Modelling and transient analysis of saturated core fault current limiters in electricity grids

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Modelling and Transient Analysis of Saturated Core Fault Current Limiters in Electricity Grids

Sasareka Malithi Gunawardana, BSc(Eng)

A thesis submitted in the fulfilment of the requirements for the award of the degree of Doctor of Philosophy from the University of Wollongong School of Electrical, Computer & Telecommunications Engineering

March 2016
Declaration

I, Sasareka Malithi Gunawardana, declare that this thesis, submitted in fulfilment of the requirements for the award of Doctor of Philosophy, of the School of Electrical, Computer & Telecommunications Engineering, University of Wollongong, is entirely my own work unless otherwise referenced or acknowledged. This manuscript has not been submitted for qualifications at any other academic institution.

Sasareka Malithi Gunawardana,

31st March 2016
Abstract

Deregulation of electricity industry has shifted the operation paradigm of power systems in the recent years. Combined with the quest for energy efficiency and stringent environmental constraints, market regulation has driven the recent advert of decentralised generation. Transmission network expansion has also been affected as a consequence of deregulation, with network operators no longer being the sole authority over generation or network expansion planning. In this environment, the integration of decentralised generation at transmission and/or distribution networks has resulted in a considerable increase in the system fault levels that are unaccounted for in long-term planning forecasts by network operators. To enable higher integration capacity and reliable operation, these networks are also becoming more interconnected adding to the already increased fault current levels. As a consequence, at certain locations of the grid, the fault current levels are approaching or exceeding existing switchgear ratings.

Utilities have traditionally employed a variety of passive fault current mitigation measures to address their fault current problems. However, numerous constraints associated with these passive measures have compelled them to re-evaluate their conventional fault current management measures and become increasingly interested in novel fault current limiting technologies. A Fault Current Limiter (FCL) is a device that can be used in electrical power transmission and distribution systems to restraint the system fault current to a manageable level so that existing switchgear can still function as expected. Amongst the emerging fault current limiting technologies, the saturated core FCL is currently one of the most promising technologies.
and forms the basis of the research studies presented in this thesis.

Provided they are cost effective alternatives for switchgear replacements, the potential market for FCLs is expected to be substantial in future utility grids. However, before deploying this technology in utility grids, it is imperative to understand the transient behaviour of these devices. While the research and development efforts into various FCL technologies have been significant, the level of awareness, regarding the potential impact of FCLs on protection schemes and the extent of other integration issues that could arise, are seen to be fairly limited.

The central premise of this thesis is to contribute to the general study of saturated core fault current limiters by addressing key research gaps prevailing in saturated core FCL network analysis research. Similar to most emerging technologies, computer-aided modelling and simulation are fundamental steps in demonstrating the benefits and impediments of this technology in network applications. Transient network simulations allow the electrical behaviour, performance and feasibility of the device to be evaluated under realistic grid conditions. In order to perform these network simulations, an accurate computer-aided model of the FCL is necessary. However, representing a saturated core FCL on transient network analysis programs has always been difficult due to the intricate magnetic characteristics of the device.

This thesis aims to establish an accurate time-domain model of the saturated core fault current limiter, that can represent the full nonlinear range of magnetic operation of the device. Two modelling approaches to representing a saturated core FCL are proposed, with their relative merits and demerits discussed. The models are implemented in the PSCAD/EMTDC Electromagnetic Transient (EMT) simulation package and are validated by experimental observations. The adaptability of the proposed modelling approaches in accommodating different saturated core FCL topologies, is also examined.

The transient behaviour of saturated core FCLs in electricity networks is also investigated in this thesis, with an aim to identify potential problems that could arise when integrating saturated core FCLs into existing systems. These include
analysing the steady state and transient behaviour of saturated core FCLs under different system conditions. Further studies are carried out to examine the influence of a saturated core FCL installation on existing power system protection schemes. Several other FCL application considerations such as effects on circuit breaker transient recovery voltage, voltage sags, harmonic distortion and transient stability are also discussed.
Publications Arising From the Thesis


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Chapter 1

Introduction

1.1 Background

Electricity is a fundamental necessity to the modern way of life. Since the inception of commercial generation of electricity in the late 19th century, the electricity grids have evolved from isolated systems that delivered electricity to limited geographical areas, to extensively interconnected widespread networks covering large geographical areas. Today’s electricity infrastructure comprises of a complex system of power generation, transmission systems, and distribution systems designed to deliver electricity, on demand, to a range of consumers.

With modern society’s heavy reliance on the continuous availability of electrical energy, the reliability of supply has become one of the main concerns of contemporary utilities. The rapid increase in population and expansion in industrialisation has resulted in a continuous increase in electricity demand, which is outpacing the network infrastructure that supports it. In the last decade alone, worldwide electricity consumption has grown by 38% to meet demand related to high economic growth and population expansion [1]. To accommodate the increasing electricity demand, electricity utilities around the world are perpetually expanding and reinforcing their electricity grids through:

- addition of new centralised generating facilities;
increased penetration of renewable and distributed power generation;

- network upgrades; and

- greater number of network interconnections as a consequence of increased generation and transmission capacity.

Consequently, with the growth in power systems, there is a considerable increase in the system fault current levels as well as the outages caused by fault currents. While the utility infrastructure is designed to withstand and counteract many of the problems that originate in the electricity grids, they are susceptible to failure and hence faults resulting from the same are inevitable. These power system faults give rise to short-circuit current levels that are much larger than the nominal load current levels and can impose excessive mechanical and thermal stresses on power system apparatus.

Stability of the power systems could also be adversely affected by occurrence of power system faults, which can even lead to system outages or cascade tripping of electricity networks. According to the annual power outages report issued by power management company Eaton Industry, 162 cases of power outages were reported in year 2013 across Australia and New Zealand, an increase from 94 cases reported in 2012 [2]. Financially, power outages can cause substantial losses to the businesses affected as well as to the utilities in the form of costs associated with fault detection, containment, recovery and financial compensation paid out to consumers for extended interruptions. Therefore, isolating the faulted part of the network from the rest of the system using protection schemes, as soon as possible, is of utmost importance in keeping the power system operational within expected boundaries.

Power system switchgear are typically designed to manage a peak current of certain magnitude. IEC standard 62271.100 [3] specifies the peak value of the maximum short-circuit current of electrical switchgear in a 50 Hz system to be (1.1), where $I_{sc}$ is the rms value of the short-circuit current.

$$I_{peak} = 2.5 \cdot I_{sc}$$  \hspace{1cm} (1.1)
With the growth in power systems, the short-circuit current levels are approaching or exceeding the existing switchgear ratings and hence, there is an increasing need to replace or uprate the existing switchgear in order to accommodate the increased fault levels. This is an expensive solution and a fairly short-term one at that, as the fault current levels will continue to grow with the continuous expansion of electricity grids, necessitating perpetual switchgear upgrades. A more appropriate and cost effective approach, would be to reduce the system fault current to a manageable level so that the existing switchgear can still continue to function.

Utilities have traditionally employed a variety of passive fault current mitigation measures. Some of these are [4, 5]:

- changes in the network topology (for instance, network splitting);
- sequential network tripping;
- introducing higher voltage connections; and
- use of fault current limiting series reactors and high impedance transformers.

Network splitting, in particular, is a common and a relatively inexpensive approach to fault current management employed by the utilities. However, it reduces the degree of inter-connectivity and flexibility and adversely affects the reliability of the electricity supply. Sequential tripping is a rather complex scheme in which an upstream circuit breaker feeding the fault is tripped first, before opening the circuit breaker within the zone of protection at the location of the fault. This scheme reduces the fault current seen by the circuit breaker closest to the fault location. However, sequential tripping increases the fault clearance time since it makes allowances for upstream breaker operation as well. The scheme also poses a significant threat to the system reliability and safety if the scheme fails. A considerable reduction in system fault level may also be achieved by upgrading to higher system voltage levels, however, constructing higher voltage systems for fault level mitigation purpose alone may not be financially viable. The use of fault current limiting series reactors is another common approach to fault current mitigation adopted by utilities, despite
the many drawbacks associated with them under normal unfaulted conditions. Since insertion of series reactors leads to a permanent increase in the impedance, not only under fault conditions but also during normal operation, series reactors can cause substantial voltage drops as well as active/reactive power losses during steady state conditions. The use of transformers with higher leakage impedance also has similar disadvantages, since the increased transformer impedance leads to an increase in voltage drop as well as reactive power losses.

Due to the shortcomings of passive fault current mitigation measures, a device that could limit the system fault current to an acceptable level without a permanent increase in the steady state impedance and associated compromises in power quality, system reliability or flexibility, would be highly desirable. A Fault Current Limiter (FCL) is such a device that is ideal for locations in the grid where fault current levels are approaching or exceeding existing switchgear ratings. Active fault current limiting devices have a small impedance at normal operation that increases rapidly under fault conditions, hence limiting the system fault currents. Some examples of active FCLs are: high voltage current limiting fuses, pyrotechnic FCLs (Is-Limiter), solid state fault current limiting circuit breakers and superconducting FCLs [6]. Amongst the fault current limiting technologies that have been proposed to date, the saturated core FCL is one of the most promising technologies that has gained significant attention from researchers and electricity utilities worldwide and forms the basis of the research presented in this thesis.

A survey conducted by Electric Power Research Institute (EPRI) in 2004 concluded that the utilities are re-evaluating their conventional fault current management measures and are becoming increasingly interested in novel fault current limiting technologies [7]. The survey results also suggested that there may be a potential market for FCLs provided they are cost effective alternatives for circuit breaker replacements. Considering the crucial role FCLs are expected to play in future utility grids, the network operators will require performance evaluations before deploying these devices. Of particular interest is the effect an FCL will have on the network
and existing switchgear [8]. However, responses received from the utilities in the 2004 EPRI survey suggested that their level of awareness, regarding the potential impact of FCLs on protection schemes and the extent of other integration issues that could arise, was fairly limited. The lack of literature available in this area, at the time of the survey, may have contributed towards this ambivalent opinion of the survey respondents.

Since then, several publications have reported on the potential grid integration issues of FCLs considering behavioural models of solid state current limiters and superconducting FCLs [9–11]. These are discussed further in Chapter 2 of this thesis. The work presented in this thesis aims to fill the existing voids in this research area by investigating the potential concerns that could arise when integrating saturated core FCLs into existing power systems. One major reason for this is the difficulty in representing saturated core FCLs in transient network analysis programs due to their intricate magnetic characteristics. Nonetheless, before deploying this technology in utility grids, it is imperative to understand the transient behaviour of these devices and the interactions they will have with the existing switchgear and protection systems.

1.2 Research Objectives and Methodologies

The aim of the work presented in this thesis is to make contributions to the general study of saturated core fault current limiters by addressing key research gaps prevailing in saturated core FCL network analysis research. Some of the areas where gaps can be identified in the current knowledge of FCLs are:

1. Absence of accurate time-domain models of saturated core FCLs, which would have the ability to predict the transient response of the devices effectively.

2. Very limited research on the transient behaviour of saturated core FCLs in electricity networks including:

   • effect on transient recovery voltage (TRV) of circuit breakers;
• effect on power quality such as voltage sags and power system harmonics;

• effect on transient stability;

• impact on the existing principle protection systems;

• evaluation of resetting and re-coordination requirements of protection schemes; and

• impact on independent power producer installations.

This research has two key objectives:

• to develop an accurate time-domain physics-based model of the saturated core fault current limiter with a view to use it in network simulation studies; and

• to investigate a variety of transient phenomena when a saturated core FCL is inserted in to a network.

Computer-aided modelling and simulation is a widely accepted approach for design verification and analysis of novel power system technologies. Especially, when investigating power system switchgear behaviour during fault events, use of a transient simulation tool for modelling is highly beneficial, due to their ability to replicate actual network behaviour during a fault event in the power system based on available network data. This thesis aims to establish an accurate time-domain model of the saturated core fault current limiter, that can provide a platform for investigating future networks containing FCLs.

Two modelling approaches to representing a saturated core FCL are proposed, with their relative merits and demerits discussed. The models are implemented in the PSCAD/EMTDC electromagnetic transient simulation package. The modelling is supported by subsequent time-domain simulations and validated by experimental observations. The adaptability of the proposed modelling approaches in accommodating different saturated core FCL topologies, is also examined.
Installation of any type of current limiting device will have certain effects on power system switchgear, which are typically configured to existing network conditions. The second objective of this thesis is to investigate the transient behaviour of saturated core FCLs in electricity networks, with an aim to identify potential problems that could arise when integrating saturated core FCLs into existing systems. These include analysing the steady state and transient behaviour of saturated core FCLs under different system conditions. Further studies are carried out to examine the influence of a saturated core FCL installation on existing power system protection schemes. Several other FCL application considerations such as effects on circuit breaker transient recovery voltage, voltage sags, harmonic distortion and transient stability are also discussed.

To simulate the transient behaviour of saturated core FCLs in real power networks, the FCL model proposed in this thesis is used along with detailed PSCAD/EMTDC network models developed based on real power system data. E-TRAN translator program is used to translate power system data from PSS/E load flow format for use in PSCAD/EMTDC and to generate the base network models. Further detailed modelling of the networks are then carried out around the main points of interest. The validation of the network models developed is accomplished through frequency scans and load-flow studies. Using the developed FCL model and the grid models, several case studies are conducted to examine the interactions between the FCL and the power grid and the impact these interactions will have on the system and the overall performance of the device.

Firstly, the performance of an FCL during steady state un-faulted conditions is examined, focusing particularly on steady state voltage drop and the effect on network power flow, and evaluated against the performance of an air core reactor. Short-circuit studies are also conducted considering different network configurations, where the operation of the device is examined under different types of fault conditions of varying magnitudes and duration. Subsequently, an analysis of the interactions between a saturated core FCL and three commonly employed protection
schemes in HV transmission systems—overcurrent, distance and differential—is carried out to ascertain possible effects and to identify potential resetting requirements of the protection schemes. The impact an FCL will have on circuit breakers, in terms of interrupting burden and the TRV associated with the circuit breakers when interrupting an FCL-limited current, is also investigated. Several other potential concerns associated with saturated core FCL application such as effects on voltage sags, power system harmonics and power system stability are also discussed.

1.3 Thesis Outline

A brief summary of the contents of the remaining chapters of this Thesis is provided here.

Chapter 2 presents a review of the relevant literature published in the subject area of this thesis. A brief overview of different types of FCL technologies that are being developed is given, focusing on three main categories of FCL technologies—solid state FCLs, superconducting FCLs and saturated core FCLs. The basic operating principle of a saturated core type FCL is discussed in detail, and a review of different saturated core FCL topologies is given. An analysis of the existing saturated core FCL modelling techniques is presented, with advantages and limitations of each modelling technique discussed. A review of existing work that has been conducted to investigate potential FCL grid integration issues is presented and key research gaps related to saturated core FCL applications and performance studies in utility grids are discussed.

Chapter 3 presents two modelling approaches to represent the FCL in transient network simulators: the Nonlinear Reluctance Model and the Nonlinear Inductance Model. The model development, simulations and experimental validation is given. The adaptability of the proposed modelling approaches in accommodating different saturated core FCL topologies, is discussed.

Chapter 4 presents several case studies at different voltage levels undertaken in the PSCAD/EMTDC transient simulation package to analyse the operational be-
haviour and performance of saturated core FCLs in utility grids. The Nonlinear Inductance Model of the saturated core FCL, introduced in Chapter 3, is utilised to model the device and predict its electrical behaviour when applied to network simulations. Different network configurations are considered and the operation of the device is tested under different fault conditions.

Chapter 5 presents a comprehensive analysis, focusing on the influence a saturated core FCL installation has on existing power system protection schemes. A review of protection schemes and their general susceptibilities to current limiters is given, followed by case studies on a real power network. The effects on each protection scheme are analysed in separate subsections and subsequently possible revisions to these protection schemes are proposed.

Chapter 6 discusses several other application considerations that network operators may need to take in to account when considering a potential FCL installation. The influence of a saturated core FCL on the interrupting duty of a circuit breaker is discussed and potential effect on voltage sags is evaluated. A harmonic analysis of a system with a saturated core FCL installation is given and the impact of FCLs on power system transient stability is discussed.

Chapter 7 consolidates the major findings and presents final conclusions on the work presented in this thesis. Recommendations and suggestions for future work are also presented.
Chapter 2

Literature Review

2.1 Introduction

Continuous growth in the electricity demand, along with the perpetual expansion and reinforcement of interconnected high voltage electricity networks, have been noted to cause an increase in system fault current levels. Chapter 1 discussed the need for fault current limitation in modern power systems and different fault current mitigation measures that have been employed by electricity utilities with varying degrees of effectiveness. A fault current limiter is one such device that has attracted significant attention from both researchers and electricity utilities. A number of different FCLs with distinct limiting technologies are currently being developed.

This chapter gives a brief overview of different types of FCL technologies that are being developed and their applications. Section 2.2 presents desirable attributes of an ideal FCL. A review of existing FCL concepts is given in Section 2.3, where three main categories of FCL technologies—solid state FCLs, superconducting FCLs and saturated core FCLs are discussed. Amongst the emerging fault current limiting technologies, saturated core FCLs are currently considered to be one of the most promising. Section 2.4 presents the basic operating principle of a saturated core type FCL, which in essence is a variable inductance iron core reactor. Different saturated core FCL topologies, including single-core topologies with bridge rectifiers, and dual-core topologies such as the open core, closed core and hybrid-core configurations,
are also discussed. Due to the intricate nonlinear characteristics of saturated core FCLs, modelling the transient behaviour of these devices have always been considerably difficult. A review of the existing saturated core FCL modelling techniques is given in Section 2.5, where advantages and limitations of each modelling technique is discussed. Section 2.6, presents an analysis of the potential grid integration issues of different types of FCLs in power systems. A review of existing work which investigates these issues is presented and key research gaps related to saturated core FCL applications and performance studies in utility grids are discussed.

2.2 Functional Requirements of an FCL

As stated in Chapter 1 utilities are becoming increasingly interested in novel fault current limiting technologies that can offer reliable current limitation with minimal impact during normal operation. The need for such a device has been established for over four decades [12] and reaffirmed in more recent studies [7].

The CIGRÉ Working Group A3.10 defined a set of parameters to characterise the limiting behaviour of an FCL in [13]. This includes parameters such as follow current ratio, peak current limiting ratio, current limiting ratio, action time, fault duration time and recovery time. From review of literature a set of desirable attributes of an ideal FCL can be established [7,13].

Under normal (un-faulted) system conditions, the FCL is expected to have:

- low impedance during normal operation;
- low voltage drop;
- minimal impact on power flow;
- low power loss; and
- minimal distortion.

In case of a fault, the FCL is expected to:
differentiate between a temporary overcurrent situation and an actual fault event and provide immediate response;

- limit the first current peak by decreasing the rate-of-rise of the current within the first quarter cycle;

- tolerate mechanical stresses;

- provide fail safe limiting operation;

- provide an appropriate follow current (less than the prospective fault current) enabling the operation of downstream protection;

- endure a sequence of recurring faults; and

- recover immediately following fault clearing;

Several other attributes such as minimal effect on existing protection, low weight and compact design, reliable operation and low level of maintenance requirements are considered desirable in an ideal current limiter. Note that while these provide general attributes for fault current limiting devices, the fault current characteristics can vary considerably between different FCL technologies.

# 2.3 Fault Current Limiting Technologies

The literature in the subject area, concerning fault current limiting technologies and concepts, is extensive covering a number of different fault current limiting devices which have been designed, prototyped and tested worldwide [8,14]. A brief review of several different fault current limiting technologies is presented in the following subsections.

## 2.3.1 Solid State Fault Current Limiter (SSFCL)

Solid state fault current limiters use a combination of inductors, capacitors, resistors and fast acting power electronic switches (GTOs or Thyristors) to limit the fault
current of the power system. Electronic switches are utilised to insert reactors or resistors into the path of the fault current following a fault event, resulting in an increase in the series impedance. There are several different types of SSFCLs proposed to date such as Resonance based devices, Impedance switch-in limiters, Bridge type FCLs and Hybrid FCLs.

Resonance based FCL

Resonance based FCLs are discussed in [15,16], where the fault current reduction is achieved through the insertion of a parallel resonant LC circuit in series with the line during a fault using thyristor switches. Two possible switching arrangements are shown in Figure 2.1 [16]. In Figure 2.1a when a fault occurs, the thyristors/GTOs are turned on in such a way that the inductor is connected in parallel across the capacitor, forming the parallel resonant circuit thus limiting the fault current. In Figure 2.1b the GTOs are turned on during normal system operation and are turned off when fault current limiting has to be achieved.

Impedance switch-in limiters

In Impedance switched bypass limiters an impedance is connected in series with the line and GTOs are connected in parallel with this impedance. Under normal operating conditions, the GTOs are in ON-state—thus by-passing the impedance.
Following fault inception, the GTOs are turned off, introducing a large impedance to the system and thus limiting the fault current [17].

Bridge type FCLs

A typical bridge type solid state FCL arrangement is given in Figure 2.2. In the bridge type SSFCL proposed in [18], following the fault inception, the stored energy of a reactor is fed to the power system by operating a thyristor bridge as an inverter. This design has been refined and further developed in [19,20].

Hybrid FCLs

A novel concept for a hybrid FCL based on a combination of semiconductors, temperature-dependent resistors, and an ultra-fast mechanical switch is shown in Figure 2.3 [21]. During normal operation, all three switches are closed and the GTO is gated-on. Following a fault, the ultra-fast mechanical switch opens and the GTO is gated-off, forcing the current into the limiting impedance path.

Whilst in general SSFCLs have the advantage of immediate recovery, many of the solid state current limiter designs have shortcomings such as the on-state losses of the semiconductors, voltage sags during faults, high cost and complicated auxiliary systems that deter them from reaching full commercial potential [22,23].

**Figure 2.2:** A typical bridge type solid state FCL arrangement
2.3.2 Superconductor Fault Current Limiter (SCFCL)

Superconducting fault current limiters use superconducting materials to limit the current directly or use superconductor DC coils to bias an iron core driving it to saturation (the latter is discussed as a separate type under saturated core FCLs). The earliest designs used low temperature superconductors (LTS) \([24]\) that have a low transition temperature. LTS materials are generally cooled with liquid helium and due to the low operating temperature the cooling costs are extremely high and therefore FCLs that use LTS are not commercially viable. Since the discovery of high temperature superconductors (HTS) in 1986, which have a relatively high transition temperature and can be cooled by relatively inexpensive and less complex liquid nitrogen systems, the SCFCL has been an area of active research interest. With the advancement of superconducting materials and magnet technologies, different types of superconducting fault current limiters have been designed and developed to date \([6,13,25–29]\).

The basic operation of an SCFCL is dependent on the nonlinear response of superconducting materials to temperature, current density and variations in magnetic field \([13]\). In steady state, the SCFCL allows for the load current to flow through it without an appreciable voltage drop across it. However, in the event of a fault, the high fault current causes the superconductor to quench resulting in a transition from negligible impedance at normal load current to a higher impedance instantaneously,
effectively limiting the current. After the fault is cleared, the SCFCL is cooled again to the temperature below its critical temperature, thus regaining its low-impedance superconducting state and becomes ‘transparent’ to the power system.

**Resistive SCFCL**

The basic circuit diagram of a resistive SCFCL is illustrated in Figure 2.4 where a superconductor is connected directly in series with the line carrying the rated load current [29–31]. During a fault, when the fault current exceeds the critical current density of superconductor, it quenches and hence increases the associated resistance exponentially. The shunt impedance ($Z_{\text{shunt}}$) limits the voltage increase across the superconductor (due to rapid increase in $R_{sc}$) as well as protects the superconductor from non-uniform heating (commonly known as hot spots) during quenching. During the limiting phase, the quenching of the superconducting material causes the superconducting element to heat up temporarily and thus a recovery time is required for the cryogenic cooling system to restore the superconductor back to the operating temperature [22]. One of the major drawbacks of this approach is the slow recovery of the superconducting element.

![Figure 2.4: Basic circuit of a resistive SCFCL](image)

**Shielded-core SCFCL**

The basic circuit diagram of a Shielded-core SCFCL is illustrated in Figure 2.5. Shielded-core SCFCL depends on the quenching action of superconductors much like the resistive SCFCL, however unlike the latter, in the shielded-core SCFCL the high temperature superconducting component is mechanically isolated from the re-
mainder of the circuit. The principal of operation is similar to a transformer with
the secondary side shunted by a superconducting element. During normal operation,
the current induced in the superconducting cylinder balances the ampere-turns in
the primary winding preventing flux penetration to the iron core and thus ‘shielding’
the iron core. During a fault, increased current on the secondary causes the super-
conductor cylinder to quench resulting in an increased resistance. Subsequently, flux
enters the iron core inserting a high impedance into the line and limiting the fault
current [22,32]. Recovery of the superconducting element of a shielded-core SCFCL
is faster than that of the purely resistive type, however, re-cooling is still required.
Another drawback of this SCFCL type is that it is relatively large in volume and
weight.

![Figure 2.5: Shielded-core SCFCL](image)

**Saturated core SCFCL**

The basic concept of saturated core type SCFCLs has been reported extensively
to date [25,33–37]. Since many saturated core FCLs also include superconducting
technologies, they are commonly categorised as superconducting devices. However,
unlike other superconducting FCLs, saturated core FCLs rely on the nonlinear char-
acteristics of ferromagnetic materials to vary the FCL impedance between faulted
and un-faulted states. Superconducting materials are used in saturated core de-
vices, largely, to enhance their performance. Use of HTS coils as DC bias coils,
for an example, can improve the current limiting capability, magnetic field intensi-
ity and reduce power losses. HTS saturated core FCLs are also more compact in
While saturated core FCLs can also be successfully designed entirely of non-superconducting components, the resultant devices are often quite bulky in size and mass.

Despite active research and development in superconductor current limiting devices, the commercialisation of the SCFCLs has been hindered by various technical and economical issues as summarised in [38].

### 2.4 Saturated Core Fault Current Limiter

#### 2.4.1 Operating principle

The basic configuration of a saturated core FCL is shown in Figure 2.6. It consists of two iron cores surrounded by two separate copper coils wound in opposite directions carrying the same AC line current. The cores can be saturated using an additional coil carrying a DC bias current (which can be either superconducting [6, 35–37, 39] or copper coils), energised by a DC power supply [28, 40, 41] or by using a permanent magnet [42, 43].

