STEADY-STATE ANALYSIS OF DIRECTLY CONNECTED SYNCHRONOUS MACHINES AND HVdc CONVERTERS

A thesis presented for the degree of Doctor of Philosophy in Electrical and Electronic Engineering at the University of Canterbury, Christchurch, New Zealand.

by

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The proverbs of Solomon the son of David, King of Israel:

To know wisdom and instruction,
To perceive the words of understanding,
To receive the instruction of wisdom,
Justice, judgement, and equity;
To give prudence to the simple,
To the young man knowledge and discretion-
A wise man will hear and increase learning,
And a man of understanding will attain wise counsel,
To understand a proverb and an enigma,
The words of the wise and their riddles.
The fear of the LORD is the beginning of knowledge,
But fools despise wisdom and instruction.

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LIST OF SYMBOLS

+ $ac$ alternate current
+ $dc$ direct current
+ $HVdc$ high voltage direct current
+ $emf$ electro magnetic force
+ $rms$ root mean square
+ $E_c$ converter terminal a.c. voltage
+ $E$ rectifier a.c. system emf
+ $E''$ rectifier a.c. system commutating voltage
+ $x_c$ Unit-Connection commutating reactance
+ $x'_c$ Group-Connection commutating reactance
+ $x$ generator synchronous reactance
+ $x''$ generator sub-transient reactance
+ $x_t$ converter transformer reactance
+ $x_B$ base reactance
+ $n$ total number of generators
+ $n_s$ number of generators in service
+ $n_p$ number of pulses in the converter bridge
+ $V_a$ d.c. voltage for one unit connected generator
+ $V_d$ d.c. voltage for $n_s$ generators
+ $V_{av}$ average d.c. voltage for $n_s$ generators
+ $V_i$ inverter end d.c. voltage per pole of the link
+ $V_r$ rectifier end d.c. voltage per pole of the link
+ $V_{term}$ a.c. voltage in the primary of the converter transformer
+ $E_{r,i}$ a.c. voltage in the secondary of the converter transformer
+ $V$ a.c. peak commutating voltage in the converter
+ $V_{d_{rms}}$ d.c. r.m.s voltage in the converter
+ $I_d$ d.c. current of the link
+ $I_{or}$ current order in the rectifier
+ $I_{oi}$ current order in the inverter
+ $I_p$ a.c. current in the primary of the converter transformer
+ $I_s$ a.c. current in the secondary of the converter transformer
+ $R_d$ resistance per pole of the link
LIST OF SYMBOLS

\begin{itemize}
  \item \( R \) \( \text{ac residuals in the ac-dc loadflow} \)
  \item \( P_{\text{term}} \) \( \text{busbar active power} \)
  \item \( Q_{\text{term}} \) \( \text{busbar reactive power} \)
  \item \( Q_{c_e} \) \( \text{capacitor charge} \)
  \item \( r \) \( \text{ratio of the converter transformer} \)
  \item \( a \) \( \text{tap of the converter transformer} \)
  \item \( \alpha \) \( \text{converter firing angle} \)
  \item \( \gamma \) \( \text{converter extinction angle} \)
  \item \( \beta \) \( \text{subtransient voltage phase angle} \)
  \item \( \mu \) \( \text{converter commutation angle} \)
  \item \( \eta_t \) \( \text{turbine efficiency} \)
  \item \( \eta_t^U \) \( \text{transmission efficiency - Unit-Connection} \)
  \item \( \eta_t^G \) \( \text{transmission efficiency - Group-Connection} \)
  \item \( \eta_{\text{eff}}^U \) \( \text{overall efficiency - Unit-Connection} \)
  \item \( \eta_{\text{eff}}^G \) \( \text{overall efficiency - Group-Connection} \)
  \item \( \phi \) \( \text{voltage phase angle} \)
  \item \( \Psi \) \( \text{power factor angle in the primary of converter transformer} \)
  \item \( \psi \) \( \text{power factor angle in the secondary of converter transformer} \)
  \item \( \Lambda \) \( \text{inductor flux} \)
  \item \( B \) \( \text{terminal susceptance} \)
  \item \( r \) \( \text{rectifier} \)
  \item \( i \) \( \text{inverter} \)
  \item \( F_b \) \( \text{ratio of the feedback converter transformer} \)
  \item \( f \) \( \text{nominal frequency} \)
  \item \( CCC \) \( \text{constant current control} \)
  \item \( CP \) \( \text{constant power} \)
  \item \( CEA \) \( \text{constant extinction angle} \)
  \item \( \vec{v} \) \( \text{vector} \)
  \item \( \dot{v} \) \( \text{variable first derivative} \)
  \item \( \ddot{v} \) \( \text{variable second derivative} \)
\end{itemize}
LIST OF PUBLICATIONS

Publications associated with this thesis:


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ABSTRACT

In this thesis the results from a conventional ac–dc load flow program for modelling the steady state behaviour of a unit connected salient-pole generator-converter are compared with those obtained from degree by degree dynamic simulation. The Transient Converter Simulation (TCS) program is used to give benchmark results. The unacceptable level of error from the load flow program indicates that the conventional ac–dc formulation is not directly applicable to the unit connected generator-converter.

A salient-pole generator converter model has been developed to reduce the errors, which allows for a voltage behind a varying sub-transient reactance to be used as the commutating voltage. The characteristics obtained from this model are better than for the conventional model but are still significantly different from the TCS results. A dynamic simulation of the operation of the unit connected generator converter is necessary, before steady state results can be obtained.

Unit and Group-Connections are also compared under varying steady state loading conditions and by reducing the number of generators in service in both schemes at nominal frequency.

An accurate algorithm of general applicability, called the Equivalent Inverter, is proposed which uses unit-connection characteristics derived from a time domain simulation.

Finally the steady state model for an alternative unit connected generator converter scheme is presented. This is based on the use of dc ripple re-injection, which permits using a single bridge (instead of two) to obtain a twelve-pulse converter operation. The proposed scheme considerably reduces the number of transformers and converter bridges, without increasing the harmonic levels with respect to the double bridge configuration.
Chapter 1
INTRODUCTION

Large amounts of energy have been transferred over considerable distances by HVdc transmission systems since the incorporation of the mercury arc power converter in high voltage transmission systems in Sweden in 1954, allowing the asynchronous connection of two separate ac systems with different frequencies. HVdc transmission systems have been proved very reliable, safe and economical compared to equivalent HVac transmission systems.

The advantages of such systems compared with pure ac systems have resulted in a fast growing number of new dc links to be commissioned around the world. However, the static power converter is considered as the main source of harmonics distortion in a power transmission system.

Some recent proposals in HVdc systems are for unit and group connected generation. In the unit connected scheme, each generator is connected directly to a converter transformer and the series and/or parallel combination of units is done on the dc side[Calverley et al., 1973]. A variation of the unit-connection called group-connection has also been suggested which has instead of a single generator, two or more generators connected directly to the converter transformer[Ingram, 1988]. Both systems are defined here as direct connected generation schemes.

In remote generating sites such as are to be found in Brazil, it is not immediately clear whether they should be connected on the ac side to make a group-connection or on the dc side to make unit-connections.

This thesis investigates means of digitally representing unit and group connections with sufficient accuracy to allow comparisons to be made between them and their effects on the ac system at the inverter end to be studied.

After discussing the technical advantages and disadvantages of the direct connected schemes, this chapter reviews the economical considerations which will finally decide whether they are ever built.

The chapter ends by examining the objectives of the work reported in the thesis and gives its overall structure.
1.1 Direct connected generation

Reducing cost and operational restrictions is an important engineering task. To this end direct connected generation was first compared with the conventional arrangement twenty years ago [Calverley et al., 1973]. The absence of ac filters, the elimination of step up transformers in the rectifier side of a conventional HVdc scheme and the flexibility of the converters being connected in series or in parallel were seen as the main features in the proposed HVdc generation scheme.

However harmonic distortion produced by the converter transformers is not minimized in direct connected generation. Generators and transformers can be adapted to cope with the extra harmonic distortion but it is important that loads are not connected to the ac rectifier system.

Non-linear phenomena, such as harmonic distortion between stator and rotor in the synchronous machine, introduce extra complexity to the model in steady-state analysis. It has been established that the synchronous machine is operating permanently in a sub-transient state when the converter operates without ac filters. In this case the reactance associated with the commutation process must include this sub-transient reactance and is thus much larger than in the conventional case. As a consequence an extra amount of field excitation is required in the synchronous machine. Previous work shows that for a 20% extra power in the direct connected scheme 10% extra excitation will be required [Sankar, 1991].

Direct connected generation also retains some of the advantages of HVdc schemes. Advantages, other than cost, in adopting unit or group-connection in mixed ac-dc schemes [Krishnayya, 1973] [Ingram, 1988] are:

i. Because of their easily controlled power transfer, HVdc links have demonstrated a significant stabilizing influence in ac power systems.

ii. Current and power flow are unaffected by ac system frequency or oscillations.

iii. Dc links may be re-established without any synchronizing check.

iv. The possibility of a total system shutdown due to a sending end ac system fault can be reduced due the absence of an interconnected ac system at the sending end.

v. Greater freedom in interconnecting different types and sizes of generation (steam, hydro, geothermal, gas, tidal, etc...).

vi. Better economic operation of the system may be possible because load dispatch and load scheduling are easier with asynchronous generations.

vii. The likelihood of the cascading incidents is less than with a synchronous system.

viii. For high power density areas, supply through mixed ac-dc schemes are cheaper, the short circuit levels are lower and the total losses in the system are smaller.
1.1 DIRECT CONNECTED GENERATION

ix. In some cases the possibility of generator self-excitation can be eliminated and ac resonant impedances avoided with the elimination of the ac filters.

The disadvantages of the direct connection are mainly due to problems produced by elimination of ac filters. Some of these problems are:

i. A larger dc filter will be necessary as the increased ac harmonics will reflect through onto the dc side.

ii. The dc filters must have adjustable tuning and size if the generators operate with variable speed. The use of wide band filters is an option already used in the ac filters of the new Hybrid New Zealand dc link.

iii. The harmonic currents in the stator and rotor windings originate pulsating electric torque. These harmonic currents and the distorted path of fluxes in the core produce additional copper and magnetic losses respectively.

iv. There is much less redundancy in the overall system.

v. The stabilizing capability of a unit connected scheme will be less than for a conventional HVdc system where there is more connected generation.

In the case of a power station coupled solely to an HVdc direct connected transmission system the variables that should be controlled are:

i. The exciter current, which in turn controls the generator voltage.

ii. The turbine and synchronous machine speed which determines the generator frequency.

iii. The generator nominal voltage, which determines the rectifier ideal no-load voltage.

iv. The active power (P).

v. The dc line direct voltage per pole ($V_d$). If all the converters in one pole are in parallel this voltage will also be the converter unit direct voltage.

vi. The converter unit direct current ($I_d$). If all the converter units are in series, the current will also be the transmission line current.

Other variables may be controlled in order to obtain or to keep the main variables at their specified values, for example the delay and extinction angles of the converters, online tap-changer (OLT C) position of the inverter transformer. Due the complexity of the whole problem only some of control strategies will be discussed in the following chapters.
1.2 Economic considerations of direct connected schemes

The unit connected generation or series/parallel combination of such schemes has already captured the interest of electricity authorities in New Zealand, Canada, Brazil and around the world. The principal interest is in their operation in remote areas or in offshore platforms where the size of the power generation facilities is an important issue. In the conventional configuration the extra generator transformers and the extra circuit breakers can add a substantial extra cost to the system. Studies are leading to more efficient and more reliable systems and contributing to further developments in HVdc generation[Thio and Rashwan, 1991].

The conventional converter station and its associated ac switchyard covers a considerable area and contains a great deal of equipment, which tends to make HVdc uncompetitive with ac except in special cases. Any means by which the amount of equipment can be reduced should reduce the capital cost and hence increase the number of cases where HVdc is a better economic choice than ac.

In many cases a mismatch exists for environmental reasons between the size of hydro stations and the economic size of an HVdc link. Another problem can be hydraulic reservoir constraints which limit the amount of generation from time to time. This is discussed in Chapter 3, where the number of units in a direct-connected scheme is reduced. In some places several ac interconnected stations would be necessary to match the power of an average size dc link. In this case it could be difficult to utilize a group connected scheme.

A direct connected hydro power generating plant has the following economic advantages[Stovall, 1987]:

i. The plant related savings in the group-connection case with a single 12 pulse bridge can be around 25% of the estimated cost of the conventional sending end. However, plant related savings are highly dependent on the arrangement and on site specific characteristics, so this figure can be up to 40% in some cases. The requirement for bypass valves and bypass switches in a series connected arrangement is a cost that must be considered.

ii. Maintenance services related savings can be up to 58% of the estimated conventional HVdc maintenance services cost.

iii. It has a very compact and simple configuration with less components and auxiliaries than a conventional scheme.

iv. It has better overall efficiency than an equivalent conventional HVdc station.

Other economical considerations are linked to the technical advantages discussed earlier.
1.3 THE MAIN OBJECTIVES

Economic studies of HVdc converters [Salgado et al., 1986] have shown that the rectifier station's total costs decrease with the firing angle for a given extinction angle in the inverter. This matter should be carefully considered due the variation of operational costs in the rectifier station for every combination of the firing angle in the rectifier with the extinction angle in the inverter. This economic factor is one of the main reasons why it has been suggested that the converters of some unit connected schemes in a system should operate as a diode rectifier bridges. This economic advantage is offset by the need for low reactance transformers to prevent the commutation angle exceeding the normal limit of 30°.

The reduction in cost at the rectifier end will not be reflected in a similar reduction at the inverter end. In fact costs here may be higher because of the need for more ac filtering.

The ac harmonic filters are extremely expensive (approximately 20% of the converter station) and produce technical problems such as the possibility of resonances with the local system, behaviour of the filters when the frequency departs from nominal and overvoltages during switching operations. However, the filters supply some of the reactive power required by the converter and wide band dc filters can be used to allow the changes in frequency of operation of the hydro machines.

Instead of comparing the conventional single generator-converter unit and the single synchronous machine unit connected scheme, a better exercise in cost comparison was suggested by Ingram [88] who recommended the use of an entire power station for each of the cases.

Operational aspects unit and of group connected systems will be discussed and compared later. One of the objectives of this work is to determine advantages and disadvantages of the operation of these schemes.

The design of both schemes is not within the scope of this work and has only been briefly mentioned at this stage. For the direct-connection to operate with a similar performance and requirements of the conventional generator-HVdc scheme further modifications may be necessary in the design of the turbine, generator and converter transformer core and winding and associated controls.

1.3 The main objectives

The main objectives of this thesis are:

\(i\). To study unit connected schemes operating in steady state using the presently available software.

\(ii\). To develop software that can make accurate steady state studies of direct connected synchronous generators and ac-dc converters.
iii. To introduce the use of dynamic techniques such as the Transient Converter Simulation (TCS) program in the steady state analysis of the direct connection of synchronous generators and converters.

iv. To create and develop a new model that combines dynamic simulation and loadflow studies for a defined operating point in order to indirectly insert the non-linearities of the direct connected generation schemes in steady state.

v. To use the experience and expertise gained to investigate the operation of the dc ripple re-injection scheme in the steady state analysis.

1.4 Thesis outline

The topics in this research study are organized using the following structure.

Chapter 2 describes a new method of modelling direct-connection schemes in steady state. This modified loadflow is a program designed to avoid some of the problems associated with the conventional loadflow. This chapter also briefly examines the structure and modelling of the TCS (Transient Converter Simulation program) in order to obtain the steady state characteristics of generator and ac–dc converter groups.

Operating characteristics for the unit and group-connection cases at nominal frequency are discussed and compared in Chapter 3.

Chapter 4 introduces the equivalent inverter model which uses charts of the direct connected system derived from a dynamic simulation of the steady state. Chapter 5 compares the results for an idealized system using the two models discussed in Chapter 2, with the equivalent inverter. Steady state results using the equivalent inverter are compared with commissioning results for the new upgraded New Zealand HVdc link to prove the validity of the model.

Chapter 6 discusses a recently developed dc ripple reinjection technique which has the advantages of a 12 pulse converter system but using only a single 6 pulse bridge. The steady state equations are determined and applied to a conventional loadflow program. Using the model the extra power capability of the system is demonstrated.

In Chapter 7 the main conclusions obtained from this research project are presented. Additional research is also suggested using a number of simulation methods and unit-connection configurations for direct connected HVdc transmission.
Chapter 2

MODELLING OF DIRECT CONNECTED SYNCHRONOUS GENERATOR-CONVERTER

2.1 Introduction

The connection of remote power generating plant via long distance HVdc transmission has already proved to be extremely reliable in many projects. In an attempt to simplify and reduce costs of the conventional design, the use of unit connected generators is being seriously considered.

Due to the importance of the topic, CIGRÉ (Conference Internationale des Grands Réseaux Électriques à Haute Tension) recently set up a working group (WG 14.09) and it is at present studying direct connected generator-converters to HVdc schemes.

One of the initial proposers of unit-connected schemes for HVdc transmission was Bowles[81], who discussed the technical and economic feasibility of using such a scheme. Later, dynamic aspects for the system under disturbances were analysed by Kanngiesser[83], Krishnayya[87].

Until recently, the modelling of the unit connected systems exploited its dynamic performance and economic aspects. The first studies for the steady state capability of synchronous generators and HVdc converters were made by Sankar[91]. By averaging results derived from dynamic simulation it was shown possible to model the effects of non-linearities embedded in the unit connected scheme for steady state studies.

Average value modelling of a direct connected six-phase synchronous machine and two six-pulse converters in systems with voltage rating up to 1.0 kV was simulated successfully by Sudhoff[92]. The dynamic simulation program was developed in a project involving aircraft power generation. In this case substantial differences exist in size, parameters and configuration between the system proposed[Sudhoff and Wasynczuk, 1992] and the systems for HVdc generation.

This chapter presents two ways of modelling the unit connected generation using the loadflow formulation. The first is a conventional loadflow adapted for use in the unit-connection. In this case the loadflow is not modified and basically an intermediary busbar
is inserted between the generator busbar and the converter transformer. The second model is a modified loadflow which also splits the rectifier system in two busbars but also includes the solution of the simplified sub-transient phasor diagram between generator and converter. Sankar[91] developed an iterative model of a simplified machine and HVdc converter. From this work, a steady state model has been created and incorporated into the fast decoupled loadflow program. This allows the generator excitation and the magnitude and phase angle of the sub-transient voltage to be included in the generator/rectifier equations. The operation of the generator-rectifier model is thus more easily controlled and the results interpreted for different loading conditions.

The dynamic modelling necessary to obtain steady-state capability charts of the unit connected system to be used in the loadflow was made using TCS - Transient Converter Simulation Program. Early work on TCS was made at the University of Manchester by Arrillaga[78]. The detailed synchronous machine model, using state variable techniques, was incorporated by Barros[76]. Improvements were made by Al-Kashali[76] who added diakoptical techniques (Kron[63], Brameller[69]). Substantial contributions made at Canterbury University by Heffernan[80], Turner[80], Watson[87] and Sankar[91] gave the present structure to the program.

Heffernan[80] developed accurate transient equivalents for the efficient modelling of disturbances in interconnected ac-dc power systems, he also made a contribution to the preliminary dynamic investigations of unit type generator-rectifier schemes. Turner[80] introduced iterative coordination between TCS and a multi-machine transient stability program in order to circumvent limitations in representing HVdc links with quasi-steady state models, Watson[87] introduced the frequency-dependent equivalents for harmonic studies. Sankar[91] improved TCS by introducing a flexible controller algorithm and stated the existence of limitations in the steady state formulation for unit connected HVdc systems and showed that characteristics must be derived using a dynamic simulation program. TCS is based on state space theory and uses nodal analysis with diakoptical segregation of the plant components that are submitted to frequent switching. This was done to avoid involving the whole network in unnecessary topological changes.

The possibility of using dynamic simulation results for steady state studies should be considered in the search for the inclusion of non-linearities in the steady state results. The accuracy limits imposed by conventional loadflow steady state de equations is a limiting factor. As an example, for a \( n_p \)-pulses converter bridge, the steady state \( de \) equations are accurate up to a commutation angle of \( \frac{2\pi}{n_p} \) radians. This is one of the reasons to introduce the modelling of TCS to obtain rectifier data that could be used for the loadflow, other reasons were listed by Sankar[91].

In this chapter, the modelling using TCS is discussed to a level necessary to justify its use in obtaining results for this research work. An excitation controller was used to automatically obtain the excitation needed by the unit-connection to match the power delivered by the conventional synchronous generator-rectifier scheme. A section
in this chapter is devoted to the excitation control showing and explaining its schematic diagram, although necessary to get results for the unit-connection this is not one of the final objectives of this contribution. The complete control philosophy in TCS was developed and explained thoroughly by Sankar[91].

This chapter also includes a discussion on the limitations, the modifications required and the possibility of continued use of the conventional ac-dc loadflow formulation. A modified algorithm for varying commutating voltages was developed to be used in the case of direct connected schemes. The attempt was made in order to modify the loadflow technique to calculate the commutating voltage magnitude and phase angle as part of the iterative solution.

2.2 The modelling of HVdc links in the loadflow program

Figure 2.1 illustrates the model of a dc link. At each end are single ac terminals and ac filters connected to the rectifier and inverter. The ac terminals are also part of the ac network. The basic equations and algorithm of a fast decoupled Newton Raphson loadflow are given in Appendices 2.A and 2.B.

Two lossless two winding transformers with on-line tap changers are present in the converter’s terminals. The equivalent parallel combination of converter transformers and also the series combination of two symmetrical bridges can be used in the model.

The following relationships, extracted from Figure 2.1, exist between the variables involved in the static conversion process in each converter[Arrillaga et al., 1983]:
\[ I_s = k \frac{3\sqrt{2}}{\pi} I_d \]  \hspace{1cm} (2.1)

\[ I_p = a \ I_s \]  \hspace{1cm} (2.2)

\[ I_s = \frac{3\sqrt{2}}{\pi} a V_{term} \cos \alpha - \frac{3}{\pi} x_c I_d \]  \hspace{1cm} (2.3)

\[ V_d \ I_d = E \ I_s \cos \phi \]  \hspace{1cm} (2.4)

\[ V_d \ I_d = V_{term} \ I_p \cos \psi \]  \hspace{1cm} (2.5)

\[ I_s = B_t \sin \phi - B_t \ a V_{term} \sin \psi \]  \hspace{1cm} (2.6)

\[ f(V_d, I_d) = 0 \]  \hspace{1cm} (2.7)

where \( B_t \) is the converter transformer leakage susceptance and \( k \) in equation (2.1) is very close to the unity. All quantities are expressed in per unit.

By combining equations (2.2), (2.3), (2.5), (2.6) and using the terminal ac busbar \( (V_{term}) \) as the reference in Figure 2.1, a reduced set of variables and equations has been proposed suitable to investigate the effect of control strategies in current use. The set of variables is:

\[ \bar{x} = [V_d, I_d, a, \cos \alpha, \phi]^T \]  \hspace{1cm} (2.8)

Each converter terminal is therefore represented by a five variable set of nonlinear equations, which are then combined with the ac system equations and solved using either simultaneous or sequential iterative algorithms.

In order to eliminate trigonometrical non-linearity and avoid overflows with infeasible operation modes, \( \cos \alpha_r \) and \( \cos \alpha_t \) are used as variables instead of \( \alpha_r \) and \( \alpha_t \).

If \( dc \) variables are specified they are left in the \( dc \) Jacobian matrix and their values fixed by including the residual equations as in equation (2.9). Appendix 2.D shows the formulation for the rectifier \( ac \) terminal in the \( ac-dc \) loadflow solution.

\[ X_n - \dot{X}_n^{ac} = 0 \]  \hspace{1cm} (2.9)
Thus the flexibility of the algorithm is kept the same for any operating condition.

2.3 The conventional ac-dc loadflow formulation

The inclusion of HVdc transmission in the fast-decoupled loadflow requires considerable modification to be made to ensure that the links are adequately represented. The behaviour of such a link within an ac system or between ac systems is very important and control specifications and steady state operating conditions must be accurately modelled. The active and reactive power balance in the ac nodes connected to HVdc links do not obey the general rules of ac power transmission, i.e. the active power is independent of phase angle relationships and the reactive power, although affected by, is not directly related to voltage variations. Under such circumstances it is sometimes difficult to visualize HVdc models compatible with the behaviour of the fast decoupled algorithm.

