid ac-dc circuits the future grids [5-8].

The past efforts in utilizing ac and dc systems together include small-scale [9-11] applications with some efforts to achieve large-scale system solutions [12-14]. Ac-dc large-scale grids are studied in the past literature and different methods are proposed to combine the two circuits in load flow analysis. Basic ac-dc circuits structure includes distribution laterals that comprise ac-dc converters to feed dc loads or to deliver power to the ac grids. Another example of the hybrid ac-dc grids is the high-voltage dc (HVDC) transmission lines [11, 12] and multi-terminal dc (MTDC) systems [15, 16] that make connections between sections of ac grids via dc lines.

In a number of past works, the ac grid is assumed to have the capability to maintain stability while supporting the dc side. For this purpose, the ac-dc converter connecting the dc and ac grids is assumed to preserve a constant dc voltage or to maintain abundant dc power [17-20]. By contrast, in a general ac-dc circuit analysis constraints such as constant coupling dc voltage, abundant ac power, or stable ac grid may not be considered a priori.
Load flow analysis in the hybrid ac-dc grids has been studied in the past literature. Two sets of equations must be solved, either simultaneously or sequentially [21-34]. In the sequential methods (used in most power flow studies) the ac and dc system equations are solved sequentially in an error loop (see for example [23-25]), whereas in unified procedures [26-34], the ac and dc system equations are solved simultaneously. In both methods, additional algebraic or dynamic constraints must be satisfied for the ac-dc coupling mechanism (i.e., inverters and rectifiers.)

While ac frequency changes do not exist in larger power systems, the islanded hybrid grid is another interesting research with variable reference frequency that requires a specific formulation [35]. In [36, 37], the Newton-Raphson algorithm is utilized to solve the dc or ac grid to find a power mismatch at the ac-dc coupling in a sequential load flow. The mismatch (error) loop is iterated in the ac and dc networks until it vanishes. Although the sequential approaches proposed in these works are easier to program than the unified power flow algorithms, their simulation time is longer since a number of iterative loops must be completed in an inner loop for convergence. In addition, a dc slack bus [37] at an inverter/rectifier location is required; i.e., a constant dc voltage constraint at the coupling, that may or may not be feasible. While dc bus voltage can be maintained constant via control, abundant power must be available. Nevertheless, this assumption is another constraint that can be relaxed by appropriate modeling.

The unified (simultaneous) power flow approach for hybrid ac-dc networks incorporating voltage source converters (VSCs) is proposed in [15] where ac, dc, and slack loss mismatch equations are solved together assuming existence of the dc slack bus. The main idea of the unified method in [30] is to model the ac-dc converters as voltage-dependent loads eliminating the dc variables; thus, weakening the approach to represent the dc system accurately. The unified power flow algorithm for the islanded hybrid ac-dc grid in [31] is based on the extension of the jacobian
matrix by considering the dc variables. The approach employs a slack bus for both ac and dc grids and is suitable for islanded operation where a real slack bus is missing in the ac grid. Thus, the issue of dc slack bus explained earlier remains for the (un-islanded) larger grids. Variable voltage source behind a coupling impedance puts an emphasis on the ac side only [38-40] and is used to conduct ac-dc load flow in a unified fashion. The work of [40] models the inverter ac voltage as a function of the dc voltage and the amplitude modulation factor; however, the model remains a voltage source in nature and cannot fully reflect the ac-dc system interactions. Ac and dc power injections are employed in [42, 43] to solve simultaneous ac and dc systems assuming that either coupling mechanism’s transferred powers or terminal voltages are known.

A component building block is introduced in [32-34] and our previous work [44] to obtain a full equivalent circuit of VSCs and to extend the conventional ac load flow to ac-dc circuits in the context of unified ac-dc load flow. The VSC mechanism in [32-34] is modeled as a complex-valued tap changer along with parallel impedances to address the converter reactive power balance and losses. While the model is the first to fully address the dc side of the VSC, it involves a variable admittance, an additional state beyond the conventional bus voltages and angles adopted in the available ac load flow methods, to treat reactive power at the dc side leading to added implementation complexity. Besides, the adjustment of the converter reactive support, a necessary converter function, is not straightforward in the proposed model. Unlike the authors’ prior work [44], the converter losses are modeled in this work. In addition, discussions on the line-commuted converters (LCCs) are provided in the context of the proposed unified ac-dc load flow, along with simulations for larger networks and comparison to existing and conventional methods. Discussions on per unit calculation with variable modulation factors are also provided.
Research in ac-dc load flow analysis is ongoing due to the following shortcomings in the available approaches:

- Programming challenges: Both sequential and simultaneous load flow methods need to incorporate new formulation for dc and coupling systems including new algorithms or new states, and thus, the available software platforms are not easily adaptable;

- While sequential methods are able to employ different algorithms, for both sequential and simultaneous methods a system-level perspective is not available for more intelligent studies. For example, unlike conventional ac grids that possess a global admittance matrix providing valuable high-level information in normal and contingent grids, sequential ac-dc models lack such a property;

- The majority of the available methods impose certain constraints on the ac-dc coupling nodes. These include a need for a dc slack bus, constant-voltage or constant-power coupling node, etc. This prevents a more general solution to the ac-dc grid and may not be achievable if dc or coupling powers are not adequate;

- Majority of the available unified methods are HVDC-oriented and thus the converter reactive power injection, a requirement for large deployment of DERs, is not addressed adequately.

The contributions of the proposed work include

- The proposed algorithm is a simultaneous (i.e., unified) hybrid ac-dc load flow algorithm that can be implemented with minimal changes in the conventional ac load flow algorithms. While in the proposed method ac-dc coupling mechanism is modeled as an admittance building block, it converts the dc circuit to an equivalent ac circuit with no additional states in the model beyond the ac nodal voltages and phases and dc
nodal voltages, and thus, the proposed algorithm can be implemented with minimal changes in the available load flow programs;

- No voltage or power constraints are necessary for modeling the ac-dc converters, although constants can be added as desired in the proposed approach;

- The proposed ac-dc coupling model can be applied to both VSC and line-commuted converters (LLCs). The converter reactive support is explicitly considered in the proposed model making it suitable for the newly recommended IEEE-1547 standard [45].

2.2 Problem Formulation

Here, we aim at an ac equivalent circuit that can represent both the dc and ac networks. Before we proceed, the following definitions are introduced:

Border node: Border nodes are the nodes of a dc sub-circuit (or its equivalent ac circuit) where the ac-dc coupling mechanism exists such as inverter and rectifier nodes (see Figure 2.1). Border nodes are also used to address nodes of the ac sub-circuits that are connected to the ac-dc couplings mechanisms usually via filter and line impedances (ex. \( V_a \), \( V_c \), and \( v_c \) in Figure 2.2).

Border branch: Border branches are the branches of the ac-dc circuit or its equivalent ac circuit that connect the ac-dc coupling device such as an inverter or a rectifier to the ac circuit (see Figure 2.1).

Isolating cut set (ICS): Each dc sub-circuit can be physically detached from the connected ac circuits by cutting the corresponding border branches. These border branches constitute a cut set. Thus, a cut set of border branches that renders a connected dc sub-circuit is called an isolating cut set (see Figure 2.1).
Dc-equivalent ac node: (DEAN): In the target ac equivalent circuit, each dc sub-circuit is converted to an equivalent ac circuit where all dc nodes are converted to proper ac nodes, here called “dc-equivalent ac nodes” (DEANs) (ex. DEAN $V_c$ corresponds to dc node $v_c$ in Figure 2.2).

Ac coupling node (ACN): The node that represents the ac side of the ac-dc coupling is called the ac coupling node (ex. $V_b$ in Figure 2.2). An ACN along with its corresponding DEAN are the two sides of an ac-dc coupling mechanism such as an inverter or a rectifier (see Figure 2.2). ACN voltage is the unfiltered fundamental harmonic of the coupling device’s ac voltage. ACNs are connected to ac border nodes via filter/line impedance.

Dc-equivalent ac branch: (DEAB): We name any branch of the original dc sub-circuit in the ac equivalent circuit a “dc-equivalent ac branch” (DEABs) as shown in Figure 2.2.

For convenience, all dc variables such as voltages, currents, and resistances in the original circuit are represented by small letters (i.e., $v$, $i$, or $r$). In addition, all ac variables including those of the DEANs and DEABs are represented by capital letters (i.e., $V$, $I$, or $Z$) to represent phasors and complex values. The target network satisfies two conditions:

2.2.1 Condition 1

The RMS values of the DEANs’ voltages are equal to the magnitude of the corresponding original dc node voltages. For instance, $v_c = |V_c|$ in Figure 2.2.

2.2.2 Condition 2

The injected complex power at the points of ac-dc coupling in the equivalent ac circuit must be the same as those of the original ac-dc circuit (see Figure 2.2).

2.2.3 Arranging DEAN Voltages

In order to satisfy Condition 1, one can build an ac equivalent circuit such that all DEAN voltages pertaining to the original dc sub-circuit are in-phase; for instance, $V_c \angle \theta$ and $V_d \angle \theta$ in
Figure 2.2. Define vector $\mathbf{v}_1 = [v_1 \ v_2 \ \ldots \ v_k]^T$ to represent the voltages at border nodes in the original dc sub-circuit. Also, define the vector $\mathbf{v}_2 = [v_{k+1} \ v_{k+2} \ \ldots \ v_n]^T$ to represent non-border nodes in the dc sub-circuit. Subsequently, 

$$
\begin{bmatrix}
y_1 & y_2 \\
y_3 & y_4
\end{bmatrix}
\begin{bmatrix}
\mathbf{v}_1 \\
\mathbf{v}_2
\end{bmatrix}
= 
\begin{bmatrix}
\mathbf{i}_1 \\
\mathbf{i}_2
\end{bmatrix}
$$

(2.1)

represents the original dc sub-circuit where $y_1$ through $y_4$ are appropriate admittance matrix blocks and $\mathbf{i}_1 = [i_1 \ i_2 \ \ldots \ i_k]^T$ and $\mathbf{i}_2 = [i_{k+1} \ i_{k+2} \ \ldots \ i_n]^T$ are current injections from the ac network and dc sources, respectively. Here, $n$ is the total number of the buses in the original dc sub-circuit (or its ac equivalent), and $k$ is the number of border nodes (i.e., connected to the ac-dc coupling mechanisms.) Note that this analysis must be repeated should more than one dc sub-circuit exist in the ac-dc network. By post-multiplying both sides of equation (2.1) by $e^{j\theta}$ ($e^{j\omega t}$ is omitted for simplicity) followed by pre-multiplying the two sides by a complex-valued matrix $\Gamma$, one has

$$
\Gamma
\begin{bmatrix}
y_1 & y_2 \\
y_3 & y_4
\end{bmatrix}
\begin{bmatrix}
\mathbf{V}_1 \\
\mathbf{V}_2
\end{bmatrix}
= 
\begin{bmatrix}
\mathbf{I}_1 \\
\mathbf{I}_2
\end{bmatrix}
$$

(2.2)

where vector $\mathbf{V}_1 = [V_1 \ V_2 \ \ldots \ V_k]^T$ represents the voltage phasors at border nodes in the ac equivalent circuit representing the original dc sub-circuit. Also, $\mathbf{V}_2 = [V_{k+1} \ V_{k+2} \ \ldots \ V_n]^T$ represents non-border nodes in (2.2) in the ac equivalent circuit. It is apparent that all DEAN voltages are in-phase. In the case of real-valued matrix $\Gamma$ border branch currents $\mathbf{I}_1 = [I_1 \ I_2 \ \ldots \ I_k]^T$ are in phase with corresponding DEAN voltages $\mathbf{V}_1$. Election of (2.2) satisfies Condition 1 and active power matching for Condition 2.

For convenience, we have considered voltage-source converters (VSC) (inverters or rectifiers) at all ac-dc coupling points. The analysis of line-commutated converters (LCC) can be conducted similarly and is explained next.
Per Unit Calculation: In order to avoid complexities involved with the ac-dc voltage ratios, we utilize per unit values; thus, the base voltage at the ac bus is smaller than that of the corresponding DEAN by a factor of $a = (\sqrt{3}/(2\sqrt{2}))M_a = 0.612 \, M_a$ for two-level PWM, where $M_a$ is the modulation factor of the VSC. Then, two approaches can be selected to treat the base voltage difference:

Method 1: One can select $M_a = 1$ for all the converters enclosed by the isolating cut set. This will make the base voltage on the dc side to be 1.634 times that of the line-to-line voltage on the ac side. Subsequent changes in $M_a$ can be made similarly to tap changers in the conventional load flow algorithms; i.e., by changing the ratio in the admittance building block.

Method 2: Alternatively, one can set base voltage on the dc side to be $\frac{1.634}{M_a}$ times that of the line-to-line voltage on the ac side with no alteration in the admittance matrix. Consequently, $V_i = v_i \angle \theta$ (for $1 \leq i \leq n$) when values are represented in per unit.

Remark 1. Here, the angle $\theta$ is an arbitrary angle but is applied to all DEAN voltages and DEAB currents (including border and non-border ones). For instance, one can set $\theta$ equal to the phase angle of the ACNs connected to the dc representative bus. Another convenient way is to set $\theta = 0$, which is selected in this work. When $\theta = 0$, there is a phase difference between voltages at all the ACNs and their corresponding DEANs. This phase difference is equal to the ACN phase angle.

Remark 2. A modest selection of $\Gamma$ in (2.2) is the identity matrix that results in $I_i = i_i \angle \theta$ (for $1 \leq i \leq n$). Another simple selection of $\Gamma$ is $e^{j\gamma}$ (with the similar current angle for all DEABs) that results in a uniform phase shift in the DEAB currents. For example, if $\gamma = \frac{-\pi}{2}$, currents are 90 degrees lagging voltage implying an inductive equivalent ac circuit. Here, $\Gamma = 1$ is selected for
convenience that implies an ac resistive network in replacement of the dc sub-circuit. Thus, the DEAB currents can be represented by $I_i = i_i \angle \theta$ for $1 \leq i \leq n$.

To this end, an ac equivalent circuit is synthesized for the dc sub-circuit. However, there is a phase difference between voltages at all the ACNs and their corresponding DEANs represented by $|V_b| \angle \phi_b$ and $|V_c| \angle 0$ in Figure 2.3, respectively, according to Remark 1. In order to provide phase matching between the ACNs and their corresponding DEANs, a phase-shifter is used at each border node as shown in Figure 2.4. Unlike in the past work [32-34], the phase shifter angle is not a state but is adjusted as a function of other states to accomplish zero reactive power in the dc equivalent ac network and to avoid additional impedance states. The converter voltage phasor $|V_b| \angle \phi_b$ is then obtained using the active and reactive power demands on the ac side of the coupling mechanism (converter); i.e., at the ACNs.

Figure 2.1. An interconnected hybrid ac-dc circuit
Figure 2.2. Ac-dc circuit model at the point of common coupling

Figure 2.3. The typical ac-dc converter bus
The phase shifter can be represented as

\[ V_{ci} = t_i V_{bi}, \quad I_{ci} = (t_i^{-1})^* I_{bi} \]  \hspace{1cm} (2.3)

where \( t_i = \frac{1}{M_a} 4 \beta_i \) (using per unit Method 1) or \( t_i = 1 4 \beta_i \) (using per unit Method 2); \( V_{bi} \) and \( V_{ci} \) are the ACN and corresponding DEAN voltage phasors, respectively; and \( \beta_i \) is the phase shift between ACN and DEAN voltages at border node “i” for \( 1 \leq i \leq k \). In addition, \( X_i \) is the converter series impedance (to include converter and line reactance), \( R_i \) is the converter resistive loss, and \( V_{ai} \) is the voltage at the connecting ac node. Here, the resistive losses include switching and line losses that are modeled on the ac side to include reactive power effect on those losses based on the selected current directions. Note that current \( I_{ci} \) maintains the same phase shift w.r.t \( I_{bi} \) as that of the corresponding voltage \( V_{ci} \) w.r.t. \( V_{bi} \) (i.e., \( 4 \beta_i \)). Thus, one has \( V_{ci} = |V_{ci}| 4 \varphi_{ci} = V_{bi} 4 \beta_i \) where \( V_{bi} = |V_{bi}| 4 \varphi_{bi} \) is the ACN voltage phasor (i.e., the converter ac voltage phasor) and \( \beta_i \) cancels the ACN phase (and yields \( \varphi_{ci} = \theta = 0 \) explained in Remark 1) as:

\[ \beta_i = -\varphi_{bi} \]  \hspace{1cm} (2.4)

2.2.4 Satisfying Complex Powers and Building Admittance Matrix

The dc sub-circuit consumes only active power and so does its equivalent ac circuit. Thus, the reactive powers at all ACNs need to be incorporated into the model appropriately (Condition
2). By considering constant reactive power injection at the ACN it is treated as a load bus in the conventional ac load flow and thus voltage and phase angle are obtained. This will enable adjustment of reactive power injection of the converter and is a more appropriate model compliant with IEEE-1547 standard [45]. In addition, the converter ACN or DEAN voltage is not assumed constant and load flow determines their values. One, however, can assign constant voltage to the ACN along with zero active power demand and let load flow obtain the required reactive power (a P-V node in the load flow context).

From the viewpoint of optimal power flow, an appropriate selection of modulation factor \( M_a \) and reactive power demand at the converter will lead to the optimal load flow solution.

\( Y_{bus} \) construction–Here a Branch Building (BB) block is aimed for the ac-dc coupling while the converter reactive current is considered explicitly. Figures 2.2 and 2.3 illustrate the connection of the ac-dc converter, the phase shifter, and the inclusion of the reactive power compensation at the ACN in the proposed approach. Here, DEAB current \( I_{cl} = i_i 4\theta \), with \( i_i \) the dc current at the original dc sub-circuit as explained earlier. Subsequently, \( I_{bi} = t^* \times I_{cl} = \frac{1}{M_a} 4-\beta_i \times I_{cl} \).

Note that current \( I_{bi} \) maintains the same phase shift w.r.t. \( I_{cl} \) as that of the corresponding voltage \( V_{bi} \) w.r.t. \( V_{ci} \) (i.e., \( 4-\beta_i \)). An additional current source is employed to allow the reactive power matching at the ACN. Figure 2.4 depicts the phasor diagram representing the voltages and currents at the ac-dc coupling node. From Figure 2.5 and by defining \( Z_i = R_i + jX_i \), current \( I_{ai} \) can be obtained as

\[
I_{ai} = I_{bi} + I_{si} = \frac{1}{Z_i} (|V_{ai}| 4\varphi_{ai} - |V_{bi}| 4\varphi_{bi})
\]

(2.5)
For convenience, the ACN phase angle \( \phi_{bi} \) is considered as the phase reference in (2.5) (i.e., \( V_{bi} = |V_{bi}| \)) and \( \alpha_i = \phi_{ai} - \phi_{bi} \) is defined. Consequently,

\[
I_{ai} = \frac{1}{|Z_i|^2} [ |V_{ai}| (R_i \cos \alpha_i + X_i \sin \alpha_i) - R_i V_{bi} ] + j \frac{1}{|Z_i|^2} [ |V_{ai}| (R_i \sin \alpha_i - X_i \cos \alpha_i) + X_i V_{bi} ] \tag{2.6}
\]

Current \( I_{ai} \) is decomposed into two components; i.e., current \( I_{bi} \) that is in phase with \( V_{bi} \) and current \( I_{si} \) that is 90 degrees behind \( V_{bi} \). Thus,

\[
I_{bi} = \frac{1}{|Z_i|^2} [ |V_{ai}| (R_i \cos \alpha_i + X_i \sin \alpha_i) - R_i V_{bi} ] \tag{2.7}
\]

\[
I_{si} = j \frac{1}{|Z_i|^2} [ |V_{ai}| (R_i \sin \alpha_i - X_i \cos \alpha_i) + X_i V_{bi} ] \tag{2.8}
\]

By considering (2.3) in (2.6) and (2.7) current \( I_{ai} \) and \( I_{ci} \) would be \( I_{ai} = \frac{1}{|Z_i|^2} (R_i \cos \alpha_i) + X_i \sin(\alpha_i) + j(R_i \sin(\alpha_i) - X_i \cos(\alpha_i)) ) \times |V_{ai}| + \frac{-Z_i'}{t_i \times |Z_i|^2} \times V_{ci} \) and \( I_{ai} = \frac{-1}{t' \times |Z_i|^2} (R_i \cos(\alpha_i) + X_i \sin(\alpha_i)) ) \times |V_{ai}| + \frac{R_i}{|Z_i|^2} \times V_{ci} \), respectively. The matrix format of \( I_{ai} \) and \( I_{ci} \) is shown in (2.9) at the top of the next page. Multiplying the first column of (2.9) by \( \frac{14 \alpha_i}{14 \alpha_i} \) yields phasor \( V_{ai} \) in the voltage vector at RHS of (2.9) leading to (2.10) as shown at the top of the next page.

Building block (2.10) can now be used to model the rectifiers and inverters along with the ac equivalent circuit of the dc sub-circuit to create an all-ac network. Thus, conventional ac analysis can be applied to the equivalent circuit. Note that the ACN node \( V_{bi} \) is omitted in the
proposed building block representation and the ac border node voltage $V_{ai}$ and DEAN voltage $V_{ci}$ are related via the proposed building block. Thus, the load flow algorithm must find $\alpha_i$ and $|V_{ci}|$. The elements of the building block (2.10) are functions of angle $\varphi_{ai}$ and $\alpha_i$. Here, a synthesized phasor $|V_{ci}| \cdot 4\alpha_i$ is selected as an unknown variable in the context of conventional load flow. Consequently, jacobian elements pertinent to $|V_{ai}|$, $|V_{ci}|$, $\varphi_{ai}$ and $\alpha_i$ needs to be constructed from power balance equations using admittance building block (2.10). After load flow is conducted the converter angle $\alpha_i = \varphi_{ai} - \varphi_{bi}$ is obtained and can be directly used for converter PWM control.

\[
\begin{bmatrix}
|I_{ai}| \\
|I_{ci}|
\end{bmatrix} = \begin{bmatrix}
\frac{1}{|Z_i|^2} (R_i \cos(\alpha_i) + X_i \sin(\alpha_i)) & -\frac{Z_i^*}{t_i |Z_i|^2} \\
-\frac{1}{t_i^* |Z_i|^2} (R_i \cos(\alpha_i) + X_i \sin(\alpha_i)) & \frac{R_i}{|Z_i|^2}
\end{bmatrix} \times \begin{bmatrix}
|V_{ai}| \\
|V_{ci}|
\end{bmatrix}
\]

(2.9)

\[
BB = \begin{bmatrix}
\frac{1}{Z_i} & -\frac{Z_i^*}{t_i |Z_i|^2} \\
-\frac{Z_i^*}{2 \cdot t_i^* |Z_i|^2} (-1 + \cos(2\alpha_i) - j \sin(2\alpha_i)) & \frac{R_i}{|Z_i|^2}
\end{bmatrix} = \begin{bmatrix}
\frac{1}{Z_i} & -\frac{Z_i^*}{t_i |Z_i|^2} \\
-\frac{1}{2 \cdot t_i^* Z_i} (-1 + \cos(2\alpha_i) - j \sin(2\alpha_i)) & \frac{R_i}{|Z_i|^2}
\end{bmatrix}
\]

(2.10)

Remark 3. Equation (2.10) depends on the phase shifter ratio $t_i$ as well as the phase difference $\alpha_i = \varphi_{ai} - \varphi_{bi}$ for $1 \leq i \leq k$. Note that $t_i = |t_i| \cdot 4\beta_i$ where $\beta_i = \varphi_{ci} - \varphi_{bi} = \alpha_i - \varphi_{ai}$ (note that $\varphi_{ci} = 0$.) In fact, building block (2.10) can be established when all the phase angles at inverters and rectifiers ACNs are known (i.e., $\alpha_i$ for all $1 \leq i \leq k$). At steady-state, these angles are constant and can be found via load flow. At transients, the adjustments on these angles are performed through linear and nonlinear controllers embedded in the ac-dc coupling devices. However, when using nonlinear transient simulators $\alpha_i$ is constant at each time step. Thus, for the purpose of ac-dc analysis, it is assumed in this chapter that angles $\alpha_i$ are constant but unknown.