The operation of a saturated core FCL can be demonstrated through an analysis of the magnetisation properties of a typical steel core material as shown in Figure 2.7. During steady state un-faulted conditions, as the AC line current flows through the AC windings, and the resulting flux density oscillates through a minor loop (C-E) on the B-H curve as shown in Figure 2.7. As can be seen, the permeability of the material in this region of the B-H curve is approximately equal to the permeability of air; resulting in an AC winding impedance equivalent to that of an air-core inductor. Following fault inception, the rising fault current generates an AC magnetic flux large enough to drive one of the cores out of saturation into the steep region of the B-H curve (towards F). As can be seen from Figure 2.7, the permeability of the core material is much larger in this region. The higher permeance results in a considerable increase in AC winding impedance, which limits the fault current.
Figure 2.6: Dual-core saturated core FCL

Figure 2.7: Material properties of M6 electric steel
One important aspect of a saturated core FCL is that, while one half of the AC cycle sets up magnetisation forces that counteracts the initial saturating field (induced by the DC coils), the magnetisation field set up by the other half of the AC cycle reinforces the DC-induced saturating field. Hence, to effectively limit both the positive and the negative half cycles of the AC current, it is necessary to have two cores and AC coils per phase. Alternatively, [44] proposed an FCL with one saturable core and a bridge rectifier per phase to limit both half cycles of the AC current. A typical arrangement of a saturated core FCL with a bridge rectifier is shown in Figure 2.8

![Saturated core FCL with a bridge rectifier](image)

**Figure 2.8:** Saturated core FCL with a bridge rectifier

For the dual core configuration shown in Figure 2.6, during un-faulted conditions the permeability of the device is approximately that of air \((\mu_0 = 4\pi \times 10^{-7} \text{H/m})\). The terminal impedance of the device at this state (insertion impedance, \(Z_{ins}\)) is approximately equal to the sum of the air-core impedances of the two AC coils, as expressed in (2.1) [28,41].

\[
Z_{ins} \approx 2\omega \mu_0 n_{ac}^2 A_{coil} \beta \frac{1}{\beta}
\]  

(2.1)
where $\omega$ is the frequency in radians per second, $n_{ac}$ is the number of turns of each AC coil, $A_{\text{coil}}$ is the cross-sectional area and $h$ is the height of each coil, and $\beta$ is a variable that is dependent on the AC coil geometry.

As explained earlier, during each half cycle of the fault current, one of the cores is driven out of saturation while the other core remains saturated. The impedance of the AC coil associated with the latter core (one that remains saturated) is equivalent to the air-core impedance of a single coil. The magnetic flux associated with the core that de-saturates, varies from the maximum value to minimum value, as the fault current increases from zero to its peak value. Hence, assuming that the entire B-H loop is traversed during each half fault cycle, the fault impedance of this coil is approximately given by [41],

$$Z_{\text{fault-single-coil}} \approx \frac{2\omega n_{ac} A_{\text{core}} B_{\text{sat}}}{\sqrt{2} I_{\text{fault}}} \quad (2.2)$$

where $A_{\text{core}}$ is the cross-sectional area of the core, $B_{\text{sat}}$ is the maximum flux density at saturation and $I_{\text{fault}}$ is the limited rms fault current.

While the actual FCL impedance during a fault event is not a constant for saturated core FCL, the ‘fault impedance’ imposed by the FCL at a given time during the fault cycle can be estimated based on individual coil impedance at each half cycle given in (2.3) and (2.4). Hence, the fault impedance ($Z_{\text{fault}}$) can be estimated as [41],

$$Z_{\text{fault}} \approx Z_{\text{fault-single-coil}} + \frac{Z_{\text{ins}}}{2} \quad (2.3)$$

and hence,

$$Z_{\text{fault}} \approx \frac{2\omega n_{ac} A_{\text{core}} B_{\text{sat}}}{\sqrt{2} I_{\text{fault}}} + \omega \frac{\mu_0 n_{ac}^2 A_{\text{coil}}}{h} \beta \quad (2.4)$$
The saturated core FCLs exhibit several desirable attributes discussed in Section 2.2. These include:

- self-triggering capability and immediate reaction to fault;
- low impedance and negligible power losses under normal grid conditions;
- maintaining a superconductive state at all times;
- post-fault immediate recovery; hence can handle successive actions unlike the quench-type FCLs; and
- failsafe operation.

While these features render saturated core FCL technology a foremost candidate for providing commercial solutions to fault current problems, size, core volume, mass, and cost concerns have hindered them from reaching their full commercial potential [37].

2.4.2 Saturated Core FCL Topologies

Since the design of the first single-phase saturated core FCL prototype in 1982 [33], a number of different saturated core FCL topologies have been designed, prototyped and tested. These include single-core topology with a bridge rectifier, and dual-core topologies such as the open-core, closed-core and hybrid-core configurations. These topology variations have been introduced due to a number of reasons - such as performance considerations, cost considerations, mass, size and construction considerations [41]. Since the focus of this thesis is on dual-core saturated core FCLs, a brief review of several dual core topologies will be presented in the following subsections.

2.4.2.1 Open-core topology

The concept of a single-phase saturated core FCL with an open-core arrangement is shown Figure 2.9. As can be seen, this topology involves the cores and the as-
associated AC coils being enclosed by a DC bias coil or coils in a magnetically open configuration, with respect to both AC and DC. Since the AC and DC systems are electrically and physically isolated, less transformer coupling between the two systems have been observed [41]. Compared to a closed-core FCL (shown Figure 2.10), where a high permeability magnetic return path is provided, in an open-core FCL the return path is through air. Hence, an open-core FCL requires considerably less material than a closed-core with equivalent performance, significantly reducing the size, mass and the complexity of the device. Biasing open-core FCLs however, can be challenging since they typically require a significant level of ampere turns to fully saturate the cores. In commercial-scale open-core devices superconducting coils are commonly used to achieve the large biasing requirement.

Figure 2.9: Single-phase open-core saturated core FCL configuration [41]

2.4.2.2 Closed-core topology

A typical arrangement of a saturated core FCL with a closed-core topology is illustrated Figure 2.10. The inner core and associated coil arrangement is similar to that of an open core arrangement, with two inner saturable iron cores and associated AC coils (carrying the AC load current) encompassed by DC coils (carrying DC bias
current). However, unlike an open core arrangement, where the return path for flux is through air, in a closed core topology a return path for flux is provided through the yokes and the two outer limbs. To ensure that these return limbs remain in a highly permeable state (and not saturate along with the inner cores), they are typically made with 10% to 30% more cross-sectional area than the inner cores [45]. Compared to an open-core device, the biasing requirement for a closed-core device is considerably less demanding [41]. However, with the extra material used in the yokes and the return limbs, this arrangement can significantly increase the mass and the size of a saturated core FCL.

2.4.2.3 Hybrid-core topology

Hybrid-core topologies are arrangements of magnetic cores and associated coils, featuring some characteristics of both open-core and closed-core topologies. A hybrid-core FCL topology is proposed in [46] where the DC bias coil is wound on a closed magnetic core that is open with respect to the AC coil. Addition of a yoke made of magnetic material between the two cores of the open core FCL (with no outer limbs such as in closed core) results in another type of hybrid-core topology, where the cores can be considered to be in an open configuration relative to the DC coils.
and in a closed configuration relative to the AC coils. Hybrid-core arrangements have several key advantages over open-core or closed-core topologies. Compared to a closed-core arrangement, hybrid arrangements typically reduce the mass of the FCL and the coupling between the AC and DC coils. The biasing requirement has also been found to be less demanding than for an open-core FCL.

2.5 Saturated Core FCL Modelling

As saturated core FCL technology is becoming more commercially and technically viable for electricity utilities, the demand for accurate simulation models that can demonstrate the transient behaviour of these devices in electricity networks is rapidly growing. Most electromagnetic devices operate predominantly in the linear region of the B-H curve. Saturated core FCL operation however, extends to the nonlinear region of the B-H curve, with impedance transition of the device occurring around the knee point of the B-H curve. The intricate nonlinear characteristics of the magnetic cores during FCL operation, has rendered modelling this device considerably difficult. Hence, experimental measurements and finite element method (FEM) analysis have been the most prevalent techniques used to characterise the transient behaviour of the device.

The electromagnetic characteristics of different FCL topologies of the saturated-core FCL has been experimentally analysed (under test conditions or in a live-grid) in a number of previous studies [28,35,36,40,41,47]. These experimental work have contributed significantly towards identifying issues associated with the technology and improving the overall performance of the device, particularly during the early development stage of saturated core FCL technology.

Saturated core FCLs are typically specialised one-off devices and hence in practice, FEM-based analysis is invariably used as a design verification tool due to its ability to accurately represent the geometry and the physical behaviour of the device [48]. Finite Element based FCL models can account for all flux effects including leakage and fringing effects, thus allowing the nonlinear characteristics of magnetic
materials to be accurately modelled. Despite its usefulness as a design verification tool, an FEM-analysis cannot be used directly to analyse the transient electromagnetic behaviour of FCLs in complex power systems. While it is possible to interface an FEM model with some electromagnetic transient (EMT) programs to perform electromagnetic analysis in complex systems, the computational time associated with such a coupling can be substantially high.

Several different analytical models have been proposed to-date to elucidate the behaviour of saturated core FCLs [49, 50]. A brief review of these modelling techniques is presented in this section.

The mathematical model presented in [49] is based on (2.5) where per phase inductance ($L$) of the FCL due to the core flux alone (i.e., ignoring leakage flux) is expressed as a function of the net magnetic field intensity $H$,

$$L = \frac{A_{\text{core}}n_{ac}^2}{l} \frac{dB}{dH} \quad (2.5)$$

where $A_{\text{core}}$ is the cross-sectional area of the iron core, $n_{ac}$ is the number of turns in the AC winding, $l$ is the length of the flux path around the core and $\frac{dB}{dH}$ is the slope of the $B - H$ curve of the core material at an operating point.

A curve fitting approach is used to approximate the $B - H$ characteristic of the core material and to estimate $\frac{dB}{dH}$ using (2.6). The net magnetic field intensity $H$ here is determined combining the effects of both the AC and the DC fields of the device as given in (2.7),

$$\frac{dB}{dH} = e^a H^e e^{ \frac{b}{H}} \quad (2.6)$$

$$H = \frac{N_{dc}I_{dc} \pm n_{ac}i_{ac}}{l} \quad (2.7)$$

where $N_{dc}$ is the number of turns in the DC winding, $I_{dc}$ is the DC current and $i_{ac}$ is the AC line current.
Two major limitations of this modelling approach are:

1. The model neglects the effect of AC fault current on the DC bias, i.e., it assumes the DC bias current to be constant. However, practically the AC fault current will have an effect on the DC bias current as shown in Chapter 3 of this thesis. While the effects are more prominent in some topologies than others, the ‘transformer coupling’ [51] between the AC and the DC systems in saturated core FCLs can have a considerable influence on the FCL behaviour.

2. Since the model, in its derivation, ignores the leakage flux of the device it is not universally applicable to most saturated core FCL topologies. The model is more applicable to closed core FCL topologies where the effects of leakage flux is negligible.

The closed form mathematical expression (2.8) proposed in [50] is another prominent model that has been used to describe the transient behaviour of the saturated core FCL. In the model proposed in [50] the $B - H$ curve of the core material is approximated to an inverse tangent function, and hence the magnetic flux density in the cores due to a single AC coil can be expressed as:

$$B(i_{ac}) = \frac{-2B_{sat}}{1 + \tan^{-1}\left(K\pi - \frac{\pi}{2}\right)} \left[1 + \tan^{-1}\left(K \frac{\pi}{I_{max}} (I_{max} - i_{ac}) - \frac{\pi}{2}\right)\right] + 2B_{sat}$$

(2.8)

where $i_{ac}$ is the instantaneous line current, $I_{max}$ is the line current at which the core is fully saturated and $B_{sat}$ is the average magnetic flux density level at full saturation. $K$ is a factor which determines the range of line currents where the magnetic state of the cores actively change from saturate to unsaturated.

The differential inductance $\tilde{L}$ can be defined as,

$$\tilde{L} = n_{ac}A_{core} \frac{\partial B(i_{ac})}{\partial i_{ac}}$$

(2.9)
where $A_{\text{core}}$ is the core cross-sectional area. Note that if $\hat{L} < L_{\text{air}}$ or $i_{\text{ac}} > I_{\text{max}}$, then $\hat{L} = L_{\text{air}}$, where $L_{\text{air}}$ is the equivalent air core inductance.

Substituting (2.8) in to (2.9),

$$\hat{L} = n_{\text{ac}} A_{\text{core}} \frac{-2B_{\text{sat}}}{1 + \tan^{-1}\left(K\pi - \frac{\pi}{2}\right)} \left\{ \frac{1}{1 + \left[K_{\frac{\pi}{I_{\text{max}}}} (I_{\text{max}} - i_{\text{ac}}) - \frac{\pi}{2}\right]^2} \right\} \left(-K\frac{\pi}{I_{\text{max}}} \right)$$  \hspace{1cm} (2.10)

Hence, the induced voltage across the FCL can be expressed as,

$$V = \frac{2n_{\text{ac}} A_{\text{core}} B_{\text{sat}}}{1 + \tan^{-1}\left[K\pi - \frac{1}{2}\right]} \left\{ \frac{1}{1 + \left[K_{\frac{\pi}{I_{\text{max}}}} (I_{\text{max}} - i_{\text{ac}}) - \frac{\pi}{2}\right]^2} \right\} \left(K\frac{\pi}{I_{\text{max}}} \right) \frac{\partial i_{\text{ac}}}{\partial t}$$  \hspace{1cm} (2.11)

To estimate the parameters ($B_{\text{sat}}$, $A_{\text{core}}$ and $I_{\text{max}}$) of the closed-form equation, [50] proposed the use of curve fitting procedures based on static FEM simulations. While the model performed reasonably well in predicting the FCL behaviour qualitatively, quantitative discrepancies in current and voltage magnitudes can be observed in model validation exercises. Further analysis revealed that, similar to the previous model discussed [49], this modelling approach does not incorporate the transformer coupling effect between the AC and DC windings during a fault.

### 2.6 FCL Grid Integration

#### 2.6.1 Potential Applications of FCLs

Possible applications of SCFCL in an urban power system is discussed in [52,53] focusing in particular on technical and economic benefits an SCFCL application at different locations on the grid could offer. The potential network locations are illustrated in Figure 2.11.
An FCL in a generator interconnection location limits the fault contribution from the generator unit, thereby lowering the stress on other system switchgear. FCL devices also have applications in power station auxiliary systems.

FCL devices can be used to couple distribution systems, particularly at the connection points of large distributed generators. A network-coupling FCL improves voltage stability, reliability and provides protection from problematic voltage sags and high fault levels.

Installing an FCL at a bus-tie location in the network, has often been preferred by the utilities due to the additional benefits such a placement offers [24, 54]. A bus-tie FCL typically allows two buses to be tied without significantly raising the fault current level of the system and enables greater network flexibility and improved reliability.

An FCL in an incoming feeder limits the contribution from the feeder to the total short-circuit current of the system. In contrast, an FCL in an outgoing feeder limits the contribution of the system to the short-circuit current in that particular feeder and protects downstream devices. An incoming feeder-FCL has the added advantage of greater system availability due to the parallel connection of the feeding generators and transformers.

An FCL can be used as a shunt path for a current limiting reactor under normal grid conditions, thereby avoiding voltage drop and losses associated with conventional reactors. FCLs can also be used to protect other superconducting devices (such as superconducting cables) to protect them from quenching.
Figure 2.11: Possible locations of FCL application in power systems [53]
2.6.2 FCL Integration Issues

With the maturity of the FCL technology, addressing issues with regard to integrating FCLs to utility grid is becoming increasingly important. In spite of the extensive scope of literature available in the subject area concerning different fault current limiting technologies, research addressing the influence of these devices on the grid and the issues with system integration is limited [4,55–58]. A number of potential FCL integration issues have been identified in [55] including:

- potential system configurations and contingencies;
- duration of fault current and the magnitude of current limitation;
- short term fault and reclosure sequences;
- recovery time between faults;
- internal protection features of FCLs, including cryogenic cooling aspects, if applicable;
- losses associated with current limiters and effects on system power flows;
- FCL maintenance requirements;
- required current and voltage ratings;
- potential operational interactions with other network equipment;
- control of follow current for coordination and to minimise changes to existing protection schemes and equipment; and
- space limitations, particularly for urban locations and retrofit applications.

Among the significant studies that have been undertaken to examine the behaviour of FCLs in utility grids, work conducted by the CIGRÉ Working Groups - A3.10, A3.16 and A3.23 - are notable. CIGRÉ Working Group A3.10 in [13], has laid out the foundation for a typical investigation on interactions between the FCL and the
electricity grid. According to [13], a standard investigation should include examining the impact of an FCL on the existing protection scheme in terms of selectivity, relay settings and compatibility, as well as the influence on the existing switchgear such as circuit breakers, impact on the independent power producer (IPP) installations and effect on the overall system reliability.

As an extension to this work, CIGRÉ Working Group A3.16 [56] analysed possible effects of several different emerging FCL technologies on the existing protection principles focusing mainly on the overcurrent, distance and differential protection in medium and high voltage systems. However, this study was not based on extensive transient models emulating the true dynamic behaviour of different FCL technologies, but rather uses simplified models of FCLs in a generalised form in its analysis. Hence, while [56] provides a general framework for future studies, given the unique characteristics of different types of FCL technologies, the impact they have on the power system and its protection schemes could differ considerably depending on the type of FCL. Therefore, a comprehensive investigation based on the behavioural models of each FCL technology is essential in examining the potential integration issues.

At the time of writing of this thesis, several such studies have reported on the impact of current limiting devices on existing switchgear and protection schemes considering dynamic models of different FCL technologies [9–11]. These will be discussed in the following sections.

2.6.3 Solid state FCLs

The effects of a solid state current limiter on power system relaying are explored in [9]—considering both the relaying effects of the SSFCL system itself and the potential impact of current limiting by the device on existing protective devices. The report examined several important aspects covering:

- Impact on adjacent relays governed by inverse-time characteristics in terms of speed of fault clearing; trip times of conventional over current protection
devices may increase due to the reduction of first peak of a fault current as well as the subsequent follow current magnitude.

- Possible impact on relay coordination due to harmonics generated by the limiting action; The voltage and current waveforms may be severely distorted due to the nonlinear impedances introduced into the short circuit path by FCLs during the limiting events causing some conventional sensors and relays to mal-operate.

- False readings of impedance relays upstream of the FCL location: The FCL action may significantly change the network impedance observed by such relays. Distant protection schemes commonly used in transmission systems will be negatively affected by such readings.

The report further recommended evaluating proposed applications on a case-by-case basis to ensure effective integration of the device.

The study presented in [10] investigated the potential impact of distorted current waveforms produced by thyristor-based SSFCL on two coordinated protection relays in a typical medium voltage (MV) application using an SSFCL model implemented on a real-time digital simulator (RTDS) platform. The simulation results demonstrated that due to the distortion of follow current, the tripping times of associated relays could increase. The study concluded that for applications that could potentially cause considerable distortion in the follow current, the coordination of relays must be re-examined, especially if the associated relays utilise different measurement techniques (such as measurement of peak versus the fundamental). Administration of proper firing angle control strategy on the SSFCL to actively control the follow current is proposed in [10], thereby eliminating undesirable effects on coordinated relays with different measurement techniques. Experimental results derived in [59] using a SEL-311C distance relay and a prototype SSFCL further verifies the simulation results of [10].
2.6.4 Superconducting FCLs

The limiting performance of a resistive SCFCL installed in an electrical distribution grid is analysed in [30, 60] using an electromagnetic transient program. Simulation studies demonstrated that further to limiting the fault current to an acceptable level, the resistive SCFCL can mitigate the voltage sag levels during a fault event. The presence of the FCL was also shown to reduce the transient over-voltage and the TRV across the circuit breaker.

The effect of a resistive SCFCL on a utility type impedance relay was investigated in [61], using a real time hardware-in-the loop experiment. The simulation results demonstrated that due to dynamically changing SCFCL impedance, faults within the protection zone may not be picked up correctly by the associated relays. The impact of a fast switching resistive FCL on a distance protection scheme is investigated in [62], with some methods of modifying the mho relay characteristic suggested. The measured impedance at the relaying point where resistive and inductive FCLs are installed are evaluated in [63, 64]. The presence of the FCLs were shown to affect the measured impedance and consequently the distance protection schemes.

2.6.5 Saturated core FCLs

The transient behaviour of saturated core FCLs in different distribution system configurations were investigated in [49] using PSCAD/EMTDC. Two different implementations of the FCL were considered: (1) two FCLs in series with HV/MV transformers on the incoming feeders (2) a single FCL on split bus. For both cases, the simulation results demonstrated that the FCLs can achieve the desired fault current reduction. However, the simulated AC current waveform during fault current limiting was shown to be clipped to a square wave, indicating significant current harmonics. In practice, the actual limited current waveforms are seen to be more closer to a modified sinusoidal wave. Hence, the square-wave-clipping operation of the ac current is likely to be a result of the limitations in the saturated core FCL.
modelling technique used.

The operational experience of the first commercial installation of HTS saturated core FCL in the United States, where a 15 kV device was installed in the Avanti distribution circuit of Southern California Edison Company is discussed in [40]. The FCL underwent extensive testing before installation and experienced its first-in-grid fault in 2010. Based on the voltage data recorded, it was seen to be a multiphase sequence of fault events lasting over a three second period during which the FCL operated as designed and limited the fault current. Since its installation, the 15 kV Avanti FCL has also sustained an unexpected loss of DC magnetic bias, causing the FCL to remain on-line in an unbiased condition. Based on the actual field data obtained during this event, simulations of a resonance event were performed in [65]. The results demonstrated that under low load conditions, at certain values of reactive shunt compensation, full insertion of the reactance of the FCL can cause a sustained but damped resonance. Resonance suppression preventive measures, including pre-installation system simulation studies with capacitor banks in the vicinity to identify these conditions, are also proposed in [65].

While some work has been conducted to investigate potential grid integration issues of FCLs, based on the review of the existing literature, it was seen that further work is required to address key research gaps related to saturated core FCL applications and performance studies in utility grids.
2.7 Conclusions

An overview of different types of FCL technologies which have been proposed and those currently being developed and their applications were discussed in this chapter. Immediate and reliable current limitation with minimal impact during normal operation were established as some of the desirable attributes of a current limiter. A review of existing FCL technologies belonging to three main categories - semiconductor-based, superconductor-based and saturated core FCLs was presented. The relative advantages of each technology as well as the shortcomings that hinder them from reaching their full commercial potential were also discussed.

Amongst the emerging fault current limiting technologies, saturated core FCLs are currently considered to be one of the most promising. The operating principle of a saturated core type FCL was discussed in detail. A saturated core FCL, in essence, utilises the dynamic and magnetic permeability of steel cores to function as a variable inductance reactor. Over the years different topology variations of saturated core FCLs have been introduced due to several considerations including performance, cost, mass, size and construction feasibility. Different saturated core FCL topologies, including single-core topologies with bridge rectifiers, and dual-core topologies such as the open core, closed core and hybrid-core configurations and their advantages and drawbacks were discussed.

An analysis of the existing saturated core FCL modelling techniques was presented, with advantages and limitations of each modelling technique discussed. The literature revealed that experimental measurements and FEM analysis have been the most prevalent techniques used to characterise the transient behaviour of these devices. Both these techniques while accurate, can not be used to analyse the transient electrical behavior of FCLs in real power systems, particularly when investigating power system switchgear behaviour during fault events. FEM-based FCL modelling, despite its usefulness as a design verification tool, can not be interfaced directly with all EMT programs that are in use today.

The closed form equations that have been presented in the literature to describe
the transient behaviour of dual-core saturated core FCLs, have been mostly developed from various approaches to approximate the B-H characteristics of the core material. To estimate the parameters of these closed-form equations, curve fitting procedures have been utilised based on static FEM simulations. While the models performed reasonably well in predicting the FCL behaviour qualitatively, quantitative discrepancies in current and voltage magnitudes have been observed in model validation exercises. Further analysis revealed two major limitations common to these modelling techniques:

1. Neglecting the effect of AC fault current on the DC bias, by assuming the DC bias current to be constant. While the effects are more prominent in some topologies than others, the ‘transformer coupling’ between the AC and the DC systems in saturated core FCLs can have a considerable influence on FCL behaviour during a fault.

2. Ignoring the leakage flux effects of the device. Hence it is not universally applicable to all saturated core FCL topologies.

In spite of the extensive scope of literature available in the subject area concerning different fault current limiting technologies, research concerning the influence of these devices on the grid and the issues with system integration was found to be limited. Among the significant studies that have been undertaken to examine the behaviour of FCLs in utility grids, work conducted by the CIGRÉ Working Groups - A3.10, A3.16 and A3.23 - were found to be of great interest.

The following key aspects in relation to saturated core FCL applications and performance studies in utility grids were identified in this chapter:

1. The significance of understanding the transient behaviour of the device in actual power networks and the limitations of the relevant research outcomes available in the subject area.

2. The need for an accurate transient model of the saturated core FCL that be confidently applied in relation to power system studies to investigate future
networks containing FCLs.

3. The need for a modelling technique that has the capacity to represent the full nonlinear range of magnetic operation including the coupling between the AC and DC coils of the device.

While some work has been conducted to investigate potential grid integration issues of saturated core FCLs, based on the review of the existing literature, it was seen that further work is required to address key research gaps related to saturated core FCL applications and performance studies in utility grids.
Chapter 3

Modelling Saturated Core Fault Current Limiters

3.1 Introduction

Chapter 2 introduced the basic operating principle of a saturated core type FCL, which in essence utilises the dynamic and magnetic permeability of steel cores to operate as a variable inductance reactor. These nonlinear characteristics of the steel cores have, however, made modelling this unique device a difficult task. Hence, as explained in the Chapter 2, experimental measurements and finite element method (FEM) analysis have been the most prevalent techniques used to characterise the transient behaviour of the device. Both these techniques while accurate, can not be used to analyse the transient electrical behavior of FCLs in real power systems, particularly when investigating power system switchgear behaviour during fault events. FEM-based FCL modelling, despite its usefulness as a design verification tool, can not be interfaced directly with some transient network analysers that are in use today.

As emphasised in Chapter 2, it is important to understand the potential transient behaviour of these devices in real power systems before deploying these devices in the utility grids. Computer-aided modelling and simulation are fundamen-
tal steps in demonstrating the benefits of this technology in network applications. Hence, there is an increasing need for an accurate model of the FCL that can be incorporated into electromagnetic transient (EMT) program such as PSCAD/EMTDC. PSCAD/EMTDC is a time-domain simulation software package frequently used for electromagnetic transient studies of large systems, particularly when detailed modelling of control systems and nonlinear devices are required.