2.3.1 Digital methods for ac-dc loadflows

The dc link may be crudely represented in a conventional ac loadflow by fixed active and reactive power injections at the two terminal busbars in the ac system. This has proved to be inadequate for analysis of precise operating modes of the link and its terminal equipment.

As far as the ac system is concerned, early HVdc models represented the converters as current sources on the secondary of the transformers. The ac system was split into two, for the sending and receiving ends dc terminals. The two separated ac ends, with no ac lines in parallel, were solved iteratively with the mesh equations describing the dc system.

The nodal iterative method and the Newton method provided a more effective representation of the dc link as varying real and reactive powers on the terminal busbars of the ac system. The dc system is then solved using the updated voltages giving improved values of terminal dc powers.

Some of the techniques that made the Newton-Raphson a competitive method for loadflow analysis involve the solution of the Jacobian matrix equation and the preservation of the sparsity by optimally ordered triangular factorization. The objective was to obtain a process which requires the least number of operations, time and memory requirements [Bodger, 1977].

For the dc link in Figure 2.1 the dc Jacobian matrix [A] contains 13 variables that can be listed as: $V_{dr}$, $V_{di}$, $E_r$, $E_i$, $\phi_r$, $\phi_i$, $\alpha_r$, $\alpha_i$, $\delta_r$, $\delta_i$, $\psi_r$, $\psi_i$, $I_d$. 
\[ V_{dr} - k_1 E_r \cos \phi_r = 0 \]
\[ V_{di} - k_1 E_i \cos \phi_i = 0 \]
\[ k_1 I_d - B_r (E_r \sin \phi_r - a_r V_{term_r} \sin \psi_r) = 0 \]
\[ k_1 I_d + B_i (E_i \sin \phi_i - a_i V_{term_i} \sin \psi_i) = 0 \]
\[ V_{dr} - k_1 E_r \cos \alpha_r + k_2 x_c, I_d = 0 \]
\[ V_{di} - k_1 E_i \cos \alpha_i + k_2 x_c, I_d = 0 \]
\[ V_{dr} - k_1 a_r V_{term}, \cos \psi_r = 0 \]
\[ V_{di} - k_1 a_i V_{term}, \cos \psi_i = 0 \]
\[ V_{dr} - V_{di} - R_d I_d = 0 \]
\[ V_{di}^{sp} - V_{di} = 0 \]
\[ \cos \alpha_r^{sp} - \cos \alpha_r = 0 \]
\[ \cos \alpha_i^{sp} - \cos \alpha_i = 0 \]
\[ P_{dr}^{sp} - V_{dr} I_d = 0 \]

The above equations solve the dc link with 13 variables present in the dc Jacobian matrix [A], five variables in each end of the link plus a minimum of three control specifications, two in one end and one in another. In this case the last four equations are control specifications. In the equations listed above, the controls are the converter’s firing angles, the dc voltage in the inverter and the power in the rectifier respectively. Any of the dc variables can be specified, as an example fixed transformer taps in the converter, constant firing angles and controlled terminal ac or dc voltages in the converter. Other possible specifications are constant current or power control, terminal reactive power and converter power factor.
With sparsity programming, only the non-zero elements of the Jacobian matrix are stored, in one or more vectors, plus some integer vectors used for identification purposes. As can be seen by the number of null positions in equation (2.10) the ordinary \textit{dc} Jacobian matrix for a two terminal \textit{dc} system in Figure 2.1.

The use of sparsity programming still can clearly improve the performance of loadflow studies by reducing the number of operations with null elements. Concerning the reduction of computation time the triangular factorization also reduces computation effort since the number of additions and multiplications reduces to \( \frac{1}{3} \) to triangulate a full matrix when compared to find the inverse. The nonzero elements in the matrix, with converter control angles, \textit{dc} link voltage and power specified, are given by:
The representation of the converter terminal busbars is very simple (a PV busbar) if there are no tap changers on the transformers and no filters or reactive power injection. If any of these are present, changes are necessary in the ac admittance matrices to represent the varying tap positions. In this case some difficulties and time consuming operations exist when representing CP/CEA or CCC/CEA controls. Optimum tap selection is needed as an active part of the solution to minimize link losses and converter reactive power consumption.

Therefore it is more convenient to include the converter transformer in the \textit{dc} link representation. Consequently the terminal busbars are on the primary \textit{ac} side of the transformers, which isolates the \textit{dc} system from the \textit{ac} network.

Different terminal representations including transformers, filters and synchronous compensators can be solved with the \textit{ac} system represented as a single node. The converter voltage/current characteristics are easily modified for various types of control, as power control, tap control, voltage control and so on. These two aspects are of great importance in the modelling of a synchronous machine directly connected to the converter as discussed in Chapter 5.
2.3.2 The sequential ac–dc algorithm

The three equations already mentioned before are solved iteratively until convergence:

\[ \Delta \tilde{P}/\tilde{V} = [B'][\Delta \phi] \]  \hspace{1cm} (2.11)

\[ \Delta \tilde{Q}/\tilde{V} = [B''][\Delta \bar{V}] \]  \hspace{1cm} (2.12)

\[ \bar{R} = [A][\Delta \bar{x}] \]  \hspace{1cm} (2.13)

The following iterative sequence, referred as P, Q, DC is illustrated in the flow chart of Figure 2.2 and can be explained in a sequence of events as:

\( i. \) Calculate \(\Delta \tilde{P}/\tilde{V}\), solve equation (2.11) and update \(\phi\).

\( ii. \) Calculate \(\Delta \tilde{Q}/\tilde{V}\), solve equation (2.12) and update \(\bar{V}\).

\( iii. \) Calculate dc residuals, \(R\), solve equation (2.13) and update \(\bar{x}\).

\( iv. \) Return to \((i)\).

Some difficulties experienced in the past with the sequential ac–dc algorithm since in this method the dc equations needed to be solved for the entire iterative process. With the residuals converged the dc system can be modelled as fixed real and reactive power injections at the converter terminal busbar. The dc residuals should be checked after each ac iteration to ensure that the dc system remains converged.

An alternative sequential method can be used, it is called as P, DC, Q, DC, where the dc equations are solved after each real power as well as after each reactive power iteration, based in the previous sequence of events the sequence of items for the alternative sequential method is: \((i), (iii), (ii), (iii)\) and \((iv)\). The difficulties mentioned before were overcome by combining the ac and dc describing equations in a simultaneous solution technique[Stott, 1971]. With all equations solved together using the Newton-Raphson method an improvement in convergence reliability and number of iterations was found over the sequential method.

With the sequential ac–dc solution techniques the reliability of solution is very much a function of the iteration scheme used \(\text{between} \ ac \ and \ dc \ systems[\text{Bodger, 1977}]. \) On the other hand, the overall convergence was determined by the speed at which the terminal powers and voltages relaxed towards the solution.
Figure 2.2 Flow diagram for the sequential ac-de loadflow algorithm.
2.3.3 Ac-dc Jacobian matrices

The Jacobian matrices for the conventional fast-decoupled ac-dc loadflow, applied to the conventional two terminal dc link, is given in equations (2.14) and (2.15):

\[
\begin{pmatrix}
\Delta P / V \\
\Delta Q / V \\
\end{pmatrix}
= 
\begin{pmatrix}
B'
AA'
BB''
\end{pmatrix}
\begin{pmatrix}
\Delta \phi \\
\Delta \phi_{r,i} \\
\Delta x
\end{pmatrix}
\]

(2.14)

\[
\begin{pmatrix}
\Delta P / V \\
\Delta Q / V \\
\end{pmatrix}
= 
\begin{pmatrix}
B''
B''_{rr,ii}

\end{pmatrix}
\begin{pmatrix}
\Delta \phi \\
\Delta x
\end{pmatrix}
\]

(2.15)
The non-zero elements of the dc Jacobian matrix \([A]\) in the case of a conventional two terminal dc link were given in the equation (2.10) and related equations, and also the other matrices are given in equations (2.16)-(2.21):

\[
\begin{bmatrix}
\frac{\partial P_{\text{term}}(dc)}{\partial x} / V_{\text{term}}
\end{bmatrix} = \begin{bmatrix}
-a_r B_r \sw_r \\
a_i B_i \sw_i \\
-a_r B_r E_r \cw_r \\
a_i B_i E_i \cw_i \\
-a_r B_r E_r \cw_r \\
-a_i B_i E_i \cw_i \\
-a_i B_i \sw_i \\
E_i B_i \sw_i
\end{bmatrix}
\]  

\[
[AA']^T = \begin{bmatrix}
-a_r B_r \sw_r \\
a_i B_i \sw_i \\
-a_r B_r E_r \cw_r \\
a_i B_i E_i \cw_i \\
a_i B_i E_i \cw_i \\
-a_r B_r E_r \cw_r \\
-a_i B_i E_i \cw_i \\
E_i B_i \sw_i
\end{bmatrix}
\]  

\[
\begin{bmatrix}
\frac{\partial Q_{\text{term}}(dc)}{\partial x} / V_{\text{term}}
\end{bmatrix} = \begin{bmatrix}
a_r B_r \sw_r \\
-a_r B_r E_r \cw_r \\
-a_i B_i E_i \cw_i \\
a_i B_i E_i \cw_i \\
-a_r B_r E_r \cw_r \\
-a_i B_i E_i \cw_i \\
2 a_i B_i V_{\text{term}} - E_i B_i \sw_i
\end{bmatrix}
\]  

\[
[BB'] = \frac{\partial R}{\partial V_{\text{term}}}
\]
or

\[
\begin{bmatrix}
- & - & - & - \\
- & - & - & - \\
- & - & - & - \\
- & - & - & - \\
- & - & - & - \\
\end{bmatrix}
\]

\[BB'' = \begin{bmatrix}
a_i B_i \sin\psi_i & k_1 a_r \cos\psi_r \\
k_1 a_i \cos\psi_i & - \\
- & - \\
- & - \\
- & - \\
\end{bmatrix}
\]  

(2.21)

where \(BB''\) depend on control equations with \(cw_r, cw_i, sw_r\) and \(sw_i\) given by:

\[cw_r = \cos(\psi_r - \phi_r)\]

\[cw_i = \cos(\psi_i - \phi_i)\]

\[sw_r = \sin(\psi_r - \phi_r)\]

\[sw_i = \sin(\psi_i - \phi_i)\]

Matrix \(B''\) for the ac system is constant and symmetric, but \(B''_{rr,ii}\) is a diagonal matrix with elements associated with the rectifier and inverter ac busbars. Equation (2.22) shows the term \(B''_{rr}\) for the rectifier as used in the matrix. This is derived from the reactive power flow across the converter transformer. Similar equation can be derived for the inverter term \(B''_{ii}\).

\[B''_{rr} = B''_{rr(nc)} + \frac{\partial Q_r(dc)/V_{term_r}}{\partial V_{term_r}}\]  

(2.22)

where \(Q_r(dc) = a_r^2 B_r V_r^2 - a_r B_r E_r V_{term_r} \cos(\psi_r - \phi_r)\).

Matrices \(AA', AA'', BB''\) illustrated in equations (2.16)–(2.21) above are non-symmetric with elements varying at every iteration. All these matrices are very sparse as can be seen in the dc Jacobian matrix in equation (2.10). However, equation (2.21) shows matrix \(BB''\) to be dependent on control equations.

However, all the matrix equations described in this section are suitable only for an ordinary two terminal dc link. In the direct connection the use of such formulation must be reconsidered. The difference is in the rectifier commutation process since, without ac
filters, it represents a line-to-line short circuit across the generator terminal and hence the commutation reactance must include the generator sub-transient reactance and also the transformer leakage. The above must be considered when the conventional algorithm is adapted for use in the unit-connection problem.

2.4 Adaptation of the loadflow for use with the unit-connection

Normally in the direct-connection the synchronous machine is coupled directly to the converter transformers and there are no ac filters. In this condition the synchronous machine will be under the effect of at least, the characteristic harmonics even in the case of balanced converter operation [Arrillaga et al., 1985].

All complexity in this case is centred in the ac-dc converter. In the averaged value dynamic modelling of the twelve pulse bridge directly connected to a six phase synchronous machine, a mutual reactance was found between the two six pulse bridges. This configuration is generally used for aircraft power generation. The consequence is that in addition to the notches caused in one bridge by its own commutations, the commutations in the other bridge will produce notches in the voltage waveforms in the first bridge, and vice versa, generating a cross effect [Sudhoff, 1992a]. The same effect is expected to occur in a three phase machine connected to a twelve-pulse converter. The notches in the voltages applied to the bridges have no effect at all on the waveform of the rectified voltage. Nevertheless they play an important role in the commutation process. In fact, the crossings of the distorted voltage waveforms applied to the bridges are delayed in respect to the crossings of the generally accepted undistorted voltage $E''$.

The direct connected generation for HVdc applications need a detailed representation of the converter transformer, with the model used in TCS was not possible to obtain the combined effect of waveform notches and transformer harmonic cross coupling [Rios, 1992] over the two six pulse bridges [Sankar, 1991]. The additional effect of transformer harmonic cross coupling is not present in low power applications since the synchronous machine is connected to the converter without the need for a converter transformer [Sudhoff and Wasynczuk, 1992].

In the absence of filters the voltage at the converter terminal is not sinusoidal and can not be used as the commutating voltage in the conventional formulation. Instead the use of the generator internal $emf$ behind sub-transient reactance ($E''$) is commonly accepted.

In the case of a non-salient rotor machine the fast decoupled loadflow formulation described in section Appendix 2A should be applicable by shifting the converter interface to a fictitious terminal (the internal $emf$ behind sub-transient reactance) where the waveform
is assumed sinusoidal.

Although $\bar{E}$ is neither accessible nor directly controllable and varies with the load, its magnitude can be derived from the generator's sub-transient phasor diagram for the nominal operating point and then kept constant (as shown by dotted lines in Figure 2.3). This is done to satisfy the generator's conventional loadflow specification (i.e. constant voltage) but at the fictitious internal $\bar{E}$ bus. The conventional loadflow is then carried out on the assumption of perfect filtering at that point.

With salient-pole generators the conventional formulation is often used by averaging the sub-transient reactances, i.e. $X'' = \frac{(X''_d + X''_q)}{2}$ and also by shifting the converter interface as in the case of non-salient rotor machine. So far the error of such an approximation has not been discussed, due to the vast number of variables that contribute to differences between steady state and dynamic related formulations. The following work was done in order that the effects of saliency could be included in the loadflow model.

Some of the constraints in the fast decoupled $ac$-$dc$ loadflow applied to unit-connection are:

i. the synchronous machine busbar was split in two busbars to simultaneously find a reference bus where the voltage waveform is supposed to be sinusoidal and to use the sub-transient voltage busbar as the converter interface.

ii. the accuracy of $dc$ equations are restricted to commutation angles lower than $\frac{2\pi}{n_p}$ were $n_p$ is the number of pulses in the converter.

iii. the averaging of the direct and quadrature subtransient reactances in the representation of synchronous machine.

iv. the difficult representation of non-linearities, in the steady state algorithms, due to the commutation and conduction periods of the converter.

In the case of a two terminal $dc$ link represented by the conventional loadflow the converter transformer taps are part of the variables describing the link. Previous work in the unit-connection described the need for an OLTC (On Line Tap Changer) at the rectifier transformer for a unit-connection operating under constant speed[Sankar, 1991].

The generator excitation control can easily take over the task of an OLTC. Tappings are not required on the rectifier transformer and hence the variable $a_r$ must be maintained at its nominal value when representing the unit-connection using the conventional loadflow. And also in this case the fast-decoupled $ac$-$dc$ technique shows a number of $ac$ iterations at most 50% bigger than for the $ac$ system alone. Slightly more $dc$ iterations are required, however.

This section shows the $ac$-$dc$ loadflows extended by the simultaneous solution of $dc$ equations with the fast-decoupled Newton method. However both (simultaneous
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and sequential) algorithms can also be applied with the required adaptation to the unit connected generation problem.

2.4.1 \textbf{Modified algorithm for varying commutating voltages}

As already indicated, the internal emf behind sub-transient reactance is not directly controllable. Instead, the generator excitation will be controlled to provide the specified firing angle ($\alpha_{\text{min}}$).

Therefore the commutating voltage ($E''$) will not be known in advance and its magnitude and phase angle ($\beta$), as shown in Figure 2.3, must be derived as part of the iterative solution.

The conventional vector equation (2.8) in the rectifier end is replaced by:

$$\vec{v} = [V_d, I_d, \cos \alpha, \phi, E, E'', \beta]^T$$

Therefore seven residual equations are needed to formulate the loadflow problem at the rectifier end [Arrillaga \textit{et al.}, 1992c].

The first two equations are common to the conventional model (with the tap ratio variable removed). Therefore:

$$V_d - k_1 E'' \cos \phi = 0$$
where \( k_1 = \frac{3\sqrt{3}}{\pi} \).

The next two are derived from the generator sub-transient phasor diagram of Figure 2.3. That is:

\[
E - E'' \cos \beta - (x - x'') | I_p | \sin(\beta + \phi) = 0
\]  
\[
E'' \sin \beta - (x - x'') | I_p | \cos(\beta + \phi) = 0
\]

In Chapter 4 the operational modes for a more accurate model will be discussed. However, in this case, the modifications made to adapt the loadflow to the unit-connection don’t change the operational modes and they are the same as in an ordinary dc link [Arrillaga et al., 1983], except that \( a_r \) now will be always specified as unity. This introduces a limit in the number of variables to be specified, instead of four described above, three will be the minimum set, i.e. one in the rectifier and two in the inverter.

A typical selection is:

\[
I_d - I_{dp} = 0
\]

\[
E - E_{dp} = 0
\]

\[
\cos \alpha - \cos \alpha_{\text{min}} = 0
\]

The three specifications where chosen as an example of a minimum set and also to show the possibility in the model to obtain results with the internal \( emf \), the minimum firing angle and the current in the link simultaneously specified.

The described modifications to the fast-decoupled loadflow were basically efforts to linearize the dc model which has many non-linear components. The operation modes of the dc link has been studied thoroughly using dynamic simulation with state-variable technique approach (TCS) [Sankar, 1991]. The dynamic simulation of the unit-connection allows one to obtain the accurate steady state for each desired operation conditions by averaging the waveforms obtained. A further idea was to develop rectifier capability charts using TCS to incorporate the effects of non-linearities in the rectifier to be used in the
unit-connection. So the Transient Converter Simulation program is the source of steady state data for the rectifier in a more accurate algorithm.

2.5 The TCS model

The TCS program uses a state space formulation to calculate linear and non-linear circuits. The program is very flexible and accepts almost any degree of ac systems and converter systems representation to meet the requirements of any particular study, e.g. determination of steady-state operation, small disturbances, faults, etc.

The non-linear differential equations in TCS are based in the representation of the power system by a network of resistive, inductive and capacitive branches. The state variables in TCS are related to the energy stored in the magnetic fields of inductors and in the electric fields of capacitors, while resistors are passive elements. Nodes are divided into three categories depending on the kind of elements are connected to them:

i. α nodes: nodes with at least one capacitive branch connection.

ii. β nodes: nodes with at least one resistive branch connection but no capacitive branch connected.

iii. γ nodes: nodes with only one inductive branch connected.

The network is described and calculated in TCS based in node voltages and branch currents according respectively to Kirchoff's voltage and current law. The network response is function also of algebraic and topological constraints, what elements are connected in a branch and how the branches are interconnected (branch characteristics).

The description of the network is made by a set of first order differential equations that describe these algebraic and topological characteristics. These group of equations derived from the previous rules constitutes the state variable technique. Using this approach the network is defined by following state equations:

\[ \dot{x} = (A)x + (B)u + (E)z \]

\[ y = (C)x + (D)u \]

where

i. \( x \) is the vector of state variables.

ii. \( y \) represents the output voltages and currents.

iii. \( z \) represents a set of dependent variables.

iv. \( u \) represents the input voltage and currents.
The dependent variables are calculated by solving the equation:

\[ \dot{z} = [F]z + [G]u \]  \hspace{1cm} (2.33)

The matrices \([A], [B], [C], [D], [E], [F]\) and \([G]\) are the coefficient matrices compatible in each case with the appropriate vectors, these matrices can be time varying and non-linear functions of \(z\) or \(u\).

The advantage of the state variable technique is the flexibility to numerical methods of analysis and the simplicity to incorporate most types of power systems non-linearities. Non-linearities are easily handled by TCS and they are normally function of current or voltage magnitude and time.

2.5.1 The integration method

The integration algorithm in TCS was chosen based in two important considerations:

\(i\). At each switching operation (twelve per cycle per bridge) extra calculations are required to restart a multi-step method.

\(ii\). Due to the harmonic content the step length should be small, in this case a simple method can be stable and sufficiently accurate.

The chosen method was the trapezoidal implicit integration method. Other methods tested had shown propagation of truncation errors by showing a constant drift in some variables when the system reach steady-state. More complex integration methods, although more accurate, can be unstable when the initial conditions are not perfectly known.

Assuming that:

\[ \dot{y} = f(t, y) \]  \hspace{1cm} (2.34)

where, \(y\) represents the state variable vector and \(t\) is the time.

Using the trapezoidal integration method the change in the state variable vector \(\Delta y\) for each time step is equal to the integral of the area under its derivative:

\[ \Delta y = \frac{h}{2} (\dot{y}_{t+h} + \dot{y}_t) \]  \hspace{1cm} (2.35)

Equation (2.35) is a non-linear equation in \(y_{t+h}\). Depending of the characteristic of each problem it can be solved by direct or iterative methods. In TCS was implemented the very simple trapezoidal iterative scheme with the calculations for the non-state variables kept in separate stages and \(y_{t+h}\) is determined iteratively as follows:

1. Assume \(\dot{y}_{t+h} = \dot{y}_t\) to get the first estimate.
2. Calculate an estimate \( y_{t+h} \) based on \( \dot{y}_{t+h} \) estimate.

\[
y_{t+h} = y_t + \frac{h}{2} (\dot{y}_{t+h} + \dot{y}_t)
\]  

(2.36)

3. \( \dot{y}_{t+h} \) is estimated from the current \( y_{t+h} \) value by:

\[
\dot{y}_{t+h} = f(y_{t+h}, t+h)
\]  

(2.37)

4. The two previous steps are performed iteratively until convergence is obtained. Convergence is reached when all state variables satisfy:

\[
\varepsilon \geq |y_{t+h}^{j+1} - y_{t+h}^j|
\]

where, \( \varepsilon \) is the state variable convergence tolerance. In TCS the trapezoidal integration has an additional convergence constraint which is specified to ensure the convergence of the state variable derivatives;

\[
\varepsilon_d \geq |\dot{y}_{t+h}^{j+1} - \dot{y}_{t+h}^j|
\]

where, \( \varepsilon_d \) is the state variable derivative convergence tolerance.

In general a maximum step length of approximately one degree of fundamental frequency will be sufficient to accurately represent the converter generated harmonics up to 2,500 Hz. Depending on the convergence performance the integration step length is automatically adjusted at each integration step to ensure fast convergence.

2.5.2 Topology changes

The instant of occurrence of parameter value or network connection changes need to be determined precisely to achieve an accurate simulation. There are two types of topological changes: those which are detected only after the event and those that can be predicted before the event. For example, the time for switching on a converter valve and the fault application are easily determined before the event. In these cases the simulation step size can be increased or lowered and the simulation step falls exactly on the required time the change is due to take place. Since the state space coefficient matrices are not functions of the step size, changing it does not impose a dramatic computational burden.

On the other hand the time for zero current in converter valves and fault branches are not easy to predict before they occur. Hence, the program detects these instants after they occur and linear interpolation, backwards in time, is used to reach the time of switching (at zero crossings). In this situation the state variables are also obtained
by linear interpolation rather than using trapezoidal integration. Due to the small simulation step size the linear interpolation gives sufficient accuracy. Trapezoidal integration is an A-stable method which despite its second order accuracy is superior to higher order Runge-Kutta integration methods.