Remark 4. The series reactance $X_i$ is used to model connecting line and filter series
reactances. Additional filter components typically come next to this impedance and thus can be accommodated by adding an additional ac bus and filter impedances (L’s and C’s). Consequently, we don’t model them in the coupling mechanism. Series resistance $R_i$ models the switching losses and other series resistances. While the converter losses depend on both voltage and current, the converter voltage does not vary much, unlike the converter current, and thus the series resistance is only used.

Modeling Line-Commutated Converter (LCC). The ac-dc voltage relationship in LCC can be expressed as $V_{dc} = 1.35 \times V_{ac} \times \cos \delta$ [46], where $V_{dc}$, $V_{ac}$, and $\delta \geq 0$ are dc voltage, ac line-to-line voltage and firing angle in a six-pulse LCC ($V_{dc} = 2 \times 1.35 \times V_{ac} \times \cos \delta$ for 12-pulse LCC [46]), respectively. That is, similar to voltage-source converters, it indicates a linear relationship between dc and ac voltages once the converter parameters are set. Thus, one can use equations (2.2) to relate ac and dc voltages and currents, where $V_{ac} = a \times V_{dc}$, $a = 0.741 \times M_a$, and $M_a = \frac{1}{\cos \delta}$. Similar to the VSC discussed earlier, one can select base voltages at the ACN and DEAN such that $V_{base ac} = 0.741 \times V_{base dc}$ for $M_a = 1$ and incorporate $M_a > 1$ via tap changer model or utilize $V_{base ac} = 0.741 \times M_a \times V_{base dc}$ as discussed before. Note that, unlike in VSC, LCC active and reactive powers are not independent. Since power factor angle is the same as firing angle $\delta_i$ [43], active and reactive currents in (2.7) and (2.8) are related to firing angle as (see diagram of Figure 2.5):

\[
\begin{align*}
I_{bi} & = |I_{ai}| \cos \delta_i \quad (2.11) \\
I_{si} & = -j|I_{ai}| \sin \delta_i \quad (2.12)
\end{align*}
\]

with $\delta_i \geq 0$ the firing angle. Equations (2.7) and (2.11) lead to $\frac{1}{|Z_i|^2} [I_{ai} \left( R_i \cos \alpha_i + X_i \sin \alpha_i \right) - R_i V_{bi}] = |I_{ai}| \cos \delta_i$. Also, equations (2.8) and (2.12) lead to $-\frac{1}{|Z_i|^2} [I_{ai} \left( R_i \sin \alpha_i - X_i \cos \alpha_i \right) + X_i V_{bi}] = |I_{ai}| \sin \delta_i$. By considering the magnitude of current $I_{ai}$ in (2.13), based on (2.6), into
\[ \frac{1}{|Z_i|^2} [|V_{ai}|(R_i \cos \alpha_i + X_i \sin \alpha_i) - R_i |V_{bi}|] = |I_{ai}| \cos \delta_i \quad \text{and} \quad \frac{1}{|Z_i|^2} [|V_{ai}|(R_i \sin \alpha_i - X_i \cos \alpha_i) + X_i |V_{bi}|] = |I_{ai}| \sin \delta_i \]

one can has (2.14) and (2.15) as

\[ |I_{ai}| = \sqrt{(|V_{ai}|^2 + |V_{bi}|^2 - 2|V_{ai}||V_{bi}| \cos(\alpha_i)) \times (R_i^2 + X_i^2)} \]

Equation (2.14) gives \( \cos \delta_i \) as a function of the angle \( \alpha_i \). Also, as discussed earlier, \( M_{ai} \) is a function of \( \delta_i \); and thus, building block (2.10) will still be used to obtain angle \( \alpha_i \) and DEAN voltage \( V_{ci} \) with given firing angle \( \delta_i \) that influences building block (2.10). That is, for per unit Method 1, \( t_i = \frac{1}{M_a} \angle \beta_i = \cos \delta_i \angle \beta_i \) in building block (2.10) whereas for per unit Method 2, \( t_i = 1 \angle \beta_i \) but \( V_{base_{ac}} = 0.741 \frac{1}{\cos \delta_i} \times V_{base_{dc}} \) is utilized. Note that unlike in VCSs, angle \( \alpha_i \) (the angle between ACN and ac border node) cannot be set in LCCs; instead, it is obtained as a function of firing angle \( \delta_i \). In this chapter, much attention is paid to VSC modeling in hybrid circuits and thus we will not discuss LCC modeling further.

### 2.3 Ac-Dc Steady-State Analysis

So far, (2.2) and (2.10) synthesize the dc sub-circuit as an ac equivalent circuit embedded in a larger ac circuit where injected currents at DEANs (i.e., vector \( I_1 \) in (2.2)) are set to zero as a result of using the proposed connecting building block. Note that (2.10) must repeat \( k \) many times to account for \( k \) many ACNs and (2.2) is constructed for each dc sub-circuit in the greater ac-dc grid. Once the ac equivalent circuit is established, one may generalize methods available for ac circuits to the synthesized all-ac system. After unifying both ac and dc grids, the conventional Newton-Raphson (fast and efficient) algorithm can be used for load flow analysis.
2.3.1 Generalized Newton-Raphson Power Flow

In the proposed load flow algorithm, no slack bus is used at the dc grid; thus, only one slack bus is considered at the original ac sub-circuit. In addition, similar to the conventional ac load flow analysis load (P-Q) and constant-voltage (P-V) buses can be considered for the ACNs. The following load flow strategies can be realized through the proposed modeling.

P-Q ACN: As mentioned earlier, the ACNs or the associated DEANs can be set as P-Q buses. This lets the inverter/rectifier ac or dc voltage be obtained in a load flow solution and not be a constraint. For convenience, the coupling mechanism is a super node with the unknown synthesized phasor \(|V_{ci}|\alpha_i\) while the node active power demand \(P_{ci}\) at the dc side (i.e., \(V_{ci}\)) and reactive power demand \(Q'_{ci}\) (reactive power injection requirement at ACN node \(V_{bi}\)) at the super node are given and can be used in the power mismatch equations of the conventional load flow algorithms (see Figures. 2.2 & 2.3). Also, for all DEANs, bus voltage magnitude is the only unknown and active power demand/injection is known in the power mismatch equations.

The reactive power injection at the ACN can be calculated as

\[
Q'_{ci} = \text{imag}\{V_{bi} \ast I_{si}\}. \\
V_{bi} = \frac{V_{ci}}{t_i} \quad \text{and} \quad I_{si} = \frac{|V_{ai}|}{|Z_i|^2} \left( (X_i \cos(\alpha_i) - R_i \sin(\alpha_i)) \right) \angle (\varphi_{bi} - 90) - \frac{x_i |V_{bi}|}{|Z_i|^2} \angle (\varphi_{bi} - 90)
\]

\[\text{(2.16)}\]

where \(Q'_{ci}\) is the reactive power demand. Selecting reactive power demand is an available setting for VSCs and thus a P-Q bus type can be chosen for ACNs. By contrast, LCCs involve reactive power consumption that needs to be determined and adequate compensation be arranged. Thus, when P-Q bus type is used with LCCs adequate reactive power consumption must be estimated and assigned. In the conventional jacobian matrix, elements pertaining ac nodes, \(|V_{ai}|\) and \(\varphi_{ai}\), as well as those pertaining synthesized phasor \(|V_{ci}|\alpha_i\) (i.e., \(|V_{ci}|\) and \(\alpha_i\)), must be generated to reflect
admittance building block (2.10). The mismatch equations can then be represented as (2.17).

\[
\begin{bmatrix}
\Delta P \\
\Delta Q
\end{bmatrix} = [J] \times \begin{bmatrix}
\Delta \delta \\
\Delta |V|
\end{bmatrix}
\]  

(2.17)

where \([\Delta P]_{(m-1)+n} = [\Delta P_{ac}, \Delta P_{dc}]^T\), \([\Delta Q]_{(m_1+k)} = [\Delta Q_{ac}, \Delta Q_c']^T\), \([\Delta \delta]_{(m-1)+k} = [\Delta \delta_{ac}, \Delta \alpha]^T\), \([\Delta |V|]_{(m_1+n_1)} = [\Delta |V_{ac}|, \Delta |V_{dc}|]^T\). Here, \(m\) and \(n\) are the number of ac and dc buses, respectively; \(m_1\) and \(n_1\) are the number of ac P-Q buses and dc load buses, respectively; and \(k\) is the number of ACNs. \([J]\) is the conventional jacobian matrix that incorporates the elements stemmed from the building block (2.10) as well. These elements are provided in Table 2.1.

To justify the functionality of the proposed unified ac-dc load flow algorithm in (2.17), the numerical analysis is used. In the numerical analysis the jacobian matrix elements are found in numerically. For instance, instead of using the derivative of active power with respect to the phase angle \(\frac{\partial P}{\partial \delta}\), the \(\frac{P_2-P_1}{\Delta \delta}\) is utilized.

P-V ACN: In case a constant known voltage is desired at the ACN or its corresponding DEAN (\(|V_{bi}|\) or \(|V_{ci}|\) in Figure 2.4), the DEAN voltage \(V_{ci}\) and active power \(P_{ci}\) are known and load flow finds \(\alpha_i\) and converter injected/absorbed reactive power \(Q'_{ci}\) where a negative value represents power injection. A special case of P-V ACN is when a dc sub-circuit isolates two ac sub-circuits. In this case, an angle reference is needed in each separated ac sub-circuit. This can be accommodated in two ways. If the isolated ac sub-circuit has a generator that can be assigned as a slack bus, a second ac slack bus is feasible to provide phase reference. However, if such a generator bus is not feasible, the feeding inverter, which is a P-V bus can serve as the phase reference by selecting the phase shifter angle \(\beta_i = 0\) (see remark 3) that yields \(\alpha_i = \varphi_{ai}\) when \(\theta = 0\) is selected for the DEANs. This, in turn, removes the reference converter’s \(\alpha_i\) from the unknown vector. P-V bus type is a more appropriate utilization for LCCs as well where a constant dc voltage can be aimed and reactive power compensation be calculated.

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Table 2.1. The jacobian matrix elements – subscripts “-bi” indicate border node values

\[
\frac{\partial P_{ac,b_i}}{\partial q_{ac,b_i}} = -\sum_{j \neq i,k} |V_{ac,b_j}| * |V_j| * |Y_{ac,b_i,j}| * \sin(q_{ac,b_i} - q_j - \theta_{ac,b_i,j})
\]

\[
\frac{\partial P_{ac,b_i}}{\partial \phi_{ac,b_i}} = \sum_{j \neq i,k} |V_{ac,b_j}| * |V_j| * |Y_{ac,b_i,j}| * \cos(-\theta_{ac,b_i,j} + \theta_{ac,b_i,j})
\]

\[
\frac{\partial P_{dc,b_i}}{\partial q_{dc,b_i}} = 2 * |V_{dc,b_i}| * |Y_{dc,b_i}| * \cos(q_{dc,b_i} - q_j - \theta_{dc,b_i,j})
\]

\[
\frac{\partial P_{dc,b_i}}{\partial \phi_{dc,b_i}} = |V_{dc,b_i}| * |Y_{dc,b_i}| * \cos(q_{dc,b_i} - q_j - \theta_{dc,b_i,j})
\]

\[
\frac{\partial Q_{ac,b_i}}{\partial q_{ac,b_i}} = \sum_{j \neq i,k} |V_{ac,b_j}| * |V_j| * |Y_{ac,b_i,j}| * \cos(q_{ac,b_i} - q_j - \theta_{ac,b_i,j})
\]

\[
\frac{\partial Q_{ac,b_i}}{\partial \phi_{ac,b_i}} = -|V_{ac,b_i}| * |Y_{ac,b_i}| * \cos(q_{ac,b_i} - q_j - \theta_{ac,b_i,j})
\]

\[
\frac{\partial Q_{ac,b_i}}{\partial q_{dc,b_i}} = 2 * |V_{dc,b_i}| * |Y_{dc,b_i}| * \cos(q_{dc,b_i} - q_j - \theta_{dc,b_i,j})
\]

\[
\frac{\partial Q_{ac,b_i}}{\partial \phi_{dc,b_i}} = |V_{dc,b_i}| * |Y_{dc,b_i}| * \cos(q_{dc,b_i} - q_j - \theta_{dc,b_i,j})
\]

\[
\frac{\partial Q_k'}{\partial q_{ac,b_i}} = \frac{|V_{ac,b_i}| * |V_{dc,b_i}| * (X_k \cos(\alpha_k) - R_k \sin(\alpha_k))}{X_k^2 + X_k^2}
\]

\[
\frac{\partial Q_k'}{\partial \phi_{ac,b_i}} = \frac{|V_{ac,b_i}| * |V_{dc,b_i}| * (X_k \sin(\alpha_k) - R_k \cos(\alpha_k))}{X_k^2 + X_k^2}
\]

\[
\frac{\partial Q_k'}{\partial q_{dc,b_i}} = \frac{|V_{dc,b_i}|}{X_k^2 + X_k^2}
\]

\[
\frac{\partial Q_k'}{\partial \phi_{dc,b_i}} = \frac{|V_{dc,b_i}|}{X_k^2 + X_k^2}
\]
2.4 Case Study and Simulation Results

Three ac-dc grids are simulated in this section to evaluate the performance of the proposed modeling and load flow method. Both constrained and non-constrained power flow studies are considered using the proposed modeling and load flow method.

2.4.1 Case I: Ac-Dc Tie Line

The tested network in [31], depicted in Figure 2.6, is used to evaluate the proposed modeling and load flow algorithm. P-V bus type is considered (P\(_g\) = 0) for the DEAN 8 and the voltage at the corresponding ac border node (Node 2) along with the required reactive power injection Q\(_r\) are obtained via load flow. This is equivalent to the constant-voltage constraint at the inverter terminals used in available methods [31]. A negative value for \(\alpha = -0.0057^\circ\) indicates a flow of power from the dc side to the ac side of the grid (inverter operation). The active power through the inverter for the proposed algorithm and the method in [31] are 0.0493\(^p.u\) and 0.0492\(^p.u\), respectively. Alternatively, P-V bus type can be assigned to the ac Border Node 2 and the corresponding DEAN voltage (Node 8) can be found via load flow when reactive power injection (Q\(_c\)) of 0.8876\(^p.u\), (from [31]) is considered at the ACN connected between Nodes 2 and 8. Similarly, a negative \(\alpha = -0.0057^\circ\) indicates a flow of power from the dc side to the ac side of the grid. The flow of active power through the inverter is unchanged. Both load flows based on the proposed method converge in four iterations (with voltage error in the Newton-Raphson algorithm less than 10\(^{-6}\)) and take 0.08s when programmed in Matlab on an Intel 2.10GHz compared to 0.16s on Core-i5, 3.30GHz in [31]. The voltage comparison is given in Figure 2.7.

2.4.2 Case II: Modified IEEE 39-Bus System

The IEEE 39-bus benchmark system with some alterations to include dc buses is considered to evaluate the proposed modeling and load flow method. Figure 2.8 illustrates the tested grid
where the dc sub-grid, using the same line resistances as in the original ac grid, is connected to the ac sub-grid through two ac-dc converters. Two DG units are connected to Buses 37 and 38 with active powers equal to those of the generators in the original ac circuit represent dc constant-power (P-Q) sources. Two different scenarios are performed here. In the first one, the VSC type is considered for both ac-dc converters, while in the other one, the dc grid is connected to the ac grid through two LCC type ac-dc converters.

Figure 2.6. Case I, ac-dc tie line [31]
Figure 2.7. Voltage profile comparison for the tested network in Figure 2.6

VSC type ac-dc converters: the modulation factors \( M_a = 1 \) is set at all VSCs. No voltage or power constraints are considered at the converter terminals and no dc slack bus is assigned, and thus, the DEAN voltages along with the converter angles \( \alpha \) are obtained via the proposed simultaneous load flow. For this purpose, inspired by the original ac grid line power exchanges, converters’ reactive power injections \( Q' \) for converters at Bus 25 and Bus 27 are assigned as
0.8268 p.u and 0.0 p.u, respectively. Angles $\alpha_1$ (between Bus 2 to Bus 25) and $\alpha_2$ (between Bus 17 to Bus 27) are -0.2551 deg and -1.5360 deg, respectively. The negative angles indicate active power flow from dc nodes to the ac nodes. The voltages at the ac generator buses (P-V) and slack bus 31 are set equal to those of the original ac grid. The results of this simulation are provided in Table 2.2. From Figure 2.9, the DG voltages in the dc circuit are higher than in the original ac grid while intermediate dc nodes have lower voltages to allow for power flow from the dc DG units. Table 2.3 summarizes selected ac-dc power exchanges and compares them with those in the original IEEE 39-bus system with the same nodal power generation levels to illustrate the convergence rate and solution feasibility. Next, the circuit of Figure 2.8 is tested when constant-voltage constraints at the inverter terminals are considered for the DEANs 25 and 27 (P-V bus types are set). For this purpose, zero active power generation along with the original ac nodal voltages are considered for converter terminals (via P-V bus type setting) and converters’ injected reactive powers $Q'_c$ are obtained via load flow. The proposed load flow obtains angles $\alpha_1$ and $\alpha_2$ as -0.02 deg and -2.0042 deg, respectively. The angles indicate the flow of active powers from dc Bus 25 (0.0446 p.u) and dc Bus 27 (2.1520 p.u) towards the ac network. Also, the reactive power flow transmitted from ac border nodes 2 and 17 to the converters are 0.3985 p.u and 0.0579 p.u, respectively, and the reactive power demand by ACNs 25 and 27 are 0.3973 p.u and -0.0174 p.u, respectively. Note that due to higher voltages set at converter terminals, DG units have higher voltages than the previous case. All simulations converge in 4 iterations (with a voltage error in the Newton-Raphson algorithm less than $10^{-6}$).

In order to validate the results from the proposed modeling, the ac and dc girds’ mismatch equations of the modified IEEE 39-bus system along with the ac-dc converters power equations are solved separately. For this purpose, three separate power mismatch equations are used; one for
the ac nodes as $[\Delta P_{ac}, \Delta Q_{ac}]^T = [J_{ac}] [\Delta \delta_{ac}, \Delta |V_{ac}|]^T$, one for the dc nodes as $\Delta P_{dc} = [J_{dc}] \Delta |V_{dc}|$

where the power mismatch at ac and dc border nodes $V_a$ and $V_c$ are modified by using $P_{\text{ac-side}} - P_{\text{dc-side}} = 0$ leading to

$$V_{bi} \times I_{bi} - \sum_j V_{ci} \times V_j \times y_{cij} = 0$$

(2.18)

and one for reactive power demand (2.16) where $V_{bi}$ and $V_{ci}$ are the ACN and corresponding DEAN voltage phasors, respectively; $V_j$ is a dc node that is connected to the DEAN $V_{ci}$, and $y_{cij}$ is the corresponding line admittance. Equations (2.16) and (2.18) will represent the active and reactive power mismatch at the both side of the coupling mechanism, this in turn helps to make a connection between ac and dc sides of the converter. Subsequently, an overall jacobian matrix $[J_{ac-dec}]$ is obtained such that $[\Delta P, \Delta Q]^T = [J_{ac-dec}] [\Delta \delta, \Delta |V|^T]$. Finally, $\delta$ and $|V|$ are calculated where $[\delta]_{(m-1) \times k} = [\delta_{ac}, \alpha]^T$ and $[|V|]_{(m+1) \times n_1} = [|V_{ac}|, |V_{dc}|]^T$ as introduced in (2.14). Figure 2.9 shows that the results from the proposed unified approach and those from the separate power mismatch equations are in good agreement.