In recent work [66], the “magnetic circuit concept” was applied to an open core saturated core FCL where the geometry of the FCL (including all substantial flux paths in the device) was represented by a magnetic reluctance circuit. This chapter makes advancements on this magnetic analysis of the open core saturated core FCL, and proposes two modelling approaches to represent the FCL in transient network simulators: the Nonlinear Reluctance Model and the Nonlinear Inductance Model.

The Nonlinear Reluctance Model presented in Section 3.2, extends the magnetic analysis of the open core configuration, by directly coupling the magnetic circuit of the device to the AC (i.e. AC coils and the grid side network) and DC (i.e. the biasing arrangement of the FCL) electrical systems of the FCL. The model implementation in PSCAD/EMTDC is presented and the validity of the proposed model is examined using experimental results obtained from single-phase saturated core FCL prototype. While the reluctance modelling approach demonstrated adequate accuracy when simulated in a simplified circuit, it was noted that, numerical oscillations and instabilities occurred when it was incorporated into a large network (even when using a very small solution time step).

A second modelling approach is proposed in Section 3.3 in order to overcome this problem. This modelling approach, termed the Nonlinear Inductance Model, introduces a single equivalent electric circuit model of lumped inductors to represent the saturated core FCL. The initial electric circuit is derived from the magnetic circuit of the Nonlinear Reluctance Model, using the principle of electromagnetic duality. The model implementation in PSCAD/EMTDC is presented and the simulation results of the time-domain model are validated against experimentally measured data.
of a single-phase prototype saturated core FCL.

A key aspect of the proposed modelling approaches is that there is no unique equivalent circuit model for all devices. Hence, in Section 3.4, the applicability of the equivalent electric circuit model developed for the open core topology, to a few other selected FCL topologies, is examined.

3.2 Nonlinear Reluctance Model of FCL

3.2.1 Derivation of analytical nonlinear reluctance model

When modelling electromagnetic systems, such as transformers and various electric machines with complex geometries, the magnetic circuit concept [67] has often been used to derive analytical models with adequate accuracy [68–71]. This concept was extended to saturated core FCL in [66], where the magnetic field of the device was represented by a magnetic circuit of lumped reluctances.

The topology on which the initial magnetic circuit analysis was applied in [66] is shown in Figure 3.1. This device represents a basic configuration of a single-phase saturated core FCL with an open core topology. It consists of two iron cores surrounded by two separate copper coils wound in opposite directions, carrying the same AC line current (AC windings). A winding carrying the DC bias current (which can be either superconducting or copper) encompasses both iron cores and the associated AC coils.

Figure 3.2 illustrates the significant paths of magnetic flux identified for this particular arrangement of the FCL. Each of these flux paths is then represented by a reluctance element in the corresponding magnetic circuit. The iron cores appear as saturable reluctances while the leakage paths are represented as linear reluctances. Reluctance elements $R_{c1}$ and $R_{c2}$ represent the flux paths through the core sections and have nonlinear characteristics resulting from the magnetic properties of the cores. $R_y$ represents the flux paths between the two AC windings (top and bottom) and, although theoretically these are leakage paths through air, it has been noted
that the extremities of the iron cores have some influence on them resulting in a
minor non-linearity. However, in most cases $\Re_y'$ can be approximated to be linear.

The leakage paths between the AC and DC coils are represented by $\Re_a'$ and $\Re_i$,
where the leakage paths linking the inner section of the AC coil with the other
paths is represented by $\Re_a'$ (top and bottom) while remaining leakage paths inside
the DC coil (linking outer section of the AC coils) is represented by $\Re_i$. $\Re_o$ represents
the leakage flux paths outside the DC coil lumped together. By using simplifying
assumptions the equivalent magnetic circuit illustrated in Figure 3.3 is derived from
Figure 3.2. The top and bottom $\Re_a'$ and $\Re_y'$ reluctance elements in Figure 3.2 are
lumped together and are represented by $\Re_a$ and $\Re_y$ respectively in the equivalent
magnetic circuit. $F_{ac1}$, $F_{ac2}$ and $F_{dc}$ represent the corresponding magnetomotive
force (mmf) due to the two AC windings and the DC winding respectively. It should
also be noted that considering the symmetry of the magnetic flux paths identified,
the equivalent magnetic circuit in Figure 3.3 is also symmetrical with identical $\Re_a$,
$\Re_i$, $\Re_o$ and $F_{dc}$ elements on the left and right sides of the circuit.

The value of each reluctance element is dependent on the particular geometry
of the device and magnetic properties of the cores. These values can be determined
by analysing the magnetic flux distribution in the device using either finite element
analysis (FEA) or experimentation. As described in [66], flux measurements are
Figure 3.2: Significant magnetic flux paths and associated reluctances [66]

Figure 3.3: Equivalent magnetic circuit of open core FCL adapted from [66]
taken under the following three test conditions over a range of applied mmf values
(sufficient to saturate the cores) to determine the values of all reluctance elements.

- Test 1 - $F_{dc}$ is varied and $F_{ac1} = F_{ac2} = 0$
- Test 2 - $F_{dc} = 0$ and $F_{ac1} = F_{ac2}$ (varied)
- Test 3 - $F_{dc} = 0$ and $F_{ac1} = -F_{ac2}$ (varied)

Based on the resulting flux measurements, standard circuit analysis techniques and
superposition are used to calculate the value of each reluctance element in the mag-
netic circuit. The magnitudes of $\mathbb{R}_i$ and $\mathbb{R}_o$ are determined using Test 1 and Test 2,
along with the series combination of $\mathbb{R}_c + \mathbb{R}_a$. Test 3 is then used to determine the
values of $\mathbb{R}_a$ and $\mathbb{R}_y$ as well as the curve for $\mathbb{R}_c$. The complete process of deriving
the reluctance values of the circuit in Figure 3.3 is summarised in the Appendix A.

In [66] the reduced reluctance model was implemented in MATLAB, where the
flux linkage of each coil was compared to the results from an FEA simulation for
sinusoidal input current conditions. Excellent agreement between the reluctance
circuit and the FEA results validated the magnetic field analysis proposed by the
model. However, [66] only described and validated the magnetic domain of the FCL
(i.e. flux linkage of each coil for a given current) and did not describe or validate
the transient electromagnetic behaviour of the model (such as the AC/DC currents
and coil voltages during a fault event).

3.2.2 PSCAD/EMTDC Implementation of the Reluctance
Model

The magnetic reluctance circuit described in Section 3.2.1 represents the magnetic
field of the saturated core FCL, including the magnetic coupling that exists between
the AC and DC windings. However, in order to determine the electrical characteris-
tics of the device it is necessary to couple the magnetics with the associated electric
circuits of the device. The saturated core FCL model, described in this section,
couples the magnetic reluctance circuit with the AC and DC electric circuits of the FCL utilising the modelling algorithm highlighted in Figure 3.4.

The model is implemented in PSCAD/EMTDC as a page module containing three mutually-dependent circuits; the AC circuit consisting of the two AC windings of the FCL - carrying the AC load current (illustrated in Figure 3.5), the DC biasing circuit consisting of an external DC power supply energising the DC winding (illustrated in Figure 3.5) and thirdly, the equivalent magnetic circuit.

The mmfs produced by the AC and DC windings are computed based on the respective current passing through them at each time step of the simulation using the relation $F = NI$; where $F$ is the mmf produced by an $N$ turn winding, carrying a current $I$. It should be noted that magnetic circuits can be analysed in a similar way to electric circuits. That is, the correlation between mmf and magnetic flux in a magnetic circuit is analogous to the correlation between voltage and current in an electric circuit. Hence, the magnetic circuit (see: Figure 3.3) is solved to derive the magnetic flux through each winding ($\phi_{ac1}$, $\phi_{ac2}$ and $\phi_{dc}$) using the mmf values computed, based on the $F = R\phi$ relation where $\phi$ is the magnetic flux and $R$ is the reluctance of the flux path. These flux values are then used to calculate the voltage induced in the DC winding ($V_{dc-coil}$) and the voltage induced in the AC windings ($V_{ac-coil1}$ and $V_{ac-coil2}$) employing (3.1) and (3.2) respectively. As illustrated in Figure 3.4 these computed back emf across AC windings are then fed back to the AC circuit where based on the back emf across each winding, the AC circuit is solved to derive the new AC current for the next time step. Similarly, the calculated back emf across the DC winding is utilised to determine the new DC current magnitude for the next time step. It should be noted that, this interdependency and the time step mismatch inherent to the model, combined with the numerical instabilities inherent to the trapezoidal integration method used in EMTDC, caused numerical oscillations and inaccuracies when the model was simulated using a standard solution time step (in the order of $1 \sim 50 \mu s$). Hence, to avoid numerical oscillations, a sufficiently small time step (in the order of $1 \sim 10 \, ns$) is required in these simulations.
Calculate MMF produced by each AC winding

Solve magnetic circuit to determine flux through each winding

Determine $R_{c1}$ and $R_{c2}$ from the characteristic reluctance curve

Compute back emf generated across each AC coil

Compute back emf generated across the DC coil

Measure AC line current

Measure DC current

$V_{ac-coil1}$, $V_{ac-coil2}$

$V_{dc-coil}$

$\phi_{ac1}, \phi_{ac2}$

$\phi_{dc}$

$F_{ac1}, F_{ac2}$

$F_{dc}$

$I_{ac}$

$I_{dc}$

Figure 3.4: Nonlinear Reluctance Model algorithm in PSCAD/EMTDC
Figure 3.5: Electric circuits of the FCL (a) AC circuit (b) DC circuit

\[ V_{dc\text{-coil}} = N_{dc} \frac{d\phi_{dc}}{dt} \]  \hspace{1cm} (3.1)

\[ V_{ac\text{-coil}1} + V_{ac\text{-coil}2} = N_{ac} \frac{d\phi_{ac1}}{dt} + N_{ac} \frac{d\phi_{ac2}}{dt} \]  \hspace{1cm} (3.2)

\[ V_{fcl} = V_{ac\text{-coil}1} + V_{ac\text{-coil}2} + i_{ac}(R_{ac\text{-coil}1} + R_{ac\text{-coil}2}) \]  \hspace{1cm} (3.3)

In (3.1) - (3.3) the number of turns in each AC coil is denoted by \( N_{ac} \), while \( N_{dc} \) is the total number of turns in the DC winding. The total flux linking the DC coil is denoted by \( \phi_{dc} \) and flux through the two AC coils are denoted by \( \phi_{ac1} \) and \( \phi_{ac2} \). \( R_{ac\text{-coil}1} \) and \( R_{ac\text{-coil}2} \) are the AC coil resistances and the total voltage across the FCL is denoted by \( V_{fcl} \) (given in 3.3).

The PSCAD/EMTDC model of the FCL is shown in Figure 3.6. As illustrated, external electrical nodes (labelled as A and B) are used to connect the FCL model in series with the network where the fault current is to be limited. The DC and AC coil voltages are calculated based on (3.1) and (3.2) respectively (using control blocks), and are passed to the electric circuits as externally controlled sources. The FCL is interfaced to the rest of the system via these voltage sources representing back EMF across the two AC coils. Note that coil voltages are initialised to be zero for
Figure 3.6: PSCAD/EMTDC model of the FCL
the first time step. A two input selector is used to select the applicable coil voltage depending on the value of the control signal (which is based on the simulation time). The voltage across the FCL ($V_{fcl}$) is the total measured voltage across the two AC coils and is recomputed at each time step.

### 3.2.3 Nonlinear Reluctance Model Validation

#### 3.2.3.1 Experimental Set-up

To validate the proposed reluctance FCL model, experimental data was obtained from short circuit tests undertaken on a demonstrator-scale 312 V$_{L-N}$ prototype saturated core FCL. The prototype device is a single-phase device with an open-core topology (i.e. the return flux paths for both cores are through air) consisting of two steel cores encompassed by separate copper coils, carrying the same AC line current. Two DC coils arranged at the centre of the FCL, were used to achieve the required level of DC bias. To avoid any cryogenic complexities associated with superconducting coils (for instance, cryogenic overheads such as expensive cryostats, cryogenic coolers and chillers, along with extensive cool-down times and fragility of the superconducting coils in the event that the prototype design was unsuccessful), copper DC coils were used on this prototype. The design parameters of the prototype device is given in Table 3.1 and a photograph of the prototype device is shown in Figure 3.7.

A schematic of the UOW FCL test system is illustrated in Figure 3.8. The AC windings of the prototype FCL were connected in series with the secondary winding of a 415 : 1039Y transformer. A resistive load bank was used to replicate normal (pre-fault) load conditions, with auxiliary contactors (controlled by a LabView program on a PC) being used to short-circuit the load bank and simulate fault conditions. The DC windings of the device were connected to a current controlled DC power supply through a protection circuit. A custom-built data acquisition system was used for data collection and to interface with control hardware. A number of short circuit tests were undertaken on this prototype device at a 1500 A prospective
symmetrical fault current and different levels of applied DC bias (varying from 60 kA-turns to 84 kA-turns). Three of these tests were selected for model verification: (a) Test 1 at 60 kA-turns, (b) Test 2 at 72 kA-turns and (c) Test 3 at 84 kA-turns—the corresponding circuit test parameters are summarised in Table 3.2.
Table 3.1: Prototype design parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core height</td>
<td>0.6 m</td>
</tr>
<tr>
<td>Core cross-section area</td>
<td>0.0064 m²</td>
</tr>
<tr>
<td>AC coil: width, depth and height</td>
<td>0.1 m, 0.1 m, 0.39 m</td>
</tr>
<tr>
<td>DC coil: width, depth and height</td>
<td>0.51 m, 0.38 m, 0.105 m</td>
</tr>
<tr>
<td>AC coil: number of turns ¹</td>
<td>60</td>
</tr>
<tr>
<td>DC coil: number of turns ¹</td>
<td>100</td>
</tr>
</tbody>
</table>

¹Parameter values given for each coil

Table 3.2: FCL test parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Prospective symmetrical current</td>
<td>1500 AA</td>
</tr>
<tr>
<td>DC bias¹</td>
<td>60 kA-turns, 72 kA-turns, 84 kA-turns</td>
</tr>
<tr>
<td>AC line voltage (L-N)</td>
<td>312 V</td>
</tr>
<tr>
<td>Frequency</td>
<td>50 Hz</td>
</tr>
<tr>
<td>AC source impedance</td>
<td>$(0.1095 + j0.177) \Omega$</td>
</tr>
<tr>
<td>Xs/Rs</td>
<td>1.616</td>
</tr>
<tr>
<td>Load resistance</td>
<td>8.79 Ω</td>
</tr>
<tr>
<td>AC coil resistance²</td>
<td>0.0098 Ω</td>
</tr>
<tr>
<td>DC coil resistance²</td>
<td>0.0733 Ω</td>
</tr>
<tr>
<td>Fault type</td>
<td>Line to neutral bolted fault</td>
</tr>
</tbody>
</table>

¹DC bias applied for Test 1, Test 2 and Test 3 respectively
²Parameter values given for each coil
3.2.3.2 Parameter Estimation

A finite element (FE) model of the prototype FCL was built in the Cedrat FLUX3D package using the physical geometric parameters of the prototype device as well as characteristics of the electromagnetic properties of each material. The FE model of the prototype device is shown in Figure 3.9.

The FE model was subsequently used to perform magnetostatic simulations of the three tests specified in Section 3.2.1. Multiple static solutions for each test were obtained by varying the mmf applied and the resultant flux linkage values were used to calculate the reluctance element of each flux path. Figure 3.10 shows the calculated reluctance values, plotted against the core mmf (the mmf directly acting on the cores). As shown in Figure 3.10, $\mathcal{R}_c$ represents a relation between magnetic flux and mmf, while it is constant at low values of applied mmf, its value increases as the iron becomes saturated. In comparison, the change in $\mathcal{R}_y$ as the applied mmf is increased is minor and thus $\mathcal{R}_y$ can be approximated as a constant reluctance. $\mathcal{R}_a$, $\mathcal{R}_i$ and $\mathcal{R}_o$ appear to be constant irrespective of the level of applied mmf.

Table 3.3 summarises the linear reluctance values derived for this particular FCL device. The characteristic curve that is used to represent the nonlinear core
Figure 3.9: Finite element model of the 312 V\textsubscript{L−N} prototype FCL

reluctance elements \( \mathcal{R}_c \) and \( \mathcal{R}_e \) is illustrated in Figure 3.11 and the corresponding magnetisation characteristic, illustrating the regions of saturation, is shown in Figure 3.12.

At each time step of the simulation, the mmf produced in each core from the previous time step is used to look up the corresponding core reluctance value (\( \mathcal{R}_c \) and \( \mathcal{R}_e \)) from the characteristic reluctance curve shown in Figure 3.11. The core reluctance values and the linear reluctance values presented in Table 3.3 were used to solve the magnetic reluctance circuit in the PSCAD/EMTDC simulations.

<table>
<thead>
<tr>
<th>Magnetic Reluctance</th>
<th>Value (A/\text{wB})</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \mathcal{R}_y )</td>
<td>237652</td>
</tr>
<tr>
<td>( \mathcal{R}_a )</td>
<td>893375</td>
</tr>
<tr>
<td>( \mathcal{R}_i )</td>
<td>2464041</td>
</tr>
<tr>
<td>( \mathcal{R}_o )</td>
<td>1006103</td>
</tr>
</tbody>
</table>

Core saturation plays a significant role in determining the transient behaviour of the saturated core FCLs. Typically, in transformer modelling, the iron core be-
Figure 3.10: Variation of reluctance elements with core MMF

Figure 3.11: Characteristic reluctance curve
Figure 3.12: Magnetisation curve of the core sections

behaviour is represented by a relationship between the magnetic flux density $B$ and the magnetic field intensity $H$. However, depending on the history of the sample, each of the magnetic field values can relate to multiple possible magnetisation levels. Hence, to characterise the core material behaviour accurately, representation of numerous associated curves (major and minor loops) may be necessary [72]. However, for practical purposes, considering the negligible influence of hysteresis loops on the magnitude of magnetisation current, the magnetic saturation of the iron cores in the saturated core FCLs are represented by the anhysteretic B-H relationship.

3.2.3.3 PSCAD/EMTDC Simulations

The experimental test conditions were modelled in PSCAD/EMTDC, as a simplified circuit as illustrated in Figure 3.13. The circuit of Figure 3.13 consists of an AC voltage source (including source impedance), the proposed FCL model and a load approximated by a resistive element. The controlled DC source used in the experiment to energise the DC bias windings of the FCL was replicated in PSCAD/EMTDC, using an externally controlled DC source and a PI controller. The single-line-to-ground fault was represented on the load side of the FCL with a fault ON resistance of
0.0001Ω. To ensure network solution accuracy a small solution time step of 10 ns was chosen and the switching events were interpolated to the precise time.

Figure 3.13: PSCAD/EMTDC equivalent circuit representing the experimental set-up with a line-to-ground fault simulated on the load-side of the FCL.

3.2.3.4 Validation Results

The plots of the fault currents—prospective, measured limited current and simulated limited current—for the three test conditions described in Section 3.2.3.1 are shown in Figure 3.14. As can be seen from the graphs, the fault was applied at different points-on-wave of the load cycle thus resulting in different asymmetrical first peak magnitudes in each test case. In the first test scenario, the FCL clipped 58% of the prospective first peak fault current and 67% of the symmetrical prospective current. In the second test scenario, 54% of the prospective first peak fault current and 63% of the symmetrical prospective current were clipped by the fault current limiting action. The third scenario shows a clipping of 52% of the prospective first peak current and 59% of the symmetrical prospective current. In all three cases, the simulated current waveforms replicate the experimental current waveforms quite closely, matching both the peak and the steady state fault currents.

Whilst several analytical models proposed to-date have been effective in predicting the fault current waveforms [49, 50], many of them have not succeeded in replicating the FCL terminal voltage behaviour, particularly the oscillatory transients of the voltage waveform. Figure 3.15 shows the plots of voltage across the FCL ($V_{fcl}$) for the three test cases. In all three scenarios, the simulations were able to closely reproduce the experimental FCL voltage waveforms quite accurately under
normal grid conditions as well as during the fault event. In particular, during the fault, the most significant non-sinusoidal intricacies of the voltage waveform were replicated by the simulations.

The steady state voltage drop across the FCL terminals was found to be approximately 1.24%, 0.99% and 0.95% of the supply voltage, for each of Tests 1, 2 and 3 respectively. Note that, while a lower DC bias improves the limiting performance of the device, the resultant voltage drop across the device during steady-state pre-fault conditions is considerably larger. Hence, the optimum DC bias for each FCL design must be determined based on the current clipping requirements and the maximum allowable steady state voltage drop constraints of each application.

Figure 3.16 shows the plots of the FCL-limited current and FCL terminal voltage \(V_{\text{fcl}}\) when the FCL is modelled and simulated using the closed form equation [50] discussed in Section 2.4. For brevity only the results from Test 2 (at 72 kA-turns DC bias level) are presented here. As can be seen, while the FCL-limited current is predicted reasonably well, the simulated FCL terminal voltage in Figure 3.16b does not conform with the experimental results, particularly, in terms of accurately reproducing the oscillatory transients of the voltage waveform.

In addition to providing accurate predictions for AC fault currents and voltages, as shown in Figure 3.17, the simulation results also demonstrate the coupling effects between the AC and DC coils in the FCL quite accurately. Figure 3.17 illustrates the comparison between the simulated DC currents and the measured DC currents for the three test scenarios. In the experimental set-up, the two DC bias currents were found to be nearly identical; therefore, in the validation exercise the average of the two measured DC currents was used.

While the proposed model produced accurate results when simulated with a sufficiently small solution time step (in the order of \(1 \sim 10\) ns), numerical oscillations were observed when a relatively larger solution time step (in the order of \(1 \sim 50\) µs) was used. Figure 3.18 shows the plots of the FCL-limited current and FCL terminal voltage \(V_{\text{fcl}}\) for Test 2 (at 72 kA(turns) DC bias level) of the above-presented
validation exercise, when simulated using a time step of $10 \mu s$. As can be seen, while the FCL-limited current is predicted reasonably well, numerical oscillations were observed in both waveforms during the transient phase of the fault. This manifested significantly more in the voltage waveform, particularly at the zero-crossing points of the waveform.
Figure 3.14: Measured and simulated fault current waveforms (a) Test 1: at 60 kA-turns (b) Test 2: at 72 kA-turns (c) Test 3: at 84 kA-turns DC bias level
Figure 3.15: Measured and simulated FCL terminal voltage waveforms (a) Test 1: at 60 kA-turns (b) Test 2: at 72 kA-turns (c) Test 3: at 84 kA-turns DC bias level. Only the first 2-3 cycles post-fault voltage waveforms are shown for clarity.
Figure 3.16: Measured and simulated results for Test 2 using the model proposed in [50] (a) Fault current waveforms (b) FCL terminal voltage (only the first 2-3 cycles post-fault voltage waveforms are shown for clarity)
Figure 3.17: Average Measured and simulated DC coil current waveforms (a) Test 1: at 60 kA-turns (b) Test 2: at 72 kA-turns (c) Test 3: at 84 kA-turns DC bias level
Figure 3.18: Measured and simulated results of the Nonlinear Reluctance Model (at 72 kA-turns DC bias level) with a solution time step of 10 µs in PSCAD/EMTDC
(a) Fault current waveforms (b) FCL terminal voltage

It was also found that, despite the use of a very small solution time step, numerical oscillations and instabilities still occurred when the Nonlinear Reluctance Model was incorporated into a large network (such as a power system with rotating machines and power electronic devices where there are occurrences of other switching events in the system).
3.3 Nonlinear Inductance Model of FCL

As described in Section 3.2, the Nonlinear Reluctance Model simulations were able to accurately reproduce the experimental results when a very small solution time step (in the order of $1 \sim 10 \text{ ns}$) was used. However, with a standard solution time step (in the order of $1 \sim 50 \text{ µs}$) numerical instabilities and inaccuracies were observed in the simulations which were attributed to the trapezoidal integration method used in EMTDC [73] as well as the non-simultaneity and the time step mismatch inherent in the modelling approach followed. While trapezoidal integration is astable when simulating linear systems, it is known to be susceptible to numerical oscillations in nonlinear systems, particularly when differentiating step changes in certain state variables [74]. For example, simulations associated with solving for voltage across an inductor following current interruption, or current in a capacitor following switching of a voltage source are likely to result in numerical oscillations. The FCL operates in a highly nonlinear region of the B-H curve and hence the potential for numerical oscillations in EMTP-programs is quite significant. This is exacerbated when the FCL model is incorporated into a stiff system (such as a power system with electric machines and power electronic devices), where the nonlinear behaviour of the device combined with the occurrences of other switching events in the system can lead to numerical oscillations, despite the use of a very small solution time step.