2.5.3 Per unit system

In power systems analysis per unit quantities are normally used rather than actual values. The program treats voltages, currents and impedances with the same degree of accuracy by scaling all to the same relative order. Dynamic analysis evaluate instantaneous phase quantities and their derivatives, some variables may change relatively fast and a large difference may take place between the order of a variable and its derivative.

Considering the sinusoid:

\[ x = A \sin(\omega t + \phi) \]  
\[ \dot{x} = \omega A \cos(\omega t + \phi) \]

It is very clear that the relative difference in magnitude between \( x \) and \( \dot{x} \) is \( \omega \), which may be substantial in some cases. Therefore a base frequency \( \omega_0 \) is defined. Then the state variables are changed by a factor \( \omega_0 \) and this requires the formulation of reactance and susceptance instead of inductance and capacitance matrices, with the integration now being with respect to electrical angle rather than time.

Equations (2.40) and (2.41) give the formulation for flux in the inductors and energy stored in the capacitors, both state variables in TCS.

\[ A_k = \omega_0 \lambda_k = (\omega_0 l_k).I_k = X_{ik}.I_k \]  
\[ Q_{ck} = \omega_0 q_c = (\omega_0 c_k).V_k = B_{ck}.I_k \]

where:

- \( l_k \) is the inductance
- \( X_{ik} \) is the inductive reactance
- \( c_k \) is the capacitance
- \( B_{ck} \) is the capacitive susceptance
- \( \omega_0 \) the base angular frequency
2.5.4 Initial conditions

At the start of the simulation the state variables require initial values. To be correct, the simulation should start from a de-energized state, however, a problem in this case can be the computationally prohibitive start-up time. Results from a loadflow program are better initial conditions. In the case of direct connected schemes the loadflow can not provide exact initial conditions since detailed synchronous machine and converter modelling are involved. In the absence of filters the set up of initial conditions become very critical since the initial conditions from a loadflow is a fundamental frequency analysis only.

A preliminary dynamic simulation uses a suitable loadflow starting point. Even with these known approximations it is time consuming to obtain the desired steady state operating point. Once obtained this information can be stored and used as initial conditions for future dynamic analysis of the system. This preliminary study provides dynamic initial conditions.

The initial conditions can be evaluated from the magnitude of the initial transient before the system get to the steady state operating point. The quality of the initial conditions is assessed by performing the FFT (Fast Fourier Transform) on the resulting steady state waveforms. This practice allows the inspection of fundamental and harmonics of voltages and currents. The practice shows that dynamic initial conditions are always better than the user entered loadflow data. This is very important for direct-connection simulation since the dynamic initial conditions contains harmonic information as opposed to magnitudes and angles at nominal frequency only.

Along with the initial conditions, the controls also should be carefully assessed since they contribute in speed and accuracy to obtain the correct operating point. The HVdc controller dynamics have been included in TCS [Sankar, 1991] as user defined modular control system blocks with arithmetic, logic and transfer functions.

2.6 HVdc controls in TCS

This feature enables the simulation of feedback controllers in a separate data file and was an important contribution to dynamic simulation by improving the representation of the control system. The accurate quantitative prediction of converter and ac-dc system stability requires not only a time domain simulation of the complete system but the representation in detail of the controls and power systems components.

The HVdc controls in TCS follow a strict hierarchical organization and they are classified as:

i. Thyristor and valve control.
ii. Converter control.

iii. Pole control.

iv. Bipole and station control.

The HVdc controls in TCS are very flexible and can be changed completely from one application to another. During the test case design it was necessary to try a number of different control possibilities, with different combinations of control parameters. The closed loop responses (time and frequency) of the control depend directly on the size and type of the synchronous machine and converter in the system.

As well as the rectifier current and extinction angle controllers, a synchronous machine excitation control should be used in the direct-connection case. The reason for this extra control is that the excitation required by the synchronous machine in the direct connected configuration must be correct for the specified operation of the generator-converter. The excitation can not be predicted in advance.

2.6.1 The synchronous machine excitation control

The control system to change the excitation in the unit connect a generator-converter to give the same power as in the conventional HVdc generation scheme is shown in Figure 2.4.
A detailed block diagram and data of the excitation control is given in Figure 2.5, where \( V_d \) is the measured rectifier dc voltage and \( V_d^{sp} \) is the desired (specified) dc voltage. The control scheme is turned on after a delay of another 0.1 radians (approximately 10 cycles) enough for the system to reach the required steady state operation point. EFD1 is a constant initial excitation and EFD0 is the excitation obtained with the feedback control enabled. This is the practice even when the simulation starts from a dynamic initial condition.

Note that the excitation system modelled here does not have to accurately represent the actual excitation system used by the generator when it is used for the purposes of this work. Its inclusion is only necessary to ensure an accurate steady state operating point.
Figure 2.5 Schematic diagram of the excitation control.

- $\lim u = 2.0$
- $\lim l = 0.5$
- $K_p = 1.3957$
- $K_i = 0.3258$
- $EFD_1 = 1.0$
- $T = 6.283$ rad.
- $TT = 32$ rad
Appendix 2A  Fast decoupled loadflow

The generalized Newton-Raphson solution method based on decoupled principle is largely used for the solution of the non-linear equations involved in loadflow calculation. Appendix 2.B shows the basics of Newton-Raphson solution method.

Further developments and assumptions from the physical properties of a practical system, the both real and imaginary parts of the Jacobian in the decoupled Newton loadflow can be made constant in value. The meaning is that they need to be triangulated only once per solution and for a particular network topology.

The real and reactive power equations in an arbitrary node $i$ connected to a node $j$ in the network are reproduced in the following equations:

$$ P_i = V_i \sum_{i,j} V_j (G_{ij} \cos\phi_{ij} + B_{ij} \sin\phi_{ij}) \quad (2.42) $$

$$ Q_i = V_i \sum_{i,j} V_j (G_{ij} \sin\phi_{ij} + B_{ij} \cos\phi_{ij}) \quad (2.43) $$

where $\phi_{ij} = \phi_i - \phi_j$.

The loadflow can be simplified by directly relating voltages to powers through the utilization of series approximations in the equations (2.42) and (2.43) [Peterson et al., 1972], where $\sin\phi = \phi - \frac{\phi^3}{6}$ and $\cos\phi = 1 - \frac{\phi^2}{2}$.

The equations, for all buses, can be expressed in a simplified matrix formulation as:

$$ [A] [\phi] = [P] \quad (2.44) $$

$$ [C] [V] = [Q] \quad (2.45) $$

where $P$ and $Q$ are the terms of real and reactive power respectively and

$$ A_{ii} = V_i \sum_{i\neq j} V_j B_{ij} $$

$$ A_{ij} = -V_i V_j B_{ij}, \quad i \neq j $$

$$ C_{ii} = V_i \sum_{i\neq j} t_{ij} B_{ij} $$

$$ C_{ij} = -B_{ij}, \quad i \neq j $$
$t_{ij}$ is the tap ratio if a transformer is in the branch between nodes $i$ and $j$.

A possible modification which also lead to the fast decoupled method is to convert the equation (2.44) to:

$$[\hat{A}] [\hat{\phi}] = [\hat{P}]$$

where

$$\hat{A}_{ii} = \sum_{k \neq j} B_{ij}$$

$$\hat{A}_{ij} = -B_{ij}, \ i \neq j$$

$$\hat{\phi}_i = \phi_i \times V_i$$

$$\hat{P}_i = P_i/V_i$$

and $[\hat{A}]$ becomes constant in value.

A similar direct method can be obtained from the decoupled voltage vectors in the two following equations:

$$[I] = [T] [\phi]$$  (2.46)

$$[J] = [U] [V - V_0]$$  (2.47)

where for the slack node $\phi_0 = 0$ and $V_i = V_0$. The values of $I_i$ and $J_i$ give the real and reactive power and $[T]$ and $[U]$ are given by

$$T_{ij} = \frac{V_i V_j}{z^2_{ij}/X_{ij}}$$  (2.48)

$$T_{ii} = -\sum_{j \neq i} T_{ij}$$  (2.49)

$$U_{ij} = -\frac{1}{z^2_{ij}/X_{ij}}$$  (2.50)

$$U_{ii} = -\sum_{j \neq i} T_{ij}$$  (2.51)

In this case $[U]$ is constant and need be triangulated only once for a solution. $[T]$ is recalculated and triangulated each iteration. Equations (2.46) and (2.47) are solved alternately until a solution is obtained.
Equations (2.46) and (2.47) can be solved by expressing the Jacobian equations as

\[
\begin{pmatrix}
\Delta P \\
\Delta Q/V
\end{pmatrix} =
\begin{pmatrix}
T & K \\
U & L
\end{pmatrix}
\begin{pmatrix}
\Delta \phi \\
\Delta V
\end{pmatrix}
\]

(2.52)

where \([\Delta P] = [\Delta J]\) and \([\Delta Q/V] = [\Delta J]\). \(T\) and \(U\) are defined in equations (2.48)–(2.51).

In equation (2.52) the values of \(K\) are very small and can be neglected, then:

\[
[\Delta P] = [T] [\Delta \phi]
\]

(2.53)

\[
[\Delta Q/V] = [U] [\Delta V]
\]

(2.54)

which is the decoupled version.

If \(V_i\) and \(V_j\) are made equal to 1.0 p.u., \([T]\) becomes constant and need be triangulated only once. The same simplification can also be applied to the decoupled voltage vectors and Newton’s method in equations (2.53) and (2.54).

The most used decoupled loadflow is that based on the Jacobian for the Newton method[Stott, 1972].

\[
\begin{pmatrix}
\Delta P \\
\Delta Q/V
\end{pmatrix} =
\begin{pmatrix}
H & N \\
J & L
\end{pmatrix}
\begin{pmatrix}
\Delta \phi \\
\Delta V
\end{pmatrix}
\]

(2.55)

where the submatrices \(H, N, J, L\) form the complete Jacobian matrix.

For the decoupled algorithm the sub-matrices \(N\) and \(J\) are neglected, they represent a weak coupling between \(P - \phi\) and \(Q - V\), then from equation (2.55) the following decoupled equations can be obtained.

\[
[\Delta P] = [H] [\Delta \phi]
\]

(2.56)

\[
[\Delta Q] = [L] [\Delta V]
\]

(2.57)

It is known that the previous equations are unstable when far from the exact solution due to non-linearities in the defining functions. Improvement in the convergence can be obtained using a technique proposed by Stott[71] to replace equation (2.57) by the current-mismatch formulation in the following equation.

\[
[\Delta I] = [U] [\Delta V]
\]

(2.58)
Dividing the right hand side of equations (2.56) and (2.57) by voltage magnitude \( V \):

\[
[\Delta P/V] = [A] [\Delta \phi]
\]

(2.59)

\[
[\Delta Q/V] = [C] [\Delta V]
\]

(2.60)

The equations are solved iteratively using the most up to date values of \( V \) and \( \phi \) available. The matrices \([A]\) and \([C]\) are sparse, non-symmetric in value and both functions of \( V \) and \( \phi \).

To further speed up the solution more approximations have been adopted resulting in an algorithm generally referred to as the fast decoupled loadflow.

The following assumptions can be made:

i. \( E_i, E_j = 1.0 \) p.u.

ii. \( G_{ij} \ll B_{ij} \), hence \( G_{ij} \) can be ignored, in general for the transmission lines the reactance/resistance ratios are bigger than unity.

iii. \( \cos(\phi_i - \phi_j) \approx 1.0 \) and \( \sin(\phi_i - \phi_j) \approx 0.0 \) since angle differences across the transmission lines are small under normal loading conditions.

This leads to:

\[
[\Delta P] = [\tilde{B}] [\Delta \phi]
\]

(2.61)

of order \((N-1)\)

\[
[\Delta Q] = [\tilde{B}] [\Delta V]
\]

(2.62)

of order \((N-M)\), where the elements of \( \tilde{B} \) are

\[
\tilde{B}_{ii} = \sum_{ij} B_{ij}
\]

\[
\tilde{B}_{ij} = -B_{ij}, \ i \neq j
\]

and \( B_{ij} \) are the imaginary parts of the admittance matrix, \( N \) is the number of nodes and \( M \) is the number of branches. Further simplification can be made by neglecting branch resistances in the calculation of the elements of \( \tilde{B} \).

An improvement over equations (2.61) and (2.62) is based on the decoupled equations (2.59) and (2.60) which has less non-linear defining functions. Applying the same assumptions listed before, the following equations can be obtained
A number of improvements make this method very successful:

i. the elimination from Jacobian in equation (2.63) of the representation of the network elements that predominantly affect the reactive power flow, e.g. shunt reactances and off-nominal in-phase transformer taps. The series resistances of lines is also neglected.

ii. the elimination from the Jacobian in equation (2.64) of the effect of phase shifters.

The resulting fast decoupled loadflow equations are then,

\[
[\Delta P/V] = [B'][\Delta \phi] 
\]  \hspace{1cm} (2.65)

\[
[\Delta Q/V] = [B''][\Delta V] 
\]  \hspace{1cm} (2.66)

where

\[ B'_{ii} = \sum_{i \neq j} \frac{1}{X_{ij}} \]

\[ B'_{ij} = -\frac{1}{X_{ij}}, i \neq j \]

\[ B''_{ii} = \sum_{i \neq j} B_{ij} \]

\[ B''_{ij} = -B_{ij}, i \neq j \]

The matrices \( B' \) and \( B'' \) are real and are of order \((N-1)\) and \((N-M)\) respectively. \( B'' \) is symmetric in value and so is \( B' \) if phase shifters are ignored. The elements of the matrices are constant and need to be evaluated and triangulated only once for a network.

Changes in system configuration easily affect the solution speed, and while adjusted solutions take many more iterations these are short in time and the overall solution time is still low[Bojger, 1977]. Figure 2.6 shows the flow diagram for the fast decoupled loadflow.

The fast decoupled loadflow can be used in optimization studies for a network and is particularly useful for accurate information of both real and reactive power for multiple loadflow studies, as in contingency evaluation for system security assessment. Hence, its application has been extended to the monitoring and control in real-time of voltages, real and reactive power flow in the system.
Figure 2.6 Flow diagram for the fast decoupled loadflow algorithm.
Appendix 2B  The Newton-Raphson Solution Method

This solution method is an iterative procedure which enables a vector, \( \bar{x} \), to be found which satisfies the equation:

\[
F(\bar{x}) = 0 \tag{2.67}
\]

Considering the single variable case of:

\[
f(x) = 0 \tag{2.68}
\]

then from an approximation \( x_i \), at any iteration \( i \), which is in error \( \Delta x_i \) from the true solution, we can state:

\[
f(x_i + \Delta x_i) = 0 \tag{2.69}
\]

Enlarging the previous equation using Taylor's theorem provides:

\[
f(x_i + \Delta x_i) = f(x_i) + \frac{(\Delta x_i)^1}{1!} f'(x_i) + \frac{(\Delta x_i)^2}{2!} f''(x_i) + \ldots \tag{2.70}
\]

If the error \( \Delta x_i \) is small then the terms in \( (\Delta x_i)^n \), where \( n > 1 \) and integer, can be neglected. Consequently:

\[
f(x_i) + (\Delta x_i)f'(x_i) = 0 \tag{2.71}
\]

or

\[
\Delta x_i = -\frac{f'(x_i)}{f(x_i)} \tag{2.72}
\]

A new estimate for variable \( x_i \) can be obtained from:

\[
x_{i+1} = x_i + \Delta x_i \tag{2.73}
\]

Equation (2.71) is usually written as

\[
f(x_i) = -J \Delta x_i \tag{2.74}
\]

In the multi-variable case with \( N \) equations in \( N \) unknowns, \( \bar{x} \) is a vector of dimension \( N \) and \( J \), the Jacobian, is a square matrix \( N \times N \) of first order partial differentials of the functions \( F(\bar{x}) \). The Jacobian represents the slopes of the tangent hyperplanes which approximate the function \( F(\bar{x}) \) at each iteration [Stott, 1971] and its elements are defined by:

\[
J_{km} = \frac{\partial F_k}{\partial x_m} \tag{2.75}
\]
The method works by estimating values of $\bar{x}_i$, calculating $F(\bar{x}_i)$ and $[J]$ from these estimates, solving for $[\Delta \bar{x}_i]$ by:

$$[\Delta \bar{x}_i] = -[J]^{-1} F(\bar{x}_i)$$  \hspace{1cm} (2.76)

and using $[\Delta \bar{x}_i]$ to obtain a better estimate of $[\bar{x}_i]$. This process is iterated to convergence which is determined when $[\Delta \bar{x}_i]$ is less than a given tolerance.
Appendix 2C  Component models in TCS

The program is backed by a large library of power systems models. The component models used in the test system by the program are:

- Transformers.
- Equivalent ac system sources.
- Static shunt elements.
- Static converter.
- Detailed synchronous machine model.

(1) Transformers

The traditional transformer equivalent circuit involving a series impedance is not suitable for a full three phase dynamic analysis because different magnetic circuits offer different impedances to the current components, and the phase shifts inherent in the different connections need to be present, especially when it is coupled to static converters.

Figure 2.7 represents a single phase transformer as it is used in TCS. Neglecting iron losses, the transformer can be represented basically of two magnetically coupled coils represented in terms of self \((L_{11}, L_{22})\) and mutual \((L_{12}, L_{21})\) inductances, the basic elements of a three phase transformer. The matrix equation for the model is:

\[
\begin{bmatrix}
  V_1 \\
  V_2
\end{bmatrix}
= \begin{bmatrix}
  L_{11} & L_{12} \\
  L_{21} & L_{22}
\end{bmatrix}
\frac{d}{dt}
\begin{bmatrix}
  I_1 \\
  I_2
\end{bmatrix}
+ \begin{bmatrix}
  R_1 & 0 \\
  0 & R_2
\end{bmatrix}
\begin{bmatrix}
  I_1 \\
  I_2
\end{bmatrix}
\]

(2.77)

The numerical relationship (in p.u.) between the inductance and the leakage reactance \((X_{la})\) in the model is:

\[
L_{12} = L_{21} = \sqrt{L_{22}(L_{11} - X_{la})}
\]

(2.78)

The three-phase transformer model with a Y – Y connection shows equal self inductances as are the mutuals which is not true in the case of a Y – Δ connection [Watson, 1987].

The generator transformer and the converter transformer data for the systems used are given in Appendices 3.A and 5.A.
(2) Equivalent ac system sources

Sometimes due to the size of the power system involved in the simulation it is necessary to use an ac equivalent to lessen the complexity. But the ac system equivalent should still be sufficiently accurate, a number of options are already available[Watson, 1987]. An ac system equivalent model, as in Figure 2.8, was used as an ac source in the inverter side of the HVdc link to the proposed direct connected scheme.

(3) Static shunt elements

Static series elements are used in this work to represent the presence of filters in the conventional ac-dc connection of a synchronous generator and converter. Figure 2.9 show the types of shunt and series elements used to build the required system, other types of component models are also available in TCS[Watson, 1987].

(4) Static series elements

These elements follow the same representation as in the shunt elements, with the exception that the components are connected between phases. This make possible the representation of 3 connected loads, circuit breakers, smoothing reactors and other components. Topology constraints from TCS structure[Barros, 1976] must be taken in account, when using these elements.
(5) Static converters

The basic TCS converter model is formed by a converter transformer a six pulse Graetz bridge configuration with a smoothing reactor (Barros, 1976). The valves, in the converter model, are represented by switches which connect the smoothing reactor with the converter transformer secondary according to the valve conduction states.

Six and twelve pulse converter models are available, the 12 pulse bridge consists of two six pulse bridges connected in series in the dc side. Connection to the ac side is made through a $Y - Y / Y - \Delta$ converter transformer respectively. This arrangement provides the $30^\circ$ phase shift required in a 12 pulse operation.

(6) Detailed synchronous machine model

The terminal voltage is far from sinusoidal in the presence of a converter, in this case phase-variable representation is necessary. The handling of high order harmonics,
Figure 2.9 Representation of filters as static shunt and series elements in TCS.

Figure 2.10 Synchronous machine model.
asymmetries, distortion effects and non-linearities become in this case an important issue, since they are present in real machines, problems which the state variable model can handle easily. Avoiding the transformation to three phase quantities at each time step counterbalances the extra calculations needed by the time varying inductances in the phase-variable model.

Manufacturer's data in the two axis d-q-o form must be transformed to phase quantities a, b and c. Although d-q-o model is retained for the rotor circuitry fitting the winding arrangement and geometry of the machine. The rotor position is specified relatively to phase a with the anti-clockwise direction of rotation of the field specified as positive.

When writing differential equations, the machine is treated as a motor and the terminal voltage may then be expressed in matrix form as follows [Barros, 1976]:

\[ V_g = \frac{d}{dt}(L_q I_q) + R_g I_q = L_q \frac{d}{dt} I_q + \omega \frac{d}{d\theta} L_q I_q + R_g I_q \]  

(2.79)

where:

\[ \omega = \frac{d\theta}{dt} \] is the angular velocity

\[ [L_q] = [L_{\theta_1}] \] is a \(6 \times 6\) matrix,

and:

\[ \frac{d}{d\theta} L_q = [G] \] which is named in the literature as the torque matrix.

The dimension of the machine model is dependent on the number of damper circuits to be included in the rotor representation. For dynamic modelling two damper circuits can be implemented, one aligned with the quadrature axis and the other with the direct axis.
Appendix 2D  Formulation for the rectifier ac terminal

An accurate power flow solution of the Unit-Connection scheme requires the use of time domain calculated rectifier characteristics[Arrillaga et al., 1992b]. However for a preliminary comparison of the characteristics of Unit and Group connected schemes the conventional ac-dc steady state power flow provides all the relevant information. By assuming that all the generators in service have identical characteristics and are equally loaded, they can be represented as a single equivalent machine. With reference to Figure 2.11 the fictitious bus behind the machine’s sub-transient reactance is used as the slack busbar on the rectifier side of the load flow solution and the sub-transient voltage is used as the commutating voltage for the converter. However the commutating voltage \(E''\) is not known in advance and its magnitude and phase are obtained as part of the ac-dc load flow solution.

The vector of independent variables of the converter model is:

\[
\bar{x} = [V_d, I_d, a, \cos \alpha, \phi]^T
\]  

(2.80)

Those are interrelated by the following residual equations:

\[
R(1) = V_d - k_1 a E'' \cos \phi
\]  

(2.81)

\[
R(2) = V_d - k_1 a E'' \cos \alpha + \frac{3}{\pi} I_d x_c
\]  

(2.82)

\[
R(3) = f(V_d, I_d)
\]  

(2.83)

\[
R(4) = control\ equation
\]  

(2.84)

\[
R(5) = control\ equation
\]  

(2.85)

where \(k_1 = 3\sqrt{2}/\pi\).

Valid converter control specifications are: converter transformer tap, dc voltage, dc current, minimum firing angle and dc power transmission.

To solve for the five variables of equation (2.80) two of them must be specified. In the absence of on-load tap changer in the rectifier side, the tap is considered fixed and the variable \(a\) is specified as unity. The second specification will normally be the dc power. However, once the control angle \(\alpha\) reaches the minimum level \(\alpha_{min}\), this value should be fixed and becomes the second specification instead of the power. With fixed firing angle the possibility to control the power by increasing or lowering \(\alpha\) will be lost[Kimbark, 1971]. The incorporation of the converter equations into the ac-dc power flow solution is well documented and is based in the algorithm described in Arrillaga[83].
Figure 2.11 Rectifier ac side for the unit-connection.
Chapter 3

OPERATING CHARACTERISTICS OF DIRECT CONNECTED PLANTS

3.1 Introduction

Among the many possibilities of connecting groups of generator-HVdc converter three have been the object of analysis and economic comparisons. These are the unit-connection series and parallel connected, shown in Figures 3.2 and 3.3 and the group-connection, shown in Figure 3.4. These arrangements will be discussed in this chapter with emphasis on their characteristics at both the rectifier and inverter sides.