LCC type ac-dc converters: Here, both LCCs are considered as P-Q buses. Also, there is no injection of reactive power ($Q'$) at the ac-dc coupling nodes since they are LCC type. After performing the proposed unified load flow the firing angle of LCCs that are connected to Buses 25 and 27 are $140.2525^{\text{deg}}$ and $180.00^{\text{deg}}$, respectively. This implies that both LCCs are in the inverter mode. The voltage profile of the network is depicted in Figure 2.10. The results are then compared with a separate load flow algorithm that was explained earlier.
Figure 2.8. Case II – modified IEEE 39-bus grid
Table 2.2. Results of the modified IEEE 39-bus system when P-Q bus type is considered for VSCs

<table>
<thead>
<tr>
<th># of Bus</th>
<th>Voltage^{p.u}</th>
<th>Angle^{deg}</th>
<th>Alpha^{deg}</th>
<th>P_g^{p.u}</th>
<th>Q_g^{p.u}</th>
<th>P_L^{p.u}</th>
<th>Q_L^{p.u}</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1.0469</td>
<td>-9.5675</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>2</td>
<td>1.0466</td>
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Figure 2.9. Voltage profile comparison of the network in Figure 2.8 – VSC

Table 2.3. The power exchanges in the ac-dc compared with those in the original ac network

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To evaluate the functionality of the proposed modeling and load flow method for the larger hybrid ac-dc grid, the IEEE 118-bus benchmark system is considered as a test network in this case. Figure 2.11 shows the modified IEEE 118-bus benchmark system where the ac circuit is connected to the dc grid through four ac-dc converters. The VSC type is considered for all four ac-dc converters in this network. The modifications in the original ac IEEE 118-bus are same as those in
IEEE 39-bus. The loads and lines’ resistance in the dc network are the same as the original ac grid. Five DG units are considered at Buses 10, 12, 25, 26, and 31 with active powers equal to those of the generators in the original ac circuit represent dc constant-power (P-Q) sources. Also, generators at Buses 1, 4, 6, 8, 15, 18, 19, 27, 32, and 113 in the original ac system that has no generation of active power do not have any representative in the dc grid. Two of these five DGs are modeled as energy storages (those on Buses 10 and 12) and the rest modeled as the PV (those on Buses 25, 26, and 31). The energy storage has effect on the impedance matrix of the network during the fault while the PV has no effect. Also, same as the Case II: Modified IEEE 39-Bus System, The modulation factors $M_a = 1$ is set at all VSCs. No voltage or power constraints are considered at the converter terminals and no dc slack bus is assigned, and thus, the DEAN voltages along with the converter angles $\alpha$ are obtained via the proposed simultaneous load flow. For this purpose, inspired by the original ac grid line power exchanges, converters’ reactive power injections $Q'$ for converters at Buses 19, 23, 30, and 33 are assigned as $0.1112^{\text{p.u.}}$, $-0.0068^{\text{p.u.}}$, $-0.4926^{\text{p.u.}}$, $0.3882^{\text{p.u.}}$, respectively. Angles $\alpha_1$ (between Bus 19 to Bus 34), $\alpha_2$ (between Bus 23 to Bus 24), $\alpha_3$ (between Bus 30 to Bus 38), and $\alpha_4$ (between Bus 33 to Bus 37) are $-3.6927^{\text{deg}}$ and $0.3897^{\text{deg}}$, $0.2715^{\text{deg}}$, and $-3.3658^{\text{deg}}$, respectively. The negative angles indicate active power flow from dc nodes to the ac nodes whereas the positive angles demonstrate the flow active power from ac node to the dc node. The voltages at the ac generator buses (P-V) and slack bus 69 are set equal to those of the original ac grid. The voltage profile of the network is shown in Figure 2.12. The results of this case are provided in Table 2.4.
Figure 2.11. Case III – modified IEEE 118-bus grid
Figure 2.12. Voltage profile of the network in Figure 2.11

Table 2.4. Results of the modified IEEE 118-bus system

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<td>117</td>
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### 2.5 Conclusion

This chapter presents an alternate method for steady-state analysis of interconnected hybrid ac-dc circuits based on an ac equivalent circuit. The method proposes an admittance matrix to represent the ac-dc coupling mechanism through which a simultaneous ac-dc load flow can be achieved with minimal modifications in the conventional load flow programs. In addition, the proposed admittance matrix provides a high-level analysis tool for a system in normal and contingent conditions and makes available relaxing of voltage constraints at the coupling nodes. The convergence rate of the proposed load flow is comparable to that of the conventional ac load flow programs.
CHAPTER 3. A NOVEL METHOD FOR HYBRID AC-DC CIRCUIT FAULT ANALYSIS

3.1 Introduction

Despite the introduction of the distributed energy resources (DERs) and the surge in popularity and usage of dc grids, the ac grid is still predominant in the power system due to the abundance of ac loads and energy sources. The ac-dc hybrid grid has emerged by connecting the ac and dc grids together through the ac-dc converters to exploit the benefits of both grids. In the hybrid grid, the ac and dc sides exchange power while the nature of current is different on the two sides of the point of common coupling (PCC) causing the complexity of the steady-state and dynamic analysis of such hybrid grids. Despite standalone dc or ac grids that can be comprehensively studied through the traditional steady-state and dynamic analysis methods, the hybrid grids have not been treated through a single analysis. This chapter proposes a new method that extends the classical ac fault analysis to the hybrid grid by obtaining a Thevenin equivalent circuit of the dc laterals.

While numerous researches have studied the steady-state analysis of the ac-dc hybrid grids [47-52], less emphasis has been given to the fault analysis of such grids. Recently, the population of DERs in the power system has considerably increased. This high penetration of DERs may have negative effect on the power network [53, 54] including making the fault current calculation more complex [55]. The type of DER in the power grid has a direct impact on the contribution of the aforementioned fault current [56]. High short circuit currents can exceed the range of current

---

protection relays and cause complications, such as loss of synchronization, power outage, and transient over/under voltage [57]. Methods that are established to manage the short circuit current include grid reconfiguration and isolation [58], installing a high impedance transformer [59], and installing a fault current limiters (FCLs) [59-63], which depend on an appropriate fault calculation method for design and application.

In order to protect the hybrid grid during the fault, one must calculate the fault current and select appropriate circuit breakers (CBs) and protective relay settings. However, new developments in the power system, such as DERs and ac-dc converters, that give the ability of deregulation to the network make the classical fault analysis complicated due to the difficulty in modeling the ac and dc systems together and incorporating the power electronic converters.

The idea of the modification of Z$_{bus}$ for conventional fault analysis is expressed in [64]. A building block of ac-dc conversion was introduced in [64]. By contrast, this chapter introduces a Thevenin equivalent circuit of the dc circuit that can be used in the fault analysis of the ac network. Emphasis is given to the ac network fault calculation in this chapter due to the predominance of the ac components in the current grids. The fault calculation in the dc side of the network can be performed similarly by applying a Thevenin equivalent of the ac circuit to the dc network and is not detailed here. Moreover, the time range of ac and dc faults are not similar where dc fault detection and protection mechanisms are different from those of their ac counterparts. Thus, this work is devoted to ac faults in the hybrid circuit. The authors are currently working on dc faults in the hybrid circuit in another effort.

3.2 Fault Analysis

A fault in a circuit is any failure, which interferes with the normal flow of current [65]. One of the most common types of faults in the power system is short circuit faults that can be classified
into symmetrical and asymmetrical short circuit faults. In symmetrical faults, a three-phase short circuit occurs somewhere in the power system and currents and voltages remain symmetrical; consequently, the single line model of the power network can be used to analyze the power system. In asymmetrical faults, however, single line to ground (SLG), line-to-line (L-L), and double line-to-ground (DLG) short circuit fault can occur where currents and voltages will not remain symmetrical; thus, the sequence circuits analysis shall be carried out to analyze the faulty power grid. In both types of faults, the impedance matrix $Z_{bus}$ provides the Thevenin impedance of the circuit and plays an important role in the fault analysis. Symmetrical short circuit faults will be considered in this chapter.

Loads and shunt capacitors will be ignored in the short circuit fault analysis because the fault current is considerably more than their currents. Consequently, voltages of all buses in the power grid can be estimated at 140 pu. If a symmetrical three-phase short circuit fault takes place at bus $j$, in an ac power system with $m$ buses, the change in the bus voltages due to the fault current $I_f$ can be determined from (3.1) where $Z_{bus}$ represents the grid impedance matrix, $v^f_j$ is the prefault voltage at the bus $j$, and prefix $\Delta$ indicates the change in the bus voltage. The fault current ($I_f^f$) can be straightforwardly calculated by (3.2) where $Z_{jj}$ represents the element $jj$ of the network’s impedance matrix ($Z_{bus}$) and is the Thevenin impedance at bus $j$, and $Z^f$ is the fault impedance. Thus, the effect of fault current is shown as

\[
\begin{bmatrix}
\Delta v_1 \\
\Delta v_2 \\
\vdots \\
\Delta v_j \\
\vdots \\
\Delta v_m \\
\end{bmatrix} =
\begin{bmatrix}
\Delta v_1 \\
\Delta v_2 \\
\vdots \\
- v^f_j \\
\vdots \\
\Delta v_m \\
\end{bmatrix} =
\begin{bmatrix}
0 \\
0 \\
\vdots \\
- I_f^f \\
\vdots \\
0 \\
\end{bmatrix}
\begin{bmatrix}
Z_{bus} \\
0 \\
\vdots \\
- I_f^f \\
\vdots \\
0 \\
\end{bmatrix}
\]

(3.1)

\[I_f^f = \frac{v^f_j}{Z_{jj} + Z^f} \approx \frac{140}{Z_{jj} + Z^f}
\]

(3.2)
3.3 Mathematical Model

Modeling of ac-dc converters is a critical part of fault analysis of hybrid circuits. The details of this modeling can be found in the previous chapter (see section 2.2). Based on Figure 2.5, \( I_{ai} = I_{bi} + I_{si} = \frac{1}{Z_i} (|V_{ai}| \angle \varphi_{ai} - |V_{bi}| \angle \varphi_{bi}) \).

As discussed earlier, for convenience, the ACN phase angle \( \varphi_{bi} \) is considered as the phase reference in (2.5) (i.e., \( V_{bi} = |V_{bi}| \)) and \( \alpha_i = \varphi_{ai} - \varphi_{bi} \) is defined. Consequently, \( I_{ai} = \frac{1}{|Z_i|^2} [|V_{ai}| (R_i \cos \alpha_i + X_i \sin \alpha_i) - R_i V_{bi}] + j \frac{1}{|Z_i|^2} [|V_{ai}| (R_i \sin \alpha_i - X_i \cos \alpha_i) + X_i V_{bi}] \).

Current \( I_{ai} \) is decomposed into two components; i.e., current \( I_{bi} \) that is in phase with \( V_{bi} \) and current \( I_{si} \) that is 90 degrees behind \( V_{bi} \). Thus, \( I_{bi} = \frac{1}{|Z_i|^2} [|V_{ai}| (R_i \cos \alpha_i + X_i \sin \alpha_i) - R_i V_{bi}] \) and \( I_{si} = j \frac{1}{|Z_i|^2} [|V_{ai}| (R_i \sin \alpha_i - X_i \cos \alpha_i) + X_i V_{bi}] \).

In this chapter, we try to find a way that the current \( I_{ai} \) can be represented only based on its own bus voltage \( (V_{ai}) \). In other words, both components of current \( I_{ai} \); i.e., \( I_{bi} \) and \( I_{si} \) should be represented based on the vector of \( V_{ai} \). This helps to find the mathematical relation between the buses \( a_i \) and \( b_i \) (ac bus and ac-dc converters) and consequently the Thevenin or Norton equivalent of dc circuits. This Thevenin or Norton equivalent circuit will then be used for fault analysis of hybrid ac-dc circuits. One can multiply (2.7) with \( \frac{14 \alpha_i}{14 \alpha_i} \) to have:

\[
\vec{I}_{bi} = \frac{(14 - \alpha_i)}{|Z_i|^2} \left[ \vec{V}_{ai} (R_i \cos \alpha_i + X_i \sin \alpha_i) \right] - \frac{R_i V_{bi}}{|Z_i|^2} \Rightarrow \vec{I}_{bi} = \vec{K}_1 \times \vec{V}_{ai} - \frac{R_i}{|Z_i|^2} V_{bi} \tag{3.3}
\]

where \( \vec{K}_1 = \frac{(14 - \alpha_i)}{|Z_i|^2} \left[ (R_i \cos \alpha_i + X_i \sin \alpha_i) \right] \).

Current \( I_{si} \) in (2.8) can be decomposed into two components \( I_{si,1} \) and \( I_{si,2} \), i.e., \( I_{si} = I_{si,1} + I_{si,2} \). One can find current \( I_{si,1} \) and \( I_{si,2} \) as
\[ I_{sl,1} = \left( \frac{1}{|z_i|^2} (R_i \sin \alpha_i - X_i \cos \alpha_i) (14.90) \right) \times |V_{ai}| \Rightarrow \left( \frac{1}{|z_i|^2} (R_i \sin \alpha_i - X_i \cos \alpha_i) \right) \times \vec{V}_{ai} \Rightarrow \]

\[ I_{sl,1} = K_2 \times \vec{V}_{ai} \quad (3.4) \]

\[ I_{sl,2} = \left( \frac{1}{|z_i|^2} (14.90) \right) \times |V_{bi}| \Rightarrow I_{sl,2} = K_3 \times \vec{V}_{bi} \quad (3.5) \]

where \( K_2 = |z_i|^{-2} \times (R_i \sin \alpha_i - X_i \cos \alpha_i) \times (14.90) \) and \( K_3 = |z_i|^{-2} \times (14.90) \).

Current \( \vec{I}_{sl,1} \) is now represented only based on \( \vec{V}_{ai} \), while currents \( \vec{I}_{bi} \) and \( \vec{I}_{sl,2} \) are not only based on \( \vec{V}_{ai} \). Next step in this chapter is to presents bus voltage \( \vec{V}_{bi} \) based on bus voltage \( \vec{V}_{ai} \). So that, one can start from (3.6) to express \( \vec{V}_{bi} \) based on \( \vec{V}_{ai} \).

\[
\begin{bmatrix} V_{dc1} \\ V_{dc2} \end{bmatrix}_{n_{dc}+1} = \begin{bmatrix} Z_{11} & Z_{12} \\ Z_{21} & Z_{22} \end{bmatrix}_{n_{dc} \times n_{dc}} \begin{bmatrix} I_{dc1} \\ I_{dc2} \end{bmatrix}_{n_{dc}+1} \]
\]

(3.6)

where \( V_{dc1} \) and \( I_{dc1} \) are the voltages and injected currents of dc border nodes (in other words \( V_{dc1} = V_c \) and \( I_{dc1} = I_c \)), \( V_{dc2} \) and \( I_{dc2} \) are the voltages and injected currents of the dc non-border nodes, and \( Z_{dc, bus} \) is the impedance matrix of the dc grid. For convenience, it is assumed that there is no injection of currents inside the dc grid; i.e., \( I_{dc2} = 0 \); therefore, \( V_{dc1} = Z_{11} \times I_{dc1} \). By considering (2.3) in \( V_{dc1} = Z_{11} \times I_{dc1} \), one can have

\[ tV_{b} = Z_{11} \times (t^{-1})^*I_{b} \Rightarrow V_{b} = Z_{11} \times I_{b} \quad (3.7) \]

One can considers (3.7) in (3.3) to find the (3.3) only based on \( V_{ai} \) as:

\[ \vec{I}_{bi} = K_1 \times \vec{V}_{ai} \Rightarrow \left( 1 + \frac{R_i}{|z_i|^2} \times Z_{11} \right) \vec{I}_{bi} = K_1 \times \vec{V}_{ai} \Rightarrow \vec{I}_{bi} = \vec{K}_4 \times \vec{V}_{ai} \quad (3.8) \]

where \( K_4 = \left( 1 + \frac{R_i}{|z_i|^2} \times Z_{11} \right)^{-1} \times K_1 \). Equations (3.7) and (3.8) can be used in (3.5) to make a relation between \( \vec{I}_{sl,2} \) and \( V_{ai} \) as:

\[ \vec{I}_{sl,2} = K_3 \times \vec{V}_{bi} \Rightarrow \vec{I}_{sl,2} = K_3 \times Z_{11} \times I_{bi} \Rightarrow \vec{I}_{sl,2} = K_3 \times Z_{11} \times K_4 \times \vec{V}_{ai} \quad (3.9) \]
\[ I_{si,2} = K_5 \times V_{ai}, \quad K_5 = K_3 \times Z_{11} \times K_4 \]

All three components of the current \( I_{ai} \) are found based on \( V_{ai} \). The next step is to add these three parts to find out the relation of \( I_{ai} \) with \( V_{ai} \). So that

\[
I_{ai} = I_{bi} + I_{si} = I_{bi} + I_{si,1} + I_{si,2} \quad \Rightarrow \quad I_{ai} = (K_4 + K_2 + K_5) \times V_{ai} \equiv \nabla_1 \times V_{ai}

(3.10)
\]

The term \( \nabla_1 \) in (3.10) shows the Thevenin equivalent of dc circuit on the ac side when there is no injection of current in the dc side (see Figure 3.1). So that the complexity of building of the admittance matrix of hybrid ac-dc grids will be solved. The conventional method for building the admittance matrix now can be utilized for the new equivalent grid of hybrid ac-dc grid as presented in (3.11).

\[
\begin{bmatrix}
I_{ac} \\
I_{a}
\end{bmatrix}_{n_{ac}+1} =
\begin{bmatrix}
Y_{11} & Y_{12} \\
Y_{21} & Y_{22}
\end{bmatrix}_{n_{ac} \times n_{ac}}
\begin{bmatrix}
V_{ac} \\
V_{a}
\end{bmatrix}_{n_{ac}+1}
\times
\begin{bmatrix}
V_{ac} \\
V_{a}
\end{bmatrix}_{n_{ac}+1}
\]

(3.11)

where \( V_{ac} \) and \( I_{ac} \) are voltages and currents of non-border ac buses, \( V_{a} \) and \( I_{a} \) are voltages and currents of border ac buses, \( n_{ac} \) is the total number of ac buses of the hybrid grid, and \( Y_{bus,equiv} \) is the admittance matrix of the ac grid by considering the effects of the dc side. All elements of \( Y_{bus,equiv} \), except \( y_{22} \), can be calculated by the classical way of \( Y_{bus,equiv} \) calculation. \( y_{22} = Y_1 + y_{ac} \), where \( Y_1 \) is already defined in (3.10) and \( y_{ac} \) is the summation of the admittances of all non-border ac branches that are connected to the border node a, i.e., \( y_{ac} \) is defined in (3.11).

The equation in (3.11) leads to implementation of the classical ac fault analysis for the hybrid ac-dc grid with minimal changes. One can find the impedance matrix of the equivalent grid of the hybrid ac-dc grid, which is the main factor in classical ac fault analysis, by making an inverse of the admittance matrix in (3.11), i.e., \( Z_{bus,equiv} = Y_{bus,equiv}^{-1} \).
After obtaining the impedance matrix of the ac equivalent of the hybrid grid, the fault current at any bus and voltage changes of all buses during the fault can be easily calculated by (3.1) and (3.2).

Figure 3.1. Thevenin equivalent of the dc circuit that has no injection of current inside.

Remark: If there is an injection of current inside the dc grid; i.e., \( I_{dc2} \) won’t be zero in (3.6), the following steps must be taken into account for finding the equivalent of dc circuits on the ac grid.

First, one can find voltages of dc border nodes \( (V_{dc1}) \) based on dc border nodes and non-border nodes current in

\[
\left[ \begin{array}{c} I_{dc1} \\ I_{dc2} \end{array} \right]_{n_{dc+1}} = \left[ \begin{array}{cc} Y_{11} & Y_{12} \\ Y_{21} & Y_{22} \end{array} \right]_{n_{dc} \times n_{dc}} \times \left[ \begin{array}{c} V_{dc1} \\ V_{dc2} \end{array} \right]_{n_{dc} \times 1}
\]

as

\[
I_{dc1} = (Y_{11} - Y_{12} \times Y_{22}^{-1} \times Y_{21}) \times V_{dc1} + Y_{12} \times Y_{22}^{-1} \times \beta
\]

\[
I_{dc2} = V_{dc1} = \gamma^{-1} \times (I_{dc1} - \beta \times I_{dc2})
\]

(3.12)

where \( V_{dc1} \) and \( I_{dc1} \) are another terminology for DEAN voltage, i.e., \( V_c \) and \( I_c \).

Second, \( V_{dc1} \) and \( I_{dc1} \) in (3.12) will be replaced by \( V_b \) and \( I_b \) by using (2.3) and then the results will be used in rewriting the (3.5).

\[
\overrightarrow{I_{si,2}} = \overrightarrow{K_3} \times \left( t^{-1} \times \gamma^{-1} \times ((t^{-1})^* \times I_b - \beta \times I_{dc2}) \right) \Rightarrow \\
\overrightarrow{I_{sl,2}} = \overrightarrow{K_3} \times t^{-1} \times \gamma^{-1} \times (t_i^{-1})^* \times I_b - \overrightarrow{K_3} \times t^{-1} \times \gamma^{-1} \times \beta \times I_{dc2} \Rightarrow \\
\overrightarrow{I_{si,2}} = \overrightarrow{K_6} \times I_b - \overrightarrow{K_7} \times I_{dc2}
\]

(3.13)

46
where $K_6 = K_3 \times t^{-1} \times \gamma^{-1} \times (t_i^{-1})^*$ and $K_7 = K_3 \times t^{-1} \times \gamma^{-1} \times \beta$. Couple more steps should be taken to express $I_{si,2}$ in (3.13) based on $V_{ai}$.

One can start from (3.3) to find a relation between $I_{bi}$ and $V_{ai}$ as follow:

$$
\vec{I}_{bi} = \vec{K}_1 \times \vec{V}_{ai} - \frac{R_i}{|Z_i|^2} \vec{V}_{bi} \quad (2.3),(3.12)
$$

$$
\vec{I}_{bi} = \vec{K}_1 \times \vec{V}_{ai} - \frac{R_i}{|Z_i|^2} \times (t^{-1} \times \gamma^{-1} \times (t_i^{-1})^* \times \vec{I}_{bi} - t^{-1} \times \gamma^{-1} \times \beta \times I_{dc_2}) \Rightarrow
$$

$$
(1 + \frac{R_i}{|Z_i|^2} \times t^{-1} \times \gamma^{-1} \times (t_i^{-1})^*) \times \vec{I}_{bi} = \vec{K}_1 \times \vec{V}_{ai} + \frac{R_i}{|Z_i|^2} \times t^{-1} \times \gamma^{-1} \times \beta \times I_{dc_2} \Rightarrow
$$

$$
\vec{I}_{bi} = \vec{K}_8 \times \vec{V}_{ai} + \vec{K}_9 \times I_{dc_2}
$$

(3.14)

where $\vec{K}_8 = (1 + R_i \times |Z_i|^{-2} \times t^{-1} \times \gamma^{-1} \times (t_i^{-1})^* \times \vec{K}_1$ and $\vec{K}_9 = (1 + R_i \times |Z_i|^{-2} \times t^{-1} \times \gamma^{-1} \times (t_i^{-1})^* \times R_i \times |Z_i|^{-2} \times t^{-1} \times \gamma^{-1} \times \beta$.

Equation (3.13) now can be rewritten based on (3.14) now. So that

$$
\vec{I}_{si,2} = \vec{K}_{10} \times \vec{V}_{ai} + \vec{K}_{11} \times I_{dc_2}
$$

(3.15)

where $\vec{K}_{10} = \vec{K}_6 \times \vec{K}_8$ and $\vec{K}_{11} = \vec{K}_6 \times \vec{K}_9 - \vec{K}_7$.