The new duality-based nonlinear inductance model presented in this section, makes advancements on the FCL reluctance model and, provides a solution to this problem. The concept of duality that exists between magnetic and electric circuits has been established for over a century [75,76], and has so far been applied to electromagnetic systems such as transformers and rotating machines to produce appropriate equivalent electric circuits [68,69,76]. In this section a new equivalent electric circuit model is derived for the saturated core FCL, using the principles of electromagnetic duality. This model allows for a larger solution time step to be used in EMT simulations and minimises the occurrence of numerical artifacts, particularly when inserted into larger network models.
3.3.1 Derivation of Equivalent Electric Circuit

According to Ampere’s law, the integral around any closed path of magnetic intensity, \( H \), is equal to the electrical current contained within that path.

\[
\int H \cdot dl = I
\]  

(3.4)

By combining (3.4) with Hopkinson’s law of magnetics given in (3.5), a law analogous to Kirchoff’s voltage law for electric circuits can be derived for magnetic circuits. Hence, in a magnetic circuit, the sum of mmf of the current-carrying windings around any closed path is equal to the sum of the products of reluctance and flux. Applying this relation to the magnetic circuit presented in Figure 3.3 of Section 3.2.1 (as shown in Figure 3.19), the set of mesh flux equations (3.6)-(3.10) can be derived. These equations relate the mesh fluxes \([\phi_n]\) to the externally applied magnetomotive forces \([F]\) with reluctances \([\mathbf{R}]\) as coefficients.

\[
[F] = [\mathbf{R}][\Phi] 
\]  

(3.5)

\[
\begin{bmatrix}
-F_{dc1} \\
F_{ac1} \\
-F_{ac1} - F_{ac2} \\
F_{ac2} \\
F_{dc2}
\end{bmatrix}
= 
\begin{bmatrix}
R_{11} & R_{12} & 0 & 0 & 0 \\
R_{21} & R_{22} & R_{23} & 0 & 0 \\
0 & R_{32} & R_{33} & R_{34} & 0 \\
0 & 0 & R_{43} & R_{44} & R_{45} \\
0 & 0 & 0 & R_{54} & R_{55}
\end{bmatrix}
\begin{bmatrix}
\phi_1 \\
\phi_2 \\
\phi_3 \\
\phi_4 \\
\phi_5
\end{bmatrix}
\]  

(3.6)
where the reluctance coefficients are:

\[
\begin{align*}
\Re_{11} &= \Re_{o1} + \Re_{i1} \\
\Re_{12} &= -\Re_{i1} \\
\Re_{21} &= -\Re_{i1} \\
\Re_{22} &= \Re_{i1} + \Re_{a1} + \Re_{c1} \\
\Re_{23} &= -\Re_{c1} \\
\Re_{32} &= -\Re_{c1} \\
\Re_{33} &= \Re_{c1} + \Re_{y} + \Re_{c2} \\
\Re_{34} &= -\Re_{c2} \\
\Re_{43} &= -\Re_{c2} \\
\Re_{44} &= \Re_{c2} + \Re_{a2} + \Re_{i2} \\
\Re_{45} &= -\Re_{i2} \\
\Re_{54} &= -\Re_{i2} \\
\Re_{55} &= \Re_{o2} + \Re_{o2} 
\end{align*}
\]

The mmf produced in an N-turn winding, carrying a current of \([I]\) is:

\[
[F] = N[I] 
\] (3.7)

The voltage induced in that particular winding is then given by:

\[
\begin{align*}
[V] &= N \frac{d[\Phi]}{dt} \\
[V] &= j\omega N [\Phi] 
\end{align*}
\] (3.8) (3.9)

Substituting (3.7) and (3.9) in (3.5):

\[
N[I] = [\Re] \frac{[V]}{j\omega N} 
\] (3.10)
By re-arranging (3.10):

\[
[I] = [\mathcal{R}] \frac{[V]}{j\omega N^2} \quad (3.11)
\]

The inductance \( L \) is inversely proportional to reluctance \( \mathcal{R} \) and is defined as:

\[
L = \frac{N^2}{\mathcal{R}} \quad (3.12)
\]

Substituting (3.12) in (3.11):

\[
[I] = \left[ \frac{1}{j\omega L} \right] [V] \quad (3.13)
\]

Written in matrix form, equation (3.14) relates the node voltages \([V_n]\) to a set of driving currents \([I]\) at the nodes, with inductive admittances as coefficients. Note that, in deriving these equations, it is assumed that all windings in the FCL have some arbitrary number of turns \(N\). However, in a practical FCL, each of the AC and DC windings will have a different number of turns. The process of establishing the actual currents and voltages in the windings, based on the number of turns in each winding, is addressed in Section 3.3.3.

\[
\begin{bmatrix}
-I_{dc1} \\
I_{ac1} \\
-I_{ac1} - I_{ac2} \\
I_{ac2} \\
-I_{dc2}
\end{bmatrix}
= \begin{bmatrix}
Y_{11} & Y_{12} & 0 & 0 & 0 \\
Y_{21} & Y_{22} & Y_{23} & 0 & 0 \\
0 & Y_{32} & Y_{33} & Y_{34} & 0 \\
0 & 0 & Y_{43} & Y_{44} & Y_{45} \\
0 & 0 & 0 & Y_{54} & Y_{55}
\end{bmatrix}
\begin{bmatrix}
V_1 \\
V_2 \\
V_3 \\
V_4 \\
V_5
\end{bmatrix} \quad (3.14)
\]
where the inductive admittance coefficients are:

\[
Y_{11} = \frac{1}{j\omega L_{o1}} + \frac{1}{j\omega L_{i1}} \\
Y_{12} = -\frac{1}{j\omega L_{i1}} \\
Y_{21} = -\frac{1}{j\omega L_{i1}} \\
Y_{22} = \frac{1}{j\omega L_{i1}} + \frac{1}{j\omega L_{a1}} + \frac{1}{j\omega L_{c1}} \\
Y_{23} = -\frac{1}{j\omega L_{c1}} \\
Y_{32} = -\frac{1}{j\omega L_{c1}} \\
Y_{33} = \frac{1}{j\omega L_{c1}} + \frac{1}{j\omega L_{y}} + \frac{1}{j\omega L_{c2}} \\
Y_{34} = -\frac{1}{j\omega L_{c2}} \\
Y_{43} = -\frac{1}{j\omega L_{c2}} \\
Y_{44} = \frac{1}{j\omega L_{c2}} + \frac{1}{j\omega L_{a2}} + \frac{1}{j\omega L_{i2}} \\
Y_{45} = -\frac{1}{j\omega L_{i2}} \\
Y_{54} = -\frac{1}{j\omega L_{i2}} \\
Y_{55} = \frac{1}{j\omega L_{o2}} + \frac{1}{j\omega L_{i2}}
\]

Matrix (3.14) can be written as nodal-voltage equations:

\[
-I_{dc1} = \frac{1}{j\omega L_{o1}} V_1 + \frac{1}{j\omega L_{i1}} (V_1 - V_2) \tag{3.15}
\]

\[
I_{ac1} = \frac{1}{j\omega L_{i1}} (V_2 - V_1) + \frac{1}{j\omega L_{a1}} V_2 + \frac{1}{j\omega L_{c1}} (V_2 - V_3) \tag{3.16}
\]

\[
-I_{ac1} - I_{ac2} = \frac{1}{j\omega L_{c1}} (V_3 - V_2) + \frac{1}{j\omega L_{y}} V_3 + \frac{1}{j\omega L_{c2}} (V_3 - V_4) \tag{3.17}
\]

\[
I_{ac2} = \frac{1}{j\omega L_{c2}} (V_4 - V_3) + \frac{1}{j\omega L_{a2}} V_4 + \frac{1}{j\omega L_{i2}} (V_4 - V_5) \tag{3.18}
\]

\[
-I_{dc2} = \frac{1}{j\omega L_{i2}} (V_5 - V_4) + \frac{1}{j\omega L_{o2}} V_5 \tag{3.19}
\]

The set of nodal-voltage equations (3.15)-(3.19) defines the electric circuit shown in Figure 3.20a. The nodal equations of the equivalent electrical circuit of Fig-
Figure 3.20a are duals of the mesh equations written for the magnetic circuit of Figure 3.19. Note that the symmetrical nature of the magnetic circuit of Figure 3.3 is preserved in the equivalent electric circuit of Figure 3.20a. Hence, the inductance elements $L_a$, $L_i$ and $L_o$ on the left and right sides of the circuit are identical. Considering this symmetry, and the polarities of the driving currents $I_{dc1}$ and $I_{dc2}$, the equivalent electric circuit of Figure 3.20a can be simplified to that of Figure 3.20b, where the two current sources $I_{dc1}$ and $I_{dc2}$ are lumped together and represented by a single driving current $I_{dc}$.

It should also be noted that the nonlinearities in the magnetic circuit are preserved in the equivalent electric circuit, so that each nonlinear reluctance element ($\mathcal{R}_{c1}$ and $\mathcal{R}_{c2}$) representing the core sections in the magnetic circuit has a corresponding nonlinear inductance element ($L_{c1}$ and $L_{c2}$) in the electric circuit of Figure 3.20b. $L_i$, $L_o$, $L_y$ and $L_a$ denote the leakage inductance values associated with the leakage flux paths between AC and DC windings and within each winding.

### 3.3.2 Topological Derivation of Equivalent Electric Circuit

The equivalent electric circuit illustrated in Figure 3.20b can also be derived by applying the principle of duality as a topological exercise [68]. The methodology of applying the topological principle of duality can be summarised as follows:

- Within each mesh of the magnetic reluctance circuit a node is marked and an additional reference node is marked outside the magnetic circuit corresponding to the outermost loop.
- The nodes are then joined, with a line passing through each element of the magnetic circuit as shown in Figure 3.21a.
- Each reluctance ($\mathcal{R}$) common to two meshes in the magnetic circuit corresponds to an inductance ($L$) connected between the two corresponding nodes (as shown in Figure 3.21b), where inductance $L$ is defined by (3.12). One of
Figure 3.20: Development of equivalent electric circuit model of FCL
(a) Equivalent electric circuit defined by nodal-voltage equations
(b) Simplified form of equivalent electric circuit
Figure 3.21: Topological derivation of equivalent electric circuit
the windings in the device is chosen as the reference winding, and \( N \) is the number of turns on the chosen reference winding.

- Each MMF source in the magnetic circuit, corresponds to a driving current in the electric circuit
- Each flux \((\phi_{ac1}, \phi_{ac2} \text{ and } \phi_{dc})\) linking a winding, corresponds to an induced voltage in these windings

Figure 3.21b from the topological exercise is identical to the equivalent electric circuit of 3.20a derived by nodal-voltage equations in Section 3.3.1.

### 3.3.3 Coupling with External Electric Circuits

The equivalent circuit of Figure 3.20b, derived from the magnetic reluctance circuit, assumes that all windings have some arbitrary number of turns \( N \). However, in a practical FCL, each of the AC and DC windings have a different number of turns. Therefore, before coupling this circuit with external electric circuits, it is necessary to replace the current sources in Figure 3.20b with ideal transformers in order to account for the actual turns in the windings and establish the real currents and voltages in the windings. In this particular case, the DC winding was chosen as the reference winding (i.e. \( N = N_{dc} \)), and ideal transformers \((T_1 \text{ and } T_2)\) having turns ratios of \( N_{dc} : N_{ac1} \) and \( N_{dc} : N_{ac2} \) were added to the two AC terminals of the equivalent circuit (replacing the two current sources \( I_{ac1} \) and \( I_{ac2} \)). No ideal transformer is required at the DC terminal since it is chosen as the reference winding and hence the 1 : 1 transformer for that particular winding can be omitted. The ideal transformers provide means to couple the AC and DC windings while isolating them from direct electrical connection. The AC winding resistances \((R_{ac-coil1} \text{ and } R_{ac-coil2})\) and DC winding resistance \((R_{dc-coil})\) are then added outside of the coupling transformers of the equivalent electric circuit. The complete equivalent electric circuit is shown in Figure 3.22.
3.3.4 PSCAD/EMTDC Implementation of the Nonlinear Inductance Model

The Nonlinear Inductance Model is implemented in PSCAD/EMTDC as a page module and inserted into the AC network using external electric nodes (A and B) as shown in Figure 3.23. The ideal coupling transformers T1 and T2 represent the coupling between the equivalent electric circuit and the AC winding. The DC circuit consisting of an external DC power supply energising the DC bias winding is connected directly to the equivalent electric circuit. \( V_{ac-coil1} \) and \( V_{ac-coil2} \) denote the voltages induced in each AC winding and \( V_{dc-coil} \) denotes the voltage induced in the DC winding. An application of this model in a simple test circuit is shown in Figure 3.13 of Section 3.2.3.3.

3.3.5 PSCAD/EMTDC Nonlinear Inductance Model Validation

The same experimental setup (312 V open core prototype FCL with the associated test circuit) and the three short circuit tests (Test 1 at 60 kA(turns), Test 2 at 72 kA(turns) and Test 3 at 84 kA(turns)) that were used to verify the performance
of the Nonlinear Reluctance Model in Section 3.2.3.1 were also used to validate the proposed Nonlinear Inductance Model.

The parameters of the equivalent electric circuit of Figure 3.22 can be obtained from flux measurements using either, (a) FEA or (b) experimentation. As in Section 3.2.3.2, FEA was chosen for the magnetic field analysis of the FCL considering the accuracy with which FEA can predict the magnetic behaviour of such a device. Since the reluctance elements of the open core prototype FCL were previously determined in Section 3.2.3.2, determining the magnitudes of the linear inductors—$L_i$, $L_o$, $L_y$ and $L_a$ of the equivalent electrical circuit—is quite straightforward using (3.20).

$$L_n = \frac{N^2}{\mathcal{R}_n} \quad (3.20)$$

where $\mathcal{R}_n$ denotes the reluctance of a certain flux path and $L_n$ denotes the corresponding inductance associated with the same flux path. Note that, since the
DC winding was chosen as the reference winding in the model derivation, \( N = N_{dc} \) must be used for these calculations. Table 3.4 summarises the linear reluctance values and the corresponding inductance values calculated for the prototype FCL.

### Table 3.4: Magnetic reluctance and inductance values

<table>
<thead>
<tr>
<th>Magnetic Reluctance</th>
<th>Value ((\text{A/} \text{wb}))</th>
<th>Inductance</th>
<th>Value ((\text{H}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>( R_y )</td>
<td>237652</td>
<td>( L_y )</td>
<td>0.1683</td>
</tr>
<tr>
<td>( R_a )</td>
<td>893375</td>
<td>( L_a )</td>
<td>0.0448</td>
</tr>
<tr>
<td>( R_i )</td>
<td>2464041</td>
<td>( L_i )</td>
<td>0.0162</td>
</tr>
<tr>
<td>( R_o )</td>
<td>1006103</td>
<td>( L_o )</td>
<td>0.0397</td>
</tr>
</tbody>
</table>

The process of estimating the nonlinear inductance values \((L_{c1} \text{ and } L_{c2})\) is not as straightforward as determining the linear inductance values. In the case where a reluctance in a magnetic circuit represents a nonlinear relationship between magnetic flux and MMF, a characteristic curve relating its flux to MMF \((\phi - \text{MMF})\) can be used to represent each reluctance element. Similarly, a nonlinear inductance element in the equivalent electric circuit can be represented by a characteristic curve depicting the flux linkage to current \((\lambda - i ; \text{ where } \lambda = N\phi \text{ and } i = \text{MMF}/N)\) relationship. Figure 3.24 illustrates the nonlinear relation between flux linkage and current derived by rescaling the magnetisation curve \((\phi - \text{MMF})\) determined in Section 3.2.3.2 for the prototype device.

If inductance is defined as the local gradient of the flux linkage to current curve for a given current,

\[
L = N \frac{d\phi}{di} 
\]  

(3.21)

From the \( \lambda - i \) curve of Figure 3.24, a characteristic curve relating inductance with current \((L - i)\) can be obtained using the relationship in (3.21). Figure 3.25 illustrates the \( L - i \) characteristic curve that is used to represent the nonlinear core inductors \( L_{c1} \) and \( L_{c2} \) of the equivalent electric circuit.
Figure 3.24: Flux linkage - current characteristic curve

Figure 3.25: Inductance - current characteristic curve
In PSCAD/EMTDC an XY Transfer Function component can be used to specify the $L-i$ characteristic. At each time step of the time domain simulation, the current through the two core inductors ($i_{Lc1}$ and $i_{Lc2}$), derived from solving the equivalent circuit model, is used to look up the corresponding core inductance value of each core inductor ($L_{c1}$ and $L_{c2}$) from the $L-i$ characteristic curve.

### 3.3.6 PSCAD/EMTDC Simulations and Validation Results

The single-line diagram of the test circuit modelled in PSCAD/EMTDC was as shown in Figure 3.13, where the Nonlinear Reluctance Model of the FCL was now replaced by the nonlinear inductance model. A relatively larger solution time step of 10 $\mu$s (compared to that of the Nonlinear Reluctance Model) was chosen for the simulations and the PSCAD/EMTDC simulation results obtained were compared with the experimental fault currents observed for the three test conditions described in Section 3.2.3.1.

As shown in Figure 3.26, for all three test cases the model provides excellent predictions for the FCL-limited currents. Figure 3.27 shows the voltage across the FCL terminals—pre-fault and during the fault—for the three test scenarios. The experimental FCL voltage waveforms are reproduced quite accurately by the model, particularly the non-sinusoidal transients of the FCL terminal voltage during the fault. Note also that use of a relatively larger time step of 10 $\mu$s does not result in numerical oscillations in either FCL-limited current or FCL voltage waveforms (shown in Figure 3.26 and Figure 3.27), as it did with the Nonlinear Reluctance Model in Section 3.2.3.4 (Figure 3.18).

The simulation results also demonstrate the coupling effects between the AC and DC coils in the FCL quite accurately as illustrated by Figure 3.28. The post-fault ripple in the DC current waveform is replicated accurately by the Nonlinear Inductance Model.
Figure 3.26: Measured and simulated fault current waveforms [A] (a) Test 1: at 60kA-turns (b) Test 2: at 72kA-turns (c) Test 3: at 84kA-turns DC bias level
Figure 3.27: Measured and simulated FCL terminal voltage waveforms [V] (a) Test 1: at 60kA-turns (b) Test 2: at 72kA-turns (c) Test 3: at 84kA-turns DC bias level. Only the first 2-3 cycles post-fault voltage waveforms are shown for clarity.
Figure 3.28: Average Measured and simulated DC coil current waveforms [A] (a) Test 1: at 60kA-turns (b) Test 2: at 72kA-turns (c) Test 3: at 84kA-turns DC bias level
The exceptional agreement between the experimental results and the simulated results highlight the accuracy of the developed model, even with a relatively larger simulation time step. Compared to the case of Nonlinear Reluctance Model, the Nonlinear Inductance Model resulted in approximately 200 times improvement in solution speed for this particular validation exercise. Hence, with the proposed modelling approach in the present work, a significant reduction in computational time can be achieved without compromising the accuracy of the solution.

When a short circuit occurs, the rms value of the symmetrical fault current is determined by the system source voltage and the total system impedance to the point of fault. However, almost all faults involve asymmetry in at least one phase. The asymmetrical peak fault current can be much larger than the symmetrical fault current. The degree of asymmetry is a function of several variables, including the parameters of the power system up to the point of the short circuit and the point on the AC wave at which the short circuit was initiated. For a given system, the largest asymmetrical fault current occurs when a fault happens at a point when the voltage is zero. The X/R ratio of the system, between the source and the fault, determines the peak asymmetric fault current and the rate of decay of the transient. The experimental and simulation cases presented above and in Section 3.2.3.4, model the behaviour of a saturated core FCL in a test system with a relatively small X/R ratio ($X/R \approx 1.616$). Hence, the peak fault current has very little asymmetry during the transient phase and reaches steady state relatively quickly. However, in practical HV networks, generators, transformers, and transmission lines are highly inductive; hence the X/R ratio is typically greater than unity.

Since experimental results were not available for such a system, FEA transient simulations and PSCAD/EMTDC simulations were used to verify the 312 V FCL prototype device performance and the corresponding Nonlinear Inductance Model behaviour in a system with a relatively large X/R ratio. A Finite Element (FE) model of the prototype FCL, built in the CEDRAT FLUX3D package, was used to perform transient simulations in a circuit with an X/R ratio of 25. The resulting
FCL-limited current waveform and FCL terminal voltage waveform for a DC bias level of 72 kA(turns) are shown in Figure 3.29a and Figure 3.29b respectively. Note that a line to ground fault was simulated at a voltage zero to produce the maximum peak asymmetrical fault current in the system. Since the saturated core FCL is primarily an inductive device, theoretically, FCL current clipping is expected to be greater in highly inductive systems, with large X/R ratios. This is corroborated in the results shown in Figure 3.29a, where the FCL current clipped at 66% of the prospective first peak fault current compared to the 54% clipping level achieved in the previous system, for a DC bias level of 72 kA(turns).

**Figure 3.29:** For a high $X/R$ system FEA-simulated and pscad/EMTDC-simulated (a) Fault current waveforms (b) FCL terminal voltage
Note also that the AC current and voltage waveforms predicted by the PSCAD/EMTDC-simulated Nonlinear Inductance Model again match exceptionally well with the waveforms produced by FEA transient simulations.

3.4 Application of Nonlinear Inductance Model to different saturated core FCL topologies

A key aspect of the modelling approaches presented in this chapter is that there is no unique equivalent circuit model for all saturated core FCL devices in general. The equivalent circuit as well as the associated inductance values are dependent on the FCL geometry and the magnetic properties of the FCL cores. Therefore, depending on the geometry and the flux distribution of a particular FCL device, the equivalent circuit may need to be adjusted to account for all substantial flux paths in order to produce an accurate model. However, to reduce complexity in cases where certain properties do not influence the performance characteristics of concern (AC current, FCL terminal voltage and DC current), parameters representing those properties can be neglected.

As discussed in Section 2.4.2 a number of different saturated core FCL configurations have been the focus of research and commercial investigation. These include different core configurations such as open core, closed core and hybrid core arrangements and different coil arrangements such as centered and Helmholtz type DC coil configurations [41]. In this section, the applicability of the nonlinear inductance model to a few of these selected FCL topologies will be examined.

3.4.1 Open Core Topology with Helmholtz type DC coil configuration

The saturated core FCL with an open core topology can operate either with dc coils arranged at the centre of the FCL cores, or in a Helmholtz-like arrangement (i.e., with dc coils at both the top and bottom of the FCL cores). The Helmholtz
type DC coil arrangement has a number of advantages over the centered DC coil arrangement. The cores can be more uniformly biased with the Helmholtz-type arrangement, resulting in more efficient biasing and lower AC impedance [77]. An open core FCL with centered DC coil configuration was discussed previously in Section 3.2. In this section the applicability of the nonlinear inductance model to an open core FCL with a Helmholtz-like two-coil arrangement, as illustrated in Figure 3.30, is investigated.

![Figure 3.30: Finite element model of the 80mm × 80mm × 600mm open core FCL with Helmholtz-type DC coil arrangement](image)

The validation procedure is similar to that followed in Section 3.3.5. Experimental results were obtained from short-circuit tests undertaken on a small-scale 312 V prototype. This prototype device shares the same geometric parameters with the open core centered DC coil design, with the exception that the DC biasing coils used were 160 × 300 × 80 mm in size and spaced apart by 0.2 m (to achieve the most uniform core saturation). The test circuit was as outlined in Section 3.2.3.1, and several short circuit tests were conducted on the prototype device at a prospective symmetrical fault current of 1500 A, with different levels of applied DC bias. Two of these short circuit tests were selected for model verification: (a) Test 1 at 55kA (turns) and (b) Test 2 at 80.5 kA (turns).
As with the open core centered DC coil design, the parameters of the equivalent circuit for the Helmholtz arrangement were determined using flux measurements carried out in FEA. A finite element (FE) model of the prototype FCL was developed in the Cedrat Flux3D package and the reluctance of each flux path and the corresponding inductance elements were estimated using the methodology set out in Section 3.3.5. The subsequent reluctance and inductance values calculated are given in Table 3.5.

**Table 3.5: Magnetic reluctance and inductance values**

<table>
<thead>
<tr>
<th>Magnetic Reluctance</th>
<th>Value (A/Wb)</th>
<th>Inductance</th>
<th>Value (H)</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \mathcal{R}_y )</td>
<td>2201645</td>
<td>( L_y )</td>
<td>0.0174</td>
</tr>
<tr>
<td>( \mathcal{R}_a )</td>
<td>163219</td>
<td>( L_a )</td>
<td>0.2354</td>
</tr>
<tr>
<td>( \mathcal{R}_i )</td>
<td>9447605</td>
<td>( L_i )</td>
<td>0.0041</td>
</tr>
<tr>
<td>( \mathcal{R}_o )</td>
<td>2921165</td>
<td>( L_o )</td>
<td>0.0132</td>
</tr>
</tbody>
</table>

The single-line diagram of the test circuit modelled in PSCAD/EMTDC was as illustrated in Figure 3.13, where the nonlinear reluctance model of the FCL was now replaced by the nonlinear inductance model. The comparison between the PSCAD/EMTDC-simulated fault currents and the experimental fault currents observed for the two test conditions, is shown in Figure 3.31. Figure 3.32 shows the plots of voltage across the FCL \((V_{fcl})\) for the three two cases, while Figure 3.33 illustrates the DC current comparison. In the experimental set-up, the two DC bias currents were found to be nearly identical; therefore, in the validation exercise the average of the two measured DC currents was used.

As can be seen from the figures, the PSCAD/EMTDC generated waveforms replicate the experimental waveforms quite closely. Hence with varied reluctance/inductance values, the magnetic circuit and the subsequent equivalent electric circuit developed
for the open core centered DC coil arrangement, can be utilised to predict the behaviour of the Helmholtz type DC coil arrangement of an open core FCL.

Figure 3.31: Measured and simulated fault current waveforms [A] (a) Test 1: at 55kA-turns (b) Test 2: at 80.5kA-turns DC bias level
Figure 3.32: Measured and simulated FCL terminal voltage waveforms [V] (a) Test 1: at 55 kA-turns (b) Test 2: at 80.5 kA-turns DC bias level
Figure 3.33: Average Measured and simulated DC coil current waveforms [A]
(a) Test 1: at 55kA-turns (b) Test 2: at 80.5kA-turns DC bias level
3.4.2 Closed Core Topology

Typically, in an open core FCL design, DC coils with a significant level of ampere turns are necessary to fully saturate the cores. In commercial-scale FCLs this is only feasible using superconductors as DC coils. A more viable alternative is presented in the form of a closed core FCL arrangement, where the biasing requirement is low compared to that of an open core arrangement. Hence, the closed core arrangement alleviates the necessity of using sensitive and expensive superconducting coils, and allows the use of robust and cheaper copper coils to bias the cores.

The closed core arrangement of a saturated core FCL was discussed in Section 2.4.2. As discussed, the inner coil and associated core arrangement of the closed core design is similar to that of an open core arrangement. However, contrary to an open core design, the top and the bottom yokes combined with the two vertical outer limbs of the closed core design provide a low reluctance path for maximum return of flux [45].

Using the same modelling approach discussed in Section 3.2.1, the significant flux paths and associated reluctances for the arrangement of Figure 2.10 were determined as shown in Figure 3.34.

![Figure 3.34: Significant reluctance paths in a closed core design](image)

The three tests described in Section 3.2.1, were again employed to determine the reluctance values for all flux paths. As in the open core case, the closed core model takes into account the effects of magnetic saturation in the core by using
nonlinear reluctances for the core flux paths $\mathcal{R}_{c1}$ and $\mathcal{R}_{c2}$. The larger return paths ensure that the reluctance values $\mathcal{R}_o$, $\mathcal{R}_i$, $\mathcal{R}_a$, $\mathcal{R}_y$ are all constant (as in the open core case), even at high core saturation levels. The closed core arrangement, with DC coil stack centred on the inner limbs, enables core saturation at low levels of DC mmf. Once the inner cores are saturated, a significantly larger mmf is required to further increase the flux through the core section or through the return limbs. Consequently, the outer limbs tend to remain in a high permeable state following inner core saturation.