The rectifier and inverter characteristics also depend very much of the ac system impedance at both ends of the dc link. The weaker the ac system, the more difficult it becomes to ensure appropriate damping and stability at the natural frequencies of the line and terminal equipment[Stovall, 1987]. Figure 3.1 shows the change in inclination in the inverter characteristic with the commutation impedance in the ac system. Variable excitation will avoid the early drop in the dc transmission voltage, provided that the excitation is not at its maximum. Only one set of curves with fixed excitation has been used in this work to demonstrate the feasibility of the model. Fixed excitation is just one limit for the capability charts, although in this work fixed excitation has been used, an extra set of curves would be necessary for each different excitation to allow variable excitation in the Equivalent Inverter model.

A rectifier system may contain several generators, some of which may be removed from service as the power transmission reduces[Ingram, 1988]. Such policy, while ensuring efficient generation, may result in poor transmission efficiency when applied to the series unit connected schemes because the dc voltage is proportional to the number of units connected in series. It is assumed in all the proposed configurations that the inverter is a single 12 pulse converter with filters. This is to ensure that a direct comparison of the different rectifier configurations can be made.
Figure 3.1 Regulation characteristics of an inverter. \(V_d = \text{direct voltage and } I_d = \text{direct current}\)

3.1.1 Series and parallel unit connected configuration

In the series arrangement, as shown in Figure 3.2, all the converters carry the same direct current. Power control and protection in case of dc line-to-ground faults are simple and can be performed very easily. Faults are eliminated by blocking one converter and by-passing the other. The converter group rating with rather high current (total current in the link) and moderate dc voltage (a fraction of the voltage per pole) allows an economical design.

If the series system operates in a bipolar configuration the outage of one unit only causes an asymmetry in the pole-to-ground voltages and a slight reduction in transmission efficiency can be observed. No current will flow to earth. The operation at partial load with only some of the units running causes a significant reduction in transmission efficiency and dc voltage. This keeps the current high and the losses are large.

In the dc parallel connection, as shown in Figure 3.3, each converter needs its own current control, because for flexibility, it is not mandatory that each parallel converter contribute with the same amount of current to the dc link. Parallel operation demands balance control to suppress power oscillations between the units, this can be achieved by
properly setting the sensitivity of the speed governors to load variations in order to avoid hunting between machines and to obtain the desired power sharing. The protection against any kind of fault is somewhat sophisticated due to the required inter-group coordination. In this case the protection should be coordinated to eliminate the fault simultaneously. The converter group rating with low direct current and the full dc pole voltage leads to high construction costs, due to the necessary increase in dc insulation and number of thyristors in series.

The commutation reactance of unit connected generation with the parallel connection doesn't change and the dc voltage remains invariant with the removal of machines. In this case the removal of machines doesn't pose any threat to the normal operation of the link in the inverter end for low power conditions.

3.1.2 Group connected configuration

In the group connection arrangement, as shown in Figure 3.4 all the units are paralleled at a common ac busbar. The design provides for efficient generation and transmission. Although the size of the converter transformers and bridges are much larger in the group-connection a trade-off exists between size and number of units when comparing the group-connection with the equivalent unit connected system. In this case the cost and size of spare units also play an important role in the decision to choose one or another configuration.
3.1.3 Comparison between unit and group connections

A dc series connection is chosen for comparison between unit and group-connection schemes. This is because of the previously mentioned properties of the series and parallel arrangements. So the more economic and simpler configurations of Figures 3.2 and 3.3 were assumed to show the characteristics and operational problems when compared with the group connection in Figure 3.4. Moreover, transmission voltage reductions caused by the disconnection of units at the rectifier end in the unit-connection configuration will immediately be followed by corresponding firing angle advances at the inverter end which require extra VAr compensation increasing operational costs. This problem would be overcome if the number of bridges and their sizes at the inverter were the same as at the sending ends. This may well be an uneconomic design when the sending end consists of many units.

The above problems should not affect the direct group-connection of generators to HVdc converters as can be seen in Figure 3.4, because in this case all the generators are paralleled at a common converter busbar, and all the converter units in series remain in service following generator removals. Further insight in the group connection can be obtained from the pros and cons for this configuration.

The advantages of each connection relative to the other are given below:

The group-connection:

i. has an efficient energy transfer at all power transmission levels, i.e. its able to trans-
unit at nominal voltage and reduced current when some generators are disconnected;

ii. has a greatly reduced number of converter bridges permitting a conventional converter configuration, i.e. the same on both sides of the HVdc link;

iii. eliminates possible harmonic intermodulation effects between individual unit groups;

iv. permits efficient inversion regardless of the number of generators in service permitting the operation of the inverter at optimal firing angles, without the need for unrealistic OLTC (On-Line Tap-Changer) ranges.

The advantages of unit-connection are:

i. lower SCR (Short-Circuit Ratio) at the generator’s bus which reduces the cost of switchgear;

ii. the commutation reactance is kept constant when individual generators are disconnected. With the link power setting reduced in the same proportion of the outage of each unit in unit-connection the scheme can always be kept at maximum overall efficiency.

iii. the converters have the same current rating and protection against faults can be performed easily.

Until recently the group connection has not been taken seriously by energy utilities. It is noted however, that at Benmore the station can operate as a group-connection in emergencies and during ac filter maintenance. This is due to the conservative design of the station. As in all new technology, the extended electric utility industry moves cautiously
in accepting innovations [Woodford et al., 1989]. This is mainly due to the harmonic distortions in the commutation process in the non-filtered converter which affects how the ac equipment should be designed or adapted to handle those non-linearities. The economic aspect also plays an important role in the decision of adopt this configuration.

At a time when direct connection is being considered by researchers [Bowles, 1981] [Krishnayya, 1987], it is important to compare the operating characteristics of the two alternatives, i.e. unit and group configurations. The results of this investigation are described in this chapter.

### 3.2 Rectifier characteristics

Figures 3.2-3.4 display the basic components of the unit-connection connected in series and parallel and the group-connection arrangement respectively. For the purposes of this comparison, it is assumed that both unit and group series schemes have generators which are identical and equally excited. It is not implied that normal operation will be at constant excitation.

If \( r \) is the converter transformer ratio of the unit-connection, then to derive the same nominal power, the corresponding ratio for the group-connection transformers needs to be \( r n \), where \( n \) is the number of possible generators in the plant.

Using as a power base the nominal rating of one generator, the commutation reactance of the unit-connection bridges is:

\[
x_{c_{pu}} = x'' + 2 x_l
\]  
(3.1)

or

\[
x_c = (x'' + 2 x_l) x_B r^2
\]  
(3.2)

in ohms, where \( x_B = V_B^2/ MVA_B \), with \( V_B \) being the base voltage at the primary side of the transformer.

The dc voltage of a twelve pulse bridge unit is:

\[
V_n = 2 \left[ \frac{3\sqrt{2}}{\pi} E'' r \cos \alpha - \frac{3}{\pi} (x'' + 2 x_l) x_B r^2 I_d \right]
\]  
(3.3)

and assuming that in Figure 3.2 all the generators have the same voltage \( V_n \), the total dc voltage output with \( n_s \) generators in service is:

\[
V_d = n_s V_n
\]  
(3.4)
i.e. the voltage output is always proportional to the number of generators in service.

In this case the open circuit voltage reduces with the number of generators in service. However the commutation reactance remains invariant and the voltage regulation does not change with the connected number of generators.

The removal of machines results in a proportional reduction in voltage and power with a small decrease in current for a constant power control. In Figure 3.5 points (a) to (f) indicate the nominal operation of the system when changing the number of machines in service from 6 to 1 respectively. This variation occurs because of the non-linearities in the converter constant firing angle characteristic combined with an hyperbolic constant power characteristic. It is implicit in this case with almost constant dc current that the firing angle increase in the rectifier and inverter following the removal of machines.

Using the same power base, the commutation reactance of the group-connection bridges with \( n_s \) generators in service is:

\[
x'_c = \left( \frac{x''}{n_s} + \frac{2 x_t}{n} \right) x_B \left( \frac{r n}{n} \right)^2
\]  

or

\[
x'_c = \frac{x''}{n_s} + \frac{2 x_t}{n} \frac{x_B}{r n}
\]  

\[
x'_c = \frac{x''}{n_s} + \frac{2 x_t}{n} x_B \left( \frac{r n}{n} \right)^2
\]  

\[
x'_c = \left( \frac{x''}{n_s} + \frac{2 x_t}{n} \right) x_B \left( \frac{r n}{n} \right)^2
\]
in ohms, and the total dc voltage of the group-connection is:

$$V_d = 2 \left[ \frac{3\sqrt{2}}{\pi} E'' r n \cos \alpha - \frac{3}{\pi} \left( \frac{x''}{n_s} + \frac{2 x_l}{n} \right) x_B (r n)^2 I_d \right]$$  \hspace{1cm} (3.7)$$

Equation (3.7) shows that for a given excitation the no-load voltage output remains invariant regardless of the number of generators in service. However, the second term of the equation depends on \( n_s \) and therefore the slope of the voltage regulation increases with fewer generators in service.

For the group-connection in Figure 3.6 the removal of machines causes a proportional reduction in current and power with a small increase in voltage (points a, b, c...). This variation occurs because of the non-linearities in the converter constant firing angle characteristic combined with an hyperbolic constant power characteristic. It is implicit in this case with almost constant dc voltage that the firing angle decreases slightly at the rectifier and inverter following the removal of machines.

The modified ac-dc load flow, is used to derive the rectifier characteristics of the unit and group connections. The vector of seven independent variables required for the converter model was described in equation (2.23).

The results for the test system specified in the Appendix 3.A are shown in Figures 3.5 and 3.6. The machines are removed to simulate operating conditions of a six
Table 3.1 Typical hydro-generator unit loading conditions.

<table>
<thead>
<tr>
<th>Power (%)</th>
<th>Operation time (% of total time)</th>
</tr>
</thead>
<tbody>
<tr>
<td>100</td>
<td>60</td>
</tr>
<tr>
<td>80</td>
<td>25</td>
</tr>
<tr>
<td>60</td>
<td>10</td>
</tr>
<tr>
<td>40</td>
<td>5</td>
</tr>
</tbody>
</table>

generator hydraulic power plant directly connected to a dc link for different loading conditions. A typical hydro-generator loading condition is given in Table 3.1. In this case the table shows that it is delivering less than 100% of its power for 40% of its total operating time. The loading condition for the entire power plant is relevant in this case, but a typical data is difficult to define, since its importance in the system is function of its size and system size.

With all the generators in service the unit and group configurations follow the same characteristic. In both cases the intersection of this curve and the maximum specified power level ($P_6 = 520$ MW) (i.e. point (a)) determines the operating current required (i.e. 1.16 kA in the test system). At a reduced power setting ($P_5 = 430$ MW) and with one generator removed, the unit and group characteristics are different, giving rise to operating points (b) in Figure 3.5 (1.15 kA) and Figure 3.6 (0.955 kA), the latter requiring less current and thus resulting in better transmission efficiency. Although the commutation voltage is larger in the case of the group-connection, the reduced open circuit voltage of the unit-connection demands more current for a given power setting. Table 3.2 shows the maximum power at the receiving end for each number of machines in operation.

Further generator removals result in operating points (c), (d), (e) and (f) in Figures 3.5 and 3.6. Figure 3.6, for the group-connection, shows small variations in the voltage due variations in commutation reactance as the units are removed from service. These variations are consistent with equations (3.5) and (3.6). For the points shown previously the currents in Figure 3.5 are respectively: 1.125 kA, 1.1 kA, 1.09 kA and 1.01 kA. In Figure 3.6 the currents for the same points are: 0.75 kA, 0.55 kA, 0.35 kA and 0.153 kA. The curved lines are the $V_d/I_d$ characteristics for the rectifier in Figure 3.5 (unit-connection) and Figure 3.6 (group-connection) and for the inverter in Figure 3.7 (unit and group-connection).

A conventional inverter configuration is assumed at the receiving end of the dc link. In this case the operational and power characteristics can reveal the possibilities and restrictions with the removal of machines so far ignored in the direct connection analysis.
Table 3.2 Maximum power received at the inverter end.

<table>
<thead>
<tr>
<th>number of machines</th>
<th>Power received (MW)</th>
<th>Power designation</th>
</tr>
</thead>
<tbody>
<tr>
<td>6</td>
<td>520</td>
<td>P&lt;sub&gt;1&lt;/sub&gt;</td>
</tr>
<tr>
<td>5</td>
<td>430</td>
<td>P&lt;sub&gt;2&lt;/sub&gt;</td>
</tr>
<tr>
<td>4</td>
<td>340</td>
<td>P&lt;sub&gt;3&lt;/sub&gt;</td>
</tr>
<tr>
<td>3</td>
<td>250</td>
<td>P&lt;sub&gt;4&lt;/sub&gt;</td>
</tr>
<tr>
<td>2</td>
<td>160</td>
<td>P&lt;sub&gt;5&lt;/sub&gt;</td>
</tr>
<tr>
<td>1</td>
<td>70</td>
<td>P&lt;sub&gt;6&lt;/sub&gt;</td>
</tr>
</tbody>
</table>

Figure 3.7 Characteristics for the unit and group-connected generation at the inverter end.

### 3.3 Inverter characteristics

In Figure 3.7 the constant power characteristics are combined with the inverter constant extinction angle characteristics for the same operating points described in the previous section. The curves take into account the effect of dc line resistance.

Each generator is taken out of service in turn, only after none of its capacity is
Points b, c, d, e and f in Figures 3.8 and 3.9 represent the maximum power for reduced numbers of generators. In the unit-connection case a gradual reduction in the number of generating units transfers the constant current control from the rectifier to the inverter end which then operates with increasing firing angle advances and reduced power factors. This is illustrated in Figure 3.8 which shows reactive versus active power levels at the inverter end for different numbers of unit-connected machines at the rectifier. The difference between the reactive demands with 6 and 5 machines is small because the complete range of tap variation is first used to delay the extra reactive demand with one machine disconnected. Once the tap change range is exhausted such demand increases fast. For instance in this particular case when changing from 6 to 4 machines the inverter reactive power increases from about 210 MVAR to 320 MVAR.

The solution to avoid the extra VAR requirements in the inverter end as shown in Figure 3.8 is to use the same series unit connected configuration at the rectifier and inverter ends. Under normal operation the number of bridges at each end will be the same. A large OLTC range will not be necessary but the cost of the inverter station will be larger. When operating at reduced voltage the transmission efficiency of the dc link will be lower.

On the other hand the group-connection permits operation with fewer generating units in service while reducing the inverter reactive power demand. This is illustrated in Figure 3.9 where the reactive versus active power characteristics with different number
of machines are practically coincident. In this case a 3.2% tap variation of the inverter end transformers can accommodate the small voltage regulation caused by the increased commutation reactance at the rectifier end.

3.4 Efficiency considerations

The following discussion relies on research work undertaken by Mr. S. MacDonald as part of his Doctorate studies.

3.4.1 Turbine efficiency

If the station is designed for a fixed speed turbine operation in order to generate power at the nominal frequency (i.e. 50 or 60 Hz.), the optimum turbine torque varies with the water head level. Therefore in the absence of other possible considerations justifying less efficient power transmission, minimum water utilization will be achieved by varying such power with the water head level. This implies the disconnection of turbine-generator groups at reducing power levels.

The performance characteristics of the 90 MW hydro turbines of the Pan Jia Kou scheme [Jaquet, 1986] are used to illustrate this effect. For a water head level of 86 meters, the turbine efficiency ($\eta_t$) versus power ($P$) curve for a power range between 45 and 90
The test system, described in the Appendix 3.A, consists of six generators connected to an HVdc link. The nominal voltage and current rating of the 500 km line are 270 kV and 1.2 kA respectively and the line resistance 11.86 ohms. The overall operating efficiency results from the combined effect of turbine and transmission efficiency components, i.e.
for the unit-connection, and

\[ \eta_r^U := \eta_r \eta_r^U \]  

(3.13)

for the group-connection.

For the turbine characteristic represented by equation (3.8) the overall efficiencies versus power with one to six machines in service are shown in Figure 3.10.

With fewer units in service, the transmission efficiency reduction in the case of the unit-connection predominates. The opposite effect occurs with the group-connection, where the nominal dc voltage can be maintained with lower powers and machine numbers.

Hence the group-connection provides higher overall operating efficiencies with reducing powers.

Regarding unit-connection performance, the crossings between the efficiency curves in Figure 3.10 (i.e. points A, B and C) indicate the power boundaries for the number of units to be used at maximum efficiency (even though the efficiencies are considerably lower than for the group-connection). However these points occur at power levels beyond the capability of the machines. Moreover, Figure 3.8 has shown that for reducing active power levels, operation with fewer machines demands increasing reactive power provision at the inverter end, a condition unlikely to be acceptable.

### 3.6 Conclusions

The conventional ac-dc steady state formulation, used in loadflows, has been modified to analyze HVdc loadflows with unit connected and/or group connected generator converter in-feeds.

The modified HVdc loadflow formulation as described in Chapter 2 has been used in this chapter to obtain the characteristics at the sending and receiving ends for unit and group connected configurations.

The characteristics of unit and group HVdc connected generation have been derived for a wide range of operating powers. Although there is no difference between the alternative configurations when all the generators are in service, the transmission voltages of the unit-connection deteriorate fast with fewer generators in service resulting in low transmission efficiencies. Moreover, operation with reduced generators in service requires a transfer of power control to the inverter end, which increases substantially the reactive power demand at that end.
Figure 3.10 Comparison of overall efficiency of the unit and group-connected generation

Group-connection presents no problems in this respect, being capable of maintaining the required transmission voltage with fewer generators in service and thus providing efficient transmission at different power levels. The group-connection also permits efficient inversion without the need for unrealistic on load tap change ranges.

The effect of turbine efficiencies taking into account the hydro water levels has been incorporated in the overall comparison to define the optimal operating strategies in each case.

It is recognized that factors other than optimum efficiency may dictate the power transmission levels via HVdc link but if water conservation becomes a critical factor (such as the likelihood of frequent dry years) the above considerations may influence the decision on the type of direct connection to be selected.
Appendix 3A  New Zealand System and Benmore Power Station Data

The New Zealand North Island primary transmission system is used at the inverter end of the link. The Benmore power station, without filters and disconnected from the South Island system, is used as the unit and/or the group connected scheme. The rectifier end of the dc link for the unit-connection is shown in Figure 2.11 in Appendix 2C. Figures 3.2 and 3.4 shows respectively the station arrangements pertinent to the unit and the group-connection schemes.

The relevant parameters for each of the six generators at the Benmore power station are:

\[
\begin{align*}
\text{Rating} & = 112.5 \text{ (MVA)} \\
\text{Voltage} & = 16.0 \text{ (kV)} \\
 x & = 1.140 \text{ (pu)} \\
x_d' & = 0.264 \text{ (pu)} \\
x''_d & = 0.174 \text{ (pu)} \\
x''_q & = 0.190 \text{ (pu)} \\
R_a & = 0.003 \text{ (pu)}
\end{align*}
\]

Converter transformers reactance: 11.5 %
4.1 Introduction

In Chapters 2 and 3, two methods of simulating the unit-connection in steady state were presented.

To overcome the restrictions on the previous models, an alternative model is proposed in which the simulation of a unit connected scheme is done by using data obtained from the previously described time domain simulation program (TCS).

The modifications required for the inclusion of the new model in the conventional ac loadflow formulation are described. The developed computer model which permits the continued use of steady state studies is also described. The objective of this work is the development of a more accurate algorithm, based on the use of rectifier characteristics derived from dynamic simulation, which is then applied to the fast decoupled ac-dc loadflow.

The TCS results, are given in the form of capability charts and an interpolation algorithm is used to obtain reliable operation conditions for any point inside the characteristics. This algorithm makes it possible to simulate the steady state system that is always operating in a subtransient state [Arrillaga et al., 1991].

4.2 Load flow model based on dynamic simulation

A time domain solution of the differential equations representing the generator and rectifier behaviour provides detailed information of the required voltage and current waveforms. The results presented here were obtained using the TCS program [Arrillaga et al., 1978], [Arrillaga, 1983] as described in Chapter 2. Once the steady state waveforms were obtained, their averaged values provide accurate output characteristics for the unit connected group. For every combination of direct current ($I_d$) and firing angle ($\alpha$) the output dc voltage ($V_d$) was calculated [Arrillaga et al., 1992c].

To get the $V_d/I_d$ characteristics of a converter in TCS, careful modelling of the
synchronous machine, transformers, converter bridges, dc line and a suitable equivalent ac source at the inverter end is required.

Prior to getting the rms values from the voltage and current waveforms, used in the steady state analysis, it is necessary to start up the the system from the de-energized condition and let it settle into steady state. This condition is reached when the current or voltage waveforms does not show any harmonic spectrum variation when analysed in its last five cycles. The steady state run is used also to identify any previous mistakes in data preparation, per unit system is one example of common source of errors in this case.

For this purpose, TCS were run for 25 cycles of simulation time to let the system settle down to the steady state. TCS has the facility to store the steady state results at the end of a run in a separate file for further studies. In this case a snapshot is always taken after 25 cycles of simulation. The snapshot file was a very useful tool to save time in this cumbersome process. For two, or sometimes three, close operating point the same snapshot can be used as a initial condition. In this case the system was run for another fifteen cycles. The same checking criteria using FFT (Fast Fourier Transform) is applied again before getting the information to be used in the equivalent inverter model.

4.2.1 Accurate derivation of rectifier characteristics

For fixed synchronous machine excitation a series of \( V_d/\alpha \) curves, one for each \( \alpha \), are necessary to store the nonlinear information about the rectifier obtained from time domain solution. This data, which is equivalent to equation (4.1) in the load flow applied to the equivalent inverter.

\[
V_d = \frac{3}{\pi} \sqrt{2} V_{term \cos} - \frac{3}{\pi} x_c \ \alpha
\]  

(4.1)

For the purposes of this discussion, two charts have been stored, the first \( V_d/\alpha \) for the rectifier operating as a diode bridge (\( \alpha = 0^\circ \)) requires a minimum of 15 points as in Figure 4.1. The second chart \( V_d/\alpha \) for the rectifier operating under constant current (\( I_d = 1.0 \) kA) requires a minimum of 9 points as in Figure 4.2. To get an intermediary point that that does not fall in the characteristic an interpolation method is used.

The type of interpolation used is a variation of the method of divided differences[Gerald and Wheatley, 1985] explained in Appendix 4.A with the degree of the interpolating polynomial being constrained to a second order.

This method was chosen because it required less arithmetic operations, its simplicity in adding or subtracting a point from the set used to construct the polynomial, and the capability of reusing previous computations.

The charts shown in Figures 4.1 and 4.2, and subsequently, do not show the effects of the interpolation. The charts show simple linear interpolation between the stored data
4.2 LOAD FLOW MODEL BASED ON DYNAMIC SIMULATION

Figure 4.1 The $V_d/I_d$ characteristic for $\alpha = 0^\circ$.

Figure 4.2 The $V_d/\alpha$ characteristic for $I_d = 1.0 \text{ kA}$.
4.2.2 The equivalent inverter model

An accurate power flow solution of the unit-connection scheme requires the use of time domain calculated rectifier characteristics [Arrillaga et al., 1992b]. The absence of tap changers and the irrelevance of $\phi$ at the generator terminals of the unit-connection scheme permits a simpler formulation, in the form of a modified equivalent inverter, as can be seen in Figure 4.3.

The modifications required in the conventional HVdc loadflow chart to simulate the equivalent inverter are shown in the bold blocks in Figure 4.4. Modifications have been made in the system control data, dc link data, control equations, calculation of residuals and in the dc Jacobian matrix equations.

With different machines or generators with different excitation in the same generation facility the problem become more complex. One chart for each machine at each operating point is necessary. And a combination of those operating characteristics should be built if a single equivalent machine is wanted in the modelling. A more accurate option for different machines or different excitations can be the simulation of each machine independently in a multi-terminal configuration. This avoids the cumbersome process of combination of characteristics.

By assuming that all the generators in service have identical characteristics and are equally loaded they can be represented as a single equivalent machine. The machine

\[
V_{dr} = f(I_d, \cos \alpha)
\]

Figure 4.3 The equivalent inverter.
4.2 Load Flow Model Based on Dynamic Simulation

![Flow Chart](image)

Figure 4.4 Flow chart modification for the equivalent inverter.
is then represented as $V_d / I_d$ and $V_d / \alpha$ characteristics in the rectifier $dc$ side of the HVdc link.