The last step in finding the ac equivalent of dc circuit is to find the mathematical relation between $V_a$ and $I_a$. For this purpose, (3.14), (3.4), and (3.15) are used as

$$
\vec{I}_{ai} = \vec{I}_{bi} + \vec{I}_{si} = \vec{I}_{bi} + \vec{I}_{si,1} + \vec{I}_{si,2} \quad (3.14),(3.4),(3.15)
$$

$$
\vec{I}_{ai} = \vec{Y}_2 \times \vec{V}_{ai} + \lambda \times I_{dc_2}
$$

(3.15)

where $\vec{Y}_2 = \vec{K}_8 + \vec{K}_9$ and $\lambda = \vec{K}_9 + \vec{K}_{11}$

Equation (3.15) shows the Norton equivalent of a dc circuit, which has injection of currents inside, at the ac grid (see Figure 3.2). First part of (3.15), i.e., $\vec{Y}_2 \times \vec{V}_{ai}$ will be used in building the admittance matrix of the hybrid ac-dc grid, while the second part i.e., $\lambda \times I_{dc_2}$, will be considered as a current source in proposed modeling. Also, (3.15) will make the building approach of admittance matrix of hybrid ac-dc grids as the conventional method as shown in (3.16)
\[
\begin{bmatrix}
I_{\text{ac}} \\
I_a \\
I_{\text{dc}}
\end{bmatrix}_{n_t+1} =
\begin{bmatrix}
y_{11} & y_{12} & 0 \\
y_{21} & y_{22} & y_{23} \\
0 & y_{32} & y_{33}
\end{bmatrix}_{n_t+n_t} \cdot
\begin{bmatrix}
V_{\text{ac}} \\
V_a \\
V_{\text{dc}}
\end{bmatrix}_{n_t+1}
\]

(3.16)

where \(V_{\text{ac}}\) and \(I_{\text{ac}}\) are voltages and currents of non-border ac buses, \(V_a\) and \(I_a\) are voltages and currents of border ac buses, \(V_{\text{dc}}\) and \(I_{\text{dc}}\) are voltages and currents injections of dc border buses, \(n_t\) is the total number of ac and dc buses of the hybrid grid, and \(Y_{\text{bus, equiv}}\) is the admittance matrix of the hybrid ac-dc grid by considering (3.15). All elements of \(Y_{\text{bus, equiv}}\), except \(y_{22}\), can be calculated by the classical way of \(Y_{\text{bus, equiv}}\) calculation. \(y_{22} = Y_2 + y_{22}^{\text{ac}}\), where \(Y_2\) is already defined in (3.15) and \(y_{22}^{\text{ac}}\) is the summation of the admittances of all non-border ac branches that are connected to the border node \(a\).

The classical ac fault analysis now can be implemented for the hybrid ac-dc grid with minimal changes. One can find the impedance matrix of the equivalent grid of the hybrid ac-dc grid, which is the main factor in classical ac fault analysis, by making an inverse of the admittance matrix in (3.16), i.e., \(Z_{\text{bus, equiv}} = Y_{\text{bus, equiv}}^{-1}\).

![Figure 3.2. Norton equivalent of the dc circuit that has injection of current inside](image)
3.4 Case Study and Simulation Results

This section aims to numerically tests and verifies the proposed method in this chapter. The proposed approach is implemented for two different hybrid ac-dc networks that are depicted in Figure 3.3 and Figure 3.4. It is noteworthy to mention that the result here is for the case that there is no injection of current in the dc grids; however, the result for the case with the presence of injection of current in the dc grid can be easily obtained by starting from (3.16).

3.4.1 Case I

The tested network (see Figure 3.3) in this case comprises one ac-dc converter. Table 3.1 indicates the line data of the tested network. Here, it is assumed that the three-phase short circuit fault with the fault impedance of \( Z_f = j \times 0.08 \) occurs on bus 4. It should be mentioned that fault can be considered and calculated at any ac bus. By using (3.2), the fault current at bus 4 would be \( I_{4f} = 0.2230 - j \times 10.7473 \) p.u. The Thevenin impedance at bus 4, the element of \( Z_{44} \) of \( Z_{\text{bus,equiv}} \), is \( 0.0019 + j \times 0.0117 \) p.u. The fault current and the \( Z_{\text{bus,equiv}} \) can be further used to find out the during fault and after fault voltage for all buses. The same approach should be followed for the case with the presence of the injected current in the dc grid.

3.4.2 Case II

This section aims to check the functionality of the proposed method for a network with more than one ac-dc converter, i.e., a more complicated hybrid ac-dc grid. In the tested hybrid ac-dc grid, a dc circuit is connected to one ac network through three different ac-dc converters (see Figure 3.4). The line data of this hybrid grid is shown in Table 3.2. Same as the previous case, the three-phase short circuit fault with the impedance of \( Z_f = j \times 0.08 \) is considered at bus 4. The fault current at bus 4 would be \( I_{4f} = 0.1062 - j \times 10.5244 \) p.u. Also, the Thevenin impedance at bus 4, the element of \( Z_{44} \) of \( Z_{\text{bus,equiv}} \), is \( 0.0009 + j \times 0.0116 \) p.u.
Table 3.1. Line data of the hybrid grid in Figure 3.3

<table>
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<td>0.0108</td>
</tr>
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<td>ac-dc converter</td>
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</tr>
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</tr>
<tr>
<td>6</td>
<td>7</td>
<td>0.0024</td>
<td>0</td>
</tr>
<tr>
<td>8</td>
<td>9</td>
<td>0.0053</td>
<td>0.0565</td>
</tr>
<tr>
<td>9</td>
<td>10</td>
<td>0.0052</td>
<td>0.0512</td>
</tr>
</tbody>
</table>
Figure 3.4. Tested ac-dc hybrid grid - case II

Table 3.2. Line data of the hybrid grid in Figure 3.4

<table>
<thead>
<tr>
<th>From</th>
<th>To</th>
<th>$R^{p,u}$</th>
<th>$X^{p,u}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>2</td>
<td>0.003</td>
<td>0.0599</td>
</tr>
<tr>
<td>1</td>
<td>3</td>
<td>0.0013</td>
<td>0.0504</td>
</tr>
<tr>
<td>1</td>
<td>12</td>
<td>0.0032</td>
<td>0.0524</td>
</tr>
<tr>
<td>2</td>
<td>14</td>
<td>0.0013</td>
<td>0.0504</td>
</tr>
<tr>
<td>3</td>
<td>4</td>
<td>0.0024</td>
<td>0.022</td>
</tr>
<tr>
<td>3</td>
<td>ac-dc converter 1</td>
<td>0.001</td>
<td>0.018</td>
</tr>
<tr>
<td>ac-dc converter 1</td>
<td>6</td>
<td>≈0</td>
<td>≈0</td>
</tr>
<tr>
<td>4</td>
<td>5</td>
<td>0.0018</td>
<td>0.008</td>
</tr>
</tbody>
</table>

Table 3.2 continued.
<table>
<thead>
<tr>
<th>From</th>
<th>To</th>
<th>$R_{pu}$</th>
<th>$X_{pu}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>5</td>
<td>ac-dc converter 2</td>
<td>0.001</td>
<td>0.017</td>
</tr>
<tr>
<td>ac-dc converter 2</td>
<td>15</td>
<td>$\approx 0$</td>
<td>$\approx 0$</td>
</tr>
<tr>
<td>6</td>
<td>7</td>
<td>0.0045</td>
<td>0</td>
</tr>
<tr>
<td>7</td>
<td>8</td>
<td>0.0024</td>
<td>0</td>
</tr>
<tr>
<td>8</td>
<td>9</td>
<td>0.0026</td>
<td>0</td>
</tr>
<tr>
<td>8</td>
<td>10</td>
<td>0.003</td>
<td>0</td>
</tr>
<tr>
<td>10</td>
<td>11</td>
<td>0.0028</td>
<td>0</td>
</tr>
<tr>
<td>12</td>
<td>13</td>
<td>0.0053</td>
<td>0.0565</td>
</tr>
<tr>
<td>13</td>
<td>ac-dc converter 3</td>
<td>0.0052</td>
<td>0.0512</td>
</tr>
<tr>
<td>14</td>
<td>ac-dc converter 3</td>
<td>0.001</td>
<td>0.015</td>
</tr>
<tr>
<td>15</td>
<td>16</td>
<td>0.0039</td>
<td>0</td>
</tr>
<tr>
<td>16</td>
<td>17</td>
<td>0.0021</td>
<td>0</td>
</tr>
<tr>
<td>16</td>
<td>18</td>
<td>0.0023</td>
<td>0</td>
</tr>
<tr>
<td>17</td>
<td>22</td>
<td>0.0021</td>
<td>0</td>
</tr>
<tr>
<td>18</td>
<td>11</td>
<td>0.0021</td>
<td>0</td>
</tr>
<tr>
<td>19</td>
<td>20</td>
<td>0.0022</td>
<td>0</td>
</tr>
<tr>
<td>20</td>
<td>21</td>
<td>0.002</td>
<td>0</td>
</tr>
<tr>
<td>20</td>
<td>22</td>
<td>0.0014</td>
<td>0</td>
</tr>
</tbody>
</table>

### 3.5 Conclusion

This chapter proposes a novel and general approach for obtaining the modern hybrid ac-dc grid model that can be used along with classical ac fault analysis to the fault analysis of a such grid fast and accurate. The result of this method can be used to obtain the fault current at any specific buses in the hybrid grid. Calculated fault currents then will be utilized in designing an effective protection scheme for the hybrid ac-dc grid. Thevenin or Norton equivalent circuit of the dc grid is obtained and used in the ac circuit. Simulation result has been provided for two different hybrid ac-dc grids to show the functionality of the proposed method.
CHAPTER 4. INTERACTION OF TRANSMISSION AND DISTRIBUTION SYSTEMS VIA CO-SIMULATION ANALYSIS; STEADY-STATE ANALYSIS

4.1 Introduction

Modern power systems take advantage of Distributed Energy Resources (DERs) and utilize renewable energy. Renewable power generation units that are connected at the distribution level fall under the category of DER. DERs, especially those based on Photovoltaics (PVs), have attracted a lot of attention during the past decade due to potential energy savings for the customers and reduced environmental footprint [66,67]. DERs can also be used to alleviate the feeder loading to allow increased load transfer capability. However, high penetration of DERs in the distribution grid can present challenges to the power system operation, such as voltage and protection issues, fault detection and accommodation, and during system restoration, and the power system control [68,69].

The conventional distribution grid has been designed assuming that the electric power is carried unidirectionally from HV/MV substations downstream toward customers. As a result, the introduction of DERs on the distribution side will affect existing protection and control philosophies, and thus, overall, the distribution system's reliability could be affected [70-73]. The adverse effects of the DERs depend on various factors, such as feeder topology, size of the DER units, operation and control strategies, and the DER location in the network [74-76]. In addition to the effects on the distribution circuits, distribution-connected DER units may have an impact on

the transmission voltage. Thus, tools must be developed to identify and mitigate these potential effects on transmission systems.

Supporting the power grid by the use of electrical energy storage converter-fed devices during high penetration of DERs is addressed in [77]. The injected/absorbed active/reactive power of the electrical energy storage devices is controlled by the frequency-voltage deviation of the distribution grid. DER Reactive power control has been investigated in a number of operating modes including constant power factor, Volt-Var, active-reactive power, and constant Var modes [78-80]. Reactive power control of DER for high PV penetration in the distribution grid is discussed in [81]. Droop characteristics of the ac-dc converters of energy storage or DER, i.e., P-f and Q-V, to maintain the distribution grid under normal condition is also utilized in [82]. Harmonic compensation, reactive power control, voltage, and frequency support methods for the distribution grid are discussed in [83] for the distribution network. Recently, the simultaneous operation of the distribution voltage regulators and the DER reactive power control have been studied in [84-91]. These studies show the effectiveness of reactive power support in maintaining the distribution voltage and reduction of downstream feeder voltage regulator operations under intermittent solar power penetration.

While recent studies consider the impact of DERs on the distribution system, the substation is always assumed to be an infinite bus. In other words, the transmission system modeling is ignored and assumed to be a stiff system with infinite short circuit capacity. At higher levels of DER penetrations, the effect of the intermittent DER power on the transmission substation voltage cannot be ignored. In order to study the effect of DER penetration on the substation equipment and transmission side, the two circuits must be modeled and co-simulated together. In addition, the substation Load Tap Changer (LTC) controller characteristics can be altered in the legacy and
modern devices to mitigate the excessive operations; however, this has not been adequately investigated in the existing literature.

This dissertation work utilizes a co-simulation environment that simulates the transmission network under the effect of distribution lumped load time series and intermittent output of the community solar farm connected at the distribution side. Thus, the effects of distribution DER units on the transmission network as well as the operation of the substation LTC can be observed. The LTC is modeled through a variable-turns-ratio transformer with constant and inverse time delays and variable voltage bandwidth capabilities [92]. Next, the reactive power control of the DER unit is implemented in the transmission load flow by adhering to the Volt-Var characteristics of the DER unit according to the IEEE Std 1547-2018 [45]. Various changes in the Volt-Var characteristics are considered and voltage stability is studied. By incorporating Volt-Var characteristic in the transmission jacobian matrix, load flow convergence is achieved rapidly when a tap operation is performed. Here, Volt-Var control is adopted via load flow that improves the simulation speed through larger time steps (seconds and larger) and avoids lengthy dynamic simulations of feedback controllers that typically require time steps in the range of milliseconds.

It is shown in this dissertation that under high penetration of DER power, excessive operation of the substation LTC is likely, especially under high reverse power flow. The LTC secondary voltage is utilized to solve the distribution load flow at each time step where line voltage regulators and switched capacitor banks are used to improve downstream voltages. It is also observed that through appropriate modification in the Volt-Var characteristic constants, a significant reduction in the LTC operation is achievable under highly intermittent DER power while the conventional or default settings may not be as effective. Limitations of the Volt-Var characteristic are also observed where excessive changes in the conventional settings could potentially lead to voltage
instability. Additionally, it is noted that increasing the LTC measurement bandwidth, adoption of block operation mode, and time delay can help reduce its excessive operations under intermittent solar power. More details on the substation LTC are provided in [92]. Finally, the 10-minute flicker is calculated for the transmission and distribution voltages based on IEEE Std 1453-2015 [93]. It is observed that under the excessive operation of LTC caused by intermittent solar power, the distribution flicker is in the visible range whereas the transmission system is not affected as much.

4.2 Network Modeling in Steady-State

For the purpose of this section a practical power system network served by 115 kV transmission lines in Central Arkansas is chosen, as shown in Figure 4.1. The rest of the 115 kV power system network was equivalenced based on Thevenin short-circuit reduction, which is represented by two voltage-behind-Thevenin impedance sources at Station 1 and Station BE, including the transfer impedance. Note that Station BE is considered as a PQ bus to reflect voltage variations due to the Thevenin impedance. The 115/13.8 kV substation under study is called Station CA, as shown in Figure 4.2. Two solar farms with different capacities; namely, a 5MW and a 20MW (rated power of a substation transformer), are considered at the downstream of the substation transformer as shown in Figure 4.2. The distribution circuit has three feeders, C-101, C-102, C-104, comprising single-phase, two-phase, and three-phase combination of residential and commercial loads. The substation LTC is located upstream of the solar farms in the substation and a number of feeder line regulators are connected downstream on each distribution feeder. Lumped loads are considered at all other transmission buses. Time series of load data at 1sample/min resolution was used for Stations 1, 2, 3, 4, H, D, and BE, as well as temporal load variations at Station CA. Detailed models of feeders C-102 and C-104 along with an aggregate load representing feeder C-101 was utilized with 1sample/min load data obtained from SCADA.
Capacitor banks at Stations 4 and CA have capacities of 20.4MVAR and 22.5MVAR, respectively.

An LTC is used at Station CA. This load tap changer has 32 steps and a default bandwidth of 2V with a 115V center tap that corresponds to 13.8kV, the substation transformer low-side voltage.

Feeders C-102 and C-104 have 841 and 962 load sections, respectively.

Figure 4.1. Multi-port Thevenin short-circuit reduced equivalent of the 115-kV transmission system

Figure 4.2. Station CA along with 13.8 kV feeders
There are three 600 kVAr capacitor banks (200kVar/phase), one single-phase line voltage regulator and one spot load on feeder C-104. Feeder C-102 has four capacitor banks including two 600kVar, a 1200kVar, and a 300kVar capacitor, all split equally among the three phases, along with three single-phase line voltage regulators. This information is summarized in Table 4.1.

<table>
<thead>
<tr>
<th>Transmission</th>
<th>Capacitor Bank</th>
<th>Voltage Regulator</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number</td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td>Location</td>
<td>Station 4 _ 20.4 MVAR</td>
<td>Station CA</td>
</tr>
<tr>
<td></td>
<td>Station CA _ 22.5 MVAR</td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Distribution</th>
<th>Capacitor Bank</th>
<th>Voltage Regulator</th>
<th>Spot load</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Three on C-104, 600 kVAR each</td>
<td>One single-phase on C-104</td>
<td>One on C-104</td>
</tr>
<tr>
<td></td>
<td>Four on C-102 (two 600 kVAR, one 300 kVAR, and one 1200 kVAR)</td>
<td>Three single-phase on C-102</td>
<td>None on C-102</td>
</tr>
</tbody>
</table>

### 4.3 Time-Series of the Solar Generation

A high-resolution time-series (1 sample/4sec) solar power was acquired from a 140-W rooftop solar panel installed on the roof of the Renewable Energy and Smart Grid Laboratory at LSU (Latitude: 30.41, Longitude: -91.18). This high-resolution measurement provides a granular pattern of the solar generation for each day of the year. Since the network load data was only available at a resolution of 1 sample/minute, it was linearly interpolated to 4-second intervals to be consistent with the time-series of the solar data. The recorded data was then normalized for the solar farms under study. The measured load time series on a specific day from 6:00 am to 8:00 pm was merged with recorded solar production on that day for this study. The solar panels across the farm are assumed to be close enough so that the measured solar sun irradiance can be applied to
all of them simultaneously. By contrast, in the case of rooftop solar panels, the footprint spans several miles and some randomness will occur in the total power generation.

4.4 PV Reactive Power Control

The Volt-Var control is utilized at the DER unit (PV farm) to help maintain the distribution voltage. With this function, the DER unit can utilize reactive power in response to the local voltage changes. The Volt-Var curve shown in Figure 4.3 is recommended by the IEEE Std 1547-2018 [45] with the constants (V₁ through V₄) shown in Table 4.2 as recommended by the Standard. These values, however, can be altered based on the allowable range in the Standard. This work studies alterations to these constants to achieve stable Q-control that mitigates the excessive tap operations. The injected/absorbed reactive power by the DER unit is calculated using:

\[
Q_{\text{injection}} = \frac{-Q_{\text{max}}}{V_2 - V_1} * (V - V_1) + Q_{\text{max}}
\]

\[
Q_{\text{absorption}} = \frac{Q_{\text{min}}}{V_4 - V_3} * (V - V_3)
\]

\[\text{(4.1)}\]
\[\text{(4.2)}\]
Table 4.2. Volt-Var control characteristics permittable range [45]

<table>
<thead>
<tr>
<th>Characteristic voltage</th>
<th>Range lower - upper (p.u.)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$V_{\text{Ref}}$</td>
<td>0.95 - 1.05</td>
</tr>
<tr>
<td>$V_1$</td>
<td>$(V_{\text{Ref}} - 0.18) - (V_2 - 0.02)$</td>
</tr>
<tr>
<td>$V_2$</td>
<td>$(V_{\text{Ref}} - 0.03) - V_{\text{Ref}}$</td>
</tr>
<tr>
<td>$V_3$</td>
<td>$V_{\text{Ref}} - (V_{\text{Ref}} + 0.03)$</td>
</tr>
<tr>
<td>$V_4$</td>
<td>$(V_3 + 0.02) - (V_{\text{Ref}} + 0.18)$</td>
</tr>
<tr>
<td>$Q_1$ and $Q_2$</td>
<td>$0.44*S_{\text{rated}}$</td>
</tr>
</tbody>
</table>

Due to frequent changes in the solar power (every 4s) load flow must be conducted frequently; i.e., at every sampling time.

Since the DER unit is connected to the transmission substation, it is essentially a generation unit in the transmission system. In order to implement the Volt-Var characteristic into the load flow simulation, first, an error loop is created where the substation voltage is applied to the Volt-Var characteristic and the resultant reactive power is fed back to the Newton-Raphson transmission load flow. If the new voltage is close to the previously used one, the loop is stopped. Otherwise, once again the substation voltage from load flow is used to obtain $Q$ from the Volt-Var characteristic. This process is shown in Figure 4.4.

Remark 1. Our observations indicate that convergence of the loop highly depends on the slope of the characteristic in the capacitive and/or inductive regions of the Volt-Var characteristic and not too much on the dead-band (i.e., the region between $V_2$ and $V_3$). Once the slope exceeds certain levels, the error loop does not converge. Table 4.5 in the Simulation section compares different Volt-Var characteristics and their effects on the load flow convergence as well as the number of LTC operations.

Remark 2. With an appropriate Volt-Var characteristic, the error loop typically converges in just a few iterations (one iteration for Error = $10^{-5}$, see Figure 4.4). However, upon a discrete change in the network, such as tap operation, a large number of iterations are needed for
convergence. Thus, a modification in the load flow is utilized such that the Volt-Var characteristic is directly implemented in the load flow jacobian matrix.

The Volt-Var characteristic is rebuilt using equations (4.1) and (4.2), and derivatives with respect to voltage are added to the jacobian matrix for the transmission load flow. Specifically, the reactive power term for the substation node is modified to (37).

$$0 = -\sum_{k=1}^{N} |V_i||V_k||Y_{ik}| \sin(\theta_i - \theta_k + \varphi_{ik}) - Q_{DER}$$

(4.3)

where i and N represent substation node and the total number of transmission nodes, respectively.

$Q_{DER}$ can be obtained by (4.4).

---

**Figure 4.4. Error loop for Volt-Var control**

The Volt-Var characteristic is rebuilt using equations (4.1) and (4.2), and derivatives with respect to voltage are added to the jacobian matrix for the transmission load flow. Specifically, the reactive power term for the substation node is modified to (37).
\[
Q_{\text{DER}} = \begin{cases} 
    Q_{\text{max}} & \text{for } V < V_1 \\
    Q_{\text{cap}} = \frac{-Q_{\text{max}}}{V_2 - V_1} \ast (V - V_1) + Q_{\text{max}} & \text{for } V_1 \leq V \leq V_2 \\
    0 & \text{for } V_2 \leq V \leq V_3 \\
    Q_{\text{ind}} = \frac{Q_{\text{min}}}{V_4 - V_3} \ast (V - V_3) & \text{for } V_3 \leq V \leq V_4 \\
    -Q_{\text{max}} & \text{for } V_4 \leq V 
\end{cases}
\]

(4.4)

, and \(Q_{\text{max}}\) is the magnitude of maximum absorbed reactive power \((Q_{\text{min}} = -Q_{\text{max}})\). When calculating \(\frac{\partial Q}{\partial V}\) at the transmission substation node, the same conditions in (4.4) apply. A potential problem with the \(\frac{\partial Q}{\partial V}\) involving conditions in (4.4) is the abrupt changes in the derivatives at the Volt-Var curve break points; i.e., at points \(V_1, V_2, V_3,\) and \(V_4\) that could potentially cause the Newton-Raphson algorithm to converge in a large number of iterations or not converge at all. Thus, the results of the two load flows are compared over the load and solar variations in the entire day as shown in Figure 4.16. The results of the two methods (error loop and jacobian matrix) match very well while the latter is 10-15 times faster in convergence upon discrete changes due to tap operations. These simulation results indicate the suitability of the modified jacobian.