Note that the magnetic circuit derived for the closed core arrangement is identical to that of the open core FCL with centred DC coils (shown in Section 3.2.1). Hence, the subsequent nonlinear inductance circuit model derived is essentially the same as for the open core case illustrated in Figure 3.22. However, the reluctance value $\mathcal{R}_i$ is the only reluctance that is modelling an air domain ($\mathcal{R}_i$ represents a flux path through air) in the closed core design, and is therefore considerably larger than all of the other constant reluctances.

To test the applicability of the nonlinear inductance model, experimental results obtained from a small-scale 312 V single-phase prototype FCL were used. The cores, yokes and the return limbs of the prototype device were constructed using M4 electrical steel. The cross-sectional areas of the return limbs and the yokes were made 1.3 times larger than the cross-sectional area of the inner cores to ensure that they remain in a high permeable state. Both inner cores have an AC coil wound around them to carry the AC load current. Two Copper DC coils, stacked centred on the inner limbs and associated AC coils, were used to achieve initial the saturation of the inner core sections. The design parameters of the prototype device are summarised in Table 3.6. A photograph of the closed core prototype is given in Figure 3.35.

The test circuit was as outlined in Section 3.2.3.1, and a series of short circuit tests were conducted on the experimental arrangement including the closed core prototype at a prospective symmetrical fault current of 1500 A and with varying levels of DC bias application. The results from two of these short circuit tests were
Table 3.6: Prototype design parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core height</td>
<td>0.6 m</td>
</tr>
<tr>
<td>Core cross-section area</td>
<td>0.0064 m²</td>
</tr>
<tr>
<td>Yoke cross-section area</td>
<td>0.0083 m²</td>
</tr>
<tr>
<td>Return limb cross-section area</td>
<td>0.0083 m²</td>
</tr>
<tr>
<td>AC coil: width, depth and height&lt;sup&gt;a&lt;/sup&gt;</td>
<td>0.1 m, 0.1 m, 0.39 m</td>
</tr>
<tr>
<td>DC coil: width, depth and height&lt;sup&gt;a&lt;/sup&gt;</td>
<td>0.51 m, 0.38 m, 0.105 m</td>
</tr>
<tr>
<td>AC coil: number of turns &lt;sup&gt;a&lt;/sup&gt;</td>
<td>60</td>
</tr>
<tr>
<td>DC coil: number of turns &lt;sup&gt;a&lt;/sup&gt;</td>
<td>100</td>
</tr>
<tr>
<td>AC coil resistance&lt;sup&gt;b&lt;/sup&gt;</td>
<td>0.0196 Ω</td>
</tr>
<tr>
<td>DC coil resistance&lt;sup&gt;b&lt;/sup&gt;</td>
<td>0.1466 Ω</td>
</tr>
</tbody>
</table>

<sup>a</sup>Parameter value given for each coil
<sup>b</sup>Resistance of the two coils

selected for the model verification: (a) Test 1 at 8 kA(turns) and (b) Test 2 at 25 kA(turns).

As with the open core equivalent circuit, the parameters of the closed core equivalent circuit were determined using flux measurements carried out in FEA. An FE model of the prototype FCL was developed in the CEDRAT FLUX3D package (shown in Figure 3.36), with the reluctance of each flux path and the corresponding inductance elements estimated using the methodology set out in Section 3.3.5. The estimated reluctance and inductance values are given in Table 3.7.

Table 3.7: Magnetic reluctance and inductance values

<table>
<thead>
<tr>
<th>Magnetic Reluctance</th>
<th>Value (A/Wb)</th>
<th>Inductance</th>
<th>Value (H)</th>
</tr>
</thead>
<tbody>
<tr>
<td>ℛ&lt;sub&gt;y&lt;/sub&gt;</td>
<td>4176710</td>
<td>ℋ&lt;sub&gt;y&lt;/sub&gt;</td>
<td>0.0096</td>
</tr>
<tr>
<td>ℛ&lt;sub&gt;a&lt;/sub&gt;</td>
<td>204996</td>
<td>ℋ&lt;sub&gt;a&lt;/sub&gt;</td>
<td>0.1951</td>
</tr>
<tr>
<td>ℛ&lt;sub&gt;i&lt;/sub&gt;</td>
<td>5560528</td>
<td>ℋ&lt;sub&gt;i&lt;/sub&gt;</td>
<td>0.0072</td>
</tr>
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<td>ℛ&lt;sub,o&lt;/sub&gt;</td>
<td>20000</td>
<td>ℋ&lt;sub,o&lt;/sub&gt;</td>
<td>2.0000</td>
</tr>
</tbody>
</table>
As can be seen from Table 3.7, $R_i$ is considerably larger than all of the other constant reluctances since it is modelling an air domain. The values for $R_a$ and $R_o$ are much lower compared to the open core arrangement as almost all of the flux is encapsulated by highly permeable paths.

The experimental test circuit with the closed core FCL model was modelled and simulated in PSCAD/EMTDC. Figure 3.37 shows the plots of the fault currents—prospective, measured limited current and simulated limited current—for the two test cases. The fault currents for Test 1, are given in Figure 3.37a where 76% clipping is achieved by the limiter, by restricting the 1500$A_{rms}$ symmetrical prospective current to 350$A_{rms}$. The Test 2 fault currents are shown in Figure 3.37b, where a 1500$A_{rms}$ symmetrical prospective current is limited to 502$A_{rms}$ by the FCL, achieving a current clipping of 66%. As demonstrated in Figure 3.37, the PSCAD/EMTDC simulation results were in excellent agreement with the experimental fault current waveforms for both test cases.

The FCL terminal voltage for the two test scenarios, are shown in Figure 3.38. The FCL terminal voltage at 8$kA$-turns DC bias level is given in Figure 3.38a, whereas Figure 3.38b shows the FCL terminal voltage at 25$kA$-turns. Corresponding with the superior current clipping provided by the smaller DC bias in Test
Figure 3.36: Finite element model of the $80\text{mm} \times 80\text{mm} \times 600\text{mm}$ closed core FCL with centered DC coil arrangement

1, the FCL at $8kA$-turns generates a larger terminal voltage across the FCL. The PSAC/EMTDC closed core model was able to replicate these distinctions in the experimental FCL voltage waveforms in the two test cases quite precisely, particularly the non-sinusoidal transients (peaks and dips) of the voltage waveform during the fault.

Another important distinction to make between the two test cases is the magnitude of the steady state (pre-fault) voltage drop of the FCL. The pre-fault (steady state) voltage drop of the FCL was found to be $4.04V_{rms}$ (1.3%) in Test 1, compared to $2.73V_{rms}$ (0.87%) in Test 2 (at $25kA$-turns). Hence, it is important to note that although the FCL produces superior current limiting performance at a lower DC bias level, the resulting steady state voltage drop across the device is also considerably larger.

In the experimental set-up, the two DC bias currents were found to be nearly identical; therefore, for the validation exercise the average of the two measured DC currents was used. As illustrated in Figure 3.39, the ripple in the DC current, during
Figure 3.37: Measured and simulated fault current waveforms [A] (a) Test 1: at 8kA-turns (b) Test 2: at 25kA-turns DC bias level
Figure 3.38: Measured and simulated FCL terminal voltage waveforms [V] (a) Test 1: at 8kA-turns (b) Test 2: at 25kA-turns DC bias level
both the transient and steady state periods, is predicted quite accurately by the FCL model for the two test cases.

The excellent agreement between the pscad/emtdc simulations and the experimental results, clearly demonstrate the flexibility of the general magnetic reluctance model (derived for the open core FCL) in accommodating the closed core FCL topology. Hence, with varied reluctance/inductance values the proposed modelling approach can be extended to other significant saturated core FCL topologies.
### 3.4.3 Three phase FCL devices

A typical layout of a three-phase FCL is shown in Figure 3.40, where all three phases are located in a single geometrical body. The arrangement consists of six iron cores with an AC coil wound on each core limb. Two DC coils encompass the iron cores and the associated AC coils. A number of three-phase FCL topologies have been described in literature [28, 40, 41, 78, 79].

![Figure 3.40: A typical layout of a three-phase FCL](image)

In a three-phase FCL device a certain amount of coupling between the phases can be expected due to the close proximity of the three phases, which requires further study. In particular, during a floating fault, the three phases are coupled electrically and hence a certain amount of inter-phase mutual magnetic effect between the cores has been observed. During a grounded fault however, the phases are de-coupled electrically and the magnetic coupling between the phases has been found to be very minimal [78]. Since the FCL needs to be characterised under all fault conditions, the inter-phase coupling has to be accounted for when modelling a three phase FCL using the magnetic circuit concept.
It should also be noted that for high voltage applications the use of three separate single-phase FCL devices, as opposed to a single three-phase FCL device, is often preferred to avoid insulation complications. Hence, for the three phase applications investigated in the course of this thesis, three separate single-phase FCL devices were used.

3.5 Conclusions

When modelling electromagnetic systems, the magnetic circuit concept has often been used to derive analytical models with adequate accuracy. Recently, this concept was extended to open core saturated core FCLs where a modelling approach was proposed to represent the magnetic field of the device by a magnetic circuit of lumped reluctances. The effects of magnetic saturation in the core were taken into account by using nonlinear reluctances to represent core flux paths, while the leakage and linkage of the AC and DC coils were represented by linear reluctances. The magnetic circuit approach also made it possible to model the magnetic coupling that exists between the AC and DC coils.

This chapter demonstrated advancements on the magnetic analysis of the open core saturated core FCL and proposed two modelling approaches to represent the FCL in transient network simulators: the Nonlinear Reluctance Model and the Nonlinear Inductance Model.

The key research contributions of this chapter can be summarised as follows:

1. Developing a PSCAD/EMTDC model of the saturated core FCL, based on the magnetic circuit concept. The Nonlinear Reluctance Model proposed, extended the magnetic analysis of the open core configuration, by directly coupling the magnetic circuit of the device to the AC (i.e. AC coils and the grid side network) and DC (i.e. the biasing arrangement of the FCL) electrical systems of the FCL. The FCL is interfaced to the rest of the system via externally controlled voltage sources representing the back EMF across the two
AC coils.

2. Establishing the validity of the proposed Nonlinear Reluctance Model by experimental results obtained from standard short circuit tests undertaken on a single-phase prototype FCL. PSCAD/EMTDC simulations were shown to accurately reproduce the experimental results - the limited fault current, the back EMF generated across the FCL and the bias current in the DC circuit.

3. Establishing the limitations of the Nonlinear Reluctance Model. While trapezoidal integration is astable when simulating linear systems, it is known to be susceptible to numerical oscillations in nonlinear systems, particularly when differentiating step changes in certain state variables. The FCL operates in a highly nonlinear region of the B-H curve and hence the potential for numerical oscillations in EMTP-programs is quite significant. While the Nonlinear Reluctance Model produced accurate results when simulated with a sufficiently small solution time step (in the order of $1 \sim 10\,\text{ns}$), numerical oscillations were observed when a relatively larger solution time step (in the order of $1 \sim 50\,\mu\text{s}$) was used. This is exacerbated when the FCL model is incorporated into a stiff system (such as a power system with electric machines and power electronic devices), where the nonlinear behaviour of the device combined with the occurrences of other switching events in the system can lead to numerical oscillations, despite the use of a very small solution time step.

4. Developing a single equivalent electric circuit model to represent the saturated core FCL, using the principles of electromagnetic duality, that can be easily implemented in EMT programs. The equivalent electric circuit model is interfaced to the main AC system via ideal transformers representing the two AC coils. The Nonlinear Inductance Model allowed for a larger solution time step to be used in EMT simulations and eliminated the occurrence of numerical artifacts, compared to the method of directly interfacing the magnetic reluctance circuit to electric circuits.
5. Establishing the validity of the proposed Nonlinear Inductance Model by experimental results obtained from standard short circuit tests undertaken on a single-phase prototype FCL. PSCAD/EMTDC simulations were shown to accurately reproduce the experimental results - the limited fault current, the back EMF generated across the FCL and the bias current in the DC circuit.

6. Demonstrating that compared to the case of Nonlinear Reluctance Model, the Nonlinear Inductance Model resulted in a 200 times improvement in solution speed in the experimental verification simulations. Hence with the Nonlinear Inductance Model a significant reduction in processing time can be achieved without compromising the accuracy of the results.

7. The ripple in the DC current waveforms, during both the transient and steady state periods, was predicted exceptionally well by both modelling approaches, confirming that the model was effective in predicting the “transformer coupling” effects of the device.

8. Verify the saturated core FCL performance and the corresponding Nonlinear Inductance Model behaviour in a system with a relatively larger X/R ratio, using FEA transient simulations and PSCAD/EMTDC simulations.

9. Demonstrating the applicability of the equivalent electric circuit model (developed for the open core arrangement with centered DC coils) to other significant FCL topologies. It was shown that the equivalent electric circuit, adequately predicted the behaviour of the Helmholtz type DC coil arrangement on a open core FCL. It was also shown that the basic magnetic circuit derived for the open core FCL sufficiently represents the closed core arrangement, and that the three tests identified for the open core case are still sufficient to determine all of the reluctance values in the closed core case.

A key aspect of the proposed modelling approaches is that there is no unique magnetic circuit for all devices. The magnetic circuit and the subsequent equivalent electric circuit with the associated inductance values are dependent on the FCL
geometry and the magnetic properties of the FCL cores. Therefore, depending on the geometry and the flux distribution of a particular FCL device, the magnetic circuit may need to be adjusted to account for all substantial flux paths to produce an accurate model. However, as demonstrated in this chapter the general magnetic reluctance model derived for the open core FCL is quite flexible in accommodating other significant FCL topologies. Hence, with varied reluctance/inductance values, the proposed modelling approaches can be extended to different alternative topologies.
Chapter 4

Analysis of Saturated Core FCLs in Power Transmission and Distribution Networks

4.1 Introduction

In Chapter 3 an equivalent electric circuit model for the saturated core FCL, consisting of a set of lumped inductors, was derived and subsequently implemented in PSCAD/EMTDC. Experimental results, obtained from a small scale prototype FCL, were used to validate the proposed model of the FCL.

In this chapter, several case studies at different voltage levels are undertaken in the PSCAD/EMTDC transient simulation package to analyse the operational behaviour and performance of saturated core FCLs in utility grids. The nonlinear inductance model of the saturated core FCL, introduced in Chapter 3, is utilised to model the device and predict its electrical behaviour when applied to network simulations. Different network configurations are considered and the operation of the device is tested under different fault conditions.

Application of saturated core FCLs to a hypothetical $11\,\text{kV}$ distribution system is considered in Section 4.2, with a potential FCL designed for the given network
also presented. The efficacy of an FCL placed on a bus-tie location in an interconnected circuit is investigated using PSCAD/EMTDC studies and subsequently verified through numerical fault analysis. The effect of the bus-tie FCL impedance on the network impedance and the subsequent fault current contributions is investigated. It is demonstrated that in a circuit with complex interconnections, suppression of fault currents require multiple FCLs in critical feeders.

The operational behaviour and performance of a saturated core FCL installed at a critical feeder of a 132 kV sub-transmission system, is analysed in Section 4.3. Both PSCAD/EMTDC and E-TRAN are employed to develop the base network model using power system data of a real power network. The network model is subsequently used to examine the behaviour of the saturated core FCL during normal steady state operation (un-faulted) as well perform the transient analysis during fault events. The effects of the FCL on network power flow during steady state un-faulted conditions are also examined and compared with that of an equivalent air-core reactor that provides the same current limitation as the FCL.

### 4.2 Saturated Core FCL performance in an 11 kV distribution system

#### 4.2.1 Background of study

Conventional distribution networks have been designed with a radial network configuration to operate on unidirectional power flow. Application of an FCL in such a network is quite straightforward, since the short-circuit current flow is also unidirectional and could be managed with the insertion of an FCL between the source and the load in a critical feeder of the network [80, 81]. However, with the advent of decentralised generation, the modern electrical distribution systems have become more interconnected to enable higher integration capacity and reliable operation [80, 82, 83]. Consequently, with these meshed and looped network config-
urations, FCL placement in distribution systems has become a far more complex problem. Installing an FCL at a bus-tie location in the network, has often been preferred by the utilities due to the additional benefits such a placement offers [54]. A bus-tie FCL typically allows two buses to be tied without significantly raising the fault current level of the system and enables greater network flexibility and improved reliability. However, in a meshed network the power flow is not unidirectional and a fault in the system could be fed from many directions from different sources. In such a system, application of a single FCL at a bus-tie location may not provide the desired fault current reduction effect. Hence, in this section, network simulation studies are undertaken in PSCAD/EMTDC transient simulation package to analyse the operational behaviour and performance of saturated core FCLs in an interconnected distribution network.

4.2.2 Case study 1

To study the current limiting behaviour of an FCL when inserted into the bus-tie of an interconnected distribution network, a simple representative test network (as shown in Figure 4.1) was chosen. The test network under consideration has two interconnected substations, with each substation having a fault current in-feed modelled as voltage sources. Each of the sources shown in Figure 4.1 could represent a grid in-feed, a distributed generation or an incoming feed from another distribution substation. The system parameters are summarised in Table 4.1. For simplicity, each substation is assumed to have the same capacity. Note that these parameters are representative and do not reflect actual values of a real 11 kV system.

4.2.2.1 FCL design and model parameter determination

The saturated core FCL design process is a multi-variable optimisation problem that involves the use of both FEA and optimisation software to determine the optimal FCL design that would meet the performance specifications of the network under consideration. From a utility’s perspective, the primary performance characteristics
Figure 4.1: 11 kV test distribution system

Table 4.1: Power system parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>System voltage (Line to line, RMS)</td>
<td>11</td>
<td>kV</td>
</tr>
<tr>
<td>Nominal frequency</td>
<td>50</td>
<td>Hz</td>
</tr>
<tr>
<td>Source MVA base (3 phase) (each)</td>
<td>100</td>
<td>MVA</td>
</tr>
<tr>
<td>Source series resistance (each)</td>
<td>0.03104</td>
<td>p.u.*</td>
</tr>
<tr>
<td>Source series reactance (each)</td>
<td>0.19868</td>
<td>p.u.*</td>
</tr>
<tr>
<td>Line impedance (each for $Z_a$ and $Z_b$)</td>
<td>$0.00273 + j0.01708$</td>
<td>p.u.</td>
</tr>
<tr>
<td>Impedance of the shorted bus-tie ($Z_c$)</td>
<td>$j1.0e^{-05}$</td>
<td>p.u.</td>
</tr>
</tbody>
</table>

*p.u. on machine base MVA*
of interest are the current limiting capability of the FCL under fault conditions and
the resulting voltage drop during steady state un-faulted conditions. Therefore, in
the FCL design process, the fault current through the FCL and the steady state
(pre-fault) voltage drop across the FCL are typically considered as the two opti-
misation criteria. The optimisation software is used to vary the main FCL design
parameters, with the aim of minimising both of these criteria. Geometric para-
ters such as the height of the cores, the area of the cores, the number of turns of the
AC coils, the height of the AC coils and the DC bias requirements are considered
as the main driving parameters during the optimisation process. Other dependent
parameters such as mass, FCL footprint, total cost and the power consumption are
subsequently calculated by the optimisation software (through multi-objective opti-
misation methods) in each design iteration. The final design is chosen from a Pareto
Frontier of possible design solutions that is best suited for the required specifications.
The Pareto Frontier is a curve that describes the most Pareto efficient solutions in
terms of the two optimisation criteria under consideration. The complete details of
this design process are not discussed here, since it is beyond the scope of this thesis.
An overview of the FCL design process is given in Appendix B.

An FCL design that was best suited for the test network illustrated in Figure 4.1
was determined using the aforementioned optimisation and design process. The
parameters for the equivalent circuit model of the 11 kV FCL were then determined
as outlined in Section 3.2.3.2. Table 4.2 summarises the model parameters, including
the values of the leakage inductance elements, derived for this particular saturated
core FCL device. As described in Chapter 3, a $\lambda - i$ characteristic curve, was used
to estimate the core inductors $L_{c1}$ and $L_{c2}$.

\subsection{PSCAD/EMTDC Network Simulations}

The test system was modelled in PSCAD/EMTDC using the system parameters spec-
ified in Table 4.1. Subsequent simulations were carried out in PSCAD/EMTDC with
and without the saturated core FCL model inserted in the bus-tie.
Table 4.2: FCL model parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>AC voltage (Line to line, RMS)</td>
<td>11</td>
<td>$kV$</td>
</tr>
<tr>
<td>DC bias</td>
<td>87</td>
<td>$kAT$</td>
</tr>
<tr>
<td>Number of turns: AC coil</td>
<td>64</td>
<td>_</td>
</tr>
<tr>
<td>AC coil resistance</td>
<td>0.04023</td>
<td>$\Omega$</td>
</tr>
<tr>
<td>Number of turns: DC coil</td>
<td>200</td>
<td>_</td>
</tr>
<tr>
<td>DC coil resistance</td>
<td>0.0702</td>
<td>$\Omega$</td>
</tr>
<tr>
<td>$L_y^a$</td>
<td>124</td>
<td>$H$</td>
</tr>
<tr>
<td>$L_o^a$</td>
<td>23</td>
<td>$H$</td>
</tr>
<tr>
<td>$L_i^a$</td>
<td>0.0037</td>
<td>$H$</td>
</tr>
<tr>
<td>$L_o^a$</td>
<td>8000</td>
<td>$H$</td>
</tr>
</tbody>
</table>

*Leakage inductance elements of the FCL equivalent electric circuit*

During normal operation both busbars are fed approximately the same current and a very small current passes through the bus tie. When a three-phase to ground fault was applied at Bus 3, without the FCL in the network (with the bus-tie in place), the fault current contribution fed from each source was approximately the same with a steady-state current of 24.5 $kArms$, as shown in Figure 4.2a and Figure 4.2b (red dashed waveforms). The fault current contribution from each source when the bus-tie FCL is in service is also shown in Figure 4.2a and Figure 4.2b (blue solid waveforms). Note that, the current contribution from Bus 2, which flows through the bus-tie FCL, has been limited to 3.8 $kArms$ (84% reduction). However, the contribution from Bus 3 is directly fed and is approximately 40.46 $kArms$, a 77% increase when compared to the case without the FCL. Consequently, the total fault current clipping achieved by the FCL for this particular fault scenario was 7% as shown in Figure 4.2c.
Figure 4.2: The fault currents with and without the bus-tie FCL in service (a) contribution fed from Bus 2 (b) contribution fed from Bus 1 (c) total fault current
4.2.2.3 Numerical Calculations

To understand and theoretically verify the PSCAD/EMTDC simulation results in Section 4.2.2.2, a fault analysis of the 11 kV test system using a bus impedance matrix approach was carried out. For the network shown in Figure 4.1, without the bus-tie FCL, the bus impedance matrix $Z_{bus}$ derived using the impedance values in Table 4.1 is given by (4.1), where each element $Z_{ii}$ on the principal diagonal represents the Thevenin impedance at Bus $i$ and the off-diagonal elements represent the transfer impedances of the buses.

\[
Z_{bus} = 0.01 \begin{bmatrix}
1.55 + j9.93 & 1.55 + j9.93 & 1.55 + j9.93 \\
1.55 + j9.93 & 1.68 + j10.78 & 1.68 + j10.78 \\
1.55 + j9.93 & 1.68 + j10.78 & 1.68 + j10.78
\end{bmatrix}
\]  (4.1)

When modifying a bus impedance matrix by adding a new branch impedance $Z_o$, between buses $m$ and $n$, each original element of $Z_{ij}$ can be modified as [84], through application of (4.2).

\[
Z_{ij\text{ (new)}} = Z_{ij} - \frac{(Z_{im} - Z_{in})(Z_{mj} - Z_{nj})}{Z_{mm} + Z_{nn} - 2Z_{mn} + Z_o}
\]  (4.2)

The effect of inserting an FCL with a fault impedance of $Z_{FCL}$, into the bus-tie, can be modelled through the addition of a new branch to the system [85]. The impedance of the new branch is given by (4.3), where $Z_c$ is the original line impedance of the bus-tie (before inserting the FCL).

\[
Z_T = \frac{-Z_c}{(Z_c + Z_{FCL})} = -\frac{Z_c(Z_c + Z_{FCL})}{Z_{FCL}}
\]  (4.3)

Therefore the modification to the entries of $Z_{bus}$, when the bus-tie FCL is active (during a fault) in the bus-tie between Buses 2 and 3, is given by (4.4).

\[
Z_{ij\text{ (new)}} = Z_{ij} - \frac{(Z_{i2} - Z_{i3})(Z_{j2} - Z_{j3})}{Z_{22} + Z_{33} - 2Z_{23} + Z_T}
\]  (4.4)
Similar to most FCL technologies, the actual FCL impedance during a fault event is not a constant for saturated core FCLs. Hence, the fault impedance of an FCL is typically defined as the equivalent steady-state impedance that would result in the same fault current limiting effect [41]. Based on this definition and equations given in Appendix B, the fault impedance of the FCL device in Section 4.2.2.1 can be estimated to be, \( Z_{FCL} = 0.267 \Omega \). Using this estimation of \( Z_{FCL} \), the modified bus impedance matrix with the bus-tie FCL in service is,

\[
Z_{bus,FCL} = 0.01 \begin{bmatrix}
1.55 + j9.93 & 1.55 + j9.93 & 1.55 + j9.93 \\
1.55 + j9.93 & 1.79 + j11.55 & 1.58 + j10.03 \\
1.55 + j9.93 & 1.58 + j10.03 & 1.79 + j11.55
\end{bmatrix}
\] (4.5)

As can be seen from (4.5), the Thevenin impedances of the network at Bus 2 \( (Z_{22,FCL}) \) and Bus 3 \( (Z_{33,FCL}) \) increase, while the transfer impedance elements \( Z_{23} \) and \( Z_{32} \) decrease, when the bus-tie FCL in service.

The short-circuit currents for the test system shown in Figure 4.1 were calculated with and without the FCL inserted to the system when a hypothetical three-phase to ground fault was applied at Bus 3. The expressions derived to calculate the total fault current at Bus 3, and the fault currents contributed to Bus 3 by the adjacent un-faulted buses (Bus 1 and Bus 2) are given in Table 4.3. Note that in deriving these equations, the faulted network was assumed to be without load before the fault occurred and hence there was no pre-fault current flow. Following that assumption, all bus voltages in the test system were then assumed to be the same as the pre-fault voltage at the faulted bus \( (V_i) \).