In this case the vector equation of independent variables for the $dc$ link in the unit-connection case in the inverter side is:

$$\bar{x} = [V_d, I_d, \alpha, \cos\alpha, \phi, \cos\gamma]^T$$  \hspace{1cm} (4.2)

which contains the five variables of the conventional (inverter) set and an extra variable ($\cos\alpha$) representing the rectifier end of the link.

In line with conventional practice the equivalent inverter will normally be on extinction angle control ($\gamma_{sp}^{min}$) and the rectifier on current control, the specified current level derived from a constant power setting ($P_d^{sp}$) at the inverter end.

Besides $P_d^{sp}$ and $\gamma_{sp}^{min}$ a third control specification must be made to match the six variable formulation. It is suggested that $\alpha_{sp}^{min}$ is used and the inverter transformer tap freed. This will provide the highest transmission voltage. Should the tap changer attempt to violate one of its limits, this limit becomes the new control specification while freeing the value of $\alpha$.

The vector of independent variables $\bar{x}$, which describe the state of the $dc$ link in the inverter side still can be obtained by the application of the Newton-Raphson algorithm for solving non-linear equations.

Increments to the independent variables vector, $\bar{x}$, are obtained from the solution of the $dc$ Jacobian matrix equation in the inverter side:

$$|R| = [A] \mid \bar{x} \mid$$  \hspace{1cm} (4.3)

for $p$ equations.

$|R|$ - is the vector of residuals for the non-linear equations representing the $dc$ link power flow and control strategies.

$[A]$ - are the first order differentials (dc Jacobian matrix), $\partial R_k / \partial x_i$, for $k,i=1,...,p$.

The complete set of residual equations $|R|$ after convergence should satisfy:

$$V_d - k_1 a V_{term} \cos \phi = 0$$  \hspace{1cm} (4.4)

$$V_d - k_1 a V_{term} \cos(\pi - \gamma) - \frac{3}{\pi} x_c I_d = 0$$  \hspace{1cm} (4.5)

$$V_d + R_d I_d - f(I_d, \alpha) = 0$$  \hspace{1cm} (4.6)
The incorporation of the equivalent inverter into a fast decoupled ac-dc loadflow algorithm requires modification of the $B^r$, $AA'$, $AA''$ and $BB''$ sub-matrices of the Jacobian matrix [Arrúñaga, 1983]. These sub-matrices are greatly simplified in the equivalent inverter model as they contain only a single element each.

4.2.3 Ac-dc Jacobian matrices

The Jacobian matrix for the modified fast decoupled ac-dc loadflow is the same as described in Chapter 2. However, in this case all the matrices, given in equations (4.10)–(4.11), are reduced to lower dimensions since the rectifier terminal in the link is represented by its $V_d/I_d$ charts.

\[
\begin{pmatrix}
\Delta P / V \\
\Delta P / V_{term} \\
R \\
\end{pmatrix}
\begin{pmatrix}
\Delta \phi \\
\Delta \phi_i \\
\Delta x \\
\end{pmatrix}
= 
\begin{pmatrix}
B' \\
AA' \\
A \\
\end{pmatrix}
\begin{pmatrix}
\cdot \\
\cdot \\
\cdot \\
\end{pmatrix}
\begin{pmatrix}
\cdot \\
\cdot \\
\cdot \\
\end{pmatrix}
\]
The modified reduced dc Jacobian matrix derived from the conventional Fast-Decoupled AC-DC loadflow, applied to the equivalent inverter, is given in equation (4.12). In the equivalent inverter the introduction of charts provides the information for the rectifier. Although the rectifier is eliminated, the presence of information extracted from the characteristics for the rectifier and the dc equations for the inverter are still necessary in the dc Jacobian matrix.

The elements of the reduced dc Jacobian matrix \([A]\) in the equivalent inverter are:

\[
[A] = \begin{pmatrix}
1 & - & -k_1 V_{\text{term}} \cos \phi & -k_1 a V_{\text{term}} \sin \phi & - \\
- & - & - & - & - \\
- & - & - & - & - \\
- & - & - & - & - \\
I_d & V_d & - & - & - & -1
\end{pmatrix}
\]

where \(V_{dr} = f(I_d, \cos \alpha)\), and also:
\[
\begin{align*}
\begin{bmatrix} AA' \end{bmatrix} &= \left[ \frac{\partial P_{\text{term}}}{\partial x} (dc) \right] / V_{\text{term}} \\
\text{or} \\
\begin{bmatrix} AA' \end{bmatrix}^T &= \\
&= \begin{bmatrix}
-a_i B_i E_i \cos(\psi_i - \phi_i) \\
- \\
- \\
E_i B_i \sin(\psi_i - \phi_i)
\end{bmatrix}
\end{align*}
\]

\[
\begin{align*}
\begin{bmatrix} AA'' \end{bmatrix} &= \left[ \frac{\partial Q_{\text{term}}}{\partial x} (dc) \right] / V_{\text{term}} \\
\text{or} \\
\begin{bmatrix} AA'' \end{bmatrix}^T &= \\
&= \begin{bmatrix}
-a_i B_i E_i \sin(\psi_i - \phi_i) \\
- \\
- \\
2 a_i B_i V_{\text{term}} - E_i B_i \cos(\psi_i - \phi_i)
\end{bmatrix}
\end{align*}
\]

\[
\begin{align*}
\begin{bmatrix} BB'' \end{bmatrix} &= \frac{\partial R}{\partial V_{\text{term}}} \\
\text{or} \\
\begin{bmatrix} BB'' \end{bmatrix} &= \\
&= \begin{bmatrix}
-a_i B_i \sin \psi_i \\
- \\
- \\
-
\end{bmatrix}
\end{align*}
\]

Matrices $A$, $AA'$, $AA''$, $BB''$ illustrated in equations (4.12)-(4.18) have elements which vary at every iteration. The matrices combine to form the very sparse but un-symmetrical matrices shown in equations (4.10) and (4.11). These matrices have smaller dimensions than those in the conventional two terminal $dc$ link discussed in Chapter 2.
4.3 Operating modes for the equivalent inverter

Provided that the desired operating points are inside the rectifier $V_d/I_d$ maximum capability chart, for the unit connected HVdc generation scheme represented as an equivalent inverter, the following must be considered:

i. The preferred dc link operation mode, having $\alpha_r$, $\alpha_i$, $P_{dr}$ and $V_d$ being the specified controls may not be always possible. If the equivalent machine is in one limit of its characteristic the load may require a variation in the rectifier that takes the model in one operating point beyond its capability chart. In this case no feasible solution is found.

ii. In Table 4.1 all the control specifications involving $a_r$ assume that the tap position is at its nominal value, that is $a_r = 1$. The absence of OLTC should be seen as an step in the overall optimization, simplification and reliability enhancement in the direct connected configuration. In the equivalent inverter the variable $a_r = 1$ is eliminated.

iii. The reactive power and also the converter power factor can't be specified at the rectifier ac terminal. These parameters are also eliminated from the problem in the equivalent inverter model. Information of these parameters are embedded in the charts from the time domain simulation.

Table 4.1 shows a summary of the operational modes of the dc link under unit connected scheme.\(^1\)

4.4 Conclusions

The formulation of ac-dc loadflow is well understood[Arrillaga et al., 1983], but some of the basic assumptions made in its formulation have been found inapplicable to the unit connected configuration[Arrillaga et al., 1991]. For example the non-linear variation of the rectifier commutation reactance can not be accurately obtained for use in the steady-state algorithms.

In the absence of local load at the rectifier end, the unit connected scheme can be considered as an equivalent HVdc generator and the complete HVdc link as an equivalent inverter.

The TCS program provides the time domain solution of the differential equations representing the generator and rectifier behaviour. TCS also gives detailed information of the required voltage and current waveforms. With the obtained steady state waveforms, their averaged values provide accurate output characteristics for the unit connected group.

\(^1\)The variable $a_r$ does not need to be specified in the equivalent inverter model.
Table 4.1 Summary of operational modes of the dc link for the direct connected scheme. Subscripts \( r \) and \( i \) designate specified parameters in the rectifier and inverter respectively.

<table>
<thead>
<tr>
<th>Case</th>
<th>Specified Controls</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>( \alpha_r \gamma_i P_{dr} V_{di} )</td>
<td>Normal dc link operation</td>
</tr>
<tr>
<td>2</td>
<td>( \alpha_r a_i P_{dr} V_{di} )</td>
<td>Normal dc link operation</td>
</tr>
<tr>
<td>3</td>
<td>( a_r a_i P_{dr} V_{di} )</td>
<td>( a_r = 1 )</td>
</tr>
<tr>
<td>4</td>
<td>( a_r \gamma_i P_{dr} V_{di} )</td>
<td>( a_r = 1 )</td>
</tr>
<tr>
<td>5</td>
<td>( a_r \gamma_i P_{dr} a_i )</td>
<td>( a_r = 1 )</td>
</tr>
<tr>
<td>6</td>
<td>( a_r \gamma_i P_{dr} \alpha_r )</td>
<td>( a_r = 1 )</td>
</tr>
<tr>
<td>7</td>
<td>( \alpha_r \gamma_i I_d V_{di} )</td>
<td>Constant Current Control</td>
</tr>
<tr>
<td>8</td>
<td>( \alpha_r \cos \phi_i P_{dr} V_{di} )</td>
<td>Converter power factor specified</td>
</tr>
<tr>
<td>9</td>
<td>( \alpha_r Q_{di} P_{dr} V_{di} )</td>
<td>Terminal reactive power specified</td>
</tr>
<tr>
<td>10</td>
<td>( \alpha_r \gamma_i P_{dr} E_i )</td>
<td>Ac voltage specified</td>
</tr>
<tr>
<td>11</td>
<td>( \alpha_r a_i P_{dr} E_i )</td>
<td>Ac voltage specified</td>
</tr>
</tbody>
</table>

For every combination of field excitation, \( I_d \) and \( \alpha \) the output dc voltage \( V_d \) is calculated and the resulting information can be stored in memory to be used in the equivalent inverter loadflow solution.

Another restriction to the conventional simulation is the permanently fixed transformer tap at the rectifier side for the unit-connection model. This restriction no longer exists in the equivalent inverter model.

In the equivalent inverter errors are eliminated in the rectifier steady state equations for firing angles beyond 30° for twelve pulse bridges.

Chapter 5 shows the results for the equivalent inverter compared with the other two proposed steady state solutions for the direct-connection problem.
Appendix 4A  Interpolation with nonuniformly spaced values

If it is assumed that the x-values, in the function given in the Table 4.2, are not equally spaced. For nonuniform spacing, a parallel development using the concept of divided differences can be used, and this technique is given here as an alternative to the Lagrangian polynomial.

The Lagrangian polynomial is perhaps the simplest way to exhibit the existence of a polynomial for interpolation with unevenly spaced data.

There are two problems when a Lagrangian polynomial is used for interpolation. First, there are more arithmetic operations than for the divided difference method now introduced. More importantly, if it is desired to add or subtract a point from the set used to construct the polynomial, then the computations must be re-done from the start. The divided difference method permits the previous computations to be used again just as have been done for interpolating polynomials derived from uniformly spaced tables [Gerald and Wheatley, 1985].

The handling of divided differences table assumes that a function, \( f(x) \), is known at several values for \( x \) as in Table 4.2.

It is not necessary to assume that the x's are equally spaced nor even that the values are arranged in any particular order.

Considering the \( n^{th} \)-degree polynomial:

\[
P_n(x) = a_0 + (x - x_0) a_1 + (x - x_0)(x - x_1) a_2 + \ldots + (x - x_0)(x - x_1) \ldots (x - x_{n-1}) a_n
\]  

(4.19)

Assuming that \( a_i \) is chosen so that \( P_n(x) \) equals \( f(x) \) at the \( n + 1 \) known points, \( x_0, x_1, \ldots, x_n \), then \( P_n(x) \) is an interpolating polynomial. Will be shown later that the \( a_i \) are readily determined by using what are called the divided differences of the tabulated value.

A special notation is used for divided differences:

\[
f[x_0, x_1] = \frac{(f_1 - f_0)}{(x_1 - x_0)}
\]  

(4.20)

is the first divided difference between \( x_0 \) and \( x_1 \),
Table 4.3 Divided differences table.

<table>
<thead>
<tr>
<th>$x_i$</th>
<th>$f_i$</th>
<th>$f[x_i, x_{i+1}]$</th>
<th>$f[x_i, x_{i+1}, x_{i+2}]$</th>
<th>$f[x_i, x_{i+1}, x_{i+2}, x_{i+3}]$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$x_0$</td>
<td>$f_0$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$x_1$</td>
<td>$f_1$</td>
<td>$f[x_0, x_1]$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>$x_2$</td>
<td>$f_2$</td>
<td>$f[x_1, x_2]$</td>
<td>$f[x_0, x_1, x_2]$</td>
<td></td>
</tr>
<tr>
<td>$x_3$</td>
<td>$f_3$</td>
<td>$f[x_2, x_3]$</td>
<td>$f[x_1, x_2, x_3]$</td>
<td></td>
</tr>
<tr>
<td>$x_4$</td>
<td>$f_4$</td>
<td></td>
<td>$f[x_2, x_3, x_4]$</td>
<td></td>
</tr>
</tbody>
</table>

Table 4.3 Divided differences table.

\[
\begin{align*}
\text{is the first divided difference between } x_1 \text{ and } x_2. \\
\text{In general,} \\
\text{is the first divided difference between } x_s \text{ and } x_t. \\
\text{Another advantage of the method is that the ordering of the points is immaterial:}
\end{align*}
\]

Second and higher-order divided differences are defined in terms of lower-order differences. For example:

\[
\begin{align*}
f[x_0, x_1, x_2] &= \frac{(f[x_1, x_2] - f[x_0, x_1])}{(x_2 - x_0)} \\
f[x_0, x_1, \ldots, x_i] &= \frac{(f[x_1, x_2, \ldots, x_i] - f[x_0, x_1, \ldots, x_{i-1}])}{(x_i - x_0)}
\end{align*}
\]

The concept is even extended to a zero order difference:

\[
\begin{align*}
f[x_s] &= f_s \\
\end{align*}
\]

Using this notation, a divided differences table, in symbolic form, is Table 4.3.

With Table 4.3 it is easy to establish that the $a_i$ terms of equation (4.19) are given by these divided differences. The equation (4.19) is written with $x = x_0, x = x_1, \ldots, x = x_n$, giving:
\[
\begin{align*}
x = x_0 & : \ P_n(x_0) = a_0 \\
x = x_1 & : \ P_n(x_1) = a_0 + (x_1 - x_0) a_1 \\
x = x_2 & : \ P_n(x_2) = a_0 + (x_2 - x_0) a_1 + (x_2 - x_0)(x_2 - x_1) a_2 \\
& \quad \quad \quad \quad \quad \quad \quad \vdots \\
x = x_n & : \ P_n(x_n) = a_0 + (x_n - x_0) a_1 + (x_n - x_0)(x_n - x_1)a_2 + \ldots + (x_n - x_0) \ldots (x_n - x_{n-1}) a_n.
\end{align*}
\]

If \( P_n(x) \) is supposed to be an interpolating polynomial, it must match the table for all \( n + 1 \) entries:

\[
P_n(x_i) = f_i \tag{4.27}
\]

for \( i = 0, 1, 2, \ldots, n \).

If the \( P_n(x_i) \) in each equation is substituted by \( f_i \), we get a triangular system, and each \( a_i \) can be computed in turn.

From the first equation, \( a_0 = f_0 = f[x_0] \) makes \( P_n(x_0) = f_0 \).

If \( a_1 = f[x_0, x_1] \), then:

\[
P_n(x_1) = f_0 + (x_1 - x_0) \left( \frac{f_1 - f_0}{x_1 - x_0} \right) \tag{4.28}
\]

and

\[
P_n(x_1) = f_1 \tag{4.29}
\]

If \( a_2 = f[x_0, x_1, x_2] \), then:

\[
P_n(x_2) = f_0 + (x_2 - x_0) \left( \frac{f_1 - f_0}{x_1 - x_0} \right) + (x_2 - x_0)(x_2 - x_1) \left( \frac{f_2 - f_1}{x_2 - x_1} - \frac{f_1 - f_0}{x_1 - x_0} \right) \tag{4.30}
\]

and

\[
P_n(x_2) = f_2 \tag{4.31}
\]

One can show in similar fashion that each \( P_n(x_i) \) will be equal \( f_i \) if \( a_i = f[x_0, x_1, \ldots, x_i] \).
Chapter 5

STEADY STATE RESULTS

5.1 Introduction

This chapter is devoted to results obtained from the three steady state unit connected generator converter models.

Initially the three models are directly compared by using the same theoretical test system. The comparison is first made assuming a diode bridge. This ensures large commutation angles where the differences between the models will be pronounced. A second set of tests examines the effects with varying firing angle.

The equivalent inverter is then examined in more detail to verify its correct operation.

Finally the equivalent inverter model is compared with commissioning tests performed on the upgraded New Zealand HVdc link where operation was similar to that of unit-connection.

5.2 Test system for theoretical studies

The test system consists of a single unit connected generator, a HVdc link with a 12 pulse converters and a 3 busbar receiving end ac system. Appendix 5.A shows all other relevant information.

The system was tested using the rectifier described for the equivalent inverter according to the charts in Figures 4.1 and 4.2 in Chapter 4.

The cases exploited in the analysis of the inverter end are:

1. the rectifier operating as a diode bridge converter ($\alpha = 0^\circ$) for various currents in the link.

2. the rectifier operating as a thyristor bridge converter for various firing angles with a constant current (1.0 kA) in the link.
the rectifier operating as a thyristor bridge for different values of firing angle ($\alpha$) and various currents in the link using the *equivalent inverter* model.

It has been shown [Arrillaga et al., 1993b] that operation with $\alpha = 0^\circ$ is highly unlikely in actual systems. Very low reactance transformers, much less than the 5% used in the test system are required. This will greatly increase the cost of the transformers and it is foreseen that actual diode systems being designed for operation under forced retard and commutation angles close to but under 30°.

### 5.3 Performance of the models of a unit connected generator converter

The idea of replacing the controlled rectifiers, by simple diode rectifiers in an HVdc station associated with a remote generating station has always been rejected due to the control problems imposed (i.e. recovery from commutation failures, limitation and recovery from dc and ac faults, etc.)

The appearance of a new generation of equipment, such the thyristor valves in the converter systems and the dc circuit breaker, the improvement in the excitation systems of the generators and the application of protective sequences has given the means of circumventing these objections and has opened the possibility of diode rectifier applications [Bowles, 1981].

Harmonic generation is a minimum in the diode rectifier mode which can be inside the tolerable levels of hydraulic generators. Therefore if a transmission system is considered in which the energy transfer is unidirectional, i.e. from a power generating station, it is possible to consider the removal of ac filters and the use of a transformer with the double function of a step up and converter transformer.

#### 5.3.1 Conventional loadflow results

In order to obtain the linear $V_d/I_d$ characteristic of a conventional HVdc scheme, the generator excitation must be controlled to keep the commutating voltage ($E''$) constant at some specified value to provide the required nominal dc voltage at minimum delay angle ($\alpha_{\text{min}}$). Figure 5.1 shows the $(V_d/I_d)$ characteristics in both models (conventional and modified) for the fast-decoupled loadflow and in the *equivalent inverter*.

In this case, the sending end of the test system can be operated as a diode rectifier bridge ($\alpha = 0^\circ$) with an averaged sub-transient reactance and a constant $E''$ (of 66.66 kV) for the salient pole machine.

Current control in this case is by the inverter with the possibility of tap control at the inverter transformer as shown in Figure 5.3. This practice ensures the efficient use of the inverter equipment (i.e. highest dc voltage) with minimum reactive power consumption
and minimum harmonic generation. The current in the \textit{dc} line (specified) is then controlled by adjusting the inverter end \textit{dc} voltage with the extinction angle fixed in the inverter. In this case the \textit{ac} voltage at the inverter side will be adjusted by variations of the transformer taps to follow variations in the \textit{dc} voltage.

The results of varying the constant current specification from 0.5 to 0.9 kA are shown in Table 5.1. The table includes the calculated tap position of the inverter end transformer needed to maintain a constant minimum extinction angle of $\gamma_{\text{min}} = 18^\circ$ ensuring a proper margin angle in the link. A 2\% variation in the tap position is observed for a variation of 0.35 pu in current in the link. Table 5.1 also shows the complex powers at the three busbars of the \textit{ac} system at the inverter end. Busbar 3 in Table 5.1 shows the reactive power absorbed at the inverter \textit{ac} busbar.

The results for a current setting of 0.9 kA cannot be included because of violation of the maximum commutation angle of 30\% in the rectifier. This indicates the inapplicability of the conventional formulation in all but a limited set of circumstances.

5.3.2 Modified steady state and equivalent inverter results

In order to highlight the limitations of the steady state formulation two examples have been chosen, one with varying \textit{dc} current and minimum firing angle ($\alpha_{\text{min}}$) and the other with variable firing angle ($\alpha$) and a constant link current setting. In both cases
Figure 5.2 Test system for the conventional and modified loadflows.
Figure 5.3 Diode rectifier with inverter current control.

<table>
<thead>
<tr>
<th>$I_d$</th>
<th>$\gamma_{\min}$</th>
<th>$a_i$</th>
<th>$P_3$</th>
<th>$Q_3$</th>
<th>$P_4$</th>
<th>$Q_4$</th>
<th>$P_5$</th>
<th>$Q_5$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5</td>
<td>-10.59</td>
<td>-10.59</td>
<td>43.07</td>
<td>-15.43</td>
<td>-140.</td>
<td>9.87</td>
<td>96.93</td>
<td>9.25</td>
</tr>
<tr>
<td>0.6</td>
<td>-10.02</td>
<td>-9.44</td>
<td>51.22</td>
<td>-18.69</td>
<td>-140.</td>
<td>11.49</td>
<td>88.78</td>
<td>10.79</td>
</tr>
<tr>
<td>0.7</td>
<td>-9.44</td>
<td>-8.85</td>
<td>59.21</td>
<td>-21.99</td>
<td>-140.</td>
<td>13.16</td>
<td>80.79</td>
<td>12.37</td>
</tr>
<tr>
<td>0.8</td>
<td>-8.85</td>
<td>-8.55</td>
<td>67.05</td>
<td>-25.36</td>
<td>-140.</td>
<td>14.86</td>
<td>72.95</td>
<td>14.00</td>
</tr>
<tr>
<td>0.85</td>
<td>-8.55</td>
<td>-8.55</td>
<td>70.91</td>
<td>-27.06</td>
<td>-140.</td>
<td>15.73</td>
<td>69.09</td>
<td>14.83</td>
</tr>
<tr>
<td>0.90</td>
<td>$\mu_r &gt; 30^\circ$</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 5.1 Conventional loadflow. Diode rectifier bridge ($\alpha = 0^\circ$). $\gamma_{\min} = 18^\circ$. With $a$ in %. P in MW and Q in MVAR.
Table 5.2 Modified loadflow. Diode rectifier bridge ($\alpha = 0^\circ$). $\gamma_{\text{min}} = 18^\circ$. With $a$ in %, $P$ in MW and $Q$ in MVAr.

<table>
<thead>
<tr>
<th>$I_d$ (kA)</th>
<th>$\gamma_i$</th>
<th>$a_i$</th>
<th>$P_3$</th>
<th>$Q_3$</th>
<th>$P_4$</th>
<th>$Q_4$</th>
<th>$P_5$</th>
<th>$Q_5$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.70</td>
<td>$\gamma_{\text{min}}$</td>
<td>-19.99</td>
<td>76.83</td>
<td>-28.57</td>
<td>-140.</td>
<td>16.50</td>
<td>63.17</td>
<td>15.57</td>
</tr>
<tr>
<td>0.80</td>
<td>$\gamma_{\text{min}}$</td>
<td>-17.71</td>
<td>78.91</td>
<td>-29.68</td>
<td>-140.</td>
<td>17.07</td>
<td>61.09</td>
<td>16.12</td>
</tr>
<tr>
<td>0.90</td>
<td>$\gamma_{\text{min}}$</td>
<td>-14.76</td>
<td>80.52</td>
<td>-30.67</td>
<td>-140.</td>
<td>17.58</td>
<td>59.48</td>
<td>16.61</td>
</tr>
<tr>
<td>1.00</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1.25</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 5.3 Equivalent Inverter. Diode rectifier bridge ($\alpha = 0^\circ$). $\gamma_{\text{min}} = 18^\circ$, $a_{\text{max}} = 20\%$. With $\gamma$ in degrees, $a$ in %, $P$ in MW and $Q$ in MVAr.