### 4.5 Substation Voltage Regulator

The substation voltage regulator controls the action of the LTC of the substation transformer. The LTC measurement transformer reduces the distribution voltage of 13.8kV to 115V as a default bandcenter. The bandcenter can be adjusted from 100V to 135V in 0.1V increment. The bandwidth of this voltage regulator is 2 volts per step around the 115-volt bandcenter; however, it can be increased or decreased by 0.1V increment until it reaches to 10V or 1V as upper and lower limits, respectively. The LTC has 32 steps, 16 steps for increasing and 16 steps for decreasing the voltage at the distribution bus. There is a time delay between each two consecutive tap operations due to the mechanical motion of the LTC (default 4s) and another adjustable time delay after issuing a command (default 30s). The latter is adjustable from 1 second to 120 seconds in 1-second
increment. Moreover, this time delay can be definite, which means the time delay is constant at all times, or inverse, which means time delay changes based on the voltage deviation in an inverse fashion. The operating mode of the LTC during the reverse power flow is selected from the following modes: Ignore (the control will act as the forward direction), Block (it prevents automatic tap change operation); and Return to Neutral (tap position is driven to the neutral). Here, ignore and block are considered as two different control modes of LTC during reverse power flow in the simulation section.

The voltage regulator observes the voltage downstream of the substation transformer and each time the measured voltage exceeds the bandwidth, for the 2-volt band; i.e., 114V or 116V, it moves the tap to an upper or lower position, respectively, and thus, each tap operation results in 

\[ \frac{\text{bandwidth}}{\text{bandcenter}} \times V_{\text{base, dist}} \] 

voltage (here \( \frac{2}{115} \times 13800 = 240V \)) increase/decrease at the distribution line. The voltage regulator checks the distribution voltage based on the Time Delay setting (every 30 seconds for example) and if the voltage is outside the permissible range, it operates the LTC after the set time delay to avoid chattering of the mechanical parts. Adjustments to bandwidth and time delay are available and create more flexibility in the newer LTC mechanisms.

Remark 3. In most applications, an LTC bandwidth of 2V is preferred and recommended by the manufacturers. It is also noted that each tap alters the voltage by ratio 2/115 of the base voltage. For the LTC bandwidth of 2V, the amount of voltage change is equal to the bandwidth. That is if the voltage on the measurement transformer drops slightly below 114V (the lower voltage limit) the LTC operates one tap and the voltage is increased by 2/115 of the 115V bandcenter which is equal to 2V, and thus reaches the upper limit of 116V. Under normal operation, an increase in the voltage reduces the losses due to the current drop when constant loads exist, and thus voltage is likely to further increase and cross the upper limit (116V). This initiates a reduction in the tap; i.e.,
chattering or hunting effect. In the real system, however, loads are combinations of constant loads, impedance loads, constant current, etc., and thus, the chance of hunting action is low.

Remark 4. In the event that inverter-based power generation such as solar exists at high penetration levels, the net load may look more like a constant-power load due to the power control mechanism at the DERs. In such events, the hunting action is likely and is verified by our simulation results. A number of solutions are proposed here to remedy the excessive operation of the LTC.

4.6 Distribution Load Flow, Steady-State Analysis

Load flow is performed by employing the EPRI OpenDSS simulator at the distribution level once the transmission load flow (Newton-Raphson algorithm) is performed with the distribution aggregate load as well as associated DER Volt-Var characteristic as explained in Section 4.4 using Matlab. Once the substation voltage is obtained, the LTC measures the voltage and compares that with the allowable voltages in the LTC bandwidth. A tap operation is then initiated after a predetermined time delay plus four seconds corresponding to tap mechanical movement. The new tap position alters the substation transformer turns ratio, which is used at the corresponding future time step. The entire algorithm during the steady-state is depicted in Figure 4.5.

4.7 Flicker

A simplified approach based on IEEE Standard 1453-2015 Clause 7 [93] was used to calculate the short-term (10-minute intervals) flicker severity as

\[ P_{st} = \left( \frac{d}{d_{Pst=1}} \right) \ast F \] (4.5)

where \( d \) is the maximum voltage variation at the analyzed interval, \( d_{Pst=1} \) is the value corresponding to the 120 V lamp curve [93], and \( F \) is the shape factor of the voltage variation that
Start

Initializing

Generate Time Series of Transmission, Distribution, and PV Load/Generation

Time Step=1
Tap=0
Set Transmission and Distribution Parameters

\[ V_{\text{base,trans}} = 115 \]
\[ V_{\text{base,dist}} = 13.8 \]
\[ a = \frac{13.8}{115} \]

\[ V_{\text{dist}} = aV_{\text{trans}}(1+2/115\times\text{Tap}) \]

Increase/Decrease Tap

Yes

If \( V_{\text{dist}} \) is Outside of the Bandwidth

Yes

If Time Delay Has Passed

No

Read Time Step Data From Time Series of Transmission, Distribution, and PV Load/Generation

Use Total Lumped Distribution Load on the Substation (Transmission Bus)

Run the Distribution Load Flow based on the Updated \( V_{\text{trans}} \) \( (V_{\text{dist}} = aV_{\text{trans}}(1+2/115\times\text{Tap})) \)

Run the Transmission Load Flow and Update \( V_{\text{trans}} \)

\[ |V_{\text{trans}} - (V_{\text{dist}}/V_{\text{base,dist}})| < \text{Error} \]

No

Yes

Save Time Step
Save Tap
Save \( V_{\text{trans}} \)
Go to the Next Time Step

End

Save Results

time=\text{time}_{\text{end}}

No

Yes

Figure 4.5. Transmission-distribution (T&D) co-simulation flowchart - steady-state analysis
can be varied between 0 and 1. Here, the voltage change/minute is set to 7.5 based on our sampling time of 4 seconds since with rapid scattered cloud movement changes in nearly every two samples is observed ($\frac{60}{8} = 7.5$). According to [93] for a rate of change of 7.5 changes/minute (Figure 5 or Table 4 in [93]), $d_{Pst1} = 1.7$. In addition, $F$ depends on the wave shape and was set to 1. The visibility thresholds for the distribution (MV) bus and transmission (HV) substation bus flicker were considered to be $0.9 \, P_{st}$ and $0.8 \, P_{st}$, respectively, based on the recommended planning levels in the IEEE Std. 1453-2015.

### 4.8 Transmission-Distribution Exchange Equations and Error Loop

In this dissertation, the distribution total load is applied as a lumped load to the transmission node. Likewise, the distribution load flow utilizes the transmission system nodal voltage as a constant value to obtain the distribution nodal voltages and total power demand. The boundary variables of the transmission and distribution networks are updated and exchanged at the end of the NR solution for the transmission system followed by the distribution solution. Once the distribution load flow solution is obtained, the new boundary voltages/loads are compared to the ones at the beginning of the transmission solution. If these values are close enough (typically with an error of $10^{-6}$) the new boundary values are selected; otherwise, they are reused to refine the error.

#### 4.8.1 Steady-State Load Flow and Time Series Analysis

In the T&D steady-state load flow, for the error loop iteration $m$ at the time series time step $k$, the exchange equations of the steady-state T&D co-simulation are

\[
S_D^m(k) = I_D\left(V_T^{m-1}(k)\right) \tag{4.6}
\]

\[
V_T^m(k) = I_T\left(S_D^m(k)\right) \tag{4.7}
\]
where subscripts $T$ and $D$ denote transmission and distribution networks, respectively; $S_D^m = [S_{D1}^m \ S_{D2}^m \ \ldots \ S_{Dn}^m]^T$ and $V_T^m = [V_{D1}^m \ V_{D2}^m \ \ldots \ V_{Dn}^m]^T$ are total distribution power demand vector and the voltage vector at the T&D coupling node at error loop iteration $m$, respectively, with $n$ the number of the T&D coupling points.

4.9 Case Study and Simulation Results

A time-series simulation was created in Matlab. This simulation called the solar irradiation at the rate of 1 sample/4s and used the interpolated loads at the same frequency for load flow. A total of 12,645 data points was used to represent a time range from 6:00 am to around 8:00 pm for a day in mid-July. A solar-powered DER unit was connected at Station CA downstream of the substation LTC as shown in Figure 4.1. The transmission had lumped loads at all stations, but at Station CA where the distribution circuit was modeled in detail comprising of three distribution feeders.

The Matlab-based load flow algorithm has been designed that solves the load flow by utilizing the reactive power provided by the DER, and the LTC tap operation, and the total distribution loads at Station CA and other stations. Once the voltage is obtained by the transmission load flow, the OpenDSS simulator is called at the time step and finds the distribution feeder nodal voltages. Subsequently, the total distribution loads and losses are updated and exported to the transmission load flow to update nodal voltages. This process is repeated until the station CA voltage in two consecutive iterations are close enough (error $<1^{-6}$).

All the preceding steps were performed at every sampling time, i.e., every four seconds. Several simulation scenarios were conducted to observe the effect of the intermittent solar power plant on the transmission and distribution voltages as well as on the operation of the LTC in the presence of the DER Volt-Var function. Also, observing the voltage fluctuation is another goal of
this chapter. Under the no-PV condition, the number of substation LTC operations in the entire day was less than six operations when using an LTC bandwidth of 2V with a time delay of 30s.

4.9.1 Scenario 1

Here, the effect of LTC bandwidth on the operation of the substation LTC, the distribution voltage, and voltages flicker during the high and low penetration of PV are discussed. It was assumed that the DER Volt-Var control is off and transmission capacitor banks are disconnected. All distribution capacitor banks and voltage regulators are on. To make a comparison, Ignore and Block modes are considered as two modes of LTC operation with definite 15- and 45-second time delays. Sixteen cases are established for high and low penetration of PV, i.e., 20 MW and 5 MW, as it is shown in Table 4.3.

Table 4.3. Eight cases were established for high penetration of PV

<table>
<thead>
<tr>
<th>Cases</th>
<th>LTC Bandwidth (Volt)</th>
<th>Tap Operation Mode</th>
<th>Time Delay Mode</th>
<th>Time Delay (Second)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>2</td>
<td>Ignore</td>
<td>Definite</td>
<td>15</td>
</tr>
<tr>
<td>2</td>
<td>3</td>
<td>Ignore</td>
<td>Definite</td>
<td>15</td>
</tr>
<tr>
<td>3</td>
<td>2</td>
<td>Block</td>
<td>Definite</td>
<td>15</td>
</tr>
<tr>
<td>4</td>
<td>3</td>
<td>Block</td>
<td>Definite</td>
<td>15</td>
</tr>
<tr>
<td>5</td>
<td>2</td>
<td>Ignore</td>
<td>Definite</td>
<td>45</td>
</tr>
<tr>
<td>6</td>
<td>3</td>
<td>Ignore</td>
<td>Definite</td>
<td>45</td>
</tr>
<tr>
<td>7</td>
<td>2</td>
<td>Block</td>
<td>Definite</td>
<td>45</td>
</tr>
<tr>
<td>8</td>
<td>3</td>
<td>Block</td>
<td>Definite</td>
<td>45</td>
</tr>
</tbody>
</table>

The aforementioned cases were repeated for low penetration of PV; i.e., 5MW PV, in Cases 9 through 16.

The 0 and 12645 in the x-axis of Figure 4.6 to Figure 4.10 and Figure 4.12 represent the 6:00 am and 8:00 pm, respectively. Each interval between two consecutive values in the x-axis of these figures indicates four-seconds. The net active power at Station CA with 20MW solar power penetration, which is the total load power plus losses less the PV generated power, along with the substation LTC operation in Case 1 are depicted in Figure 4.6 using LTC Ignore mode. Figure 4.6
indicates that the substation LTC operates during drastic changes in the net load. While these changes could occur any time throughout the day due to cloud movement, large changes in the net load occurred in the reverse power time period due to cloud movement leading to the change in the current direction and the series voltage drop. Figure 4.7 compares the LTC tap operation with 2V and 3V bandwidths around the same 115V tap center. The figure shows that increasing the bandwidth of the substation LTC can significantly decrease the number of LTC operations under highly intermittent solar power. Case 3 utilizes the 2V bandwidth but applies Block mode operation and compares the results with those of Case 1 as shown in Figure 4.8. The Block mode can decrease the number of LTC operations but it cannot prevent the LTC hunting action. Figure 4.9 depicts the 13.8 kV distribution bus voltages in Cases 1 and 2 (see Figure 4.2).

The voltage at the end of Feeders C-102 and C-104 are depicted in Figure 4.10. The high voltage fluctuations in the figure are due to the substation LTC operations whereas the smaller ones are caused by the intermittency in the DER power generation. Flicker levels at the distribution 13.8 kV bus and Station CA 115 kV transmission bus for Cases 1 and 2 are shown in Figure 4.11. Unlike at the distribution bus where a relatively high flicker amount was observed, the transmission flicker was not significant. The net power at Station CA when the 5MW solar farm was used at the distribution bus (Case 9 through Case 16) is depicted in Figure 4.12 along with the substation LTC operation in Case 9. The summary of the substation LTC operation for all of the 16 cases is expressed in Table 4.4. From Table 4.4, the number of LTC operation decreases by increasing the bandwidth, changing the mode of operation from Ignore to Block, or increasing the time delay; however, increasing the bandwidth is more effective than the other options.
Figure 4.6. Net power at Station CA with 20MW solar farm and LTC operation in case 1

Figure 4.7. Effects of bandwidth on the substation LTC operation
Figure 4.8. Effects of the substation LTC block mode – case 1 and case 3

Figure 4.9. Distribution bus voltage – case 1 and case 2
Figure 4.10. Voltages at the end of Feeders C102 and C104 - case 1 and case 2

Figure 4.11. Flicker at Station CA and the distribution bus - case 1 and case 2
Figure 4.12. Net power at Station CA and the LTC operation in case 9

Table 4.4. Summary of the substation LTC operation in scenario 1, cases 1-16

<table>
<thead>
<tr>
<th>Cases</th>
<th># of LTC operation</th>
<th>Cases</th>
<th># of LTC operation</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>66</td>
<td>9</td>
<td>22</td>
</tr>
<tr>
<td>2</td>
<td>2</td>
<td>10</td>
<td>2</td>
</tr>
<tr>
<td>3</td>
<td>30</td>
<td>11</td>
<td>22</td>
</tr>
<tr>
<td>4</td>
<td>2</td>
<td>12</td>
<td>2</td>
</tr>
<tr>
<td>5</td>
<td>30</td>
<td>13</td>
<td>16</td>
</tr>
<tr>
<td>6</td>
<td>2</td>
<td>14</td>
<td>2</td>
</tr>
<tr>
<td>7</td>
<td>18</td>
<td>15</td>
<td>16</td>
</tr>
<tr>
<td>8</td>
<td>2</td>
<td>16</td>
<td>2</td>
</tr>
</tbody>
</table>

4.9.2 Scenario 2

In this scenario, the effects of characteristics of DER Volt-Var control, i.e., $V_1$, $V_2$, $V_3$, and $V_4$ in Figure 4.3, on the operation of LTC was studied and 20 different cases were presented in Table 4.5.
The distribution voltage and flicker during high PV penetration (20MW) with the LTC bandwidth of 2V and time delay of 45s were studied. Here, 2V bandwidth with 45s time delay were considered as the base cases and were not altered. The results of this scenario for different cases are shown in Table 4.5. In Table 4.5, the min and max underneath the characteristics show the lower and upper permissible ranges according to Table 4.2 of the IEEE Std 1547-2018 [45]. Each characteristic was simulated for two different LTC modes of operation; i.e., Ignore (Ignr) and Block (Blk). The number of the substation LTC operations for Ignore and Block modes are given in column 6. In column 7, V\textsubscript{2}-V\textsubscript{1} represents the slope/steepness of the Volt-Var curve (see Figure 4.3) under constant Q\textsubscript{max}. When the steepness of the DER Volt-Var curve was large, it made the power grid unstable and caused the load flow program to not converge due to inappropriate reactive power injection/absorption amounts. The last column in the table represents the average injected/absorbed reactive power (in KVAR) over the entire day. The average reactive power was used as an indicator of the reactive power control cost. For instance, Figure 4.13 represents the injected/absorbed reactive power in Case 8, which has the highest average of reactive power, and case 10, which has a moderate average of reactive power. From Table 4.5, for the steeper slope and smaller deadband the average injected/absorbed reactive power (KVAR) was higher. Figure 4.14 and Figure 4.15 are drawn to illustrate the effect of the LTC operation mode as well as the Volt-Var characteristics on the flicker at the distribution bus and the voltage at the end point of the feeder C-104. The distribution bus voltage in Case 9 was utilized to verify the accuracy of the result when using DER Volt-Var control in the jacobian matrix. shows that the results of both approaches; i.e., when the DER Volt-Var control is in the error loop and when it is considered in the jacobian matrix, are very similar (see Figure 4.16).
### Table 4.5. Scenario 2 results comparison – effect of DER Volt-Var control on LTC tap operations

<table>
<thead>
<tr>
<th>Cases</th>
<th>( V_1 ) (pu) ( V_2 ) (pu) ( V_3 ) (pu) ( V_4 ) (pu)</th>
<th># Tap Op</th>
<th>( V_2 - V_1 ) pu</th>
<th>DeadBand (Figure 4.3)</th>
<th>Average of Q (KVAR)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>( \min, \max )</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1-2</td>
<td>0.96 0.77, 0.95, 1.03</td>
<td>1.01 0.95, 1.05</td>
<td>1.04 0.97, 1.18</td>
<td>30 18</td>
<td>0.03 0.02</td>
</tr>
<tr>
<td>3-4</td>
<td>0.97 0.77, 0.95, 1.03</td>
<td>1.001 0.95, 1.05</td>
<td>1.03 0.97, 1.18</td>
<td>2 2</td>
<td>0.029 0.002</td>
</tr>
<tr>
<td>5-6</td>
<td>0.975 0.77, 0.95, 1.03</td>
<td>1.001 0.95, 1.05</td>
<td>1.025 0.97, 1.18</td>
<td>Conv failed</td>
<td>0.024 0.002</td>
</tr>
<tr>
<td>7-8</td>
<td>0.974 0.77, 0.95, 1.03</td>
<td>1.001 0.95, 1.05</td>
<td>1.026 0.97, 1.18</td>
<td>2 2</td>
<td>0.025 0.002</td>
</tr>
<tr>
<td>9-10</td>
<td>0.96 0.77, 0.95, 1.03</td>
<td>1.007 0.95, 1.05</td>
<td>1.04 0.97, 1.18</td>
<td>12 4</td>
<td>0.033 0.014</td>
</tr>
<tr>
<td>11-12</td>
<td>0.97 0.77, 0.95, 1.03</td>
<td>1.008 0.95, 1.05</td>
<td>1.033 0.97, 1.18</td>
<td>8 2</td>
<td>0.025 0.013</td>
</tr>
<tr>
<td>13-14</td>
<td>0.97 0.77, 0.95, 1.03</td>
<td>1.02 0.95, 1.05</td>
<td>1.06 0.97, 1.18</td>
<td>6 2</td>
<td>0.026 0.024</td>
</tr>
<tr>
<td>15-16</td>
<td>0.97 0.77, 0.95, 1.03</td>
<td>1.01 0.95, 1.05</td>
<td>1.036 0.97, 1.18</td>
<td>6 2</td>
<td>0.026 0.014</td>
</tr>
<tr>
<td>17-18</td>
<td>0.967 0.77, 0.95, 1.03</td>
<td>1.01 0.95, 1.05</td>
<td>1.039 0.97, 1.18</td>
<td>8 2</td>
<td>0.029 0.014</td>
</tr>
<tr>
<td>19-20</td>
<td>0.972 0.77, 0.95, 1.03</td>
<td>1.01 0.95, 1.05</td>
<td>1.034 0.97, 1.18</td>
<td>Conv failed</td>
<td>0.024 0.014</td>
</tr>
</tbody>
</table>

4.9.3 Scenario 3

In this scenario, the large DER installation located upstream of the distribution feeder was replaced with several low power DERs, in the form of PV units inside the distribution feeders. For this purpose, ten 2MW PVs were considered on Feeder C-104, three at beginning of the feeder close to the substation, three at the middle of the feeder, and 4 at the end of the feeder. The
substation LTC settings are set to 2V bandwidth, 115V bandcenter, 45s time delay, and Ignore as
the mode of operation. The results of this scenario are very similar to those of Case 5 (Scenario 1)
discussed earlier as shown in Figure 4.17, which shows the voltage at the end of the feeder C104
for Scenario 3 and Case 5 of Scenario 1.

Figure 4.13. Injected/Absorbed reactive power by Volt-Var control for scenario 2 - case 8 and
    case 10
Figure 4.14. Flicker at the distribution bus. (a) case 1 and case 2 (b) case 3 and case 4
Figure 4.15. C104 end point voltage. (a) case 1 and case 2 (b) case 3 and case 4
Figure 4.16. Distribution bus voltage in scenario 2, case 9. DER Volt-Var control in error loop vs. jacobian matrix (proposed approach)

Figure 4.17. Voltage at the end of Feeder C-104 - scenario 3 and scenario1, case 5
4.10 Conclusion

This chapter investigates the effects of distribution-level DER units on the transmission and distribution systems including substation LTC operation, voltage fluctuation, and flicker in both transmission and distribution networks. It was shown that with the conventional LTC settings, the high penetration of DER units can cause excessive operation of the substation LTC that leads to a higher fluctuation in distribution voltage and consequently higher flicker in the distribution side. In order to mitigate the excessive LTC operations, the LTC controller mode of operation was changed from Ignore (conventionally used by industry) to Block. While the new setting reduced the number of tap operations by half, the LTC still operates more than the normal. Further reduction in tap operations is achieved by an increase in the bandwidth of the substation LTC. In addition, it was shown that the DER Volt-Var control can be employed to alleviate the hunting action in the substation LTC if the values of Volt-Var characteristics are chosen appropriately. The advantage of using the DER Volt-Var control being that the LTC bandwidth can be still retained at the conventional setting of 2V and a reasonable time delay of 45s can be used, which offers tighter regulation for distribution feeder voltage profiles. The proposed enhanced load flow algorithm which directly incorporates the DER Volt-Var characteristics as part of the Jacobian matrix was benchmarked with the error loop method. While both methods showed similar results, the Jacobian matrix approach was much faster in convergence during discrete changes such as substation LTC tap operations.
CHAPTER 5. INTERACTION OF TRANSMISSION AND DISTRIBUTION SYSTEMS VIA CO-SIMULATION ANALYSIS; DYNAMIC ANALYSIS

5.1 Introduction

Recently, the population of Distributed Energy Resources (DERs) has been remarkably increased in the power systems [66, 67]. The feeder loading can be reduced by DER units to tolerate higher load transfer. Nevertheless, the power system operation faces challenges during the high presence of DERs in the distribution system, such as voltage and frequency deviation, protection issues, fault detection, dynamic issues during system restoration, higher flicker, and load tap changer (LTC) excessive operation [68-69, 94-95].