Using the expressions derived for the fault current contributions (given in Table 4.3) and the bus impedance matrices of the system \( (Z_{bus} \) and \( Z_{bus,FCL} \)), the fault current magnitudes were calculated over a range of FCL impedance values. Note that, the pre-fault voltage at the faulted bus (Bus 3) was assumed to be \( V_i = 1.0 \text{ pu} \) and the FCL impedance values were varied from 0 to 0.4 pu. Figure 4.3a shows how the Thevenin impedance of the network at the faulted Bus 3 varies with the
Table 4.3: Fault current calculations - with and without the bus-tie FCL

<table>
<thead>
<tr>
<th></th>
<th>Without FCL</th>
<th>With FCL</th>
</tr>
</thead>
<tbody>
<tr>
<td>Total fault current at Bus 3, $I_{3,f}$</td>
<td>$\frac{V_i}{Z_{33}}$</td>
<td>$\frac{V_i}{Z_{33,FCL}}$</td>
</tr>
<tr>
<td>Current contribution from Bus 1, $I_{13,f}$</td>
<td>$\frac{V_i}{Z_{b}} \left(1 - \frac{Z_{13}}{Z_{33}}\right)$</td>
<td>$\frac{V_i}{Z_{b}} \left(1 - \frac{Z_{13,FCL}}{Z_{33,FCL}}\right)$</td>
</tr>
<tr>
<td>Current contribution from Bus 2, $I_{23,f}$</td>
<td>$\frac{V_i}{Z_{c}} \left(1 - \frac{Z_{23}}{Z_{33}}\right)$</td>
<td>$\frac{V_i}{Z_{c} + Z_{FCL}} \left(1 - \frac{Z_{23,FCL}}{Z_{33,FCL}}\right)$</td>
</tr>
</tbody>
</table>

*where the pre-fault voltage at Bus 3 is given by $V_i$, the elements of the original bus impedance matrix in (4.1) are denoted by $Z_{ij}$, and elements of the modified bus impedance matrix (with the bus-tie FCL in service) are denoted by $Z_{ij,FCL}$. 
FCL impedance. The Thevenin impedance of the network increases rapidly as the magnitude of the FCL impedance is increased, and subsequently plateaus at higher FCL impedance values. The resulting variation of total fault current ($I_{3,f}$) at Bus 3, with the FCL impedance is shown in Figure 4.3b. As expected, the total fault current decreases with the addition of the FCL. However, the decay is exponential and hence, the additional clipping offered by higher values of FCL impedance is marginal. The fault current contributed to Bus 3 by adjacent un-faulted Bus 2 and Bus 1 are shown in Figure 4.3c and Figure 4.3d respectively. While the fault current that flows through the FCL from the Bus 2 side is reduced by the FCL action, the fault current that is directly fed to Bus 3 from Bus 1 increases when the bus-tie FCL is in service. Note that these calculated bus impedance matrices and the behaviour of the resulting fault current contributions, agree with the PSCAD/EMTDC simulated results presented in Section 4.2.2.2.

4.2.3 Case study 2

In Section 4.2.2 it was shown that installing a single FCL at a bus-tie location, in an interconnected circuit, may not provide the desired fault current reduction. In such cases multiple FCLs may need to be applied at critical locations of the circuit, in order to achieve the necessary fault current reduction. In Case study 2 the same 11 $kV$ test system as outlined in Section 4.2.2 is considered, with two identical FCLs, one in each incoming feeder, installed as a possible solution. The proposed FCL locations are illustrated in Figure 4.4. Each FCL was modelled with the design parameters given in Table 4.2. When a three-phase to ground fault was applied at Bus 3, the total fault current at Bus 3 and the fault currents contributed to Bus 3 by the adjacent un-faulted buses (Bus 1 and Bus 2), with and without the FCLs, are shown in Figure 4.5. The fault current contribution fed from each source, with the in feeder FCLs in service, was approximately 9.95 $kArms$ at the steady-state (58% reduction), as shown in Figure 4.5a and Figure 4.5b. Consequently, for this
Figure 4.3: Effect of bus-tie FCL impedance on (a) Thevenin impedance of the network at Bus 3 ($Z_{33}$) (b) total 3-phase to ground fault current at Bus 3 ($I_{3,f}$) (c) fault current contribution from Bus 2 ($I_{23,f}$) (d) fault current contribution from Bus 1 ($I_{13,f}$)
particular fault scenario, the total fault current at Bus 3 with the in feeder FCLs, was approximately 19.88 kArms (58% reduction).

In a large power system with complex interconnections, suppression of fault currents may need many FCLs. However, installation of multiple FCL devices (one or two FCLs per circuit) may not be an economically viable solution. As discussed in Chapter 2, several techniques have been proposed to determine the optimum number and the best placement of FCLs. A technique similar to those in [85,86] may need to be adopted when determining the optimum number and placement of saturated core FCLs in a meshed/looped network.
Figure 4.5: The fault currents with and without the in feeder FCLs in service
(a) contribution fed from Bus 2 (b) contribution fed from Bus 1 (c) total fault current
4.3 Saturated Core FCL performance in a 132 kV sub-transmission system

4.3.1 Background of study

As detailed in Chapter 1, the need for installing fault current limiting devices is driven by the increasing system fault current levels due to network expansion and reinforcement measures that utilities undertake to meet rising demand. In one such case, a forecast of new significant loads associated with proposed coal seam gas and coal mining developments in the Surat Basin North West area of Queensland, has driven Powerlink - Queensland’s transmission network service provider (TNSP) - to augment and expand their existing network. Powerlink’s network operates from 110 kV to 330 kV transmission voltage levels and comprises of 15,000 circuit kilometers of transmission lines and 132 substations.

The existing supply arrangement to the Surat Basin North West area consists of two 132 kV feeders that transfer electricity from the 275/132 kV Tarong substation to local 132/33 kV and 132/66 kV transformers. The summer peak demand in the area at present is approximately 100 MW. The demand forecast scenarios developed by the utility, incorporating the new loads, estimate that the summer peak demand in the area could go as high as 1100 MW by 2020. A load development of this magnitude is well beyond the existing supply network infrastructure and hence thermal limitations were forecast to arise in the event of a single contingency (i.e., an outage of one of the 275/132 kV transformers at Tarong, or an outage of one of the 132 kV lines between Tarong and the Surat Basin area), if no network expansion was carried out. Developing local generation as an alternative to meet the future supply requirements was deemed to have economic disadvantages.

The proposed network augmentation includes establishing a new 275 kV substation at Columboola and connecting the existing Columboola 132 kV substation to the 275 kV substation with two 275/132 kV transformers as shown in Figure 4.6. As part of proposed augmentations, the fault ratings at the Columboola 132 kV
substation were also upgraded to 40 kA.

4.3.2 Description of the Fault Current Limiting Requirements

As illustrated in Figure 4.6, the Columboola 132 kV substation has a double bus system with a bus coupler that is normally kept closed. While the plant rating is 40 kA (plant rating reflects the rating of the lowest rated plant at each location), the utility plans to have a short-circuit level of 25 kA on each 132 kV bus. However, in the present context (with the above-discussed network augmentations), this can only be achieved with an open circuit between the two buses. A closed bus coupler results in a short-circuit level greater than 25 kA. This is the background to the utility’s interest in installing saturated core FCLs at Columboola substation, where the insertion of FCLs is expected to limit the symmetrical three phase fault current below the desired level of 25 kA. However, as shown in Section 4.2.2, installing a single FCL at a bus-tie location of an interconnected network may not provide the desired fault current reduction. Hence, multiple FCLs may need to be applied at critical locations of the network to achieve the necessary fault current reduction. In the course of this case study, the application of a saturated core FCL at one critical feeder of the Columboola substation will be considered.

The utility requirements for an FCL application at the Columboola substation are listed in Table 4.4.

4.3.3 Saturated Core FCL Design and Modelling

Following the saturated core FCL design process outlined in Section 4.2.2.1, an FCL design that would meet the performance specifications of the network under consideration was determined. The design parameters of the optimal solution chosen from the Pareto Frontier are summarised in Table 4.5. Once the FCL design was chosen, an FE model of the prototype FCL was developed in the CEDRAT FLUX3D package using the geometric parameters of the device determined from the optimisation pro-
Figure 4.6: Columboola 132 kV substation with proposed network augmentations
<table>
<thead>
<tr>
<th>Parameter</th>
<th>Data</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rated nominal voltage</td>
<td>132 kV</td>
</tr>
<tr>
<td>Line frequency</td>
<td>50 Hz</td>
</tr>
<tr>
<td>Maximum line voltage (extreme tap)</td>
<td>145 kV</td>
</tr>
<tr>
<td>Line voltage at the provided fault level below</td>
<td>132 kV</td>
</tr>
<tr>
<td>Continuous normal current</td>
<td>305 A (rms)</td>
</tr>
<tr>
<td>Maximum normal current (magnitude and duration)</td>
<td>765 A (rms)</td>
</tr>
<tr>
<td>Maximum allowable steady state voltage drop (at continuous nominal current)</td>
<td>1.0%</td>
</tr>
<tr>
<td>Lightning impulse voltage withstand level</td>
<td>650 kV</td>
</tr>
<tr>
<td>Power frequency voltage withstand level</td>
<td>275 kV</td>
</tr>
<tr>
<td>Prospective unlimited peak fault current for a three phase fault</td>
<td>100 kA (peak)</td>
</tr>
<tr>
<td>Peak limited current desired for a three phase fault</td>
<td>&lt; 62.5 kA (peak)</td>
</tr>
<tr>
<td>Prospective unlimited symmetrical fault current for a three phase fault</td>
<td>41.9 kA(rms)</td>
</tr>
<tr>
<td>Symmetrical limited current desired for a three phase fault</td>
<td>&lt; 25 kA (rms)</td>
</tr>
<tr>
<td>Fault duration</td>
<td>1.0 s</td>
</tr>
<tr>
<td>Reclosure sequence (if applicable)</td>
<td>0-10s-CO</td>
</tr>
<tr>
<td>Three phase equivalent fault impedance (Ohms) or fault X/R ratio</td>
<td>X/R = 45</td>
</tr>
<tr>
<td>Single phase equivalent fault impedance (Ohms) or fault X/R ratio</td>
<td>X/R = 39</td>
</tr>
<tr>
<td>Load power factor</td>
<td>0.90</td>
</tr>
</tbody>
</table>
Table 4.5: 132kV FCL design parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core height</td>
<td>3.1 m</td>
</tr>
<tr>
<td>Core cross-section area</td>
<td>0.32 m²</td>
</tr>
<tr>
<td>AC coil height</td>
<td>2.6 m</td>
</tr>
<tr>
<td>AC coil width (outer)</td>
<td>0.71 m</td>
</tr>
<tr>
<td>AC coil depth (outer)</td>
<td>0.71 m</td>
</tr>
<tr>
<td>DC coil height</td>
<td>0.65 m</td>
</tr>
<tr>
<td>DC coil width (outer)</td>
<td>1.80 m</td>
</tr>
<tr>
<td>DC coil depth (outer)</td>
<td>1.03 m</td>
</tr>
<tr>
<td>AC coil: number of turns¹</td>
<td>72</td>
</tr>
<tr>
<td>DC coil: number of turns¹</td>
<td>300</td>
</tr>
<tr>
<td>DC bias</td>
<td>143.39 kAT</td>
</tr>
<tr>
<td>Total AC coil resistance</td>
<td>16.06 mΩ</td>
</tr>
<tr>
<td>Total DC coil resistance</td>
<td>0.70 Ω</td>
</tr>
</tbody>
</table>

¹Parameter values given for each coil

cess as well as the electromagnetic properties of each material. Cedrat Flux3D was subsequently used to perform magnetostatic simulations and to determine the equivalent electric circuit parameters of the FCL model proposed in Chapter 3. The resulting FE FCL model, corresponding linear inductance values, and the $L - i$ characteristic curve that was used to represent the nonlinear core inductances are given in Appendix C.1.

The chosen FCL design was then modelled in PSCAD/EMTDC, using the calculated FCL design parameters along with the subsequent inductance values determined from FE simulations. A simplified three-phase equivalent circuit approximation of the network, developed based on the network data given in Table 4.4, was used to initially verify the limiting performance of the FCL model. A single-phase-to-ground fault was simulated on the load side of the FCL, with the fault control signal configured automatically through a timed fault logic component. A solution
time step of 10 µs was chosen for these simulations and the switching events were interpolated to the precise time.

Transient simulations using FE models are typically undertaken as part of the FCL design process to demonstrate the viability of a particular design and to verify that the FCL design has met the original performance requirements (such as those set out in Table 4.4). To verify the fault current limiting performance of the device under consideration, the results from the PSCAD/EMTDC simplified circuit simulation were compared with that of FE transient simulations. Figure 4.7a shows the PSCAD/EMTDC simulated fault current of the simplified circuit compared to the FEA simulated fault current. As can be seen, the PSCAD/EMTDC generated fault current waveforms replicate the FEA generated waveforms quite closely, providing a 46% reduction in symmetric fault current for a prospective symmetrical fault current of 41.66 kArms. As shown in Figure 4.7b, the PSCAD/EMTDC simulated FCL terminal voltage of the simplified circuit also compares well with the FEA simulated FCL terminal voltage. Hence, the PSCAD/EMTDC model accurately predicted the fault limiting performance and other performance characteristics of the FCL design.

4.3.4 Network Model Development and Validation

The base network model used for this study was developed using detailed power system data provided by Powerlink. Powerlink, similar to many of their contemporary utilities, use loadflow and transient stability programs such as PSS/E to study the fundamental frequency behavior of power systems and have network data available for their entire system in load-flow program format. However, these programs are not suitable for studies involving dynamic power system transients, particularly when it involves nonlinear magnetic devices such as saturated core FCLs. E-TRAN is an electrical translation program for power systems that can be used to translate power system data from PSS/E for use in EMT programs such as PSCAD/EMTDC.
Figure 4.7: FEA and PSCAD/EMTDC simulated (a) fault current [kA] (b) FCL terminal voltage [kV]
In this study, E-TRAN was employed to generate the preliminary network model in PSCAD/EMTDC from the PSS/E load flow data files (*.raw and *.seq) provided by the utility. E-TRAN uses an auto-routing algorithm to generate an accurate graphical representation of the network including buses, generators, loads, transmission lines etc. Real and reactive power data, along with the source voltage (magnitude and phase angle) information, from the solved load flow file were used to initialise generators/sources.

In order to define the boundary of the network to be represented in detail (i.e. the kept network) the frequency scan component of PSCAD/EMTDC was used [87]. This involved converting the PSS/E data file to PSCAD/EMTDC, keeping the details of 2, 3, 4, 5, 6 and 7 buses away from the main point of interest (132 kV Bus 2 at Columboola substation) and generating six different PSCAD/EMTDC network models. Once the portion of the network to be converted in detail was set, multi-port network equivalents were generated by E-TRAN to represent the remaining network using the available fundamental frequency impedance and power flow data. The entire circuit was reduced to sub pages according to the voltage levels, with another subpage generated to represent the rest of the network. Frequency scans were carried out for each of these converted network models and plots of impedance vs. frequency of the system, as seen by the 132 kV Bus 2 at Columboola substation, were generated. Figure 4.8 shows a comparison of frequency scans of different network equivalents. As can be seen, at 7 buses away from the point of interest the frequency scan of the equivalent network is almost identical to the frequency scan at 6 buses away. Hence, it is reasonable to assume that the equivalent 6-bus network provides an adequate approximation for the whole network. Hence, a detailed system was representing up to 6 buses away from the main point of interest, was identified as suitable for the scope of this study. A section of the E-TRAN generated single-line diagram in PSCAD/EMTDC is shown in Figure 4.9.
Figure 4.8: Frequency scan at the 132 kV bus
As can be seen from Figure 4.9, while the buses, transmission lines and other devices are accurately drawn with their respective interconnections, the diagram does not provide an exact geographical representation of the network. The buses in the network are represented by electrical nodes and hence the busbar arrangements and the incoming/outgoing feeder connections at the Columboola substation are not clearly discernible. Therefore, a more detailed model of the Columboola substation was developed based on substation layout drawings provided by the utility. The station model was developed as a page component (replacing the station equivalence generated by E-TRAN), using the E-TRAN generated transmission network model as the base case representing the system interconnection. Part of the Coloumboola station model is illustrated in Figure 4.10.

It must be noted that the extent of the network model required for the simulations will vary depending on the study requirements and hence must be decided on a case-by-case basis. While fault current limiting by itself is predominantly a localised fundamental frequency phenomenon, the 6-bus-radius network model described above was developed with a view to utilise it to simulate a variety of transient studies including remote amplification, harmonic propagation and voltage sag propagation. Depending on the study requirements the substation model was further amended in the subsequent studies in this thesis, to include more detailed representations of station equipment where appropriate. The details of these amendments are discussed in the respective chapters. Further details about system modelling and associated assumptions are given in Appendix C.2. Note that for the work presented in Section 4.3.6 and Section 4.3.7, a reduced system model with suitable equivalents to represent the rest of the system is adequate.

The complete network model was validated by performing a load flow study in PSCAD/EMTDC and comparing the results with those of an equivalent PSS/E load flow study. Table 4.6 shows the comparison of load flows at Columboola Bus 1 and Bus 2. It was found that the results from PSCAD/EMTDC and PSS/E were in very good agreement, with the maximum difference being less than 1.32%. 

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Figure 4.9: Section of the E-TRAN generated single-line diagram
Figure 4.10: Section of the detailed station model
Table 4.6: Comparison of active and reactive power flow data

<table>
<thead>
<tr>
<th>Nodes From→To</th>
<th>PSS/E</th>
<th>PSCAD/EMTDC</th>
<th>Percentage difference</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Bus 1</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>41940→41941</td>
<td>-63.9</td>
<td>-27</td>
<td>-63.63</td>
</tr>
<tr>
<td>41940→40830</td>
<td>-5</td>
<td>-7.1</td>
<td>-5.066</td>
</tr>
<tr>
<td>41940→42000</td>
<td>-99.1</td>
<td>29.7</td>
<td>-99.1</td>
</tr>
<tr>
<td>41940→40131</td>
<td>73.6</td>
<td>-17.6</td>
<td>73.47</td>
</tr>
<tr>
<td>41940→30181</td>
<td>-22.9</td>
<td>-7.2</td>
<td>-22.88</td>
</tr>
<tr>
<td><strong>Bus 2</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>41941→42001</td>
<td>-34.8</td>
<td>-22.1</td>
<td>-34.81</td>
</tr>
<tr>
<td>41941→40131</td>
<td>73.6</td>
<td>-17.6</td>
<td>73.48</td>
</tr>
<tr>
<td>41941→40831</td>
<td>13.5</td>
<td>-16</td>
<td>13.46</td>
</tr>
<tr>
<td>41941→40015</td>
<td>-99.9</td>
<td>118.4</td>
<td>-99.97</td>
</tr>
</tbody>
</table>
4.3.5 Network Simulations

The FCL model designed and validated for this particular application, was inserted into one of the critical feeders (identified as Feeder N1 henceforth) of the Columboola station model in PSCAD/EMTDC (as shown in Figure 4.10). Note that for high voltage applications, such as the case under study, use of three single-phase FCL devices as opposed to a three-phase FCL device, is often preferred to avoid insulation complications. Therefore, three single-phase devices were used for this study—the network simulations were carried out under three different conditions:

1. Without a current limiter
2. With an FCL placed in the Feeder N1
3. With an equivalent reactor placed in the Feeder N1

The inductance of the equivalent air-core reactor that would provide the same current limiting as the FCL was calculated using the following process.

The fault current of a network is given by,

\[ I_{\text{fault}} = \frac{V_S}{R_S + j\omega L_S} \]  

(4.6)

where \( V_S \) is the line-to-ground voltage of the system, \( \omega \) is the angular frequency, and \( R_s \) and \( L_s \) are the single phase equivalent source resistance and inductance of the system (upstream of the short-circuit) respectively. All line capacitances are neglected, since the network is assumed to be in a steady state condition prior to the occurrence of fault.

The fault current with a series reactor installed is given by,

\[ I_{\text{fault, Reactor}} = \frac{V_S}{R_S + j\omega(L_s + L_R)} \]  

(4.7)
Therefore, the inductance $L_R$ of an equivalent air-core reactor providing the same current limiting as the FCL can be calculated using,

$$L_R = \frac{1}{\omega} \left[ \left( \frac{V_S}{I_{\text{fault,FCL}}} \right)^2 - R_S^2 \right] - L_S \quad (4.8)$$

Using Equation (4.8), the inductance of the equivalent air-core reactor that would provide the same symmetrical fault current limiting as the FCL was calculated to be 6.0987 mH.

### 4.3.6 Steady State Behaviour of the Saturated Core FCL

Simulations were carried out in PSCAD/EMTDC to examine the performance of the FCL under normal steady state (un-faulted) conditions. The simulations focused particularly on the steady state currents, voltage drop and the effects on active and reactive power flows in the bus coupler and some selected incoming and outgoing feeders. The performance of the FCL was then compared with that of the equivalent air-core reactor, providing the same fault current limiting at the same network location. A summary of the key results are presented in Table 4.7.

As demonstrated in Table 4.7, the equivalent air-core reactor has a much greater effect on the line currents and associated power flows at steady state compared to the FCL. While the FCL causes a reduction, of approximately 3.5% to 3.7% in the line currents and the active, reactive power flows, an air-core reactor with the same current limiting capability causes approximately 9% reduction in the line current and the associated power flows in the steady state. The pre-fault (steady state) voltage drop of the FCL is approximately 486.89 $V_{\text{rms}}$ (0.37%) compared to the 1198.76 $V_{\text{rms}}$ (0.91%) voltage drop of the equivalent reactor. For this particular network, the maximum allowable voltage drop across the current limiter, as specified by the utility, was 1.0% at rated line current—the pre-fault voltage drop of the equivalent reactor at 0.91% is close to this maximum allowable value.
Table 4.7: Steady state performance comparison

<table>
<thead>
<tr>
<th></th>
<th>Without a current limiter</th>
<th>With FCL</th>
<th>With equivalent air-core reactor (6.0987 mH)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bus Coupler</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Steady state voltage drop ($V_{rms}$)</td>
<td>N/A</td>
<td>486.89</td>
<td>1198.76</td>
</tr>
<tr>
<td>Steady state current ($I_{rms}$) [reduction]</td>
<td>687.6</td>
<td>662.38 [3.7%]</td>
<td>625.79 [9.0%]</td>
</tr>
<tr>
<td>Active Power Flow (MW) [reduction]</td>
<td>134.10</td>
<td>129.1 [3.7%]</td>
<td>122.3 [8.8%]</td>
</tr>
<tr>
<td>Reactive Power Flow (MVAr) [reduction]</td>
<td>-93.45</td>
<td>-90.22 [3.5%]</td>
<td>-85.11 [8.9%]</td>
</tr>
</tbody>
</table>

4.3.7 Limiting Behaviour in the Network

To examine the performance of the FCL under fault conditions EMT simulations were carried out, under four different fault scenarios.

1. Case 1 - Three phase to ground fault at the FCL terminals
2. Case 2 - Three phase ungrounded fault at the FCL terminals
3. Case 3 - Single phase (Phase A) to ground fault at the FCL terminals
4. Case 3 - Phase A to Phase B fault (ungrounded) at the FCL terminals

These fault scenarios were also simulated without a current limiter in the network as well as with the equivalent air-core reactor. For all of the fault scenarios it was assumed that the bus coupler between Columboola Bus 1 and Bus 2 is kept closed.

To ensure network solution accuracy, a small solution time step of 10 µs was chosen and the switching events were interpolated to the precise time. The faults were simulated at voltage zero, to obtain the maximum peak asymmetrical current, for a duration of about 10 cycles. A summary of the key results are presented in Table 4.8.
<table>
<thead>
<tr>
<th>Case 1 - Three phase to ground fault</th>
<th>Prospective Current</th>
<th>With FCL</th>
<th>With Reactor</th>
</tr>
</thead>
<tbody>
<tr>
<td>First Peak of fault current (pk kA)</td>
<td>72.21</td>
<td>52.75</td>
<td>26.95%</td>
</tr>
<tr>
<td>Symmetrical fault current (rms kA)</td>
<td>26.96</td>
<td>16.13</td>
<td>40.17%</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Case 2 - Three phase ungrounded fault</th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>First Peak of fault current (pk kA)</td>
<td>72.21</td>
<td>49.71</td>
<td>31.16%</td>
</tr>
<tr>
<td>Symmetrical fault current (rms kA)</td>
<td>26.96</td>
<td>16.89</td>
<td>37.35%</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Case 3 - Single phase (Phase A) to ground fault</th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>First Peak of fault current (pk kA)</td>
<td>87.46</td>
<td>59.22</td>
<td>32.29%</td>
</tr>
<tr>
<td>Symmetrical fault current (rms kA)</td>
<td>32.52</td>
<td>18.85</td>
<td>42.04%</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Case 4 - Phase A to Phase B fault</th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>First Peak of fault current (pk kA)</td>
<td>58.33</td>
<td>39.69</td>
<td>31.96%</td>
</tr>
<tr>
<td>Symmetrical fault current (rms kA)</td>
<td>23.09</td>
<td>13.40</td>
<td>41.97%</td>
</tr>
</tbody>
</table>
Figure 4.11 and Figure 4.12 show the resulting fault currents observed in Phase A, for the four fault scenarios, with and without the current limiting devices connected. Note that, the equivalent reactor was sized to provide the same symmetrical fault current limiting as the FCL and thus, as can be seen from Figures 4.11 and 4.12, the current limiting provided by the FCL and the reactor are slightly different at the asymmetrical transient phase of the fault currents; however, these are approximately the same during the steady state of the faults. The highest fault clipping was achieved by both the FCL and the equivalent air-core reactor for the most severe fault scenario - single phase to ground fault as illustrated in Figure 4.12a. However, even for less severe fault scenarios, such as the line to line fault scenario shown in Figure 4.12b, both the FCL and the equivalent reactor provide consistent fault performance, with an adequate level of current limiting.

While the fault performance of both the FCL and the equivalent reactor is consistent, as demonstrated in Table 4.7, the equivalent reactor imposes greater constraints on the network power flows and has significantly larger steady state voltage drop under steady-state pre-fault conditions. Hence, considering both the steady state and the fault performance, the saturated core FCLs have significant merit over the conventional current-limiting series reactors.
Figure 4.11: Faulted line current [kA] for a (a) three-phase to ground fault (b) three-phase fault
Figure 4.12: Faulted line current [kA] for a (a) single-phase to ground fault (b) phase A to phase B fault
4.4 Conclusions

In Chapter 3 of this thesis an equivalent electric circuit model was proposed to represent the saturated core FCL, with its validation against experimental data also presented. In this chapter, the nonlinear inductance model developed in the previous chapter is used to analyse the operational behaviour and performance of a saturated core FCLs in utility grids at different voltage levels, during the steady state as well under fault conditions.