<table>
<thead>
<tr>
<th>$I_d$ (kA)</th>
<th>$\gamma_i$</th>
<th>$a_i$</th>
<th>$P_3$</th>
<th>$Q_3$</th>
<th>$P_4$</th>
<th>$Q_4$</th>
<th>$P_5$</th>
<th>$Q_5$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.70</td>
<td>$\gamma_{\text{min}}$</td>
<td>-18.93</td>
<td>66.22</td>
<td>-24.00</td>
<td>-140.</td>
<td>14.32</td>
<td>73.78</td>
<td>13.47</td>
</tr>
<tr>
<td>0.80</td>
<td>$\gamma_{\text{min}}$</td>
<td>-14.82</td>
<td>71.81</td>
<td>-26.60</td>
<td>-140.</td>
<td>15.66</td>
<td>68.19</td>
<td>14.76</td>
</tr>
<tr>
<td>0.85</td>
<td>$\gamma_{\text{min}}$</td>
<td>-12.61</td>
<td>74.25</td>
<td>-27.82</td>
<td>-140.</td>
<td>16.29</td>
<td>65.75</td>
<td>15.37</td>
</tr>
<tr>
<td>0.90</td>
<td>$\gamma_{\text{min}}$</td>
<td>-10.27</td>
<td>76.44</td>
<td>-28.98</td>
<td>-140.</td>
<td>16.89</td>
<td>63.56</td>
<td>15.95</td>
</tr>
<tr>
<td>1.00</td>
<td>$\gamma_{\text{min}}$</td>
<td>-5.19</td>
<td>80.10</td>
<td>-31.13</td>
<td>-140.</td>
<td>17.99</td>
<td>59.90</td>
<td>17.04</td>
</tr>
<tr>
<td>1.25</td>
<td>$a_{\text{max}}$</td>
<td>23.06</td>
<td>75.37</td>
<td>-38.62</td>
<td>-140.</td>
<td>21.82</td>
<td>64.63</td>
<td>20.89</td>
</tr>
</tbody>
</table>

the generator excitation is kept constant at the value necessary to achieve the nominal $dc$ voltage of 80.0 kV when the nominal link current is 1.0 kA. The identical excitations are confirmed for the modified steady state and the equivalent inverter case in Figure 5.1 for zero $dc$ current. However in Figure 5.1 for the conventional loadflow the excitation should be controlled to achieve a constant commutating voltage in the link as explained in the previous section.

Tables 5.2 and 5.3 illustrate the major differences between the conventional steady state formulation and the accurate solution based on dynamic simulation characteristics. At the lower end of the current setting range (i.e. below 0.7 kA), the tables show that the arbitrary maximum transformer tap setting of 20% is exceeded when using the conventional formulation, whereas this is not the case in the equivalent inverter results. From
0.7 to 0.9 kA, differences of 30% occur in the calculated values of the inverter tap position and 8% in the calculated values of complex power in the ac system. Further increases in current setting result in violations of the commutation angle limit and thus invalidate any results obtained from the steady state.

On the other hand the equivalent inverter shows that minimum extinction angle is still maintained at a current settings in excess of 1.0 kA and that changeover from tap to \( \gamma \) control takes place at a current less than 1.25 kA. Although the commutation angle cannot be determined in the case of the equivalent inverter model, the results from the dynamic simulation are valid for angles above and below 30°.

The second example is used to illustrate the effect of firing angle variation and the effect on the inverter tap, extinction angle and rectifier commutation angle are given in Table 5.4. The differences in the first row (\( \alpha_{\text{min}} = 0 \degree \)) have already been discussed in the previous example. For firing angles between 5° and 20° both algorithms show a similar pattern but differences of 6.5% are observed in the values of commutation overlap. It is also difficult to judge between the loadflow and modified loadflow as to which gives results closer to those of the equivalent inverter. For \( \alpha = 30 \degree \) only the modified loadflow show a changeover from \( \gamma \) to tap control. For 45° and beyond, all models predict tap control, the actual values of \( \gamma \) differing by up to 33%.

The conventional and modified loadflow show less accurate results when compared with the equivalent inverter. The reason for that is based primarily in the way that the ac-dc systems are represented in steady state at rectifier side.

The conventional loadflow shows a small and constant slope in the dc voltage

<table>
<thead>
<tr>
<th>( \alpha )</th>
<th>Loadflow</th>
<th>Modified loadflow</th>
<th>Equivalent inverter</th>
</tr>
</thead>
<tbody>
<tr>
<td>( a )</td>
<td>( a_i )</td>
<td>( \gamma_i )</td>
<td>( \mu_r )</td>
</tr>
<tr>
<td>0°</td>
<td>-7.26</td>
<td>27.19</td>
<td>-6.71</td>
</tr>
<tr>
<td>5°</td>
<td>-6.11</td>
<td>24.40</td>
<td>-3.97</td>
</tr>
<tr>
<td>10°</td>
<td>-1.23</td>
<td>17.84</td>
<td>6.66</td>
</tr>
<tr>
<td>20°</td>
<td>7.90</td>
<td>14.27</td>
<td>a( \text{max} )</td>
</tr>
<tr>
<td>30°</td>
<td>a( \text{max} )</td>
<td>31.93</td>
<td>11.14</td>
</tr>
<tr>
<td>45°</td>
<td>a( \text{max} )</td>
<td>40.15</td>
<td>10.48</td>
</tr>
<tr>
<td>50°</td>
<td>a( \text{max} )</td>
<td>61.75</td>
<td>9.18</td>
</tr>
<tr>
<td>65°</td>
<td>a( \text{max} )</td>
<td>81.81</td>
<td>8.64</td>
</tr>
</tbody>
</table>

Table 5.4 Results with \( I_d = 1.0 \) kA and variable \( \alpha \). \( \gamma_{\text{min}} = 18 \degree \), \( a_{\text{max}} = 20\% \).
regulation and the commutating voltage changes in magnitude and angle as the load changes. Also the representation of the subtransient reactance as the average of the direct and quadrature axis subtransient reactance for the synchronous machine has its effect in the results. In this case, as shown in the previous comparisons, all the parameters were affected by what is considered a poor representation of the rectifier at sending end.

The modified loadflow show a variable voltage regulation more compatible with the representation of the non-linear unit connected system. In this case, the characteristic is created, as explained in Chapter 2, by the fact that the commutating voltage is kept constant in magnitude but its phase is allowed to change with the load. The subtransient reactance is averaged and made constant, as in the previous model, giving a constant commutating reactance. These approximations made the model closer but still showing unacceptable differences with the equivalent inverter. The modified loadflow model is not satisfactory since dynamic simulation tests made by Sankar[91] shows a non-linear and unpredictable commutating reactance in the rectifier. This commutating reactance can only be accurately incorporated in the steady state analysis by using it included in the \( V_d/I_d \) characteristics obtained from dynamic simulation.

From the above results and discussion it is clear that among the algorithms discussed here only the equivalent inverter can be relied upon to provide realistic loadflow information when the system contains unit connected in-feeds.

5.4 Performance of the equivalent inverter for a thyristor bridge converter

The rectifier characteristics derived from dynamic simulation for different firing angles as in Figure 5.4 are applied to the unit-connection using the equivalent inverter model. In this case, looking at the characteristics for three different values of \( \alpha \) show that even with firing angles uniformly spaced these characteristics are not uniformly spaced. In Figure 5.5, for larger currents, above 1.2 kA, the constant firing angle characteristics cross over so that the maximum values of dc voltage increases for increasing firing angles.

5.4.1 The equivalent inverter results

Using the rectifier characteristics in Figure 5.4, the number of steady state operating points to analyse the equivalent inverter model can be enormous. The cases presented are limited to operating points between \( \alpha_{\text{min}} = 0^\circ \) and \( \alpha_{\text{max}} = 20^\circ \) in the rectifier charts and between 0.8 kA and 1.3 kA in the range of currents as shown in the enlarged picture in Figure 5.5.

The current in the dc link \( (I_d) \) in the test system is increased from 0.8 kA to 1.3 kA in steps of 0.1 kA, as can be seen in Figure 5.6. For each value of dc current, the firing
angle (\(\alpha\)) in the rectifier is increased from a minimum 0° to a 20° with increments spaced by 1°. The variables displayed in this case for the inverter side (for each combination of \(I_d\) and \(\alpha\)) are the tap of the transformer and the commutation and extinction angles.

Figures 5.4 and 5.5 show the \(V_d/I_d\) characteristics extracted from time domain simulation. These characteristics show that for a particular \(I_d\) the dc voltage may rise or decrease when increasing the firing angle \(\alpha\).

For a current of \(I_d = 1.2\, \text{kA}\) the dc voltage decrease with increasing firing angles but for a dc current of 1.3 kA the dc voltage increase from around 54 kV (\(\alpha = 0°\)) to approximately 59 kV (\(\alpha = 5°\)) which represents a temporary gain in capability of 6.5 MVA (8% of the unit power) by only increasing the firing angle by 5°.

In Figure 5.6 the tests for the proposed unit connected system the dc link run at minimum extinction angle (\(\gamma_{\text{min}} = 18°\)) up to the nominal current (1.0 kA) for the previously specified range of firing angles. For values of \(\alpha = 0°\) the bridge can have inverter current control as in Figure 5.7 and for \(\alpha \neq 0°\) is the rectifier current control as in Figure 5.9. With a constant decrease in voltage for \(I_d = 1.1\, \text{kA}\) the extinction angle increases from around 20° to 24° for \(\alpha = 0°\) to 20°. In this case the change in inclination in the curve is due the fact that for this value of current at \(\alpha = 15°\) the tap of the transformer hit its limit and \(\gamma\) that has been controlled is freed.
Figure 5.5  Dc voltage/dc current characteristics in the equivalent inverter extracted from dynamic simulation for $I_d$ from 0.8 to 1.4 kA.

Table 5.5  Control table for the equivalent inverter results.

<table>
<thead>
<tr>
<th>$I_d^{sp}$</th>
<th>$\gamma^{sp}$</th>
<th>$\alpha^{sp}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.8</td>
<td>18°</td>
<td>free</td>
</tr>
<tr>
<td>0.9</td>
<td>18°</td>
<td>free</td>
</tr>
<tr>
<td>1.0</td>
<td>18°</td>
<td>free</td>
</tr>
<tr>
<td>1.1</td>
<td>free</td>
<td>$a_{var1}$</td>
</tr>
<tr>
<td>1.2</td>
<td>free</td>
<td>$a_{var2}$</td>
</tr>
<tr>
<td>1.3</td>
<td>free</td>
<td>$20^\circ$</td>
</tr>
</tbody>
</table>

With $\mu_l = \frac{360^\circ}{n_p}$ being the commutation angle limit in Figure 5.6 (b) and $n_p$ the number of pulses of the inverter bridge. Beyond this commutation limit the steady state equations for the inverter show an unacceptable level of error [Arrillaga et al., 1983].

The use of constant current control can also be applied to the thyristor bridge rectifier as in the diode bridge rectifier. The inverter control in this case can have two out of three variables specified, the current in the inverter side with an specified current margin.
Figure 5.6 Variables at inverter end: (a) transformer tap, (b) commutation angle and (c) extinction angle.
as in Figure 5.7, the extinction angle ($\gamma$) and the tap of the transformer ($a$). The control in Figure 5.7 is used in this case for the rectifier thyristor bridges ($\alpha \neq 0^\circ$). Table 5.5 shows the control table for the equivalent inverter used in Figure 5.6. Table 5.6 shows the tap variation with the current in the link and with the firing angle in the rectifier. In some circumstances with the need of constant extinction angle control (in the inverter), the conventional control as in Figure 5.9 should be used. In the same figure the tap control can be used in the inverter side if commutation angle need to be kept below the precision limit $\mu_l$ for the steady state equations.

The inverter extinction angle $\gamma_i$ is controlled in Figure 5.6 for $I_d = 1.1$ kA and 1.2 kA to avoid inverter commutation angles bigger than $30^\circ$ for a twelve pulse bridge. This limit should be set to keep the results inside the precision range of the inverter steady state equations that still exist in the equivalent inverter.

Figure 5.6 shows that for the maximum current (1.3 kA) the transformer tap is fixed at its maximum value of 20% for the whole range of firing angles. For this current
Figure 5.8 Test system for the Equivalent Inverter.
and firing angle $\alpha$ between 0° and 5°, the extinction angle $\gamma_i$ and the commutation angle $\mu_i$, the slope of the curves have a different sign to those for the remaining values up to $\alpha = 20°$. This is due to the fact that the dc voltage at $I_d = 1.3 \, kA$ and $\alpha = 0°$ is smaller than the dc voltage for the the same current and $\alpha = 5°$. For the subsequent values of firing angles at the same current the dc voltage show a small decrease as can be seen in Figure 5.5.

The tests made so far are based in idealized systems. Verification of the equivalent inverter accuracy is necessary and this is achieved using steady state results from practical tests for the upgraded New Zealand dc link. This results are then compared with equivalent inverter results for the system.
5.5 Performance of the equivalent inverter for the upgraded New Zealand dc link.

In order to verify the equivalent inverter model, some practical test results obtained by TransPower were examined and simulated. The tests were made for commissioning purposes and thus don’t match the requirements of this project exactly. However they are sufficiently similar to the operation of the HVdc link as a unit-connection to make the results of value to this project.

Two sets of test results are compared with the equivalent inverter model. The first test is for the HVdc link running in steady state with several small current order changes made to Pole 1 and the effects recorded. In the second test the current order change is much greater.

In both these tests the HVdc system is not being operated in a manner which directly allows the equivalent inverter to simulate its behaviour. The rectifier terminal is not connected solely to group connected generation and there is a connection via the Benmore transformers to the South Island system. The generators are also on automatic voltage control whereas for validation purposes fixed field operation would have been preferable. However the action of the AVRs is relatively slow and while significant over a 40 seconds test period, it may be taken as a constant during the period that each current order change takes place.

<table>
<thead>
<tr>
<th>$\alpha$</th>
<th>$a_{var1}$</th>
<th>$a_{var2}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$1^\circ$</td>
<td>$17% &lt; a_{var1} &lt; 20%$</td>
<td>$19% &lt; a_{var2} &lt; 20%$</td>
</tr>
<tr>
<td>$2^\circ$</td>
<td>$17% &lt; a_{var1} &lt; 20%$</td>
<td>$19% &lt; a_{var2} &lt; 20%$</td>
</tr>
<tr>
<td>$3^\circ$</td>
<td>$17% &lt; a_{var1} &lt; 20%$</td>
<td>$19% &lt; a_{var2} &lt; 20%$</td>
</tr>
<tr>
<td>$4^\circ$</td>
<td>$17% &lt; a_{var1} &lt; 20%$</td>
<td>$19% &lt; a_{var2} &lt; 20%$</td>
</tr>
<tr>
<td>$5^\circ$</td>
<td>$17% &lt; a_{var1} &lt; 20%$</td>
<td>$20%$</td>
</tr>
<tr>
<td>$10^\circ$</td>
<td>$17% &lt; a_{var1} &lt; 20%$</td>
<td>$20%$</td>
</tr>
<tr>
<td>$15^\circ$</td>
<td>$17% &lt; a_{var1} &lt; 20%$</td>
<td>$20%$</td>
</tr>
<tr>
<td>$16^\circ$</td>
<td>$20%$</td>
<td>$20%$</td>
</tr>
<tr>
<td>$17^\circ$</td>
<td>$20%$</td>
<td>$20%$</td>
</tr>
<tr>
<td>$18^\circ$</td>
<td>$20%$</td>
<td>$20%$</td>
</tr>
<tr>
<td>$19^\circ$</td>
<td>$20%$</td>
<td>$20%$</td>
</tr>
<tr>
<td>$20^\circ$</td>
<td>$20%$</td>
<td>$20%$</td>
</tr>
</tbody>
</table>

Table 5.6 Inverter tap variation for the equivalent inverter with $I_d$ and $\alpha$. 

5.5 Performance of the equivalent inverter for the upgraded New Zealand dc link.
Figure 5.10 shows the basic configuration of the upgraded New Zealand HVdc link. Pole 1 consists mainly of the original equipment (mercury arc valves) connected directly to the Benmore generators and to the rest of the ac system via two transformers. The ac filters are on the centre tap of the transformers. The major change from the original configuration is that the valve groups are now connected series/parallel instead of just series. The two parallel paths operate in unison. The new pole (Pole 2) is connected in series with Pole 1 and is directly connected to the 220 kV system.

5.5.1 The small current increments test

In the first test Pole 1 is given five step changes to its current order and the effect on the system is observed. An objective of the test is to ensure that the dc voltage is maintained constant regardless of the dc current. Each step consists of a 50 Amp. change in current through the link, i.e. 25 Amp. change in each valve group in Pole 1. The change, although called a step, is in practice ramped to reduce the disturbance in the HVdc link and through the power system. For every current step the link is maintained in its normal operating condition as in Figure 5.11; constant current control (CCC) provided by a high gain regulator in the rectifier end (Benmore) and constant extinction angle (CEA) control in the inverter end (Haywards).

The test results for the series of current order changes are shown in Figure 5.12 for the rectifier and Figure 5.13 for the inverter end. A description of the changes as they occur is given below. The steady state periods between ramping of the dc current order are identified by the letters A to E.

Figure 5.12.(a) shows the current order for a valve group at Benmore. The actual dc current is almost identical to the order and has not been shown. Note that the initial change between points A and B consists of two separate 25 Amp. per valve steps.

Figure 5.12.(b) shows the dc voltage (\(V_{d_r}\)) across Pole 1 and it can be seen to be maintaining a constant voltage except during the transient periods. Similarly in Figure 5.12.(c) the firing angle \(\alpha\) maintains a reasonably constant value of 15°.

Figure 5.12.(d) is the no-load voltage (\(V_{d_{ro}}\)) as applied to the rectifiers. This gives a good indication that the AVR's and the ac system beyond the transformers are having some effect on operation and cannot be totally ignored.

Figure 5.13.(a) shows the dc voltage (\(V_{d_i}\)) at the inverter being subjected to regulation as the dc current (\(I_d\)) increases. In Figure 5.13.(b) the extinction angle (\(\gamma\)) at the inverter can be seen as to at first being maintained constant but drops later on during the test. Figure 5.13.(c) shows the no-load voltage (\(V_{d_{io}}\)) applied to the inverter and it can be seen to be dropping as the test proceeds.

Initially the system is operating at a current order of 490 Amp. (per valve). This is shown as point A in Figure 5.12. Current control is with the rectifier. The current ramps to 515 Amp. and immediately after to 540 Amp. (point B) in two distinct operations. The
Figure 5.10 The upgraded New Zealand HVdc link.
transient increase in dc voltage is caused by current changes through the high inductance of the dc link. The transient reduction in $\alpha$ in the rectifier and in $\gamma$ in the inverter is brought about by the rectifier transient increase in the dc voltage. The increased current in the link causes voltage regulation and to prevent further dropping in $V_{d_{\text{st}}}$ the extinction angle reduces transiently below its steady state minimum value. This is a temporary phase during the current ramping which is shown in Figure 5.11. Point 1 is the pre-change operating point and point 2 is a representation of the transitional operating state. On completing the ramping, the link settles down to a new position shown as point 3 where $\gamma$ is back to its steady state minimum value but with a reduced $V_{d_{\text{st}}}$. At the rectifier end, $\alpha$ and $V_{d_{r}}$ are almost back to their initial settings. the influence of the generators AVR's is slight but may be sufficient to explain the small change in $V_{d_{\text{st}}}$.

The ramps from B to C and C to D can be explained in the same way as above but the last ramp from D to E, the conditions change somewhat. The no-load voltage at the inverter ($V_{d_{\text{st}}}$) has now dropped sufficiently to prevent the extinction angle ($\gamma$) recovering to its steady state value which also is necessary to pull the rectifier firing angle $\alpha$ back to its normal value. A further current order increase is shown in the inverter waveforms in Figure 5.13 but data collection at the rectifier ceased before this event occurred.
Figure 5.12 Commissioning results for small current increment test at rectifier end (Benmore):
(a) current order, (b) dc voltage, (c) firing angle, (d) no-load dc voltage in pu.
Figure 5.13 Commissioning results for small current increment test at the inverter end (Haywards): 
(a) dc voltage, (b) extinction angle, (c) no-load dc voltage in kV.
5.5.2 Steady state results for the small current increment test

Table 5.7 shows the commissioning test results for Pole 1 at the points of interest. The steady state tests using the equivalent inverter were set to match the first condition (A) in the tables. The rectifier characteristics for the idealized system require some adjustments to give the operating conditions in point A. In this case all the other curves for different firing angles were also adjusted following the same pattern. The change in the characteristics were in fact to allow an extra 10% in voltage for the same current to match the nominal power of each generator.

In the idealized system the machines are similar to the synchronous generators at Benmore. However some differences can be expected since they are not the exact characteristics for the Benmore generators. The excitation in this case is adjusted to give the voltage for the dc load conditions. The South Island is also connected to the 220 kV at Benmore. The effect of this is a decrease in commutation reactance which will affect the characteristics. The actual loading of the Benmore generators is not known, information given by TransPower NZ mention that four generators were connected during the tests. Equal loading is assumed for each of the generators at Benmore with no load from the rest of the South Island.

In the steady state the results obtained are dependent of the variables specified for the solution of the link. Two different set of solutions are required for each test to obtain the deviation in the results in both ends. In the first set of solutions, in Table 5.8, the specified variables for the operating conditions of the link in points from A to E are: $V_{di}$, $I_d$ and $\gamma_i$.

Comparing Tables 5.7 and 5.8, the decrease of commutation reactance due the connection of the Benmore power station to the rest of the South Island system causes $\alpha_r$ to increase. There is a possibility that this is the cause of much of the 1.7° error in $\alpha_r$ at point A. The results of $\alpha$ for points B to E have almost the same error as was established.
for point A. This is particularly significant in the case of point E where $\gamma$ has had to change from $18^\circ$ to $17^\circ$.

Changing the specified variables in the link to $V_{di}$, $I_d$ and $\alpha_r$, the values in the rectifier are the same as in the commissioning tests. However the noticeable differences are now present in the extinction angles in the inverter, that in this case are allowed to be free as shown in Table 5.9. The errors in $\gamma_i$ are very similar to those obtained for $\alpha_r$ in Table 5.8.

The level of error present in Tables 5.8 and 5.9 is transferred to the reactive power demand in the rectifier or inverter depending of the variables that has been specified. Unfortunately no total MVAR data is available from the commissioning tests so this error could not be checked.

<table>
<thead>
<tr>
<th>Point</th>
<th>$I_d^{sp}$ (A)</th>
<th>$V_{dr}$ (kV)</th>
<th>$\alpha_r$ (deg.)</th>
<th>$P_{d_r}$ (MW)</th>
<th>$V_{di}^{sp}$ (kV)</th>
<th>$\gamma_i^{sp}$ (deg.)</th>
<th>$P_{d_i}$ (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>980.</td>
<td>270.</td>
<td>16.26</td>
<td>264.6</td>
<td>255.</td>
<td>18.</td>
<td>250.</td>
</tr>
<tr>
<td>B</td>
<td>1080.</td>
<td>270.</td>
<td>16.64</td>
<td>291.6</td>
<td>253.5</td>
<td>18.</td>
<td>274.</td>
</tr>
<tr>
<td>C</td>
<td>1130.</td>
<td>270.</td>
<td>16.49</td>
<td>305.2</td>
<td>253.</td>
<td>18.</td>
<td>286.</td>
</tr>
<tr>
<td>D</td>
<td>1180.</td>
<td>270.</td>
<td>16.69</td>
<td>318.6</td>
<td>252.5</td>
<td>18.</td>
<td>298.</td>
</tr>
<tr>
<td>E</td>
<td>1230.</td>
<td>270.</td>
<td>16.76</td>
<td>332.2</td>
<td>252.</td>
<td>17.</td>
<td>310.</td>
</tr>
</tbody>
</table>

Table 5.8 Steady state results from *Equivalent Inverter*. Current control in the rectifier.