The electric power in the conventional power system is carried unidirectionally, i.e., from the transmission system downstream toward customers. Consequently, the presence of DERs on the distribution network will affect existing control mechanisms, and thus, the reliability of the distribution grid is affected [70-73]. Challenges that DERs bring to the power network depend on various factors, such as the size of the DER, feeder topology, DER control mechanisms, and the DER location in the grid [74-76]. DER units at the distribution level also affect the transmission system. Therefore, tools must be established to find and alleviate these potential impacts on transmission networks.

The interaction of the distribution voltage regulators and the DER reactive power control via steady-state and time series analysis have been explored in [84-91, 94]. There are different operation modes for the DER reactive power control such as active-reactive power, Volt-Var, constant Var, and constant power factor modes [78-80] at steady-state conditions. In most studies, the effects of DER units were measured on the distribution network, while the substation was considered to be an infinite bus and thus the transmission network is ignored in the modeling. However, at the high presence of DER units, the effect of the intermittent DER output power on
the transmission substation voltage must be taken into account. In order to study the effect of DER penetration on the substation equipment and transmission side, the two circuits must be modeled and co-simulated together. In addition, the substation Load Tap Changer (LTC) controller characteristics can be altered in the legacy and modern devices to mitigate the excessive operations; however, this has not been adequately investigated in the existing literature. To study the dynamic impacts of DER units on the substation equipment, transmission network, and generators the transmission and distribution systems must be modeled in detail and co-simulated with one another. The effects of the high penetrations of photovoltaic (PV) power on the operation of the substation load tap changer (LTC) is presented in [94] via a steady-state co-simulation algorithm. Mitigation of voltage fluctuation due to the intermittency of the PV power is also proposed in the prior works via DER Volt-Var control [81, 95].

The dynamic effects of the DER units on the distribution and transmission systems are conventionally conducted using the composite load models that incorporate the dynamics of the DER units and those of the distribution loads. However, the available models require many parameters to set and are typically difficult to match to a specific distribution system. For instance, the impacts of a distribution network in dynamic analysis of transmission systems are modeled by the Western Electricity Coordinating Council (WECC) [96] composite load model that has more than 100 parameters. Alternatively, one can exploit a T&D co-simulation method to capture the details of both systems without the need to adjust parameters.

Prior efforts to conduct T&D co-simulation include decoupled, semi-coupled, and coupled methods. In the decoupled method, the transmission simulation is performed with a distribution system replaced with lumped loads. Then, the results of transmission voltage are used to conduct distribution simulation [97]. The semi-coupled approaches include parallel and series simulations.
In the parallel simulation algorithms, transmission simulation is performed in parallel to the
distribution simulation using prior time step data for both the systems [98-100]. In series
approaches, the transmission simulation is performed first followed by distribution simulation
using transmission voltages at each time step [100]. The coupled methods are a special type of the
series co-simulation where the distribution loads are updated and fed back to the transmission
simulation in an error loop till distribution power remains the same in two consecutive iterations
[99].

The coupled T&D algorithms are the most accurate among the co-simulation methods due
to the power matching properties provided by the error loop. Prior work in this field includes [98]
where transmission sequence networks and three-phase distribution networks are employed to
perform asymmetrical co-simulation. Other prior works utilize transmission and distribution three-
phase systems [101] which is more computationally intensive [98].

While the dynamic co-simulation approaches are under investigation, the effects of DER
units are not fully studied in this context. Thus, in this work, the effects of the DER_A mechanism
[102] are investigated on the distribution and transmission systems via a coupled T&D co-
simulation. A transmission positive sequence and distribution three-phase models are considered.
This model is appropriate since the effect of distribution unbalanced loads on the transmission
voltage imbalance are not significant [103]. Also, the proposed co-simulation algorithm can be
conveniently extended to three sequences for an asymmetrical transmission system. The
distribution substation transformer is included in the transmission system along with the
downstream DER unit and single-cage three-phase induction motor. The DER_A mechanism is
modeled via a 9th-order dynamical system embedded in the transmission simulation (see section
4.e).
5.2 Transmission Differential-Algebraic Equations (DAEs)

The dynamic model of a power system is extensively studied in the past [104, 105]. The dynamics of the power system at time instant \( t \) can be mathematically expressed by the DAEs system of equations (5.1) and (5.2) as

\[
\dot{x}_T(t) = f_T(x_T(t), V_T(t), \theta_T(t)) \tag{5.1}
\]

\[
0 = g_T(x_T(t), V_T(t), \theta_T(t)) \tag{5.2}
\]

where \( x \) is a vector of dynamical state variables, \( V \) is a vector of bus voltages, and \( \theta \) is a vector bus voltages’ angles. Here, subscript \( T \) indicates the transmission network. The differential equations include those of the dynamic devices such as synchronous generators, induction motors, and DER units as detailed in the following. The algebraic equations include the nodal power balance equations along with algebraic constraints on the dynamical devices.

5.3 Synchronous generator

A sixth-order synchronous generator model is used to include the sub-transient behavior of the synchronous machine [105]. Figure 5.1 shows the stator and rotor windings position as well as the axis of a synchronous generator. The details of 6th order model are provided in the following:

- Ignore stator and sub-transient dynamics (damper windings 1d and 2q)
- The turbine-generator model includes
  - 6th-order generator model (6 dynamics)
  - IEEE Type I Exciter/AVR
  - HP-LP single reheat turbine and governor

The mathematical representation of this model can be found as follow (from (5.3) to (5.10)). The values of parameters in (5.3) to (5.10) can be found in Table 5.1.
5.3.1 Rotor’s Differential Equations

\[
T_d' \frac{dE_d}{dt} = -E_q' - (X_d - X'_d) \left[ I_d - \frac{x'_d - x''_d}{x'_d - x_{ls}} (\psi_{1d} + (X'_d - X_{ls}) I_d - E_q') \right] + E_{fd} \tag{5.3}
\]

\[
T_q' \frac{dE_q}{dt} = -E_d' + (X_q - X'_q) \left[ I_q - \frac{x'_q - x''_q}{x'_q - x_{ls}} (\psi_{2q} + (X'_q - X_{ls}) I_q - E_d') \right] \tag{5.4}
\]

\[
T_{q0}'' \frac{d\psi_{2q}}{dt} = -\psi_{2q} - E_d' - (X'_q - X_{ls}) I_q \tag{5.5}
\]

\[
T_{d0}'' \frac{d\psi_{1q}}{dt} = -\psi_{1q} + E_q' - (X'_d - X_{ls}) I_d \tag{5.6}
\]

5.3.2 Rotor’s Motion Differential Equations

\[
\frac{d\delta}{dt} = \omega - \omega_s \tag{5.7}
\]

\[
2H \frac{d\omega}{\omega_s dt} = P_M - \left( \frac{x''_d - x_{ls}}{x''_d - x_{ls}} \right) E_d' I_q - \left( \frac{x''_d - x_{ls}}{x''_d - x_{ls}} \right) \psi_{1q} I_q - \left( \frac{x''_q - x_{ls}}{x''_q - x_{ls}} \right) E_q' I_d + \left( \frac{x''_q - x_{ls}}{x''_q - x_{ls}} \right) \psi_{2d} I_d - \left( \frac{x''_q - x_{ls}}{x''_q - x_{ls}} \right) \psi_{1q} I_d - P_{FW} \tag{5.8}
\]

5.3.3 Algebraic Equations

\[
0 = R_s I_d - X''_d I_q - \left( \frac{x''_q - x_{ls}}{x''_q - x_{ls}} \right) E_d' \psi_{2q} + V_s \sin(\delta - \theta_s) \tag{5.9}
\]

\[
0 = R_s I_q - X''_q I_d - \left( \frac{x''_q - x_{ls}}{x''_q - x_{ls}} \right) E_q' \psi_{1d} + V_s \cos(\delta - \theta_s) \tag{5.10}
\]

Figure 5.1. Stator and rotor windings position of a generator as well as the axis.
5.3.4 IEEE Type I Exciter/AVR Model

The mathematical representation of this kind of excitation system can be found either in (5.11) to (5.14) or in Figure 5.2.

\[
T_{Ej} \dot{E}_{fdj} = -K_{Ej}E_{fdj} + V_{Rj} \tag{5.11}
\]

\[
T_{Aj} \dot{V}_{Rj} = -V_{Rj} + K_{Aj}R_{Fj} - \frac{K_{Aj}K_{Fj}}{T_{Fj}}E_{fdj} + K_{Aj}(V_{refj} - V_{j}) \tag{5.12}
\]

\[
V_{Rj}^{\text{min}} \leq V_{Rj} \leq V_{Rj}^{\text{max}} \tag{5.13}
\]

\[
T_{Fj} \dot{R}_{Fj} = -R_{Fj} + \frac{K_{Fj}}{T_{Fj}}E_{fdj} \tag{5.14}
\]

Table 5.1. Selected value for parameters in 6\textsuperscript{th} order model of generator

<table>
<thead>
<tr>
<th>$O_{rd}$</th>
<th>1</th>
<th>$K_{E}$</th>
<th>1.0</th>
<th>$R$</th>
<th>0.05</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\chi_{dp}$</td>
<td>0.006</td>
<td>$K_{F}$</td>
<td>0.03</td>
<td>$V_{Rj}^{\text{max}}$</td>
<td>10</td>
</tr>
<tr>
<td>$\chi_{qp}$</td>
<td>0.006</td>
<td>$T_{A}$</td>
<td>0.02</td>
<td>$\chi_{dpp}$</td>
<td></td>
</tr>
<tr>
<td>$\chi_{d}$</td>
<td>0.02</td>
<td>$T_{E}$</td>
<td>0.75</td>
<td>$\chi_{qpp}$</td>
<td></td>
</tr>
<tr>
<td>$\chi_{q}$</td>
<td>0.019</td>
<td>$%T_{F}$</td>
<td>1.0</td>
<td>$T_{dp0}$</td>
<td></td>
</tr>
<tr>
<td>$D$</td>
<td>10.0</td>
<td>$T_{F}$</td>
<td>10.0</td>
<td>$T_{dq0}$</td>
<td></td>
</tr>
<tr>
<td>$H$</td>
<td>1.0</td>
<td>$T_{RH}$</td>
<td>10.0</td>
<td>$R_{s}$</td>
<td></td>
</tr>
<tr>
<td>$T_{d0}$</td>
<td>7.0</td>
<td>$T_{CH}$</td>
<td>0.25</td>
<td>$X_{IS}$</td>
<td></td>
</tr>
<tr>
<td>$T_{q0}$</td>
<td>0.7</td>
<td>$T_{SV}$</td>
<td>1.0</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$K_{A}$</td>
<td>40.0</td>
<td>$K_{HP}$</td>
<td>0.25</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Figure 5.2. IEEE type I exciter/AVR model
5.3.5 IEEE Type I Exciter/AVR Model Equivalent Model

Figure 5.3 along with \( R'_F = R_F - \left( \frac{K_F}{T_F} \right) E_{fd} = \left( \frac{K_F}{T_F} \right) \times \left( (1 + sT_F)^{-1} - 1 \right) E_{fd} \) describe the fundamental of this model.

5.3.6 HP-LP Single Reheat Turbine

Equations (5.15) through (5.17) that represents the HP-LP Single Reheat Turbine model are stemmed out from Figure 5.4, which is the control block diagram the HP-LP Single Reheat Turbine model. Equations (5.15) and (5.16) are representation of turbine model, while (5.17) is used to model speed governor. Also, in (5.15) to (5.17), the steam valve power (\( P_{SVj} \)) must meet the following 0 ≤ \( P_{SVj} \) ≤ \( P_{SVj}^{max} \).

![Figure 5.3. IEEE type I exciter/AVR equivalent model](image-url)
Figure 5.4. Control block of HP-LP single reheat turbine

\[
\begin{align*}
T_{RHj} & \hat{T}_{Mj} = -T_{Mj} + \left(1 - \frac{K_{HPj}T_{RHj}}{T_{CHj}}\right)P_{CHj} + \frac{K_{HPj}T_{RHj}}{T_{CHj}}P_{SVj} \\
T_{CHj} & \hat{P}_{CHj} = -P_{CHj} + P_{SVj} \\
T_{SVj} & \hat{P}_{SVj} = -P_{SVj} + P_{Cj} - \frac{\omega_j}{R_j \omega_s}
\end{align*}
\]  

(5.15)  
(5.16)  
(5.17)

5.3.7 HP-LP Single Reheat Turbine Equivalent Model

Figure 5.5 shows the control block diagram of this model. The parameters that are selected for IEEE Type 1 Exciter/AVR model and HP-LP Single Reheat Turbine in this dissertation are provided in Table 5.2.
Figure 5.5. Block control of HP-LP single reheat turbine equivalent model

Table 5.2. Selected value for parameters in 4th order model of generator

<table>
<thead>
<tr>
<th>parameter</th>
<th>value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$O_{rd}$</td>
<td>1</td>
</tr>
<tr>
<td>$T_{q0}$</td>
<td>0.7</td>
</tr>
<tr>
<td>$T_{RH}$</td>
<td>10.0</td>
</tr>
<tr>
<td>$x_{dp}$</td>
<td>0.006</td>
</tr>
<tr>
<td>$K_A$</td>
<td>40.0</td>
</tr>
<tr>
<td>$T_{CH}$</td>
<td>0.25</td>
</tr>
<tr>
<td>$x_{qp}$</td>
<td>0.006</td>
</tr>
<tr>
<td>$K_E$</td>
<td>1.0</td>
</tr>
<tr>
<td>$T_{SV}$</td>
<td>1.0</td>
</tr>
<tr>
<td>$x_d$</td>
<td>0.02</td>
</tr>
<tr>
<td>$K_F$</td>
<td>0.03</td>
</tr>
<tr>
<td>$K_{HP}$</td>
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</tr>
<tr>
<td>$x_q$</td>
<td>0.019</td>
</tr>
<tr>
<td>$T_A$</td>
<td>0.02</td>
</tr>
<tr>
<td>$R$</td>
<td>0.05</td>
</tr>
<tr>
<td>$D$</td>
<td>10.0</td>
</tr>
<tr>
<td>$T_E$</td>
<td>0.75</td>
</tr>
<tr>
<td>$V_R^{max}$</td>
<td>10</td>
</tr>
<tr>
<td>$H$</td>
<td>1.0</td>
</tr>
<tr>
<td>$%T_F$</td>
<td>1.0</td>
</tr>
<tr>
<td>$T_{do}$</td>
<td>7.0</td>
</tr>
<tr>
<td>$T_F$</td>
<td>10.0</td>
</tr>
</tbody>
</table>

5.4 Induction Motor Model

The model of induction motors was added to the co-simulation program to perform contingency analysis including induction motors. The model of PSSE CIM5 single-cage/double-cage rotor induction motor is considered in the T&D co-simulation. Figure 5.6 shows the PSSE CIM5 model for the double cage rotor induction motor. Note that in single-cage motors, the second rotor branch in Figure 5.6 does not exist.
5.4.1 Dynamical Model for an Induction Motor – PSS/E Model

The mathematical representations of an induction motor model are in (5.18) to (5.20). This model can be used for both single-cage and double-cage rotors. To implement this model, parameters $H$, $T'$, $T''$, $X_L$, $X'$, and $X''$ are needed. Table 5.3 show how to calculate $T'$, $T''$, $X_L$, $X'$, and $X''$ based on the parameters in Figure 5.6.

Table 5.3. Calculation of parameters in (5.18) to (5.20) based on the variables in Figure 5.6

<table>
<thead>
<tr>
<th>Type I</th>
<th>Double cage</th>
<th>Single cage</th>
</tr>
</thead>
<tbody>
<tr>
<td>$L = L_a + L_m$</td>
<td>$L = L_a + L_m$</td>
<td></td>
</tr>
<tr>
<td>$L_1 = L_a$</td>
<td>$L_1 = L_a$</td>
<td></td>
</tr>
<tr>
<td>$L' = L_a + \frac{1}{L_m + L_1}$</td>
<td>$L' = L_a + \frac{1}{L_m + L_1}$</td>
<td></td>
</tr>
<tr>
<td>$L'' = L_a + \frac{1}{L_m + \frac{1}{L_1 + L_2}}$</td>
<td>$L'' = 0$</td>
<td></td>
</tr>
<tr>
<td>$T'_0 = \frac{L_a + L_m}{\omega_0 R_1}$</td>
<td>$T'_0 = \frac{L_a + L_m}{\omega_0 R_1}$</td>
<td></td>
</tr>
<tr>
<td>$T''_0 = \frac{L_a + L_m}{\omega_0 R_2}$</td>
<td>$T''_0 = 0$</td>
<td></td>
</tr>
</tbody>
</table>

\[
\frac{dE_d'}{dt} = \omega_s s E_q' - \frac{E_d'-(x-x')}{T'} I_{qs} \tag{5.18}
\]

\[
\frac{dE_q'}{dt} = -\omega_s s E_d' - \frac{E_d'-(x-x')}{T'} I_{ds} \tag{5.19}
\]

\[
\frac{ds}{dt} = \frac{T_{nom}(1+s)^2 - T_e}{2H} \tag{5.20}
\]
5.4.2 Development of Single-Cage Induction Motor Differential-Algebraic Equations

Before going through the induction motor dynamic and algebraic equations, it is noteworthy to say that all variables in this section are normalized, i.e., they are in p.u.

5.4.2.1 Stator and Rotor Voltage Equations

The stator and rotor voltage on d-q reference frame for the single-cage induction motor are as (5.21) to (5.24). It should be mentioned that the rotor voltage is zero because the rotor is short circuited.

\[
\begin{align*}
V_d &= R_s i_d + \frac{1}{\omega_s} \frac{d\psi_d}{dt} - \psi_q \\
V_q &= R_s i_q + \frac{1}{\omega_s} \frac{d\psi_q}{dt} + \psi_d \\
0 &= V_{d_r} = R_r i_{d_r} + \frac{1}{\omega_s} \frac{d\psi_{d_r}}{dt} - \frac{\omega_s - \omega}{\omega_s} \psi_{q_r} \\
0 &= V_{q_r} = R_r i_{q_r} + \frac{1}{\omega_s} \frac{d\psi_{q_r}}{dt} + \frac{\omega_s - \omega}{\omega_s} \psi_{d_r}
\end{align*}
\]

where \( V_s \) is the stator voltage in p.u., \( R_s \) is stator resistance, \( i_s \) is the stator current in p.u., \( \omega_s \) is the angular velocity of the stator field (rad/s), \( \psi_s \) is the stator linkage flux, \( V_r \) is the rotor voltage in p.u., \( R_r \) is rotor resistance, \( i_r \) is the rotor current in p.u., \( \psi_r \) is the rotor linkage flux, \( \omega \) is the angular velocity of the rotor field (rad/s), and the subscripts d and q represent the d-q reference frame.

5.4.2.2 Acceleration Equation

The electromagnetic torque developed by the motor drives the mechanical load. If there is the mismatch between the electromagnetic torque and the mechanical load torque, the differential torque accelerates the rotor mass. Consequently,

\[
\begin{align*}
\frac{2H}{\omega_s} \frac{d\omega}{dt} &= T_e - T_m \\
T_e &= \psi_{d_s} i_{q_s} - \psi_{q_s} i_{d_s} = \psi_{q_r} i_{d_r} - \psi_{d_r} i_{q_r}
\end{align*}
\]

where \( T_e \) is the electromagnetic torque in p.u., \( T_m \) is the mechanical load torque in p.u., \( \omega \) is the angular velocity of the rotor field in mechanical rad/s, and \( H \) is the combined inertia constant of the rotor and connected load.
5.4.3 Induction Motor; Algebraic and Differential Equations

The algebraic and differential equations of an induction motors are provided in the following as

5.4.3.1 Stator and Rotor Flux Linkages

\[ \psi_{ds} = X_{ls}i_{ds} + X_m(i_{ds} + i_{dr}) \] (5.27)

\[ \psi_{qs} = X_{ls}i_{qs} + X_m(i_{qs} + i_{qr}) \] (5.28)

\[ \psi_{dr} = X_{lr}i_{dr} + X_m(i_{ds} + i_{dr}) \] (5.29)

\[ \psi_{qr} = X_{lr}i_{qr} + X_m(i_{qs} + i_{qr}) \] (5.30)

Expanding equations (5.27) to (5.30) yields to:

\[ \psi_{ds} = (X_{ls} + X_m)i_{ds} + X_m i_{dr} \] (5.31)

\[ \psi_{qs} = (X_{ls} + X_m)i_{qs} + X_m i_{qr} \] (5.32)

\[ \psi_{dr} = (X_{lr} + X_m)i_{dr} + X_m i_{ds} \] (5.33)

\[ \psi_{qr} = (X_{lr} + X_m)i_{qr} + X_m i_{qs} \] (5.34)

where \( \psi_s \) and \( \psi_r \) are stator and rotor linkages flux, respectively, \( X_{ls} \) is the stator leakage reactance, \( X_{lr} \) is the rotor leakage reactance, and \( X_m \) is the magnetizing reactance.