The key research contributions of this chapter can be summarised as follows:

1. In a meshed system, the application of a single FCL at a bus-tie location was shown to be ineffective in providing the desired fault current reduction. Through PSCAD/EMTDC simulations carried out on an interconnected 11 kV distribution system, it was shown that while the bus-tie FCL limits the current that flows through it, the current contribution that is directly fed to the fault from the opposite side significantly rises with the added FCL impedance. Consequently, the total effective current limiting achieved by the FCL was shown to be marginal.

2. These simulation results were theoretically verified through a numerical approach to fault analysis, using bus impedance matrices. It was demonstrated that the Thevenin’s impedance of the network increases rapidly as the magnitude of the bus-tie FCL impedance is increased, subsequently plateauing at higher FCL impedance values.

3. Through subsequent PSCAD/EMTDC simulations carried out on the same test network it was shown that, in an interconnected system, to achieve the desired fault current reduction application of multiple FCLs at critical locations of the circuit is necessary. However, cost might be a prohibitive factor in implementing this commercially and hence a suitable FCL placement technique may need to be adopted when determining optimum placement for saturated core FCLs in interconnected systems.
4. The potential performance of a saturated core FCL, installed at a critical feeder of the 132 kV sub-transmission system was analysed, with an FCL design that would meet the performance specifications of the network under consideration presented. It FCL was shown to provide approximately 40% reduction in symmetrical fault current, for a three phase to ground fault, when inserted into the network.

5. The application of an FCL was shown to have significant merit over the conventional current-limiting series reactors. In comparison to the FCL, an equivalent air-core reactor was shown to impose greater constraints on the network power flows with considerably larger steady state voltage drop under pre-fault conditions. Fault performance of both the devices was shown to be consistent, with the highest fault clipping achieved by both devices for the most severe fault scenario - single phase to ground fault. However, even for less severe fault scenarios, such as the line to line fault scenario, both the FCL and the equivalent reactor was shown to provide adequate level of current limiting.
Chapter 5

Saturated Core FCL Interaction with Power System Protection

5.1 Introduction

With the recent developments in FCL technology, identifying and addressing any issues that are likely to emerge as a result of integrating FCLs into utility networks is becoming increasingly important. The FCL installations can be either an integral part of a new system design or an addition to an existing system. In the former case, the whole system, including the protection schemes, would be designed to account for the FCL and its operation. Application of FCLs to an existing system on the other hand, may have an impact on the protection schemes that were designed to operate under existing network conditions (prior to FCL application), and requires further investigation [4,56].

The effects of FCLs on power system protection schemes depend on several factors such as the type of FCL technology, protection schemes/principles considered, network configuration and FCL/fault location. As stated in Chapter 2, CIGRÉ Working Group A3.10 [13] and CIGRÉ Working Group A3.16 [56], have laid out the foundation for a typical investigation on interactions between FCLs and the power grid. CIGRÉ Working Group A3.16 analysed the possible influence of several
different FCL technologies on existing protection schemes, focusing mainly on the overcurrent, distance and differential protection in medium and high voltage systems. The study was based on four types of FCLs—the current limiting reactors, resistive superconducting FCLs, solid-state fault current limiters and pyrotechnic fault current limiters—represented by their characteristic principles, rather than the real operational behaviour of each technology. Although the study provides a general framework for further studies, a comprehensive investigation based on accurate models of each FCL technology is essential in identifying the grid integration issues for each particular FCL technology.

A comprehensive analysis, focusing on the influence a saturated core FCL installation has on existing power system protection schemes, is presented in this section. The three basic protection schemes considered in the course of this analysis are:

- Overcurrent Protection
- Distance Protection
- Differential Protection

A review of these three protection schemes and their general susceptibilities to current limiters is undertaken in Section 5.2, followed by case studies on a real power network in Section 5.3. A range of simulation studies are carried out in PSCAD/EMTDC to demonstrate and analyse the effects of a saturated core FCL application on existing protection schemes of a sub-transmission level utility substation. The effects on each protection scheme will be analysed in separate subsections and subsequently possible revisions to these protection schemes will be proposed to ensure proper operation and co-ordination of the protection relays.
5.2 Effects of FCLs on Protective equipment and Protection Schemes

5.2.1 Overcurrent Protection

Overcurrent relays are widely used for primary protection in distribution systems and as back-up protection in transmission networks. The relay operation is based on the evaluation of the current magnitude sensed by the current transformer (CT) (in directional overcurrent relays, both the current magnitude and the phase angle are considered as inputs to the relay for the purpose of selectivity) against a predetermined threshold value (pickup current). The relay is set to operate if the current sensed exceeds the pickup current setting. Overcurrent protection relays can be categorised by their time/current characteristic as follows [88]:

1. Instantaneous over current relays - operate instantaneously in response to the current sensed by the CT without any set time delay. To ensure proper operation, these relays are applied in circuits where there is a substantial reduction of fault current as the fault is moved away from the location of the relay towards the far end of a line.

2. Definite time overcurrent relays - are set to operate after a predefined time delay after its pickup time. The relay operation is independent of the magnitude of current above the pickup current. In radial power systems line protection discrimination with definite time overcurrent relays is achieved by grading the time delay settings.

3. Inverse time overcurrent relays - operating time depends on the magnitude of the current that is sensed by the CT. The relay operating time (or the preset time delay) is inversely proportional to the magnitude of the sensed current, i.e. larger currents will cause a faster operation of the relay. Protection discrimination with these relays is achieved by coordinating the time/current characteristics of the relays in a particular circuit. The inverse time overcurrent relays are further divided based on the steepness of the time-overcurrent characteristic as moderately inverse,
very inverse and extremely inverse.

The operation and coordination of overcurrent protection schemes can be affected by the presence of FCLs within the protected circuit in several ways. In cases where the magnitude of the FCL-limited current is less than the pickup current setting of the overcurrent relay, the relay will fail to identify the fault conditions causing it to mal-operate. In such situations the operating characteristics of the relays need to be adjusted according to the new fault level of the system. To ensure proper operation of the relay, the FCLs must also allow the fault currents to flow at least for the duration of the processing time of the relay and any additional preset delay times [56]. Overcurrent relay coordination can also be affected by the presence of FCLs inside or outside of the protection zone. Figure 5.1 illustrates a case where the FCL is located inside of the protected zone with an inter-in-feed connection (represented by $I_{in}$) between the FCL and the relay.

Note that in such a scenario the current flowing through the FCL is not equal to the current sensed by the relay. In the case of a fault further downstream of the FCL, the limited current contribution through the FCL will not be sensed by the relay, causing the relay to be blinded for a downstream fault. Even if the FCL is located outside of the protected zone, the FCL impedance increases the source impedance ratio ($Z_{source}/Z_{fault}$) altering the existing current gradings of the inverse time relays. In such cases overcurrent relays will need re-coordination [56].

![Figure 5.1: Effect of FCLs on relay pickup and coordination with inter-in-feed connections](image)

Figure 5.1: Effect of FCLs on relay pickup and coordination with inter-in-feed connections
5.2.2 Distance Protection

Distance relay operation is based on evaluating the downstream impedance seen by the relay against the impedance of the line up to a predetermined point (referred to as the reach point). The apparent impedance at the relaying point is the ratio between the voltage and current sensed by the current transformer (CT) and the voltage transformer (VT) associated with the relay. One end of the protection zone is defined by the position of these instrument transformers, and the other end by the reach point impedance setting of the relay. If the measured impedance at the relaying point is less than the reach point impedance, then a fault inside the protected zone (internal fault) is detected. The operating characteristics of distance relays are typically represented in the complex $R - X$ plane as shown in Figure 5.2 [89].

![Diagram of Distance Relay Operating Characteristic](image)

**Figure 5.2:** Distance relay operating characteristic on the $R - X$ diagram: (a) impedance (b) mho (c) reactance (d) quadrilateral [89]
Some of most commonly used characteristics are [88,90]:

1. Impedance characteristic - The operation of the impedance relay is independent of the phase angle between current and the voltage and hence the operating characteristic is a circle with its centre at the origin with a radius equal to its setting in ohms (Figure 5.2a). Any impedance value less than the radius of the circle will trigger the relay operation. The relay characteristic is typically non-directional; however, directional control can be achieved by adding a separate directional control element.

2. Mho characteristic - The characteristic of a Mho relay is a circle whose circumference passes through the origin of the $R - X$ diagram, as illustrated in Figure 5.2b. Any impedance value within the characteristic circle will trigger the relay operation. The impedance characteristic is adjusted by setting two parameters - the impedance reach along the diameter ($Z_R$) and the angle of displacement of the diameter from the R axis ($\phi$, also known as the relay characteristic angle). Hence, the Mho relay is inherently directional and can provide fault discrimination through both reach control and directional control.

3. Reactance characteristic - The characteristic of a reactance relay is a straight line at the set point parallel to the R axis, as illustrated in Figure 5.2c. It is a non-directional relay and is typically used in combination with an admittance relay.

4. Quadrilateral characteristic - The quadrilateral characteristic is made up from a combination of a reactance line (top), two resistive units (left and right side, load blinders) and a directional line (bottom), as illustrated in Figure 5.2d. An advantage of this characteristic is that the reach of the relay in both the $R$ and $X$ directions can be adjusted independently. It therefore provides better resistive coverage for short lines than any Mho-type characteristic.

An FCL installed in the protected circuit can affect the operation and coordination of distance protection schemes. The presence of an FCL in the fault loop increases the zone impedance and may cause the impedance seen by the relay to fall
outside of the relay operating region. In such situations the relay may under-reach and may fail to detect faults inside the protected zone. If that is the case, the operating characteristic of the relay needs to be adjusted to account for the impedance associated with the FCL. Severe distortions in current waveforms due to FCLs may result in phase angle shifts which could influence the impedance and direction determination process of the relay. Even if the FCL is located outside of the protected zone, the presence of the FCL increases the effective source impedance and alters the system $X/R$ ratio. This may influence the direction and reach determination of the relay [56]. Distance relay coordination may also be affected by the presence of FCLs inside or outside of the protection zone. In a case where there is an inter-in-feed between the relay and an internal fault (See Figure 5.1), an FCL inside the protection zone may further reduce the reach of the distance relay. An increase in the effective source impedance and the resulting low voltages of the faulty phases due to the presence of an FCL, may affect the accuracy of the voltage measurements. In such cases re-coordination of distance relays is necessary.

5.2.3 Differential Protection

Differential relay schemes are unit protective schemes that protect the circuit or equipment between two CTs that are fitted on either side of the equipment. The relay uses the currents sensed at both ends of the protected zone and uses their vector and/or scalar sums to detect fault conditions. The relay operation is based on the premise that under normal conditions current at the two terminals of the protected equipment should be equal. In practice however, non-zero differential quantities may be detected under normal un-faulted conditions due to line charging currents, CT mismatching and current measurement inaccuracies etc. Furthermore, differential currents may be detected under through-fault conditions, due to CT saturation during faults outside of the protection zone. Therefore, provisions have to be made to prevent relay mal-operation under these conditions. Two main current differential protection relays are used in modern power systems: high impedance (or unbiased
differential protection) and low impedance (or biased differential protection).

1. Low impedance (biased) differential protection

This type of relay is characterised by two actuating quantities - restraint and operate. Low impedance differential relays present a low impedance to the flow of CT secondary current. The relay uses the vector sum of the currents from the CTs to detect the difference current (referred to as the operating current or $I_{OP}$) resulting from an internal fault. To account for inaccuracies in CT performance, the relay also uses the scalar sum of these currents to determine a restraint current ($I_{RT}$). The operating current from the vector summation $I_{OP}$, is then compared with the restraint current $I_{RT}$. The relay operates when $I_{OP}$ exceeds a minimum threshold and a percentage of $I_{RT}$, defined by the slope setting of the relay. This protection scheme, however, is susceptible to mal-operation due to external faults causing CT saturation. One solution to improving reliability under severe CT saturation is the introduction of an additional saturation stabilisation area to the relay characteristic [89,91].

2. High impedance (unbiased) differential protection

High-impedance differential relays present a very high impedance to the flow of CT secondary current. Similar to the low impedance differential scheme, the relay uses the vector sum of the currents from the CTs to detect the difference current due to an internal fault. Since the relay is extremely sensitive to the CT difference current, high accuracy CTs are used in this protection scheme. The difference current is then forced through the high impedance of the differential relay resulting in a voltage drop across the relay. The relay operates when this voltage drop is higher than the relay tripping voltage threshold. To prevent the voltage drop across the relay from becoming too high, the relay is typically connected in parallel with a nonlinear resistor. The high impedance principle also uses a stabilisation resistor, which forces the differential current due to CT saturation to flow via the saturated CT impedance.
Therefore, it is less susceptible to mal-operation due to CT saturation during internal or external faults [89,91].

Low impedance differential relays with an overcurrent pickup or guarding function may be affected by the presence of an FCL in the circuit. It must be ensured that the FCL-limited current exceeds the pickup setting. For internal faults, the lower fault currents due to FCLs may affect the sensitivity of the low impedance differential relays. Sensitivity of the high impedance differential relays may also be affected by the reduced currents, but to a lesser extent [56]. For a fault outside of the protection zone, the FCL limited current also results in a reduced relay current. This signifies a positive effect on protection stability since it improves the reliability of the current differential scheme and minimises the occurrence of false-tripping. Note that, unlike other protection schemes, a double-end feed system (where a fault in the system is fed from both sides) could affect the differential relay operation. In such a system, if an FCL limits only one of the feed currents, for an internal fault, it may lead to a significant phase shift of the limited fault current, depending on the degree of limitation and X/R ratio. This could lead to a reduction in differential current sensed by the relay and cause mal-operation of the relay. There is also risk of having insufficient sensitivity since the scalar sum of the secondary currents remain unaffected.

The use of current limiting devices is known to have a favourable effect on current differential schemes by reducing the possibility of relay mal-operation due to CT mismatching, which can occur as a result of a previous internal fault [56] [91]. Even if the two CTs at the ends of the protected zone are identical, a considerable amount of remnant flux could be left in the CT cores after clearing an internal fault. Since the excitation characteristics of the CT cores are nonlinear in nature, the effects of remnant flux on the two cores could differ. The persistence of remnant flux can cause the two CTs to operate at different points on their excitation characteristics and may result in only one of the CTs to saturating during external faults that occur at a later time. Consequently, the core saturation can cause a differential current to
flow in the relay, and may cause the relay to mal-operate [91]. The presence of an FCL will reduce the maximum fault peak experienced by the relay, and hence will provide protection against core saturation for the CTs. In a double-end feed system however, it must be ensured that FCLs are installed at both ends of the protected unit to ensure protection against core saturation, for faults occurring at either end.

5.3 Saturated Core FCL integration with an existing protection system - Case Studies

In Section 5.2 the general sensitivities of different protection principles to fault current limiting devices were discussed. However, the interaction of different FCLs with the power system and its protection schemes can differ considerably depending on the technology adopted in each FCL type and its limiting behaviour [56]. Hence, in this section, a comprehensive investigation on the effects of a saturated core FCL on power system protection schemes is presented. Case studies are undertaken at a sub-transmission level utility network, with possible implications of a saturated core FCL installation on the existing protection schemes of the system identified through PSCAD/EMTDC simulations.

The 132kV base network model along with the Columboola station model and the FCL design presented in Section 4.3 are used in these simulation studies. The existing feeder protection schemes at the Columboola station – distance and differential schemes – were then incorporated into the station model using the relay parameters provided by the utility. In the actual network, each feeder has two protection systems enabled, ‘X group’ and ‘Y group’ to account for redundancy. Each protection group is self-contained and independent of each other, capable of detecting and isolating all types of faults. Hence, failure of one protection group to operate for a certain fault will not affect the operation of the other group. Although both groups of protection systems were modelled in PSCAD/EMTDC for each feeder, during this study, it was assumed that only one group is enabled at a time as the
intention of study is to examine the impact on each protection scheme, irrespective of redundancy.

5.3.1 Distance Protection

The distance relays at the Columboola substation feeders are enabled with two zones of fault protection set with the Mho characteristic. Zone 1 is set to operate instantaneously in response to a tripping signal produced. Zone 2 protection on the other hand, is typically set to operate after a predefined time delay from fault pick up. The distance relay modelling algorithm utilised in the PSCAD/EMTDC simulation study is shown in Figure 5.3.

Faults on a transmission line can severely distort the voltage and current signals, resulting in high frequency oscillations, lower order frequency components and decaying DC components. These distortions could affect the performance of relays. To remove the DC offset components and to eliminate the higher/lower frequency components, online Fast Fourier Transform (FFT) components with low-pass anti-aliasing filters were used in PSCAD/EMTDC [73]. The FFT block samples the input signals and extracts the fundamental magnitudes and phases. The sequence filter blocks were subsequently used to calculate the magnitudes and phase angles of sequence components, based on the magnitudes and phase angles of the phase quantities (line voltage and line current) extracted by the FFT. The signal processing modelling in PSCAD/EMTDC, is illustrated in Figure 5.4.
Figure 5.3: Distance relay modelling algorithm in PSCAD/EMTDC
Figure 5.4: Signal processing employed in distance protection scheme modelled in PSCAD/EMTDC
Distance relays are typically designed to detect faults that are either three phase or single phase, and line to ground or line to line. Hence it is necessary to evaluate the line to ground impedance as well as the line to line impedance as seen by the ground distance relay in each phase, against the Mho characteristic settings for each zone. The line to ground impedance and the line to line impedance are calculated using the phase and the sequence voltage and current quantities calculated based on (5.1) and (5.2) respectively.

\[
\text{Line to ground impedance} = \frac{V_{\text{phase}}}{I_{\text{phase}} + kI_o} \tag{5.1}
\]

where \(V_{\text{phase}}\) is phase voltage, \(I_{\text{phase}}\) is phase current, \(k\) is residual compensation factor and \(I_o\) is zero sequence current, and

\[
\text{Line to line impedance} = \frac{V_{\text{phase}1} - V_{\text{phase}2}}{I_{\text{phase}1} - I_{\text{phase}2}} \tag{5.2}
\]

where \(V_{\text{phase}1}\) and \(V_{\text{phase}2}\) are phase voltages and \(I_{\text{phase}1}\) and \(I_{\text{phase}2}\) are phase currents.

The computed impedance values are then evaluated against the Mho characteristic component. The output produced by the Mho component is 1 if the point defined by the computed impedance—R and X—is inside the characteristic circle. Each zone of the distance relay scheme requires six Mho characteristic components; three to detect line to ground faults and three to detect line to line faults. Figure 5.5 shows the zone 1 protection scheme of a distance relay that was modelled in PSCAD/EMTDC.
Figure 5.5: Zone 1 protection scheme of a distance relay modelled in PSCAD/EMTDC
Several scenarios were examined to identify possible implications of a saturated core FCL installation on a transmission line feeder to the existing distance relays protecting the line. The primary zone of protection, Zone 1, covers 80% of the line while 120% of the line is covered by protection Zone 2. The distance relay settings of the transmission line feeder under consideration, are given in Table 5.1. Note that the reach impedances are expressed with respect to the CT primary winding.

### Table 5.1: Distance relays A1 settings

<table>
<thead>
<tr>
<th>Zone</th>
<th>Reach (Ω)</th>
<th>Line Angle</th>
<th>R (Ω)</th>
<th>X (Ω)</th>
<th>Time delay (s)</th>
<th>Residual compensation factor (k)</th>
<th>Magnitude</th>
<th>Angle</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>15</td>
<td>60º</td>
<td>7.5</td>
<td>12.99</td>
<td>0</td>
<td>1.5</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>22.5</td>
<td>60º</td>
<td>11.25</td>
<td>19.49</td>
<td>0.4</td>
<td></td>
<td>22º</td>
<td></td>
</tr>
</tbody>
</table>

Case 1 - FCL within the protection zone of the relay – internal fault

A single line (Phase A) to ground fault and a line to line fault (Phase A to Phase B) were simulated within the primary protection zone of relay A1 as illustrated in Figure 5.6. These simulations were undertaken with and without a saturated core FCL installed within the protection zone of the relay. The resulting impedance trajectories seen by the ground distance relay, plotted in the R-X plane are shown in Figure 5.7. Each trajectory represent a line to ground impedance or a line to line impedance for each of the three phases. Zone 1 and Zone 2 characteristic circles are represented in red and blue respectively. As can be seen, the fault impedance

![Figure 5.6: FCL within the protection zone of the relay – internal fault](image-url)
trajectory is inside Zone 1 of the relay operating characteristic (red circle) for each of these fault cases.

The Relay A1 operation for the L-G fault scenario described above is shown in Figure 5.8 and Figure 5.9 for the cases without and with the FCL respectively. As can be seen, the fault is detected and the relay trip signal is triggered immediately following the fault in both scenarios. Relay A1 operation for a L-L internal fault, without and with the FCL, is also seen to be similar to that shown in Figure 5.8 and Figure 5.9. Hence, the presence of an FCL within the protection zone is not seen to affect the Mho relay operation for an internal fault on this particular feeder.
Figure 5.7: Case 1 - impedance trajectory of (a) L-G fault without FCL (b) L-G fault with FCL (c) L-L fault without FCL (d) L-L fault with FCL, at the primary zone of protection (Zone 1) as seen by the Mho Relay.
Figure 5.8: Mho Relay operation for a L-G internal fault without an FCL. Subfigures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) current sensed at relay A1 (c) relay A1 trip signal (accounting for pre-set time delays when applicable)
Figure 5.9: Mho Relay operation for a L-G internal fault with a FCL located inside the protection zone of the relay. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on)  (b) current sensed at relay A1  (c) relay A1 trip signal (accounting for pre-set time delays when applicable)
Case 2 - FCL within the protection zone of the relay – external fault

A single line to ground fault (Phase A) and a line to line fault (Phase A to Phase B) were simulated within the backup protection zone (Zone 2) of Relay A1, with and without a saturated core FCL installed within the protection zone of the relay. The resulting impedance trajectories seen by the ground distance relay plotted on the R-X plane, are shown in Figure 5.11. As can be seen, the impedance seen by the relay is inside the Zone 2 region of the Mho circle, with and without the fault current limiter in the system, for both fault types.

The Relay A1 operation for the L-G fault scenario described above is shown in Figure 5.12 and Figure 5.13, for the cases without and with the FCL respectively. Typically, the trip signal for Zone 2 is only triggered if the fault is still detected after the predefined delay time from its pickup time as illustrated by Figure 5.12. However with the FCL in the system, the follow current (i.e. steady state fault current) limited by the FCL falls under the threshold pickup value, resulting in $Z_{\text{fault}} > Z_{\text{pickup}}$ under steady state fault conditions. Since the fault is not detected for the total duration of the preset delay time of the relay (0.4 s), the backup protection of Relay A1 is not triggered, as shown in Figure 5.13.

Note that the relay operation was not affected by the presence of the FCL for the internal fault (Zone 1 fault) discussed previously. The large first peak of fault current, although limited, still exceeds the threshold pickup value, resulting in a fault impedance that falls inside of the Mho relay operating zone ($Z_{\text{fault}} < Z_{\text{pickup}}$). Since the relay is set to trigger immediately after an internal fault detection, the magnitude of the follow current does not affect the FCL operation.
Figure 5.11: Case 2 - impedance trajectory of (a) L-G fault without FCL (b) L-G fault with FCL (c) L-L fault without FCL (d) L-L fault with FCL, at the backup zone of protection (Zone 2) as seen by Relay A1
Figure 5.12: Mho Relay operation for a L-G external fault without an FCL. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) current sensed at relay A1 (c) relay A1 trip signal (accounting for pre-set time delays when applicable)
Figure 5.13: Mho Relay operation for a L-G external fault with an FCL in the protection zone of the relay. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) current sensed at relay A1 (c) relay A1 trip signal (accounting for pre-set time delays when applicable)
Figure 5.14: Under-reach of Mho relay caused by the presence of a saturated core FCL

Figure 5.14 illustrates the under-reach of the Mho relay caused by the presence of the FCL. As shown, the impedance seen by the relay \( Z_{F,new} \), which is equal to the sum of the FCL fault impedance \( X_{FCL} \) and the system impedance without the FCL \( Z_F \), falls outside of the Zone 2 reach \( Z_{R2} \) of the relay. Hence, the relay zone settings need to be adjusted to account for the FCL impedance and ensure proper operation of the distance relay. The adjusted relay zone settings can be calculated based on the FCL insertion impedance and the line impedance as given in (5.3) - (5.8).

Line impedance seen by the relay without the FCL,

\[
Z_S = R_S + jX_S = (9.375 + j16.2375) \Omega \tag{5.3}
\]

Fault impedance of the inserted saturated core FCL,

\[
Z_{FCL} = R_{FCL} + jX_{FCL} = j8.7265 \Omega \tag{5.4}
\]

Line impedance seen with the FCL,

\[
Z_{S,new} = Z_S + Z_{FCL} = \sqrt{(R_S)^2 + j(X_S + X_{FCL})^2} = (9.375 + j24.964) \Omega \tag{5.5}
\]
Adjusted Zone 1 relay setting to accommodate the presence of the FCL,

\[
Z_{R1} = 0.8 \left( \sqrt{(R_S)^2 + j(X_S + X_{FCL})^2} \right) \tag{5.6}
\]
\[
Z_{R1} = (7.5 + j19.97) \, \Omega \tag{5.7}
\]

Adjusted Zone 2 relay setting to accommodate the presence of the FCL,

\[
Z_{R2} = 1.2 \left( \sqrt{(R_S)^2 + j(X_S + X_{FCL})^2} \right) \tag{5.8}
\]
\[
Z_{R2} = (11.25 + j29.96) \, \Omega \tag{5.9}
\]

The relay operation for a L-G external fault, when simulated with the adjusted Mho relay settings is illustrated in Figure 5.15. As can be seen, with the new reach settings the fault is detected for the total duration of the preset delay time of the relay (0.4 s), and hence the backup protection of Relay A1 is triggered after the set delay time.