<table>
<thead>
<tr>
<th>Point</th>
<th>$I_d^{sp}$ (A)</th>
<th>$V_{dr}$ (kV)</th>
<th>$\alpha_r^{sp}$ (deg.)</th>
<th>$P_{d_r}$ (MW)</th>
<th>$V_{di}^{sp}$ (kV)</th>
<th>$\gamma_i$ (deg.)</th>
<th>$P_{d_i}$ (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>980.</td>
<td>270.</td>
<td>14.5</td>
<td>264.6</td>
<td>255.</td>
<td>16.02</td>
<td>250.</td>
</tr>
<tr>
<td>B</td>
<td>1080.</td>
<td>270.</td>
<td>15.</td>
<td>291.6</td>
<td>253.5</td>
<td>16.17</td>
<td>274.</td>
</tr>
<tr>
<td>C</td>
<td>1130.</td>
<td>270.</td>
<td>15.</td>
<td>305.2</td>
<td>253.</td>
<td>16.31</td>
<td>286.</td>
</tr>
<tr>
<td>D</td>
<td>1180.</td>
<td>270.</td>
<td>15.</td>
<td>318.6</td>
<td>252.5</td>
<td>16.09</td>
<td>298.</td>
</tr>
<tr>
<td>E</td>
<td>1230.</td>
<td>270.</td>
<td>15.</td>
<td>332.2</td>
<td>252.</td>
<td>15.13</td>
<td>310.</td>
</tr>
</tbody>
</table>

Table 5.9 Steady state results from *Equivalent Inverter*. Current control in the inverter.
5.5.3 The large current increment test

In the second test, the bi-pole is transferring 540 MW, and Pole 2 is dropped. This is a severe test with the mercury arc valves in Pole 1 (old dc link) jumping from 220 MW to 540 MW. The current in each half of Pole 1 (poles 1A and 1B) have a large current step from 0.5 kA to 1.0 kA.

Point A in Figure 5.15.(a) shows the current in Pole 1 before the current step, at this point the system is working at CCC in the rectifier and at CEA in the inverter as in the steady state control diagram shown in Figure 5.11. The initial firing angle in the rectifier is 15° and the initial extinction angle is 18° in the commissioning tests. The controls are set to keep the voltage constant in the rectifier end as in Figure 5.15.(b).

The initial operating state is as in point 1 in Figure 5.11. After the loss of Pole 2 it is in position 4 in Figure 5.14. During the current step the control in the link is switched to CCC in the inverter, as in Figure 5.14, due to the inability of the controls to keep the firing angle and the extinction angle constants under such a large current step. The current jumps to \( I_{d4} \) and the operating point moves from point 1 as in Figure 5.11 to point 4 in Figure 5.14 with the firing angle decreasing to its minimum. In this case Figure 5.15.(c) shows the firing angle in the rectifier decreasing to its minimum of 5°. This action keeps the \( dc \) voltage at the rectifier at its nominal value.

Because of the step in current, there is an initial drop in voltage. This voltage is also
affected by the changes in current in the smoothing reactors in the HVdc link. The firing angle then has a high initial post fault value. This causes the no-load voltage to decrease initially, but eventually it reaches a steady state point B at a slightly higher value than before the step in current. However, it can be seen in Figure 5.15.(d) that there are two small step reductions in the no-load voltage \( V_{dl} \) which suggest that On Line Tap Changer (OLTC) is being used to keep the rectifier \( dc \) voltage at its nominal value.

Figure 5.16.(a) shows some increased regulation in the \( dc \) voltage in the inverter end for steady state point B. Figure 5.16.(b) show an initial increase in \( \gamma \) due the fault condition which quickly drop back to its original value. The slower increase in \( \gamma \) from 18 to 24° is due to the change in \( V_{dio} \) as in Figure 5.16.(c).

### 5.5.4 Steady state results for the large current increment test

Two steady state simulations are made in this case in the same way as in the small steps current tests, one test before and another after the change in current. The first simulation (point A) results from specifying the \( dc \) voltage in the inverter, the \( dc \) current and the inverter extinction angle. Figures 5.15 and 5.16 show the simulation results.

The dropping of Pole 2 switches the control to CCC in the inverter in order to allow the firing angle to be reduced to its minimum in the rectifier. The inverter \( dc \) voltage is kept the same with the specified reduction of the firing angle in the rectifier. The result is an increase in the extinction angle in opposition to the tendency of the no-load voltage to increase at that end when switching to CCC. In this case the rectifier firing angle specified is the 5° obtained from the commissioning test results. Thus the second test (point B) is achieved by specifying the firing angle in the rectifier, the \( dc \) voltage in the inverter and the \( dc \) current.

Table 5.12 shows an error of 2° for the extinction angle at the inverter which compares well with the 1.7° error obtained in the pre-fault result. The rectifier \( dc \) voltage is kept the same by the decrease of firing angle and the new \( dc \) voltage regulation is controlled by the reactive power demand increase in the inverter end. The reactive power demand

<table>
<thead>
<tr>
<th>Point</th>
<th>( I_d ) (A)</th>
<th>( V_{dr} ) (kV)</th>
<th>( \alpha_r ) (deg.)</th>
<th>( P_{dr} ) (MW)</th>
<th>( V_{di} ) (kV)</th>
<th>( \gamma_t ) (deg.)</th>
<th>( P_{di} ) (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>980.</td>
<td>270.</td>
<td>15.</td>
<td>264.6</td>
<td>255.</td>
<td>18.</td>
<td>250.</td>
</tr>
</tbody>
</table>

Table 5.10 Large current step steady state commissioning test results at rectifier and inverter ends of Pole 1.
Figure 5.15 Commissioning results for large current increment test at rectifier end (Benmore): (a) current order, (b) dc voltage, (c) firing angle, (d) no-load dc voltage in pu.
Figure 5.16 Commissioning results for large current increment test at the inverter end (Haywards):
(a) de voltage, (b) extinction angle, (c) no-load de voltage in kV.
5.6 Conclusions

Results from steady state techniques have been shown to be in considerable error when the generators contain rotor saliency (probably the most common case for remote generating plant). The new concept, called the equivalent inverter which uses unit-connection characteristics derived from time domain simulation, has been presented as a better alternative for general loadflow studies involving unit connected schemes.

Averaging of reactances in conventional steady state was shown in Tables (5.1) to (5.4) to contribute to the inadequacy of the steady state analysis of salient pole unit connected generators. In this case, for accuracy, a dynamic related technique should be built [Sudhoff, 1992a].

With the results obtained from steady state techniques it has become clear that the best option to study the unit-connection in the steady state is with a dynamic simulation related technique. However the steady state algorithm can be restricted in its application to large power conditions due to the large commutation overlap caused by the extra reactance of the unit-connection. One restriction that should be eliminated is the long

<table>
<thead>
<tr>
<th>Point</th>
<th>$I_{sp}^d$ (A)</th>
<th>$V_{dr}$ (kV)</th>
<th>$\alpha_r$ (deg.)</th>
<th>$P_{dr}$ (MW)</th>
<th>$V_{sp}^d_i$ (kV)</th>
<th>$\gamma_i^{sp}$ (deg.)</th>
<th>$P_{di}$ (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>980.</td>
<td>270.</td>
<td>15.74</td>
<td>264.6</td>
<td>255.</td>
<td>18.</td>
<td>250.</td>
</tr>
</tbody>
</table>

Table 5.11 Point A for the large current step test result for the Equivalent Inverter. Current control in the rectifier.

<table>
<thead>
<tr>
<th>Point</th>
<th>$I_{sp}^d$ (A)</th>
<th>$V_{dr}$ (kV)</th>
<th>$\alpha_r^{sp}$ (deg.)</th>
<th>$P_{dr}$ (MW)</th>
<th>$V_{sp}^d_i$ (kV)</th>
<th>$\gamma_i$ (deg.)</th>
<th>$P_{di}$ (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>B</td>
<td>2000.</td>
<td>270.</td>
<td>5.</td>
<td>540.</td>
<td>230.</td>
<td>22.0</td>
<td>460.</td>
</tr>
</tbody>
</table>

Table 5.12 Point B for the large current step test results for the Equivalent Inverter. Current control in the inverter.
time spent getting the system into the steady state. The mentioned restriction can be eliminated by the utilization of an algorithm that can provide fast and accurate initial conditions for the system modelled using the time domain state variable technique.

A fast steady state algorithm [Garcia, 1990], to be applied to HVdc systems, is under investigation at Canterbury University. This technique is studied to obtain better steady state initial conditions for systems with non-linear elements, as in the direct connected schemes, taking advantage of an hybrid analysis in the time and frequency domain. This algorithm should be able to quickly find reasonable initial conditions for a non-linear system by obtaining the solution over one cycle of the voltages and currents in the system under analysis.

The steady state results from the *equivalent inverter* compared with the commissioning tests show small errors in the angles, at the rectifier and inverter ends for both tests. If specific characteristics for the Benmore generator system had been available and if the South Island had not been connected, it could be assumed that better results would have been obtained. Despite the deficiencies the results justify the use of the *equivalent inverter*.

Results from the conventional and modified loadflow models were not compared with the *equivalent inverter* for the New Zealand upgraded HVdc link for two reasons. The first is due to the difficulty in establishing the initial operating point A for both commissioning tests. The second reason is that previous results for idealized models have shown very large commutation angles which when greater than 30° (twelve pulse operation) invalidate the converter steady state dc equations.

The theoretical and practical results obtained from the *equivalent inverter* clearly show that the combination of steady state techniques with time domain simulation is at the moment the fastest feasible option to the solution of HVdc direct connected systems in steady state. The steady state model of the unit-connection will be very useful when large systems must be analysed and where the use of a dynamic analysis program would be prohibitive.
Appendix 5A  Theoretical model system components

All following parameters are quoted in per unit except where otherwise specified.

(1)  Salient Pole Generator

Rating: 100 MVA
Terminal Voltage: 13.8 kV
Direct-axis Reactance: 1.2
Quadrature-axis Reactance: 0.8
Direct-axis Subtransient Reactance: 0.2
Quadrature-axis Subtransient Reactance: 0.367

(2)  DC Link

Type: 12 pulse
Nominal Current: 1.0 kA
Nominal Voltage: 80.0 kV

(3)  Converter Transformer(s)

Rating: 50 MVA
Reactance: 5%
Voltage: 13.8-30.36 kV

(4)  System Branch Impedances

The system branch impedances in p.u. for the test systems in Figures 5.2 and 5.8 are given in Table 5.13.

(5)  System Conditions

Table 5.14 shows the initial voltage, generation and load conditions for the test systems in Figures 5.2 and 5.8. $P_r$, $Q_r$, $P_i$ and $Q_i$ are the active and reactive injections at the converters busbars obtained from dc equations.
<table>
<thead>
<tr>
<th>Branch</th>
<th>$R_L$</th>
<th>$X_L$</th>
<th>$B_L$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$L_1$</td>
<td>0.</td>
<td>0.9165</td>
<td>0.</td>
</tr>
<tr>
<td>$L_2$</td>
<td>0.</td>
<td>0.0400</td>
<td>0.</td>
</tr>
<tr>
<td>$L_3$</td>
<td>0.</td>
<td>0.0300</td>
<td>0.</td>
</tr>
<tr>
<td>$L_4$</td>
<td>0.</td>
<td>0.0300</td>
<td>0.</td>
</tr>
</tbody>
</table>

Table 5.13 System branch impedances.

<table>
<thead>
<tr>
<th>Busbar</th>
<th>Type</th>
<th>$V_{(pu)}$</th>
<th>$P$ (MW)</th>
<th>$Q$ (MVAR)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bus 1</td>
<td>Slack</td>
<td>1.548</td>
<td>—</td>
<td>—</td>
</tr>
<tr>
<td>Bus 2</td>
<td>P,Q</td>
<td>—</td>
<td>$P_r$</td>
<td>$Q_r$</td>
</tr>
<tr>
<td>Bus 3</td>
<td>P,Q</td>
<td>—</td>
<td>$P_i$</td>
<td>$Q_i$</td>
</tr>
<tr>
<td>Bus 4</td>
<td>Slack</td>
<td>1.000</td>
<td>—</td>
<td>—</td>
</tr>
<tr>
<td>Bus 5</td>
<td>P,V</td>
<td>1.000</td>
<td>$P = -140.$</td>
<td>—</td>
</tr>
</tbody>
</table>

Table 5.14 AC system initial conditions.
Chapter 6

THE DC RIPPLE REINJECTION APPLIED TO THE UNIT-CONNECTION IN STEADY STATE.

6.1 Introduction

In order to keep the amount of filtering to a suitable low level without complex bridge configuration, the twelve pulse double six-pulse bridge has become standard for high voltage ac-dc conversion.

For a six pulse bridge unit connected scheme the number of commutations are halved relatively to the standard twelve-pulse ac-dc conversion system. The reduction of commutations in this case are the cause of inadmissible harmonic content. The two widely used methods of decreasing the harmonic output from converters are the conventional use of filters and the increase of pulse number by harmonic reinjection.

Figure 6.1 shows an alternative six-pulse bridge with harmonic reinjection which consists of the combination of a single main bridge and an auxiliary feedback dc ripple reinjection bridge. This increases the pulse number from 6 to 12 and can be compared with the standard 12 pulse configuration. The dc ripple reinjection technique modelled here is basically one of the possible arrangements of components in ac-dc generation described by Baird[81] to show an efficient decreasing in the harmonics in the ac and dc sides.

Filtering needs in the standard twelve-pulse scheme can be reduced drastically by using a system with an artificially increased number of pulses[Villablanca, 1992]. The use of passive filters is not an option with variable-frequency operation, in this case the ripple reinjection technique becomes the most viable alternative to the harmonic elimination.

One of the advantages of the dc ripple reinjection scheme is that, because only one bridge is required for twelve pulse operation, high commutation reactances, as experienced in the unit-connection, will not cause the commutation angle to exceed the limit of $\frac{2\pi}{np}$. The steady state operation of the scheme has therefore been analysed as part of this work.

This chapter first presents the concepts of the dc ripple reinjection scheme and then
derives a steady state model suitable for inclusion into the loadflow program. From this an operating chart is produced. This is based on the same size generator-converter unit as used in previous chapters, permitting direct comparison of capability.

6.2 The dc ripple reinjection technique

Static converter systems produce a ripple voltage at their dc output. With six-pulse rectification the ripple has a frequency six times of the nominal frequency. However, with respect to the star point of the converter side transformer windings, each dc pole has a non-sinusoidal third-harmonic ripple voltage. These voltages between points A-N and B-N in Figure 6.1 have the same phase relationships in each pole and are referred to in this chapter as common mode dc ripple voltages [Arrillaga, 1983].

In Figure 6.1 the circuit shown between points A, N, B and A', B' is the feedback circuit with the two single phase reinjection transformers (T) and the reinjection bridge [Arrillaga et al., 1992a]. The reinjection bridge create a voltage which is added to the normal six-pulse dc output voltage doubling the number of pulses of the output voltage, between points A' and B'. The dc ripple reinjection is generally applicable to converters with natural commutation and 120° conducting valves. The converter transformer should have star connection on the rectifier side and must have a Δ primary.

The principle of ripple reinjection, used as a basis for the proposed harmonic re-
duction system is explained in the following subsections with reference to its components, main advantages and limitations.

6.2.1 The triple-frequency source

The two single phase transformers (called the feedback transformers) (T) are connected in series on the primary and secondary sides but with reverse polarity. Both primary windings are connected to the common mode dc ripple voltages. These transformers provide the commutating voltages to a single-phase full wave rectifier (called the feedback converter) and are connected to the transformers secondary windings.

The output of the feedback converter is connected in series with the dc output of the six-pulse converter. The frequency of harmonic injection is determined by the supply frequency and therefore the problem of synchronizing the harmonic source with the mains frequency does not exist.

6.2.2 Current injection, phase control and harmonic reduction

In the twelve-pulse configuration the phase currents of the two six pulse series converters are combined in the conversion process to obtain a dc voltage output with harmonic spectrum being $12n$ with $n$ being a positive integer[Arrillaga et al., 1985].

In the dc ripple reinjection model previously proposed by Baird[81] and Villablanca[92], the dc output of the six-pulse converter is superimposed to the feedback converter output which produce a dc waveform as in the twelve-pulse bridge converter. The feedback converter works as a current source with an ac current (triple frequency) proportional to the dc current, the proportionality constant being the feedback transformer ratio (Fb). The harmonic injection assumes the current wave shape of the dc load current. For a fully inductive dc load, the ideal harmonic injection is a square wave[Baird, 1981].

A Fourier analysis of the ac waveforms show that for a particular ratio of the injected current to main rectifier current ratio all harmonics of order $6n \pm 1$ (where $n=1,3,5,\ldots$) are zero while the other harmonic orders (i.e. $n=2,4,6,\ldots$) retain the same relationship with the fundamental as in the original twelve-pulse converter[Villablanca and Arrillaga, 1992]. The result is that the original six-pulse converter configuration has been converted into a twelve-pulse converter system from the point of view of ac and dc system harmonics[Baird, 1981].

The problem of injected current phase adjustment is solved by using controlled-rectifier feedback. The firing angle control of the feedback converter is thus locked to the main rectifier control, i.e. the firing of the thyristors of the feedback converter is a fixed angle after the corresponding main converter thyristors.
6.2.3 The power dissipation

When the main converter operates as a rectifier the feedback converter acts also as a rectifier. The increased fundamental current, predicted by Fourier Analysis [Baird, 1981] is dissipated into the the dc load (injected in the inverter). The feedback converter is able to convert fundamental ac power to dc power and vice versa and being able to operate as an inverter and taking power from the dc side to provide the feedback current.

The full six-pulse bridge converter with feedback converters can clearly be suitable for pumping storage schemes where the direction of the power changes frequently. In this case for a fixed dc voltage the dc current is reduced because of the back emf of the feedback converter and the fundamental current is correspondingly reduced. The dc power input to the main rectifier is about 93% of the ac output power, with the remaining ac output being provided by the feedback converter.

6.2.4 Advantages and limitations of the reinjection scheme

It is apparent that with the proposed increased pulse number scheme several advantages can be found:

i. The feedback converter is automatically synchronized with the source frequency.

ii. The amplitude of the injected current is defined by the dc load current and the feedback transformer ratio.

iii. The phase of the injected current can be adjusted easily by firing control of the feedback converter (it should be noted that where the main converter operates with zero degree firing delay, the feedback converter may have ordinary diodes in it which will be correctly commutated with the common mode dc ripple voltage).

iv. More than one harmonic order is nullified at the correct operating point, as explained in subsection 6.2.2.

v. The output from the feedback converter is in series with the main converter and the power that could dissipate in the triple frequency source is, in this case, effectively dissipated in the dc load.

It is apparent in Figure 6.1 that a variation of the capacitance (C) affects the voltage across the feedback transformer [Baird, 1981] which in turn affects the resulting dc ripple voltage. The capacitor (C) can always be selected for minimum cost which is given by equation (6.1), where V is the peak value of the commutating ac voltage in the main converter

\[
C_{\text{min}} = \frac{I_d F b 2 \pi}{12 f 3 \sqrt{3} V} \quad \text{(Farads)} \quad (6.1)
\]
In the case of different dc loading conditions the feedback transformer (I) need to be designed for the resultant voltage and for a range of primary \((I \times Fb)\) and secondary currents \((I)\). It should be noted that magnetizing voltage in this case is at three times the fundamental frequency with a consequent reduction in core area and in the size and cost of the single phase transformers. An economical design can be obtained for the triple frequency reinjection transformers by combining the design of capacitors and transformers for minimum cost.

There are some limitations to the steady state operation of the dc ripple reinjection scheme:

i. The magnetizing current in the reinjection transformers should be kept low to avoid distortion of the current waveform.

ii. The finite value of the blocking capacitors \((C)\). The modified ac current waveform is unaffected by the size of blocking capacitors while the dc voltage waveform will contain some six-pulse modulation. A relatively large capacitor should be used to keep the levels of sixth harmonic voltage within reasonable limits. However the invariant polarity of the dc voltage in unit connected schemes permits the use of electrolytic capacitors in the feedback circuit with considerable size and cost reductions.

iii. The elimination of harmonics depends on the ratio of feedback current (through the feedback thyristors or diodes) to the dc current in the link. It means that for a correct harmonic elimination and twelve pulse generation all the parameters in the model should be set up for a defined operation point or a range of dc load conditions.

### 6.3 The model

The system was modelled in steady state using the Newton-Raphson fast decoupled ac-dc loadflow. This study was made using the ac-dc loadflow in order to overcome the impossibility of correct modelling the feedback circuit (between points A-A’ and B-B’ in Figure 6.1) in EMTDC (Electromagnetic Transient Direct Current Program). Due the existence of a function that directly relates the commutating voltage and firing angle to the direct voltage in the rectifier, \(V_d = f(E_c, \alpha)\), the time consuming steady state simulations using TCS can be avoided. \(E_c\) is the ac rms commutating voltage.

The steady state modelling is based entirely in the ideal shapes of the waveforms in the ac and dc sides. Some factors that contribute for the distortions in the waveforms, such as the magnetizing current in the transformers, are neglected in steady state but should be considered in a step by step dynamic simulation.

With reference to the single bridge converter with dc ripple reinjection the implementation of the model in steady state involves the assessment of dc average voltage
which is obtained with an infinite dc smoothing reactance, this voltage consists of three components, i.e.

the six-pulse ripple  \[ \frac{\sqrt{3}}{2} V \sin \left( x - \frac{\pi}{6} \right) \quad \frac{\pi}{6} + \alpha < x < \frac{5\pi}{6} + \alpha \]

feedback ripple  \[ - \frac{F_b}{2} V \sin \left( x - \frac{2\pi}{3} \right) \quad \frac{\pi}{2} + \alpha < x < \frac{2\pi}{3} + \alpha \]

\[ + \frac{F_b}{2} V \sin \left( x - \frac{2\pi}{3} \right) \quad \frac{2\pi}{3} + \alpha < x < \frac{5\pi}{6} + \alpha \]

where \( F_b \) is the ratio of feedback current to dc current through thyristors (diodes) in the reinjection bridge. That is, it is the ratio of the feedback converter transformers.

### 6.3.1 The determination of feedback ratio

The feedback ratio is obtained based in the Fourier analysis of the waveform of one phase with inductive dc load (Figure 6.2) with the assumption that the system has infinite dc inductance and zero commutating reactance.