Here, terms that are needed in obtaining the induction motor algebraic equations is defined as

\[ X = X_d = X_q = X_s = X_{ls} + X_m \] (5.35)

\[ X_r = X_{lr} + X_m \] (5.36)

\[ X' = X_d' = X_q' = X_s' = \frac{x_s^2}{x_r} = \frac{x_s^2 \times x_r - x_m^2}{x_r} \] (5.37)

\[ \frac{x_s^2}{x_r} = \frac{x_s^2 \times x_r - x_m^2}{x_r} \] (5.38)

\[ \frac{x_s^2}{x_r} = \frac{x_s^2 \times x_r - x_m^2}{x_r} = \frac{x_m^2}{x_r + x_m} \] (5.39)

\[ \frac{x_s^2}{x_r} = \frac{x_s^2 \times x_r - x_m^2}{x_r} = \frac{x_m^2}{x_r + x_m} \] (5.40)

\[ E_q' = \frac{x_m}{x_r} \psi_{dr} \] (5.41)

\[ E_d' = -\frac{x_m}{x_r} \psi_{qr} \] (5.42)

\[ T_0' = T_0' = T_0' = \frac{x_r}{\omega_s R_r} \] (5.43)
where $X'$ is the transient reactance of the induction machine, $T_0'$ is the transient open circuit time constant of the induction machine (in rad) that characterizes the decay of the rotor transients when the stator is open-circuited, $E'_d$ and $E'_q$ are voltage behind transient impedance align with d-q reference frame.

5.4.3.2 Differential Equations of Stator and Rotor Linkage Flux

From (5.21), \[
\frac{1}{\omega_s} \frac{d\psi_{ds}}{dt} = -R_s i_{ds} + \psi_{qs} + V_{ds}.
\] In addition, from (5.31) and (5.33), $i_{ds} = \frac{\psi_{ds} - X_m i_{dr}}{X_s}$ and $i_{dr} = \frac{\psi_{dr} - X_m i_{ds}}{X_r}$. By substituting $i_{dr}$ in $i_{ds}$:

\[
i_{ds} = \frac{\psi_{ds} - X_m \times \psi_{dr} - X_m i_{ds}}{X_s} \Rightarrow i_{ds} = \frac{(X_s X_r - X_m^2)}{X_r} = \psi_{ds} - \frac{X_m}{X_r} \psi_{dr}
\]

By considering $X' = \frac{X_s X_r - X_m^2}{X_r}$ (5.37) and $E'_q = \frac{X_m}{X_r} \psi_{dr}$ (5.41) in (5.44), the $i_{ds}$ would be:

\[
i_{ds} \times (X') = \psi_{ds} - E'_q \Rightarrow i_{ds} = \frac{1}{X'} (\psi_{ds} - E'_q)
\]

Finally, by substituting $i_{ds} = \frac{1}{X'} (\psi_{ds} - E'_q)$ in \[
\frac{1}{\omega_s} \frac{d\psi_{ds}}{dt} = -R_s i_{ds} + \psi_{qs} + V_{ds}
\]

By considering the above-mentioned approach for $V_{qs} = R_s i_{qs} + \frac{1}{\omega_s} \frac{d\psi_{qs}}{dt} + \psi_{ds}$, the differential equation for $\psi_{qs}$ would be:

\[
\frac{1}{\omega_s} \frac{d\psi_{qs}}{dt} = -R_s \psi_{qs} - \psi_{ds} + \frac{R_s}{X'} E'_q + V_{qs}
\]

5.4.3.3 Differential Equations of Voltage Behind Transient Impedance

The goal of this section is to find the $\frac{dE'_d}{dt}$ and $\frac{dE'_q}{dt}$ that are differential equations of voltage behind transient impedance align with d-q reference frame. So that one can do the following steps:
First, let’s find out the $\frac{dE'_q}{dt}$ From (5.41), (5.42), and (5.43), $E'_q = \frac{x_m}{x_r} \psi_{d_r}$, $E'_q = -\frac{x_m}{x_r} \psi_{q_r}$, and $T'_0 = \frac{x_r}{\omega_s R_r}$, respectively. From (5.23), $0 = R_r i_{d_r} + \frac{1}{\omega_s} \frac{d\psi_{d_r}}{dt} - \frac{\omega_s - \omega}{\omega_s} \psi_{q_r}$. From (5.31), (5.33) and (5.34), $\psi_{d_s} = (X_{i_s} + X_m) i_{d_s} + X_m i_{d_r}$, $\psi_{d_r} = (X_{i_r} + X_m) i_{d_r} + X_m i_{d_s}$, and $\psi_{q_r} = (X_{i_r} + X_m) i_{q_r} + X_m i_{d_s}$, respectively. From (5.37), (5.39), and (5.41), $X' = \frac{x_s x_r - x_m^2}{x_r}$, $X_s - X' = \frac{x_m^2}{x_r}$, and

$$\frac{x_s - X'}{X'} = \frac{x_m^2}{x_s x_r - x_m^2},$$

respectively. By making derivation from (5.41):

$$\frac{dE'_q}{dt} = \frac{x_m}{x_r} \frac{d\psi_{d_r}}{dt}$$

(5.48)

From (5.23), one can have:

$$\frac{d\psi_{d_r}}{dt} = \omega_s \left( -R_r i_{d_r} + \frac{\omega_s - \omega}{\omega_s} \psi_{q_r} \right)$$

(5.49)

From (5.31) and (5.33), $i_{d_s} = \frac{\psi_{d_s} - x_m i_{d_r}}{x_s}$ and $i_{d_r} = \frac{\psi_{d_r} - x_m i_{d_s}}{x_r}$. By substituting $i_{d_s}$ in $i_{d_r}$:

$$i_{d_r} = \frac{\psi_{d_r} - x_m \times \frac{\psi_{d_s} - x_m i_{d_r}}{x_s}}{x_r} \Rightarrow i_{d_r} \times \left( \frac{x_s x_r - x_m^2}{x_s} \right) = \psi_{d_r} - \frac{x_m}{x_s} \psi_{d_s}$$

(5.50)

Multiplying $\left( \frac{x_m}{x_r} \right)$ into (5.50) yields to $\left( \frac{x_m}{x_r} \right) \times i_{d_r} \times \left( \frac{x_s x_r - x_m^2}{x_s} \right) = \left( \frac{x_m}{x_r} \right) \times \left( \psi_{d_r} - \frac{x_m}{x_s} \psi_{d_s} \right)$.

Then by considering $X' = \frac{x_s x_r - x_m^2}{x_r}$ and $E'_q = \frac{x_m}{x_r} \times \psi_{d_r}$:

$$i_{d_r} = \left( \frac{x_s}{x_m + X'} \right) E'_q - \left( \frac{x_m}{x_m + X'} \right) \psi_{d_s}$$

(5.51)

Then, by substituting (5.51) inside of (5.49):

$$\frac{d\psi_{d_r}}{dt} = -\omega_s R_r \left( \frac{x_s}{x_m + X'} \right) E'_q + \omega_s R_r \left( \frac{x_m}{x_m + X'} \right) \psi_{d_s} + (\omega_s - \omega) \psi_{q_r}$$

(5.52)

Next step is to consider (5.52) in (5.47) that leads to: $\frac{dE'_q}{dt} = -\omega_s \times R_r \times X_r^{-1} \times X_s \times X_r^{-1} \times E'_q + \omega_s \times R_r \times X_r^{-1} \times X_r^2 \times (X_r + X')^{-1} \times \psi_{d_s} + (\omega_s - \omega) \times X_m \times X_r^{-1} \times \psi_{q_r}$. Finally, by incorporating (5.40), (5.42), and (5.43):

$$T'_0 \frac{dE'_q}{dt} = -\left( \frac{x_s}{X'} \right) E'_q + \left( \frac{x_s - X'}{X'} \right) \psi_{d_s} - (\omega_s - \omega) T'_0 E'_q$$

(5.53)

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Now we go and find the $\frac{dE'_d}{dt}$. All steps in finding $\frac{dE'_d}{dt}$ is same as the aforementioned steps for $\frac{dE'_d}{dt}$. So that, From (5.41), (5.42), and (5.43), $E'_q = X_m \times X_r^{-1} \times \psi_{dr}$, $E'_d = -X_m \times X_r^{-1} \times \psi_{qs}$, and $T'_0 = \frac{x_r}{\omega_s R_r}$, respectively.

From (5.24), $0 = R_r i_{qr} + \frac{1}{\omega_s} \frac{d\psi_{qr}}{dt} + \frac{\omega_s - \omega}{\omega_s} \psi_{dr}$.

From (5.31), (5.33) and (5.34), $\psi_{ds} = (X_{ls} + X_m)i_{ds} + X_m i_{dr}$, $\psi_{dr} = (X_{lr} + X_m)i_{dr} + X_m i_{ds}$, and $\psi_{qr} = (X_{lr} + X_m)i_{qr} + X_m i_{qs}$, respectively.

From (5.37), (5.39), and (5.40), $X' = (X_s \times X_r - X_m^2) \times X_r^{-1}$, $X_s - X' = X_m^2 \times X_r^{-1}$, and $\frac{x_s - x'}{x'} = X_m^2 \times (X_s \times X_r - X_m^2)^{-1}$, respectively.

By making derivation from (5.42):

$$\frac{dE'_d}{dt} = -\frac{x_m}{x_r} \frac{d\psi_{qr}}{dt}$$

From (5.24), one can have:

$$\frac{d\psi_{qr}}{dt} = \omega_s \left(-R_r i_{qr} - \frac{\omega_s - \omega}{\omega_s} \psi_{dr}\right)$$

From (5.32) and (5.34), $i_{qs} = \frac{\psi_{qs} - X_m i_{qr}}{x_s}$ and $i_{qr} = \frac{\psi_{qr} - X_m i_{qs}}{x_r}$. By substituting $i_{qs}$ in $i_{qr}$:

$$i_{qr} = \frac{\psi_{qr} - X_m \times \psi_{qs} - X_m i_{qr}}{x_s} \Rightarrow i_{qr} \times \left(\frac{x_s X_r - X_m}{X_s}\right) = \psi_{qr} - \frac{x_m}{x_s} \times \psi_{qs}$$

Multiplying $\left(\frac{x_m}{x_r}\right)$ into (5.56) yields to $\left(\frac{x_m}{x_r}\right) \times i_{qr} \times \left(\frac{x_s X_r - X_m}{x_s}\right) = \left(\frac{x_m}{x_r}\right) \times \left(\psi_{qr} - \frac{x_m}{x_s} \times \psi_{qs}\right)$.

Then by considering $X' = \frac{x_s X_r - X_m^2}{x_r}$ and $E'_d = -\frac{x_m}{x_r} \times \psi_{qr}$:

$$i_{qr} = \left(-\frac{x_s}{x_m \times x'}\right) E'_d - \left(\frac{x_m}{x_r \times x'}\right) \psi_{qs}$$

(5.57)

By substituting (5.57) inside of (5.55) one can have:

$$\frac{d\psi_{qr}}{dt} = \omega_s R_r \left(\frac{x_s}{x_m \times x'}\right) E'_d + \omega_s R_r \left(\frac{x_m}{x_r \times x'}\right) \psi_{qs} + (\omega_s - \omega) \psi_{dr}$$

(5.58)
Next step is to consider (5.58) in (5.54) that leads to:

$$\frac{dE'_d}{dt} = -\frac{\omega_s R_r}{x_r} \left( \frac{X_s}{x'} \right) E'_d - \frac{\omega_s R_r}{x_r} \left( \frac{X_m}{x_r x'} \right) \psi_{q_s} - (\omega_s - \omega) \frac{X_m}{x_r} \psi_{q_r}.$$  

Finally, by incorporating (5.40), (5.42), and (5.43):

$$T'_0 \frac{dE'_d}{dt} = -\left( \frac{X_s}{x'} \right) E'_d - \frac{X_s - x'}{x'} \psi_{q_s} + (\omega_s - \omega) T'_0 E'_q$$  

(5.59)

5.4.3.4 Acceleration Equation Based on Voltage Behind Transient Impedance ($E'_d$ & $E'_q$)

From (5.25), $\frac{2H \, d\omega}{\omega_s \, dt} = T_e - T_m$. To represent $\frac{d\omega}{dt}$ based on $E'_d$ & $E'_q$, first we should represent $T_e$ based on $E'_d$ & $E'_q$. From (5.26), $T_e = \psi_{d_s} i_{q_s} - \psi_{q_s} i_{d_s} = \psi_{q_r} i_{d_r} - \psi_{d_r} i_{q_r}$. The next step is to represent the $T_e$ based on $E'_d$ & $E'_q$ in order to find $i_{d_s}$ and $i_{q_s}$ based on the voltage behind transient impedance. So that, by considering $E'_q = \frac{X_m}{x_r} \psi_{d_r}$ in (5.44), $i_{d_s}$ would be:

$$i_{d_s} = \frac{1}{x'} \left( \frac{\psi_{d_s} - E'_q}{x'} \right)$$  

(5.60)

By substituting (5.34) in (5.32), i.e., $i_{q_r}$ in $i_{q_s}$, one can has $i_{q_s} \times (X_s X_r - X^2_m) \times X_r^{-1} = \psi_{q_s} - X_m \times X_r^{-1} \times \psi_{q_r}$. As it was shown in (5.42), $E'_d = -\frac{X_m}{x_r} \psi_{q_r}$. So that,

$$i_{q_s} = \frac{1}{x'} \left( \frac{\psi_{q_s} + E'_d}{x'} \right)$$  

(5.61)

Substituting (5.59) and (5.60) in $T_e$ yield to:

$$T_e = \frac{\psi_{d_s}}{x'} \left( \psi_{q_s} + E'_d \right) - \frac{\psi_{d_s}}{x'} \left( \psi_{d_s} - E'_d \right) = \frac{1}{x'} \left( \psi_{d_s} E'_d + \psi_{d_s} E'_q \right)$$  

(5.62)

Finally, by considering (5.62) in (5.25):

$$\frac{2H \, d\omega}{\omega_s \, dt} = \frac{1}{x'} \left( \psi_{q_s} E'_q + \psi_{d_s} E'_d \right) - T_m$$  

(5.63)

5.4.3.5 Stator Linkage Flux Based on the Stator Current ($i_{d_s}$ & $i_{q_s}$) and Voltage Behind Transient Impedance ($E'_d$ & $E'_q$)

To represent (5.31) and (5.32) only based on stator current ($i_{d_s}$ & $i_{q_s}$) and voltage behind transient impedance ($E'_d$ & $E'_q$) one can express $i_{d_r}$ and $i_{q_r}$ based on the $E'_d$ and $E'_q$. So,
Currents $i_{ds}$ and $i_{qs}$ in (5.33) and (5.34) are the targeted ones while $\psi_{dr}$ and $\psi_{qr}$ are the undesired terms. So, $\psi_{dr}$ and $\psi_{qr}$ should be replaced by $E'_d$ and $E'_q$. Therefore, one can do the following steps to reach to the desired. By multiplying $i_{dr}$ and $i_{qr}$ with $X_m \times X_m^{-1}$:

\[
i_{dr} = \frac{1}{X_m} \left( \frac{X_m}{X_r} \psi_{dr} - \frac{X_m}{X_r} i_{ds} \right)
\]

\[
i_{qr} = \frac{1}{X_m} \left( \frac{X_m}{X_r} \psi_{qr} - \frac{X_m}{X_r} i_{qs} \right)
\]

By considering (5.39), (5.41), and (5.42), in (5.64) and (5.65):

\[
i_{dr} = \frac{1}{X_m} \left( E'_q - (X_s - X') i_{ds} \right)
\]

\[
i_{qr} = \frac{1}{X_m} \left( -E'_d - (X_s - X') i_{qs} \right)
\]

The final step is to consider (5.66) and (5.67) in (5.31) and (5.32), respectively. Thus,

\[
\psi_{ds} = X' i_{ds} + E'_q
\]

\[
\psi_{qs} = X' i_{qs} - E'_d
\]

5.4.3.6 Eliminating the Linkage Flux in Differential Equations of Voltage Behind Transient Impedance

Considering (5.68) and (5.69) into (5.53) and (5.59) yields to:

\[
T'_0 \frac{dE'_d}{dt} = (X_s - X') i_{ds} - E'_q - (\omega_s - \omega) T'_0 E'_d
\]

\[
T'_0 \frac{dE'_q}{dt} = -(X_s - X') i_{qs} - E'_d + (\omega_s - \omega) T'_0 E'_q
\]

5.4.3.7 Induction Motor Algebraic Equations

Removal of stator dynamics yield to induction motor algebraic equations. To find out the algebraic equations based on the stator dynamic equations, one can consider large $\omega_s$ in the stator dynamic equations. So, by considering large $\omega_s$ in (5.46), (5.47), (5.21) and (5.22),:

\[
-\psi_{qs} = -\frac{R_s}{X'_q} \psi_{ds} + \frac{R_s}{X'_q} E'_q + V_{ds}
\]

\[
\psi_{ds} = -\frac{R_s}{X'_q} \psi_{qs} - \frac{R_s}{X'_q} E'_d + V_{qs}
\]

\[
\psi_{qs} = R_s i_{ds} - V_{ds}
\]

\[
\psi_{ds} = -R_s i_{qs} + V_{qs}
\]
Considering (5.74) and (5.75) in (5.72) yields to
\[
\frac{R_s}{X'} E_q' = \frac{R_s}{X'} \psi_{ds} - \psi_{qs} - V_{ds} = \frac{R_s}{X'} (-R_s i_{qs} + V_{qs}) - (R_s i_{ds} - V_{ds}) - V_{ds} = \frac{-R_s^2}{X'} i_{qs} + \frac{R_s}{X'} V_{qs} - R_s i_{ds}. 
\]
Finally,
\[
E_q' = -R_s i_{qs} + V_{qs} - X' i_{ds} \tag{5.76}
\]

Equation (5.44) is the algebraic equation representation for voltage behind transient impedance of induction machine align with q axis. By substituting (5.74) and (5.75) in (5.73),
\[
\frac{R_s}{X'} E_d' = \frac{-R_s}{X'} \psi_{qs} - \psi_{ds} + V_{qs} = \frac{-R_s}{X'} (R_s i_{ds} - V_{ds}) - (-R_s i_{qs} + V_{qs}) + V_{ds} = \frac{-R_s^2}{X'} i_{ds} + \frac{R_s}{X'} V_{ds} + R_s i_{qs}. 
\]
Finally,
\[
E_d' = -R_s i_{ds} + V_{ds} + X' i_{qs} \tag{5.77}
\]

Equation in (5.77) is the algebraic equation for voltage behind transient impedance of induction machine align with d axis.

5.4.4 Parks Equations

The d-q reference frame representation of the stator voltage and current based on their Cartesian form would be:

\[
V_{ds} = V_s \sin(\delta - \theta_s) \tag{5.78}
\]
\[
V_{qs} = V_s \cos(\delta - \theta_s) \tag{5.79}
\]
\[
i_{ds} = I_s \sin(\delta - \varphi_s) \tag{5.80}
\]
\[
i_{qs} = I_s \cos(\delta - \varphi_s) \tag{5.81}
\]

Algebraic equations (5.72) to (5.81) along with dynamics equations will be used in dynamical model of the induction machine.

5.4.5 Induction Motor Representation in Load Flow

The terminal voltage of the induction machine based on the stator current and internal emf would be:
\[
(V_d + jV_q)e^{j(\frac{\delta - \pi}{2})} = (R_s + jX)(i_{d_s} + ji_{q_s})e^{j(\frac{\delta - \pi}{2})} + (E'_d + jE'_q)e^{j(\frac{\delta - \pi}{2})} = V_s e^{j\theta_s} = (R_s + jX')(i_{d_s} + ji_{q_s})e^{j(\frac{\delta - \pi}{2})} + j(E'_q - jE'_d)e^{j(\frac{\delta - \pi}{2})}
\]

By initially setting \( E'_q = 0 \), the \((E'_q - jE'_d)e^{j\delta} \) will be equal to \( E'_d e^{j\delta} \). So that by rewriting (5.82) based on \( E \), the real part and imaginary parts of \( E \) would be:

\[
\text{Real}(E) = V_s \cos(\theta_s) - (R_s I_s \cos(\varphi_s) - X'I_s \sin(\varphi_s))
\]

\[
\text{Imag}(E) = V_s \sin(\theta_s) - (R_s I_s \sin(\varphi_s) + X'I_s \cos(\varphi_s))
\]

5.4.6 Induction Motor, Steady-State Conditions

Considering (5.78) to (5.81) in (5.74) and (5.75) to find linkage fluxes \( \psi_{d_s} \) and \( \psi_{q_s} \) and then in (5.72) and (5.73) to find \( E'_d \) and \( E'_q \) will complete steady-state initialization. So that, from (5.74) and (5.75), \( \psi_{q_s} = R_s i_{d_s} - V_{d_s} \) and \( \psi_{d_s} = -R_s i_{q_s} + V_{q_s} \). By substituting (5.78) to (5.81), i.e.,

\[
V_{d_s} = V_s \sin(\delta - \theta_s), \quad V_{q_s} = V_s \cos(\delta - \theta_s), \quad i_{d_s} = I_s \sin(\delta - \varphi_s), \quad \text{and} \quad i_{q_s} = I_s \cos(\delta - \varphi_s),
\]

in (5.45) and (5.46):

\[
\psi_{q_s} = R_s I_s \sin(\delta - \varphi_s) - V_s \sin(\delta - \theta_s)
\]

\[
\psi_{d_s} = -R_s I_s \cos(\delta - \varphi_s) + V_s \cos(\delta - \theta_s)
\]

From (5.72), \(-\psi_{q_s} = -\frac{R_s}{X'} \psi_{d_s} + \frac{R^2_s}{X'} E'_q + V_{d_s} \) that yields to

\[
E'_q = \frac{X'}{R_s}(-\psi_{q_s}) + \psi_{d_s} - \frac{X'}{R_s}(V_{d_s})
\]

By rewriting (5.87) based on (5.86) and (5.85) one can have: \( E'_q = -X' \times R_s^{-1} \times (R_s I_s \sin(\delta - \varphi_s) - V_s \sin(\delta - \theta_s)) + (-R_s I_s \cos(\delta - \varphi_s) + V_s \cos(\delta - \theta_s)) - X' \times R_s^{-1} \times V_s \sin(\delta - \theta_s) \).