Figure 6.2 can be represented by the following Fourier series:

\[ a_n = \frac{4}{\pi} \int_0^{\pi/2} f(x) \sin(nx) \, dx \quad (6.2) \]

which represents an odd function plus a half wave symmetry. Where:
\[ f(x) = 0 \quad 0 \leq x < \frac{\pi}{6} \]
\[ f(x) = 1 - F_b \quad \frac{\pi}{6} \leq x < \frac{\pi}{3} \]
\[ f(x) = 1 + F_b \quad \frac{\pi}{3} \leq x \leq \frac{\pi}{2} \]

and

\[
a_n = \frac{4}{\pi} \left[ \int_{\frac{\pi}{6}}^{\frac{\pi}{3}} (1 - F_b) \sin (nx) \, dx + \int_{\frac{\pi}{3}}^{\frac{\pi}{2}} (1 + F_b) \sin (nx) \, dx \right]
\]
\[
= \frac{4}{\pi} \left[ \frac{(1 - F_b)}{n} \left( - \cos(nx) \right) \bigg|_{\frac{\pi}{6}}^{\frac{\pi}{3}} + \frac{(1 + F_b)}{n} \left( - \cos(nx) \right) \bigg|_{\frac{\pi}{3}}^{\frac{\pi}{2}} \right] 
\]  
(6.3)

where \( n = 1, 3, 5, 7, \ldots \)

For \( n=1 \):

\[
a_1 = \frac{4}{\pi} \left[ \frac{\sqrt{3}}{2} + F_b \left( -\frac{\sqrt{3}}{2} + 1 \right) \right]
\]

For \( n=5 \):

\[
a_5 = \frac{4}{5 \pi} \left[ -\frac{\sqrt{3}}{2} + F_b \left( \frac{\sqrt{3}}{2} + 1 \right) \right]
\]

In general can be shown that for \( n = 1, 11, 13, 23, \ldots \)

\[
a_n = \frac{4}{n \pi} \left[ \frac{\sqrt{3}}{2} + F_b \left( -\frac{\sqrt{3}}{2} + 1 \right) \right]
\]

and for \( n = 5, 7, 17, 19, \ldots \)

\[
a_n = \frac{4}{n \pi} \left[ -\frac{\sqrt{3}}{2} + F_b \left( \frac{\sqrt{3}}{2} + 1 \right) \right]
\]

These harmonics are eliminated by making \( a_n = 0 \), in this case \( F_b \left( \frac{\sqrt{3}}{2} + 1 \right) = \frac{\sqrt{3}}{2} \), i.e. :

\[
F_b = \frac{\sqrt{3}}{2 + \sqrt{3}} = 0.4641
\]

The feedback transformer's ratio shows a constant value of 46.41\% to eliminate the dc voltage and ac characteristic harmonics when increasing the number of pulses from six to twelve. The feedback transformer ratio also depends on the number of pulses that will be generated by the unit-connected scheme with dc ripple reinjection [Arrillaga et al., 1992a].
6.3.2 Relevant equations

When combining the $dc$ current with the triple frequency source (feedback transformers) current the waveform shape in each phase for an inductive $dc$ load is given in Figure 6.2. Figures 6.3 and 6.4 shows respectively the synthesis of the standard twelve-pulse current waveform and the current waveform obtained in the six-pulse reinjection scheme. Figure 6.4 also shows the superposition of the rectifier current before modification and the triple frequency injected current. To obtain the final twelve-pulse waveform Figure 6.4 shows the combination of two phase waveforms as in Figure 6.2 displaced by 120°.

Equation (6.4) shows how the ripple reinjection technique add up the waveforms
Figure 6.4 Twelve-pulse waveform obtained from harmonic reinjection for an inductive dc load.

- (a) rectifier current
- (b) triple-frequency current
- (c) phase current, rectifier winding
- (d) second phase
- (e) resultant phase current
with six-pulse ripple and the two components of the triple frequency feedback injection in order to obtain the average dc voltage.

\[ V_{d_{avg}} = \frac{3V}{\pi} \left[ \int_{\frac{\pi}{2} + \alpha}^{\frac{3\pi}{2} + \alpha} \sqrt{3} \sin(x - \pi) \, dx - \int_{\frac{\pi}{2} + \alpha}^{\frac{3\pi}{2} + \alpha} \frac{Fb}{2} \sin(x - \frac{2\pi}{3}) \, dx \right. \\
\left. + \int_{\frac{3\pi}{2} + \alpha}^{\frac{5\pi}{2} + \alpha} \frac{Fb}{2} \sin(x - \frac{2\pi}{3}) \, dx \right] \quad (6.4) \]

Integrating the three previous components of the ripple reinjection scheme as in equation (6.4) between the given limits [Baird, 1981] yields

\[ V_{d_{avg}} = \frac{3V}{2\pi} \left[ \sqrt{3} + Fb \left( 2 - \sqrt{3} \right) \right] \cos \alpha \quad (6.5) \]

To obtain the r.m.s. dc voltage the three superimposed components should be integrated according the equation that follows [Baird, 1981].

\[ V_{d_{rms}} = \frac{V}{\pi} \left[ \int_{\frac{\pi}{2} + \alpha}^{\frac{3\pi}{2} + \alpha} \left( -\frac{Fb}{2} \sin(x - \frac{2\pi}{3}) \right) + \frac{\sqrt{3}}{2} \sin(x - \frac{\pi}{6}) \right] dx \]

\[ + \frac{V}{\pi} \left[ \int_{\frac{3\pi}{2} + \alpha}^{\frac{5\pi}{2} + \alpha} \left( \frac{Fb}{2} \sin(x - \frac{2\pi}{3}) \right) + \frac{\sqrt{3}}{2} \sin(x - \frac{\pi}{6}) \right] dx \]

\[ (6.6) \]

With equation (6.6) the first part of the dc voltage expression is obtained for the rectifier and suitable to be used in the steady state formulation.

\[ V_{d_{rms}} = \frac{V}{\sqrt{\pi}} \sqrt{4 \sqrt{3} \left( \frac{\pi}{6} \frac{\sqrt{3}}{4} \cos(2\alpha) + \frac{\sqrt{3}}{2} Fb \cos(2\alpha) + \frac{\pi}{2} + \frac{3\sqrt{3}}{4} \cos(2\alpha) \right)} \quad (6.7) \]

The effective dc voltage equation applied in the ac-dc loadflow is given in the following equation which includes the voltage regulation term

\[ V_d = V_{d_{rms}} - \frac{3}{\pi} x_e I_d \quad (6.8) \]

where \( V_d \) is a function of \( V \) and the firing angle \( \alpha \).

In equation (6.5) at \( \alpha = 0 \) and \( Fb = 0 \) which means no ripple reinjection:

\[ V_{d_{avg}} = \frac{3\sqrt{3}V}{2\pi} \]

at \( \alpha = 0 \) and \( Fb = \frac{\sqrt{3}}{2+\sqrt{3}} \) the ripple reinjection gives:

\[ V_{d_{avg}} = \frac{3V}{\pi} \left[ \frac{\sqrt{3}}{2} \left( 1 + \frac{2 - \sqrt{3}}{2 + \sqrt{3}} \right) \right] \]

which is a percentage increase of 7.18% in the average dc voltage.
In equation (6.7) at $\alpha = 0$ and $Fb = 0$ which means no ripple reinjection:

$$V_{d_{\text{rms}}} = V \times 0.8277$$

at $\alpha = 0$ and $Fb = \frac{\sqrt{3}}{2+\sqrt{3}}$ the ripple reinjection gives:

$$V_{d_{\text{rms}}} = V \times 0.8864$$

which is a percentage increase of 7.09% in the rms dc voltage. Figure 6.5 shows the voltage regulation charts in both cases.

The substitution of $Fb$ by $\frac{\sqrt{3}}{2+\sqrt{3}}$, which has its value in order to eliminate the $6n \pm 1$ ($n=1,3,5$) current harmonics in the ac side, results in an increase over $V_{d_{\text{avg}}}$ of 7.18% and over $V_d$ of 7.09% with respect of the twelve-pulse dc output [Baird, 1981]. This allows a corresponding reduction in the secondary voltage of the transformer by 7.18%.

Power capability charts in steady state are discussed in section 6.4, where the unit connected twelve-pulse scheme and the six-pulse scheme with reinjection are compared. The unit connected scheme was simulated using the modified loadflow model for varying commutating voltages discussed in Chapter 2 and the six-pulse bridge with dc ripple reinjection was also simulated in the modified ac-dc loadflow with the dc voltage in the rectifier side being described by equation (6.8).
6.3.3 The implementation of the model

The model is implemented using the modified algorithm for varying commutating voltages as explained in Chapter 2, subsection 2.4.1. Also in this case the unknown commutating voltage should be calculated in its magnitude and phase during the iterative process to satisfy the vectorial diagram in Figure 2.3.

The vector of residual equations in the rectifier end has seven variables as in equation (6.9) and seven equations are needed to formulate the loadflow problem at the rectifier end in the same fashion as in subsection 2.4.1 from equations (2.24)–(2.30).

\[ x = [V_d, I_d, \cos \alpha, \phi, E, E', \beta]^T \]  

(6.9)

In the seven equations in subsection 2.4.1 only equation (2.25) is modified in steady state to include the dc ripple reinjection effect. The new equation in the set being:

\[ V_d - V_{d_{\text{rms}}} - \frac{3}{\pi} x_c I_d = 0 \]  

(6.10)

In equation (6.10) the commutating voltage is in the \( V_{d_{\text{rms}}} \) voltage expression in equation (6.7) since \( V \) is the peak voltage in the commutating busbar. The difference in this case with the proposed equivalent inverter is that here an equation could be found...
to represent the \( dc \) voltage dependence of the commutating voltage and the rectifier firing angle.

### 6.4 Results and discussion

To verify the theoretical assumptions described in previous sections the model was implemented in steady state using both the conventional twelve-pulse bridge and the six-pulse bridge with \( dc \) ripple reinjection. The first results to be compared are the power conversion capabilities in steady state in each case.

The conversion process in steady state for the conventional twelve-pulse bridge is represented by the following expression:

\[
V_d = \frac{3\sqrt{2}}{\pi} E_c \cos \alpha - \frac{3}{\pi} x_c I_d \tag{6.11}
\]

In the normal twelve-pulse unit connected scheme (without filters), the commutation voltage \( E_c \) is assumed to be the generator internal e.m.f. behind subtransient reactance [Arrillaga et al., 1991], which for a given excitation varies with the load. Therefore equation (6.11) will be non-linear with the terminal \( dc \) voltage reducing with increasing \( dc \) currents. The same considerations can be made using equation (6.8) for the six-pulse scheme with reinjection. Figure 6.6 shows the results of both schemes together.

The power conversion capability is discussed with reference to the generator and transformer parameters given in Appendix 5. Maximum \( dc \) output characteristics, are shown in Figure 6.6 for the conventional double bridge converter i.e. with \( \alpha = 0^\circ \) for \( \mu < 30^\circ \) and \( \alpha \) minimum for \( \mu = 30^\circ \), and the proposed twelve-pulse single bridge configuration with ripple reinjection i.e. with \( \alpha = 0^\circ \) for \( \mu < 60^\circ \) and \( \alpha \) minimum for \( \mu = 60^\circ \).

In the mentioned test system, the two configurations are designed to operate at a common nominal point (i.e. point A). However, for the same excitation the capability to provide temporary extra power is slightly lower (85.13 MW) in the case of a single bridge (with reinjection) as compared with the double bridge (90.41 MW) configuration.

Figure 6.6 also shows curves of constant power loci. In each case the maximum power locus which is a tangent to the \( dc \) voltage characteristic determines the power conversion capability. To avoid errors in the steady state \( dc \) equation the commutation angle is limited to \( \mu = 30^\circ \) for twelve-pulse standard configuration and \( \mu = 60^\circ \) for six-pulse with \( dc \)-ripple reinjection as in Figure 6.7, which shows the limitation of commutation angle to satisfy the steady state equations.

The different inclinations in commutation angle variation in both cases can be explained by the dependence of the \( dc \) load current of the \( dc \) ripple reinjection scheme.
For very low currents the harmonic reinjection scheme is less effective if the lower currents are far from the desired range of operation points, in this case around 0.7 kA.

To allow the same maximum power conversion as the conventional double bridge, the proposed configuration requires approximately a 6% increase in excitation, another option can be firing angle control. In the case of increasing excitation the extra capability is available on permanent basis. The possibility of firing angle increase is not available in the case of diode rectification. This makes the increase in excitation the only possibility of control available when the diode rectifier unit is operating as a base generation.

6.5 Conclusion

A generator-converter scheme has been proposed which halves the number of bridge units and related equipment while maintaining the waveforms of the twelve-pulse configuration[Arrillaga et al., 1992a]. It can also be observed that in the steady state the maximum capability of the new scheme is only 5.84% lower than the conventional twelve-pulse unit connected scheme. This result shows that a slight increase in rotor rating is needed to provide the same maximum power conversion as the double bridge configuration.

The dc ripple reinjection with a single six pulse converter bridge has the same harmonic performance as a twelve pulse converter bridge. This configuration can also
have the same practical applications as listed for the unit-connection schemes.

The ripple reinjection scheme is very effective due the possibility to control simultaneously the firing angles of the main and feedback converters. The feedback ratio is kept constant in order to eliminate the \textit{ac} harmonics. These parameters can easily be controlled by the designer in a new scheme.

The \textit{dc} ripple reinjection scheme, in steady state, shows a wider range of validity for the steady state equations when compared with the standard twelve-pulse \textit{ac-dc} converter scheme. This reduces the need in the limitation of the commutation angle to simulate the model in steady state.

Detailed costings of the scheme would be necessary. No matter how many bridges are in the system, thyristor ratings to full line voltage are required. Although the number of the main bridges is reduced to half the cost of the bridge units would not be halved using harmonic reinjection.

These steady state studies were made after some investigation of components, the development of a dynamic model and exhaustive laboratory experiments required to check the feasibility of the scheme [Baird, 1981] [Villalba, 1992]. The results obtained so far are sufficiently encouraging for the scheme to be considered as an alternative to the twelve-pulse unit connected generation. However an in depth evaluation of all its' aspects should be made before the scheme could be accepted in HVdc systems.
Chapter 7

CONCLUSIONS AND SUGGESTIONS FOR
FUTURE RESEARCH WORK

7.1 Conclusions

The application of unit and group-connections to electrical power systems have been studied for several years and the benefits which can be obtained by integrating HVdc converter stations directly with generating units are now well documented.

The unit-connection is used in a wide variety of applications. For example, generator-rectifier systems and in excitation systems of large electric generators. In these applications, the converter may consist of diodes connected as a six or twelve-pulse bridge rectifier (uncontrolled) or, if rapid control of the dc output voltage is desired, the diodes are replaced by thyristor valves whereupon phase control can be used to regulate the average output voltage.

If a twelve-pulse thyristor bridge is used, the converter can be controlled so the direction of power transfer from the dc system to the synchronous machine can be changed whereupon the converter-machine operates as an inverter-motor. Thus, line-commutated converter synchronous machine systems can be used in pumping-storage schemes and are suitable for operation in high power variable-speed generation schemes and low power electric drive systems.

A fast steady state analysis incorporating a direct-connection system is of great interest to the power industry because of the advantages foreseen in the integration of HVdc direct connected generators to existing ac networks. The need for such a steady state model is driven by a constant search by the power industry for more reliable, lower cost and more efficient power generation systems.

Further, in the operation and planning of an electrical system, it is important to know the value of the maximum power that can be delivered to the loads while technical and operating constraints are met. The maximum power and the capability charts in the direct connection case are only obtainable with accuracy by using dynamic simulation related techniques.
The operating characteristics of the unit-connection have been developed using the conventional steady state formulation. In the absence of harmonic filters, the use of such formulation as a reference is very questionable. One of the main objectives of this thesis has been to show some of the problems with the conventional steady state and to propose and create a new formulation using steady state characteristics (operating charts) derived from dynamic simulation[Sankar, 1991] of unit connected HVdc systems.

The conventional loadflow can be adapted for use with the unit-connection but has several limitations, a major one being the averaging of the quadrature and direct axis sub-transient reactances. This is not satisfactory due non-linearities present in the converter commutating reactance[Sankar, 1991]. A reference bus, where the voltage is assumed sinusoidal, must be created for the converter interface. This fictitious bus is usually taken to be behind the sub-transient reactance of the synchronous machine. For valid results from the \( \text{dc} \) equations used in the loadflow the commutation angle needs to be restricted to \( 2\pi \). Also due to the commutation and conduction periods of the converter being affected by harmonics the non-linearities are very difficult to be represented in steady state algorithms.

A modified loadflow was initially produced in an attempt to overcome some of the above difficulties. In the program the phase variation of the commutating voltage with respect to the load was introduced, for a constant excitation in the synchronous machine. The results obtained from this program showed improvements in some cases but the other deficiencies prevented any great gains being made.

The modified loadflow was capable of being used to analyse the performance of unit and group connected schemes when working with a reduced number of generators. This was done for a wide range of operating powers. The work involved two researchers: the electrical work was done by the author and the efficiency considerations were made by Mr. S. MacDonald.

Although there is no difference in the characteristics between the two configurations when all the generators are in service, the transmission voltage of the series connected unit-connection is a direct function of the number of generators and thus with fewer generators in service, the transmission efficiency is reduced. The lower transmission voltages cause transfer of power control to the inverter end, which substantially increases the reactive power demand at that end.

Alternatively the group-connection presents no problems in this respect, being capable of maintaining the required transmission voltage regardless of the number of generators in service and thus it provides efficient transmission at different power levels. The group-connection also permits efficient inversion without the need for an unrealistic on-load tap change range.

The unit or group-connections are suitable for adaptation into practically any conventional power plant configuration. Ingram(88) has discussed the reducing costs and
savings advantages of direct connected schemes compared with a similar conventional power plant.

The first part of the work reported here has shown that models employing steady state equations are unsuitable for application to unit-connection schemes. The second section of the work has been to use operating charts, as first described by Sankar[91], and to incorporate them into an equivalent inverter representation.

The equivalent inverter requires capability charts obtained from many dynamic simulation studies over a range of operating conditions. This is done by using points obtained by repeatedly solving the model in the step by step dynamic simulation described in Chapter 2. The cost and accuracy of steady state simulation of unit-connection is directly proportional to the number of required points in each chart.

In the rectifier characteristics derived from dynamic simulation, the constant firing angle characteristics cross over each other at large currents. In this situation, for a range of currents, the maximum value of dc voltage can increase for increasing firing angles. This effect cannot be modelled using the conventional steady state simulation.

The comparison between the conventional, the modified loadflow and the equivalent inverter showed large discrepancies between their results for the same operating specification. For example differences up to 30% in inverter tap position and 8% in complex power in the ac system were obtained between the modified loadflow and the equivalent inverter.

It is also difficult to judge between the loadflow and modified loadflow as to which gives results closer to those of the equivalent inverter.

The accuracy of the equivalent inverter was demonstrated in simulations of two commissioning tests for the upgraded New Zealand HVdc link. Despite all the approximations made in the model, the non-specified variables showed good agreement with the real system results.

Three disadvantages of the equivalent inverter technique are:

i. The need to maintain a data base with capability charts for the various possible number of machines in operation and for the range of variation of generator excitation and rectifier firing angle for a given direct connected power station.

ii. The need for a dynamic simulation program such as TCS.

iii. The amount of time spent obtaining the charts using dynamic simulation. An average time of half an hour for each point was required. This was for a system similar to that shown in Figure 2.4 using TCS on a VAX 3500 computer. The number of points in each chart for each firing angle can be in a range between ten and thirty and thus the amount of time consumed to obtain these charts can be significant. However, these charts need only be obtained once for a given generation configuration.
None of these disadvantages are fundamental limitations to the use of the equivalent inverter which in this work has been demonstrated to be superior to other computer models. The equivalent inverter will allow a better analysis of the operation of unit connected generator inverters. From this any extra requirements, such as higher excitation, will be easily determined.

A third and final part of the work reported in this thesis was to investigate the use of a dc ripple reinjection technique in direct connected HVdc generating stations. The original proposal for ripple reinjection was made by Baird[81] and extended by Villablanca[92]. Their work involved using dynamic analysis and practical laboratory tests. This has now been extended by further analysis to obtain the steady state equations which have been included in the loadflow program.

The results from this work have been demonstrated by the production of an operating chart which shows an improved power capability of 7%. Together with the elimination of one of the main rectifier bridges, this makes the dc ripple reinjection technique very attractive for unit-connection despite the extra cost of two single phase transformers, two capacitor banks, a single-phase rectifier bridge and additional control for the feedback converters.

This thesis has concentrated on the steady state modelling of the unit-connection and dc ripple reinjection. No attempt has been made to justify the scheme in terms of initial cost, running costs (other than $I^2R$ losses) and reliability. These figures will be of great influence in the adoption of one or another scheme for power systems and extra work is needed in this area to assess the value of each component in the chosen configuration regarding the three aspects above.

### 7.2 Suggestions for further research in HVdc direct connected schemes

This work has extended the knowledge of HVdc direct connected schemes and provided a means of analysing their behaviour in the steady state. Much more work is necessary however specially in the dynamic operation of these schemes.

The commutation and conduction periods in the twelve-pulse converter bridge in the unit-connection scheme cannot be treated in the same way as in conventional schemes. In this case the unpredictable commutation and conduction stages under non-sinusoidal commutating voltage have a substantial effect on the steady state dc voltage and current in the link.

Dynamic simulation for direct connected scheme has been studied thoroughly in previous publications for low voltage direct connected schemes, in this case, the synchronous machine is connected to the converter without the need for a transformer[Sudhoff and Wasynyczuk, 1992]. An investigation of the rectifier commutation and conduction periods...
for the HVdc direct connected systems will be complicated by the effects of transformer harmonic cross coupling.

A frequency domain technique is under study at Canterbury University [Rios, 1992]. The implementation of the converter bridge in the harmonic domain can lead to a model which will be useful for detailed study of the direct connection in time domain, frequency domain and in steady state. This model has proved to be faster than dynamic simulation for ac systems. However, it is in its initial test stages. The introduction of the converter bridge into the harmonic domain can lead to a fast technique to obtain the characteristics to be used in the equivalent inverter.

In this thesis steady state studies of unit and group-connections were concentrated in the synchronous machine and in the converter arrangements and controls. This investigation need to go further in terms of capability charts. Continuation of this research must include thermal restrictions in the synchronous machine and transformer, and operating restrictions in the (thermal or hydraulic) turbines. This can introduce further modifications in the steady state characteristics obtained from dynamic simulation at low power operation, due turbine operating at non ideal conditions. Modification in the characteristics can occur also at higher power levels if the system needs to operate temporarily above the rating of the machines. In this case thermal limitations in the synchronous machines and transformers introduce restrictions in the equivalent inverter characteristics.

Further work should be done related to the design of the synchronous machine in HVdc direct connected schemes. In conventional schemes the power factor is one of the key variables in its design and the terminal voltage waveform is considered sinusoidal. In the direct-connection case the terminal waveform is not sinusoidal and the traditional power factor definition can no longer be used. A solution could be the use of finite element analysis to obtain information of distribution of flux in stator and rotor magnetic core, the distribution and depth of the conductors slots along the stator and rotor of the machine. The increase in heating in the core and refrigeration needed also can be obtained using the same technique.

The effect of distortions, handling of high order harmonics, asymmetries and non linearities can be clarified in the synchronous machine by using finite element techniques. This can be a place for further comparisons with the machines in conventional schemes. These studies would give insight into whether substantial changes are needed in the geometry of the stator and rotor of a conventional machine to operate as a direct connection and should provide information about how to keep the magnitude of flux related losses the same in both cases.

Another interesting area for future studies is the overall efficiency of the unit connection in the case of variable speed operation and combined with variable water heads in the hydro generation case. This complimentary investigation would be very useful for pumping storage schemes.
Studies of the self excited configuration of the unit connected schemes are in their early stages at present. One of the configurations that would be the focus of future research is the self-excitation of the synchronous machine from dc ripple rectification. In this case the excitation in the machine will be a complex function of the amount of $h$ harmonic in the $dc$ line and the firing angle in the converter [Arrillaga et al., 1985].

Dynamic simulation of generator HVdc converter units are very demanding computationally. Since the initial values for the simulation are obtained from a single-phase simplified analysis the computation time can be reduced by starting the dynamic simulation from better initial values. Research is already underway to adapt a fast steady state technique [Garcia, 1990] to the unit-connection. The objective is to obtain better initial values from a steady state hybrid analysis in the time and frequency domain. This will have a direct influence in the equivalent inverter by lowering the cost of creating steady state charts for a given unit or group connected power plant.

Transient stability programs cannot be used for systems involving unit-connections, since they are based on the steady state and quasi steady-state formulations. Instead, the characteristics derived from dynamic simulation could be used in a stability program to represent unit connected schemes.

Another area of work presently being carried out at the University of Canterbury is to model various types of transformers and the converter in the harmonic domain. This will be undoubtedly an interesting contribution to the steady state analysis of direct connected systems.

The effectiveness of the dc ripple reinjection scheme is judged in this thesis by its steady-state performance. However, by suitably modifying a dynamic simulation program to include the synchronous machine model and the ripple reinjection, the program will be able to investigate dynamic, voltage stability and steady state operation. Possible investigations are: the influence of the presence of the feedback circuit over the synchronous machine and over the six-pulse converter under different conditions of loading and firing angle, and the comparison of results from steady state equations and the results from step by step dynamic simulation.
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