So that, \( E'_q = V_s \times X' \times R_s^{-1} \times \sin(\delta - \theta_s) + \cos(\delta - \theta_s) - X' \times R_s^{-1} \times \sin(\delta - \theta_s) + (-X'I_s \sin(\delta - \varphi_s) - R_s I_s \cos(\delta - \varphi_s)) \). By doing a simplification in \( E'_q \), one can find a new representation for \( E'_q \) same as (5.88) that provide a steady-state condition for an induction motor.

\[
E'_q = I_s(-X' \sin(\delta - \varphi_s) - R_s \cos(\delta - \varphi_s)) + V_s \cos(\delta - \theta_s)
\]
To find another representation for steady-state condition of an induction motor, one can start from (5.73), \( \psi_{ds} = -\frac{R_s}{X'} \psi_{qs} - \frac{R_s}{X'} E'_d + V_{qs} \) that yields to
\[
E'_d = -\frac{X'}{R_s} (\psi_{ds}) - \psi_{qs} + \frac{X'}{R_s} (V_{qs})
\] (5.89)

Next step is to find the steady-state representation of (5.89), so, by considering (5.85) and (5.86) in (5.89) one can have: \( E'_d = -X'R_s^{-1}(-R_s I_s \cos(\delta - \varphi_s) + V_s \cos(\delta - \theta_s)) - (R_s I_s \sin(\delta - \varphi_s) - V_s \sin(\delta - \theta_s)) + X'R_s^{-1}(V_s \cos(\delta - \theta_s)) \). So that, \( E'_d = I_s(X' \cos(\delta - \varphi_s) - R_s \sin(\delta - \varphi_s)) + V_s(-X'R_s^{-1} \cos(\delta - \theta_s) + \sin(\delta - \theta_s) + X'R_s^{-1} \cos(\delta - \theta_s)) \). And finally,
\[
E'_d = I_s(X' \cos(\delta - \varphi_s) - R_s \sin(\delta - \varphi_s)) + V_s \sin(\delta - \theta_s)
\] (5.90)

5.5 DER_A Model

The NERC-2018 DER_A model [102] (see Figure 5.7) is adopted in the T&D co-simulation environment to reflect the aggregate impacts of DER units in the distribution systems.

The DER_A model was developed as a complement to the WECC composite load model [96] and is used to present DER units at the distribution substation. Various control capabilities are available by the DER_A model such as dynamic voltage support, reactive power control, frequency control, post fault active power recovery, and voltage source interface. Different flags pertaining to the DER_A various operation modes include P\_flag, Freq\_flag, and PQ\_flag and are detailed below.

P\_flag: When P\_flag is set to 0, reference reactive power Q_{ref} is used. When P\_flag is set to 1, the active power demand and reference power factor are used to determine the Q_{ref}.

Freq\_flag: When Freq\_flag is set to 0, the frequency deviation does not affect the active power demand. When Freq\_flag is set to 1, the frequency error signal is added to the active power demand and thus the DER_A mechanism will generate additional active power as needed.
\( P_{\text{Priority}} \) and \( Q_{\text{Priority}} \): The maximum active current \( I_{\text{max}} \) is the converter’s maximum current capacity. In \( P_{\text{Priority}} \) mode, \( I_{\text{max}} \) is allocated to the DER active current component \( I_p \) first and any remaining capacity to the reactive current \( I_q \). In the \( Q_{\text{Priority}} \) mode, the current capacity is given to the reactive current and the remaining current capacity is given to active current \( I_p \). \( PQ_{\text{flag}} \) setting will determine this option.

The modeling details of the DER_A, which includes all dynamical and algebraic equations that can be obtained from Figure 5.7, is considered in the proposed T&D co-simulation.

5.6 Transmission Model, Dynamic Analysis

Here, it is assumed that all transmission loads are balanced and only a balanced three-phase short circuit fault is considered. As mentioned earlier, the algorithm can be used with sequence networks for unbalanced faults. Thus, a positive sequence, single-phase model is considered for both steady-state and dynamic analyses in the context of the Newton-Raphson (NR) solution method.

5.7 Distribution Model, Dynamic Analysis

Same as the steady-state analysis in section 4.6, a complete three-phase model is considered to model the distribution system to include an unbalanced network. The distribution network may include single-phase, double-phase, or three-phase loads. All loads in the distribution systems are constant power. Distribution level DERs can be considered in the distribution model with no dynamics. These DERs act like a P-Q bus (load bus) with negative power demand and may be time varying.
Figure 5.7. DER_A model [102]
5.8 Transmission-Distribution Exchange Equations and Error Loop

In this dissertation, the distribution total load is applied as a lumped load to the transmission node. Likewise, the distribution load flow utilizes the transmission system nodal voltage as a constant value to obtain the distribution nodal voltages and total power demand. The boundary variables of the transmission and distribution networks are updated and exchanged at the end of the NR solution for the transmission system followed by the distribution solution. Once the distribution load flow solution is obtained, the new boundary voltages/loads are compared to the ones at the beginning of the transmission solution. If these values are close enough (typically with an error of $10^{-6}$) the new boundary values are selected; otherwise, they are reused to refine the error. The error loop is performed one time for static load flow and at each time step for dynamic stability analysis.

5.8.1 Dynamic Simulation

The exchange equations of the proposed T&D co-simulation for the error loop iteration $m$ at the time step $t$ are defined in (5.91) and (5.92) as

$$S_D^m(t) = h_D\left(V_T^{m-1}(t)\right)$$

$$V_T^m(t) = h_T\left(S_D^m(t), V_T^{(m-1)}(t), \theta_T^{m-1}(t), x_T^{m-1}(t)\right)$$

where $x_T$ contains all the transmission dynamic states including those of the DER_A.

5.8.2 Co-Simulation Algorithm; Dynamic Analysis

As mentioned earlier, a coupled T&D co-simulation is adopted where the transmission system is modeled as a symmetrical single-phase network whereas the three-phase model of the distribution system is used. The two networks then exchange voltage and power at the coupling points to achieve convergence of voltage/power at that point. The algorithm is explained in the following and is illustrated in Figure 5.8.
T&D Co-simulation:

Step 1. Initialize the transmission network with voltages $V_T^{(0)}$ at the flat start for load flow. $m = 1$;

Step 2. Perform distribution load flow using the available transmission voltages and obtain total power demand $S^m_D$ (loads, generations, and losses) at the distribution substation (T&D coupling point) using (4.6). Perform transmission load flow to update transmission voltages and find $V_T^m$ using (4.7);

Step 3. If $|V_T^m - V_T^{m-1}| < \varepsilon$ ($\varepsilon$ is the convergence tolerance), Go to Step 4. If not, set $m = m + 1$ and go back to Step 2;

Step 4. Save $V_T^m$, $\theta_T^m$, and distribution voltage phasors as initial values for dynamic simulation;

Step 5. Set $t = 0$. Initialize dynamical states $x_T^0(t)$ by using the voltages from Step 4. Set $S_D^0 = S_D^m$ and $V_T^0 = V_T^m$;

Step 6. Reset $m = 1$;

Step 7. Set $t = t + \Delta t$;

Step 8. If $t = t_{\text{max}}$ go to Step 11;

Step 9. Perform distribution load flow using the available transmission voltages and obtain total power demand $S^m_D$ (loads, generations, and losses) at the distribution substation (T&D coupling point) using (5.91). Perform transmission DAE solution for one-time step using (5.92) to update transmission voltages $V_T^m(t)$;

Step 10. If $|V_T^m(t) - V_T^{m-1}(t)| < \varepsilon$, save $V_T^m$, $\theta_T^m$, and distribution voltage phasors. Go to Step 7. If not, set $m = m + 1$ and go back to Step 9;

Step 11. Finish.

It must be mentioned that contingencies will start the process from Step 1 with initial conditions set by a fault initialization routine.
Figure 5.8. Transmission-distribution (T&D) co-simulation flowchart – dynamic analysis
5.8.3 Solution of DAE System

There are several algorithms to numerically solve the DAE system of equations (5.1) that utilize one of the two methods that are widely used in the power system simulation tools; namely, simultaneous-implicit (SI) method and partitioned-explicit (PE) method [105].

In the SI method, firstly, the differential equations in (5.1) are converted to a set of algebraic equations. This conversion is done either by Euler’s method or the trapezoidal integration method. Then, the new algebraic equations are solved simultaneously with other algebraic equations in (5.2) by utilizing Newton’s method at each time step. On the contrary, in the PE method, the differential equation in (5.1) and algebraic equation (5.2) are solved separately, via the system solution and integration steps, respectively. Although decoupling (5.1) and (5.2) makes the PT method simpler than the SI method, it may add numerical convergence difficulties, such as interface error [105].

Here, the trapezoidal integration approach of the SI method is considered to numerically solve the DAEs system of equations in (5.1) and (5.2).

5.8.3.1 Trapezoidal Integration Method

By considering the trapezoidal integration method to the integral the differential equation in (5.1) from \( t_n \) to \( t_{n+1} \), i.e., \( x_{n+1} = x_n + \int_{t_n}^{t_{n+1}} f_o(x, I_{d-q}, \bar{V}, u) dt \), one can have,

\[
x_{n+1} = x_n + \frac{\Delta}{2} \left[ f_o(x_{n+1}, I_{d-q,n+1}, \bar{V}_{n+1}, u_{n+1}) \right] \tag{5.93}
\]

subscripts \( n \) and \( n + 1 \) denote time-steps \( t_n \) and \( t_{n+1} \), respectively. Now the Newton method can be utilized to solve the algebraic equation in (5.93) along with the other algebraic equations in (5.2) when they are rewritten for time-step \( n + 1 \).
5.9 Case Study and Simulation Results

This part aims to explore the effect of the DER_A mechanism, such as control flags $P_{\text{flag}}$, $F_{\text{reqflag}}$, and $PQ_{\text{flag}}$ along with voltage controller $K_{\text{q}}$ and maximum of current $I_{\text{max}}$ (see Figure 5.7), as well as the effects of the induction motor(s) location on the dynamic of the power system during transients. Four different scenarios were performed here to show the effect of DER_A model and induction motor location on the dynamics of transmission-distribution network.

In the dynamic simulation, constant-impedance loads are applied at each transmission substation to account for 25% of the size of the constant load on the substation. A 1500 MW synchronous generator is connected at Station 1. Also, one solar farm is considered as a negative load and connected downstream of Station D. In addition, LTC and line regulators don’t operate due to the short duration of the transients and the solar power is interfaced through the dynamic DER_A model. Besides, all constant loads are converted to constant impedance during faults. In one case, one three-phase 7000hp 13.8kV induction motor is considered in the transmission system (connected downstream of Station CA). This induction motor is loaded at 0.7 times the aggregate load at Station CA leaving 30% of the load to distribution constant loads ($P_L = 12.1022 * 0.3 = 3.63\text{MW}$). And in another case, two single-phase 7000hp 13.8kV induction motors that are loaded at 0.7 times the aggregate load at Station CA are considered inside Feeder C-102. Dynamic simulations performed using a transmission-distribution dynamic co-simulation. A step time of 0.0005s was used to illustrate 10-seconds simulation time. The base MVA in this part was 100 MVA ($S_{\text{base}} = 100 \text{ MVA}$). The distributed DER-A Model (NERC 2018) is fully implemented in the dynamic simulator (details of the model can be found in section 4.e).

The following assumptions were considered in this section:

- Only the Station CA has a detailed distribution system
• All constant loads are converted to constant impedance during the fault

• DER_A control is enabled at Station D

• Short-circuit fault with admittance of 1000p.u. \((Y=1000pu)\) is applied at Substation CA at 0.5s and continues for 17 cycles. The fault clear at 0.79s

• Constant impedance loads are added at each station of the transmission system in order to have ZIP load models. The constant impedance load at each station is 25% of the size of constant load on that station

• Constant impedance load at each station is modeled with one parallel conductance \((G)\) and susceptance \((B)\).

DER parameters that are used in this simulator and NERC recommended values are provided in Table 5.4.

Table 5.4. DER parameters that are used in this simulator and NERC recommended values

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<td>-</td>
<td>1.2- V_{Drop}</td>
<td>1.2- V_{Drop}</td>
<td>1.2- V_{Drop}</td>
<td>1.2- V_{Drop}</td>
</tr>
<tr>
<td>tvl0</td>
<td>-</td>
<td>0.16</td>
<td>0.16</td>
<td>1.5</td>
<td>0.16</td>
</tr>
<tr>
<td>tvl1</td>
<td>-</td>
<td>0.16</td>
<td>0.16</td>
<td>1.5</td>
<td>0.16</td>
</tr>
<tr>
<td>tvh0</td>
<td>-</td>
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<td>0.16</td>
<td>0.16</td>
<td>0.16</td>
</tr>
<tr>
<td>tvh1</td>
<td>-</td>
<td>0.16</td>
<td>0.16</td>
<td>0.16</td>
<td>0.16</td>
</tr>
<tr>
<td>Vrfrac</td>
<td>-</td>
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<td>0</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>fltrp</td>
<td>-</td>
<td>59.3</td>
<td>59.5 or 57.0</td>
<td>58.5 or 56.5</td>
<td>58.5 or 56.5</td>
</tr>
<tr>
<td>fhtrp</td>
<td>-</td>
<td>60.5</td>
<td>60.5 or 62.0</td>
<td>61.2 or 62.0</td>
<td>61.2 or 62.0</td>
</tr>
<tr>
<td>tf1</td>
<td>-</td>
<td>0.16</td>
<td>2.0 or 0.16</td>
<td>300.0 or 0.16</td>
<td>300.0 or 0.16</td>
</tr>
<tr>
<td>tfh</td>
<td>-</td>
<td>0.16</td>
<td>2.0 or 0.16</td>
<td>300.0 or 0.16</td>
<td>300.0 or 0.16</td>
</tr>
<tr>
<td>tg</td>
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<td>0.02</td>
<td>0.02</td>
<td>0.02</td>
<td>0.02</td>
</tr>
<tr>
<td>rrpwr</td>
<td>-</td>
<td>0.1</td>
<td>0.1</td>
<td>2</td>
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<tr>
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<td>0.02</td>
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</tr>
</tbody>
</table>

Table 5.4 continued.
5.9.1 Scenarios 1-4

These scenarios aim to explore the effect of the DER control flags $P_{\text{flag}}$ and $PQ_{\text{flag}}$ (see Figure 5.7) along with the effects of the induction motor(s) location on the dynamic of the power system during transients. Station CA and Station D voltages and DER output reactive and active power are then observed under various scenarios as sorted in Table 5.5 and are illustrated in Figure 5.9 through Figure 5.16, respectively.

Figure 5.9, Figure 5.11, Figure 5.13, and Figure 5.15 present the effect of the DER injected currents during fault and after fault periods. The DER contribution to the voltage maintenance during fault leads to faster voltage recovery but at the expense of some voltage overshoot immediately after the fault clearance. Thus, it is beneficial if DER units have higher fault-ride-through capabilities and can maintain operation during the faulty low voltages. In addition, from
Figure 5.9, Figure 5.11, Figure 5.13, and Figure 5.15, the voltage overshoot after the fault clearance with distributed induction motors at Feeder C-102 is slightly higher than that with aggregated motor at the transmission system in all the Scenarios. This is partly due to additional distribution losses pertinent to the induction motors distributed in the distribution network. This in turn causes more reactive power generation during fault under reduced voltage. The additional reactive current persists immediately after the fault and decays shortly after. This behavior is observed due to the DER voltage feedback.

<table>
<thead>
<tr>
<th>Scenarios</th>
<th>P\text{flag} \ 1: \ ON, \ 0: \ OFF</th>
<th>PQ\text{flag} \ 1: P_{\text{priority}}, \ 0: Q_{\text{priority}}</th>
<th>\text{Freq\text{flag}} \ 0: \ OFF</th>
<th>I_{\text{max}} \ (p.u.)</th>
<th>K_{qv}</th>
</tr>
</thead>
<tbody>
<tr>
<td>14</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0.5</td>
<td>-0.1</td>
</tr>
<tr>
<td>15</td>
<td>1</td>
<td>0</td>
<td>0</td>
<td>0.5</td>
<td>-0.1</td>
</tr>
<tr>
<td>16</td>
<td>0</td>
<td>1</td>
<td>0</td>
<td>0.5</td>
<td>-0.1</td>
</tr>
<tr>
<td>17</td>
<td>1</td>
<td>1</td>
<td>0</td>
<td>0.5</td>
<td>-0.1</td>
</tr>
</tbody>
</table>

Figure 5.9. Stations CA and D voltages - scenario 1
Figure 5.10. DER reactive and active power - scenario 1

Figure 5.11. Stations CA and D voltages - scenario 2
Figure 5.12. DER reactive and active power - scenario 2

Figure 5.13. Stations CA and D voltages - scenario 3
Figure 5.14. DER reactive and active power - scenario 3

Figure 5.15. Stations CA and D voltages - scenario 4
5.10 Conclusion

This chapter explores the dynamic impacts of distribution-level DER units on the power systems in a co-simulation environment. It is observed that the DER units can improve the voltage of transmission when a short circuit happened in the system and provides faster voltage recovery. Dynamic models of generator and induction motor, constant and impedance load models, and NERC DER_A model are considered in the proposed dynamic T&D Co-simulation. It is shown that the distributed induction motors can lead to DER higher current in response to voltage fluctuations.
CHAPTER 6. CONCLUSION AND FUTURE WORK

Hybrid grids are explained in this dissertation and then methods for steady-state and stability analyses of these grids are discussed. In general, hybrid grids are referred to networks that couple circuits from different kinds. Ac-dc grids and transmission-distribution networks are well-known examples of hybrid systems that were discussed in this dissertation. In this dissertation, the unified analysis and sequential analysis are considered for steady-state and stability analyses of the hybrid ac-dc grids and transmission-distribution hybrid systems, respectively.

In the context of hybrid ac-dc power systems, an ac equivalent circuit is developed for the ac-dc system and the dc transmission circuit is replaced with its ac equivalent circuits and then conventional Newton-Raphson based load flow algorithm or classical fault analysis is performed for the hybrid grid. The proposed unified analysis can solve the issues that the past works faced, such as programming challenges, lack of system-level perspective, imposing certain constraints, and computational efficiency.

Next, in order to investigate the effects of distribution systems on the transmission grids, sequential methods for analyzing the transmission-distribution hybrid system are proposed to and coupled transmission-distribution (T&D) co-simulation is developed that solve the transmission and distribution systems of equations in an error loop. The proposed T&D co-simulation can perform the steady-state and dynamic analyses of transmission-distribution hybrid systems with high accuracy. The results of the T&D co-simulation show that under the steady-state condition the excessive operation of substation LTC is likely to happen during the high presence of DER units. As a consequence of this excessive LTC operation, the voltage flicker inside of the distribution system is considerable and goes beyond the visibility threshold. Also, it was shown that the excessive operation of substation LTC can be diminished either by changing the bandwidth
of LTC or by adapting the Volt-Var curve that is presented in IEEE 1547 std [45]. Besides, it was observed that during the contingency, the DER units can improve the voltage of transmission when a short circuit happened in the system and provides faster voltage recovery. Moreover, it is shown that the distributed induction motors can lead to DER higher current in response to voltage fluctuations.

As a future work, one can extend the proposed models in this dissertation work into different areas as:

1. The modulation factor of the ac-dc converter \( m_a \) in chapter I is considered constant. But, one can consider a non-constant \( m_a \) in the proposed unified load flow algorithm. In this case, the unknown variable \( m_a \) and its equation must be to mismatch equations in (17), which in turn increases the complexity and the simulation time. Finding the right value of \( m_a \) based on the heuristic method or machine learning approaches is another way of calculation \( m_a \), prior to solve (17). This, in turn, prevents (17) from becoming more complex;

2. Since the jacobian matrix \((J)\) in (17) is highly nonlinear, the Kaczmarz algorithm can be adopted to inverse the jacobian matrix. Right now, the LU factorization is implemented for inversion of the jacobian matrix;

3. The approach of the proposed unified ac-dc load flow in chapter I can be used to develop an optimization algorithm for the load flow analysis of hybrid ac-dc grids;

4. One can find the dc Thevenin equivalent of ac grids on the dc side based on the presented steps in finding ac Thevenin equivalent of dc grids in chapter II to find a short circuit current in the dc side. However, more steps should be considered for this special case since the nature of dc fault is different from the ac one;
5. The transmission system can be modeled as a complete three phase system in the proposed T&D co-simulation so that the effects of asymmetrical faults on the transmission and distribution networks also can be examined.
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A New Approach for Hybrid ac-dc Circuit Fault Analysis
Mohammad Nehdi Rezvani and Shahab Mehraeen
2020 IEEE Energy Conversion Congress and Exposition (ECCE)

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Interaction of Transmission-Distribution System in the Presence of DER Units—Co-simulation Approach

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ABSTRACT The effects of distribution-connected solar farms on the transmission and distribution systems are studied in this paper using a practical power system network. Both the transmission and distribution networks are modeled and co-simulated for intermittent effects of solar power and changing load profiles. These effects include flicker in both the transmission and distribution systems, operation of the substation Load Tap Changer (LTC) and feeder regulators, and distribution feeder voltage profiles. The results of the study show a potential hunting effect causing excessive LTC operation especially during periods of high reverse power flow condition and a significant visible flicker in the distribution system but lower levels of flicker at the transmission level. It is shown that the hunting behavior of the LTC can be mitigated and downstream feeder voltage profiles can be improved via enhanced bandwidth, block operation mode, and adjusting time delay of the substation LTC controller. In addition, the inverter Volt-Var control can be used to effectively diminish excessive LTC operation based on IEEE Std 1547-2018 Volt-Var characteristics with modified settings. Finally, an enhanced load-flow algorithm with modified Jacobian is presented that incorporates the Distributed Energy Resource (DER) Volt-Var characteristics in the conventional Newton-Raphson method.

INDEX TERMS Distributed Energy Resource (DER), Flicker, Load Tap Changer (LTC), Volt-Var control.

I. INTRODUCTION

Modern power systems take advantage of Distributed Energy Resources (DERs) and utilize renewable energy. Renewable power generation units that are connected at the distribution level fall under the category of DER. DERs, especially those based on Photovoltaics (PV), have attracted a lot of attention during the past decade due to potential energy savings for the customers and reduced environmental footprint [1], [2]. DERs can also be used to alleviate the feeder loading to allow increased load transfer capability. However, high penetration of DERs in the distribution grid can present challenges to the power system operation such as voltage and protection issues, fault detection, and during system restoration [3], [4].

The conventional distribution grid has been designed assuming that the electric power is carried unidirectionally from HV/MV substations downstream toward customers. As a result, the introduction of DERs on the distribution side will affect existing protection and control philosophies, and thus, overall, the distribution system’s reliability could be affected [5]–[8]. The adverse effects of the DERs depend on various factors, such as feeder topology, size of the DER units, operation and control strategies, and the DER location in the network [9]–[11]. In addition to the effects on the distribution circuits, distribution-connected DER units may have an impact on the transmission voltage. Thus, tools must be developed to identify and mitigate these potential effects on transmission systems.

Supporting the power grid by the use of electrical energy storage converter-fed devices during high penetration of DERs is addressed in [12]. The injected/absorbed active/reactive power of the electrical energy storage devices is controlled by the frequency-voltage deviation of the
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Rezvani, Mohammad Mehdi; Mehraeen, Shahab; Ramamurthy, Jayanth; Field, Thomas
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