Note that, these fault scenarios only consider the operation of Relay A1. Provided that the primary protection of that particular line section operates accurately, the fault will still be detected and cleared by the primary relay, immediately following the fault. However, for proper protection coordination it is necessary to ensure timely operation of both primary and backup protection of relays.
Figure 5.15: Mho Relay operation for a L-G external fault with adjusted relay reach settings. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) current sensed at relay A1 (c) relay A1 trip signal (accounting for pre-set time delays when applicable)
Case 3 - FCL outside the protection zone of the relay – internal fault

A single line to ground fault (Phase A) and a line to line fault (Phase A to Phase B) were simulated within the primary protection zone of Relay A1, with and without an FCL installed outside of the primary protection zone of the relay (as illustrated in Figure 5.16). The resulting impedance trajectories seen by the ground distance relay plotted on the R-X plane, are shown in Figure 5.17. As can be seen, the impedance is inside Zone 1 of the Mho circle in each of these fault cases.

The corresponding Relay A1 operation for the L-G fault scenario described above is shown in Figure 5.18 and Figure 5.19 for the cases without and with the FCL respectively. As can be seen, the fault is detected and the relay trip signal is triggered immediately following the fault in both scenarios. Hence, the FCL installation outside of the protection zone is seen to have no effect on the A1 Mho relay operation for an internal fault in this particular feeder.
Figure 5.17: Case 1 - impedance trajectory of (a) L-G fault without FCL (b) L-G fault with FCL (c) L-L fault without FCL (d) L-L fault with FCL, at the primary zone of protection (Zone 1) as seen by the Mho Relay
Figure 5.18: Mho Relay operation for a L-G internal fault without an FCL. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) current sensed at relay A1 (c) relay A1 trip signal (accounting for pre-set time delays when applicable)
Figure 5.19: Mho Relay operation for a L-G internal fault with a FCL located outside of the protection zone of the relay. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) current sensed at relay A1 (c) relay A1 trip signal (accounting for pre-set time delays when applicable)
5.3.2 Differential Protection

The current differential relays at the Columboola substation feeders are enabled with the dual slope percentage biased restraint characteristic to determine the tripping criteria of the relay. The differential relay modelling algorithm utilised in the PSCAD/EMTDC simulation study is shown in Figure 5.20.

![Differential relay modelling algorithm in PSCAD/EMTDC](image)

**Figure 5.20:** Differential relay modelling algorithm in PSCAD/EMTDC

As can be seen from Figure 5.20, based on the measured primary line currents at both ends of the protected zone, the secondary currents from the CT windings are determined using the Jiles-Atherton Two CT Differential Configuration [73].
The CT parameters are set according to the data provided by the utility. The secondary current signals from the CTs are subsequently sampled using the FFT block, with the fundamental magnitudes and phases of the CT currents extracted. The PSCAD/EMTDC model for signal processing of the differential protection is illustrated in Figure 5.21.

**Figure 5.21:** Signal transformation and processing for differential protection scheme modelled in PSCAD/EMTDC

The magnitudes and the corresponding phase angle values of the two current signals were then evaluated against the tripping criteria set by the Dual Slope Current Differential Relay component. The tripping criteria of the relay are given in (5.10) and (5.11) [73].
Case 1:

\[
\text{If } |I_{bias}| < I_{S2} \\
\text{and } |I_{diff}| > K_1|I_{bias}| + I_{S1} \text{ then trip} \quad (5.10)
\]

Case 2:

\[
\text{If } |I_{bias}| \geq I_{S2} \\
\text{and } |I_{diff}| > K_2|I_{bias}| - (K_2 - K_1)I_{S2} + I_{S1} \text{ then trip} \quad (5.11)
\]

In (5.10) and (5.11), \( K_1 \) and \( K_2 \) are the lower and the higher percentage bias settings respectively, \( I_{S1} \) is the differential current threshold setting and \( I_{S2} \) is the bias current threshold setting. The relay output signal will be ‘1’ provided that the trip condition is satisfied for more than the hold time specified. The PSCAD/EMTDC implementation of the differential protection scheme is shown in Figure 5.22.

Several cases were examined to identify possible implications of a saturated core FCL installation on current differential relays protecting a transmission line. The relay settings of the transmission line feeder under consideration, are given in Table 5.2. Given the influence that a double-end feed system could have on current differential schemes, a double-end feed system was considered in the simulation studies.

The operation of the current differential scheme without the presence of an FCL is shown in Figure 5.23 for an internal single line to ground fault (Phase A), and in and Figure 5.24 for an external fault. These two figures are used as benchmarks when comparing the differential relay performance in the presence of an FCL, as discussed in the following cases.
Figure 5.22: Dual slope current differential relay scheme modelled in PSCAD/EMTDC

Table 5.2: Differential Relay B1 settings

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Differential current threshold $I_{S1}$</td>
<td>0.2A</td>
</tr>
<tr>
<td>Bias current threshold $I_{S2}$</td>
<td>2A</td>
</tr>
<tr>
<td>Lower percentage bias settings $K_1$</td>
<td>30%</td>
</tr>
<tr>
<td>Higher percentage bias settings $K_2$</td>
<td>150%</td>
</tr>
<tr>
<td>CT ratio</td>
<td>400 : 1</td>
</tr>
<tr>
<td>Hold time</td>
<td>10ms</td>
</tr>
</tbody>
</table>
Figure 5.23: Differential Relay operation for a L-G internal fault without the FCL. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) Relay B1 fault detect signal (0 when no fault is detected, 1 when a fault is detected) (c) current sensed at CT1 (d) Relay B1 trip signal to CB X1 (e) current sensed at CT2 (f) Relay B1 trip signal to CB Y1 (accounting for pre-set time delays when applicable)
Figure 5.24: Differential Relay operation for a L-G external fault without an FCL. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) Relay B1 fault detect signal (0 when no fault is detected, 1 when a fault is detected) (c) current sensed at CT1 (d) Relay B1 trip signal to CB X1 (e) current sensed at CT2 (f) Relay B1 trip signal to CB Y1 (accounting for pre-set time delays when applicable)
Case 1 - FCL within the protection zone of the relay – internal fault

Figure 5.25: FCL within the protection zone of the relay – internal fault

A single line to ground fault (Phase A) was simulated within the primary protection zone of Relay B1, with a saturated core FCL installed within the protection zone of the relay as illustrated in Figure 5.25. Note that although the fault is fed from both sides, fault current contribution from only one side is limited by the FCL. The relay operation for this fault scenario is shown in Figure 5.26. As can be seen, the relay operation is similar to that of Figure 5.23, i.e. the fault is detected and the relay trip signal is triggered immediately following the fault indicating that the FCL had no discernible effect on relay operation. Hence it is reasonable to assume that the lower fault current due to the FCL has not affected the sensitivity of the current differential relay and that the FCL caused no significant phase shift in the limited current in this particular case.
Figure 5.26: Differential Relay operation for a L-G internal fault with a FCL located inside the protection zone of the relay. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) Relay B1 fault detect signal (0 when no fault is detected, 1 when a fault is detected) (c) current sensed at CT1 (d) Relay B1 trip signal to CB X1 (e) current sensed at CT2 (f) Relay B1 trip signal to CB Y1 (accounting for pre-set time delays when applicable)
Case 2 - FCL outside the protection zone of the relay – internal fault

A single line to ground fault (Phase A) was simulated within the primary protection zone of Relay B1, with a saturated core FCL installed outside the protection zone of the relay, as illustrated in Figure 5.27. The relay operation with the FCL is shown in Figure 5.28. As in Case 1, fault current contribution from one side is limited by the FCL. The current differential relay operation following the fault is also similar to Case 1, with the relay trip signal being triggered immediately after the fault as shown in Figure 5.28. The relay performance is also comparable with that shown in Figure 5.23 for an internal fault without the presence of an FCL. Hence it can be assumed that the presence of an FCL outside the protection zone has no adverse effect on relay operation for an internal fault.
Figure 5.28: Differential Relay operation for a L-G internal fault with a FCL located inside the protection zone of the relay. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) Relay B1 fault detect signal (0 when no fault is detected, 1 when a fault is detected) (c) current sensed at CT1 (d) Relay B1 trip signal to CB X1 (e) current sensed at CT2 (f) Relay B1 trip signal to CB Y1 (accounting for pre-set time delays when applicable)
Case 3 - FCL within the protection zone of the relay – external fault

A single line to ground fault (Phase A) was simulated within the primary protection zone of Relay B1, in the presence of a saturated core FCL installed within the protection zone of the relay, as illustrated in Figure 5.29. In this case the relay trip signal is not triggered by the external fault as shown in Figure 5.30 and the relay operation is consistent with the scenario without a current limiter shown in Figure 5.24. Further, as seen in Figure 5.30, a reduced fault current flows through both CTs, hence any differential current due to discrepancy between the excitation characteristics of the two CTs is most likely to be insufficient to cause a false relay tripping. The current limiter, therefore, improves the protection stability of the differential scheme for an external fault.
Figure 5.30: Differential Relay operation for a L-G external fault with a FCL located inside the protection zone of the relay. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) Relay B1 fault detect signal (0 when no fault is detected, 1 when a fault is detected) (c) current sensed at CT1 (d) Relay B1 trip signal to CB X1 (e) current sensed at CT2 (f) Relay B1 trip signal to CB Y1 (accounting for pre-set time delays when applicable)
Case 4 - Two FCLs outside the protection zone of the relay – internal and external fault

**Figure 5.31:** Two FCLs outside the protection zone of the relay – internal fault

The presence of an FCL will reduce the maximum peak fault current experienced by the relay, and hence will provide protection against core saturation for the CTs. However, it is preferred that FCLs are installed at both ends of the protected unit to ensure protection against core saturation for an external fault between the FCL and the CTs, as shown in Figure 5.31.

Case 4 is based on such a system, where the differential relay operation for a single line to ground fault (Phase A), incepted both within the protection zone as well as outside the protection zone of the relay, with an FCL installed at each end of the feeder is analysed. The relay performances for each of the two scenarios are illustrated in Figure 5.32 and Figure 5.33 respectively. As can be seen, the performance of the relay under each scenario is consistent with the performance displayed without the current limiters in the system (shown in Figure 5.23 and Figure 5.24). Hence, presence of FCLs at both in-feeds, outside the protection zone, has no discernible effect on relay operation.
Figure 5.32: Differential Relay operation for a L-G internal fault with two FCLs located outside the protection zone of the relay. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) Relay B1 fault detect signal (0 when no fault is detected, 1 when a fault is detected) (c) current sensed at CT1 (d) Relay B1 trip signal to CB X1 (e) current sensed at CT2 (f) Relay B1 trip signal to CB Y1 (accounting for pre-set time delays when applicable)
Figure 5.33: Differential Relay operation for a L-G external fault with two FCLs located outside the protection zone of the relay. Sub-figures from top to bottom show: (a) fault signal (0 when fault is off, 1 when fault is on) (b) Relay B1 fault detect signal (0 when no fault is detected, 1 when a fault is detected) (c) current sensed at CT1 (d) Relay B1 trip signal to CB X1 (e) current sensed at CT2 (f) Relay B1 trip signal to CB Y1 (accounting for pre-set time delays when applicable)
5.4 Conclusions

Application of FCLs to an existing system is likely to influence the protection schemes that were only designed to operate under existing network conditions (prior to FCL application). Depending on factors such as the type of FCL technology, protection schemes/principles considered, network configuration and FCL/fault location etc., the effects FCLs may have on the existing protection schemes could vary.

In this chapter, a review of the three fundamental protection principles—overcurrent, distance, differential protection—was presented and their generic exposure to current limiters were discussed. Influence of the FCLs to the four protection subsystems - sensing, pickup, processing and coordination were considered for each protection scheme while giving due consideration to both the FCL location as well as the fault location with respect to the primary protection zone. Although the issues identified in the review provides a general framework for a future study, establishing the impact of a certain type of FCL technology to an existing protection scheme requires in depth analysis, accounting for both network specific and protection specific issues. Hence, an example of a simulation-based comprehensive analysis, focusing primarily on the influence of a saturated core FCL application on existing power system protection schemes at a high voltage system, was presented.

The existing feeder protection schemes—distance and differential—at a 132kV utility substation were modelled based on the relay parameter settings provided by the utility. While in the real network, each feeder has two protection systems enabled to account for redundancy, for the purpose of this study, it was assumed that only one group was enabled at a time. Influence of a FCL installation on the two feeder protection schemes was investigated.

The findings from these case studies are summarised in the following Table 5.3:
**Table 5.3:** Summary of case studies - saturated core FCL integration with an existing protection system

<table>
<thead>
<tr>
<th>FCL location</th>
<th>Distance</th>
<th>Internal fault</th>
<th>External fault</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>FCL inside</strong></td>
<td>Fault detected in Zone 1 and relay trip signal triggered immediately following the fault — No discernible effect due to FCL</td>
<td>Fault is not detected for the total duration of relay preset time delay, relay trip signal for backup protection is not initiated — FCL impedance caused under-reach of Mho relay</td>
<td></td>
</tr>
<tr>
<td><strong>FCL outside</strong></td>
<td>Fault detected in Zone 1 and relay trip signal is triggered immediately following the fault — No discernible effect due to FCL</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>FCL location</th>
<th>Differential</th>
<th>Internal fault</th>
<th>External fault</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>FCL inside</strong></td>
<td>Fault detected and relay trip signal triggered immediately after — No discernible effect due to FCL</td>
<td>Relay trip signal is not triggered by the external fault — No discernible effect due to FCL</td>
<td></td>
</tr>
<tr>
<td><strong>FCL outside</strong></td>
<td>Fault detected and relay trip signal triggered immediately following the fault — No discernible effect due to FCL</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
An FCL installed in series with the fault loop, increases the zone impedance. Hence a relay, such as a distance relay, which is operated based on the analysis of the downstream impedance seen, may mal-operate due to the added FCL impedance. This was found to be the case in one of the fault scenarios, where a fault simulated within the backup zone of protection (Zone 2) of a distance relay failed to operate after the pre-set delay time, due to the presence of a saturated core FCL within the protection zone of the relay. It must be noted, however, that the relay operation was not affected by the presence of an FCL for an internal fault (Zone 1 fault). The large first peak of fault current in these scenarios, exceeds the threshold pickup value, resulting in a fault impedance that falls inside of the Mho relay operating zone \((Z_{\text{fault}} < Z_{\text{pickup}})\). Since the relay is set to trigger immediately after an internal fault detection, the magnitude of the follow current does not affect the Mho relay operation.

Based on available research and the observations from the study under consideration, the following revisions and recommendations are proposed:

1. To ensure proper operation of the relay, the FCLs must also allow the fault currents to flow at least for the duration of the processing time of the relay and any additional preset delay times.

2. In cases where the magnitude of the FCL-limited current is less than the pickup current setting of the overcurrent relay, the relay will fail to identify the fault conditions causing it to mal-operate. In such situations the operating characteristics of the relays need to be adjusted according to the new fault level of the system.

3. Where overcurrent devices using different techniques for measuring current magnitude (for an example RMS vs. peak of the fundamental frequency component) are set to coordinate with each other, the distortions in follow current can lead to relay mal-operation. In such cases it is recommended that the coordination of associated relays be re-examined to ensure the current waveforms
are evaluated accurately.

4. In cases where the presence of an FCL in the fault loop increase the zone impedance, causing the impedance seen by the relay fall outside of the relay operating zone, the operating characteristic of the relay should be adjusted to account for the impedance associated with the FCL.

5. Severe distortions in current waveforms due to FCLs may result in phase angle shifts which could influence the impedance and direction determination process of the relay. In such cases the operating characteristic of the relay should be adjusted to account for the phase angle shifts caused by the distorted current waveforms.

6. If an FCL limits only one of the feed currents in a double-end feed system, for an internal fault, it may lead to a significant phase shift of the limited fault current depending on the degree of limitation and X/R ratio. This could lead to a reduction in differential current sensed by the relay and cause mal-operation of the relay. There is also risk of having insufficient sensitivity since the scalar sum of the secondary currents remain unaffected. This can be avoided by installing FCLs at both ends of the protected unit. This will also ensure protection against core saturation, for faults occurring at either end.

It is concluded that depending on the type of FCL and its characteristics as well as its location with respect to the protection zones defined, the existing protection schemes may have to be revised to ensure selectivity, proper coordination and operation. While the potential issues identified in the cases studied in this chapter and in literature gives an insight into potential grid integration issues of saturated core FCL, it is recommended that each FCL installation be evaluated on a case-by-case basis to identify the specific protection issues associated with the installation.
Chapter 6

Application of Saturated Core FCLs - Other Considerations

6.1 Introduction

In Chapter 5, an analysis of the interactions between a saturated core FCL and three commonly employed protection schemes—overcurrent, distance and differential—was carried out to ascertain possible effects and to identify potential resetting requirements of the protection schemes. It is quite evident from the discussions in Chapter 5, that installation of any type of current limiting device will have certain effects on the protection schemes, which are typically configured to existing network conditions.

This chapter aims to address several other application considerations that network operators may need to take in to account when considering a potential FCL installation. The impact an FCL will have on circuit breakers, in terms of interrupting burden and the TRV associated with the circuit breakers when interrupting a FCL-limited current, is discussed in Section 6.2. In an era where power quality has become a very important aspect of power delivery [92], evaluating possible effects on voltage sags and power system harmonics is imperative—these are addressed in Section 6.3 and Section 6.4 respectively. In Section 6.5 potential impact on power
system stability with regard to a saturated core FCL application is discussed.

6.2 Effect on Circuit Breaker Transient Recovery Voltage (TRV)

The use of current limiting devices is known to have a favourable effect on circuit breakers by mitigating the interrupting burden imposed on them. However, the TRV associated with circuit breakers when interrupting series reactor-limited short-circuit currents on transmission lines, has been reported to be severe [93–95]. Therefore when considering FCL applications, it is important to analyse the subsequent TRVs of relevant circuit breakers in the vicinity, to ensure that the TRV withstand capability of the associated circuit breaker is not exceeded.

In this section, the influence of a saturated core FCL on the interrupting duty of a circuit breaker was investigated and compared to that of a series reactor. According to IEEE Standard 37.011-1994 [93], the most severe recovery voltages tend to occur across the first pole that opens in a circuit breaker when interrupting a symmetrical three phase ungrounded fault at its terminal when the system voltage is maximum. The TRV associated with asymmetrical current interruption has been found to be less severe than when interrupting a symmetrical current [93]. Therefore, a symmetrical three-phase ungrounded fault, occurring close to the FCL terminal and the associated circuit breaker, was considered in the simulation study.

The study was carried out in PSCAD/EMTDC based on the 132 kV saturated core FCL application described in Section 4.3 of Chapter 4. Since circuit breaker TRV is predominantly a localised phenomenon, in EMT simulations, a detailed representation of the substation of interest is necessary. Hence, the 132 kV substation model described in Section 4.3.4 was further amended to include more detailed representations of station equipment. This involved accounting for the effective stray capacitances and inductances of various substation equipment such as circuit breakers, disconnector switches, surge arrestors, transformers, bus VTs and series reactors.
Where the capacitance values of station equipment were not available, the minimum recommended values in IEEE Standard C37.011-2011 [96] were adopted as given in Appendix D.2. Part of the detailed station model is illustrated in Figure 6.1. Note that the nonlinear inductance model of the FCL was used to represent the behaviour of the saturated core FCL in PSCAD/EMTDC.

The TRV performance of a 170 kV, 40 kA circuit breaker (referred to as Breaker X in Figure 6.1) was evaluated for a symmetrical three-phase ungrounded fault occurring close to the FCL terminal and the associated circuit breaker. Breaker X was simulated to interrupt the fault at the current zero crossing.

Figure 6.2 shows the results of the TRV analysis of Breaker X for three scenarios: (a) in the absence of a current limiter, (b) with an equivalent series reactor and (c) with a saturated core FCL in the circuit. Note that the series reactor was modelled as a lumped inductance with a parallel resistance added for realistic high frequency damping to avoid numerical integration instabilities [97)]. Figure 6.2 also shows the maximum terminal fault TRV withstand capability of the breaker at 100% of its rated short-circuit current (T100) as defined by the IEEE Standard C37.06-2009 [98]. For circuit breakers rated 100 kV and above, the TRV envelope is represented by three line segments, which are defined by means of the parameters given in Table 6.1.

For both the FCL-limited and series reactor-limited fault scenarios the associated circuit breaker is interrupting a reduced fault current which is approximately equal to 45% of the breaker’s rated short-circuit current. Therefore, a TRV capability envelope for a 170 kV breaker at 45% test duty (T45) was also derived by a method of interpolation. The relevant calculations are given in Appendix D.3 and the parameters of the TRV capability envelope at 45% test duty are given in Table 6.1.
Figure 6.1: Section of the detailed station model
**Figure 6.2:** First pole-to-clear TRV of a 170kV, 40kA circuit breaker interrupting a symmetrical three-phase ungrounded terminal fault with T100 envelope

**Table 6.1:** TRV capabilities of a 170kV circuit breaker at 100% and 45% test duties for terminal faults

<table>
<thead>
<tr>
<th>TRV Parameters</th>
<th>Percentage of interrupting capability</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>100%</td>
</tr>
<tr>
<td>First reference voltage, $u_1$ [kV]</td>
<td>135</td>
</tr>
<tr>
<td>Time to reach $u_1$, $t_1$ [µs]</td>
<td>68</td>
</tr>
<tr>
<td>TRV peak value, $u_c$ [kV]</td>
<td>253</td>
</tr>
<tr>
<td>Time to reach $u_c$, $t_2$ [µs]</td>
<td>272</td>
</tr>
<tr>
<td>Rate of rise of recovery, $\frac{u_1}{t_1}$ [kV/µs]</td>
<td>2</td>
</tr>
</tbody>
</table>
As demonstrated in Figure 6.2, the TRV of the circuit breaker is within the standard T100 TRV envelope for the case with no current limiter. However, in both the series reactor-limited and saturated core FCL-limited fault scenarios, the system TRVs exhibit lower peak magnitudes, but significantly higher Rate-of-Rise-of-Recovery Voltages (RRRV) when compared with no current limiter case. The high RRRV is more severe in the series reactor-limited case. Note also that high frequency oscillations can be observed in both the FCL-limited and series reactor-limited TRVs, particularly at the very beginning of the waveform. Despite the higher RRRV and the high frequency oscillations, the system TRV in the FCL-limited fault current case is still within the standard-defined TRV capability at 45% fault duty—as shown in Figure 6.3. However, in the series-reactor limited case, the RRRV significantly exceeds the standard TRV capability defined by both the T100 and T45 envelopes. In cases such as this [96] recommends adding a capacitance in parallel to the reactor to reduce the RRRV to an acceptable level, or use a definite purpose circuit breaker for fast transient recovery voltage rise times.

Figure 6.3: First pole-to-clear TRV of a 170kV, 40kA circuit breaker interrupting a symmetrical three-phase ungrounded terminal fault T45 envelope
6.3 Effect on Voltage Sags

Voltage sags are one of the most significant power quality issues affecting modern industrial customers, predominantly due to the advent of “sensitive loads”, such as adjustable speed drives and process-control equipment [92]. Voltage sags are defined as short duration decreases in rms voltage, caused predominantly by short circuits on utility lines or by the starting of large loads in the power system. A voltage sag lasting just 4-5 cycles can cause customer loads to drop out, if they are sensitive to supply voltage deviations.

Transmission systems are generally tightly interconnected for reliable operation, with their protection systems designed to detect and isolate faults quickly—typically within 3-6 cycles. Hence, a fault on the transmission system, does not cause interruption at low voltage distribution levels. However, while the fault is on the transmission system, the entire system, including the distribution system, will experience a voltage sag. Hence, while an interruption caused by a short circuit can be considered as a “local problem”, a voltage sag is more of a “global problem” affecting many circuits [92]. Addressing the voltage sag problem is therefore imperative in ensuring the quality and reliability of the supply.

The level of voltage sags experienced by the system during a fault, is proportional to the magnitude of the short-circuit current. Hence, introducing a limiting impedance to the most exposed feeders of the system can reduce the voltage sag experienced by the system and improve the quality of power [99,100]. To demonstrate and verify the effect a saturated core FCL installation in a transmission line feeder may have on voltage sag, a simulation study was carried out in PSCAD/EMTDC based on the 132 kV saturated core FCL application described in Chapter 4.

The detailed station model that was utilised for the study is as illustrated in Figure 6.1. The pertaining network data of the 132 kV sub-transmission system as well as the FCL design parameters, are given in Table 4.4 and Table 4.5 of Chapter 4 respectively. The FCL was inserted into Feeder N1 of the 132 kV substation, and the bus voltages for a simulated three-phase ground fault scenario (adjacent to the
FCL on the incoming feeder) were observed. The fault was simulated for a duration of approximately 10 cycles with the circuit breaker operating at 0.3 s to clear the short-circuit. The resulting voltages at the 132 kV bus (Bus 2) during the fault, with and without the presence of the FCL, are shown in Figure 6.4a. The bus voltage with an equivalent air-core reactor of 6.0987 mH, providing the same symmetrical fault current limitation as the FCL, is also shown in Figure 6.4a. Since the fault is closer to the 132 kV bus, a significant depression in the voltage can be seen during the fault, in the absence of a current limiter in the system. In comparison, the saturated core FCL limits the voltage sag experienced by Bus 2 by maintaining approximately 34% - 48% of the nominal voltage during the fault. The series-reactor performance is comparable with that of the FCL retaining approximately 40% of the nominal voltage during the fault.

For the same fault scenario, the voltage sag experienced by a remote load connected at the 33 kV voltage level is shown in Figure 6.4b. Note that while the voltage sag is propagated to all lines connected to a faulted bus, the magnitude of the resulting voltage during the fault will depend on the distance from the fault, network configuration and source and line impedances. This is contrary to a radial system where the voltage sag due to an upstream fault will propagate downstream without attenuation. As can be seen from Figure 6.4b the voltage sag experienced by the remote load at the 33 kV level is less severe than at the 132 kV bus - with the load voltage being approximately 80% below the nominal voltage. The saturated core FCL limits the voltage sag experienced by the load by retaining approximately 46% - 58% of the nominal voltage during the fault. The series-reactor performance is also comparable with that of the FCL retaining approximately 50% of the nominal voltage during the fault.

By mitigating the voltage sag to an acceptable level, the FCL improves the voltage quality and the reliability of the supply network, thus allowing some of the loads connected to the bus to ride through a shallower sag.
Figure 6.4: RMS (one-cycle) voltage for the voltage sag (with 256 samples in a cycle) (a) experienced by Columboola Bus 2 (b) experienced by a remote load connected downstream from Columboola Bus 2
Table 6.2: System parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Source voltage, $V_s$</td>
<td>$132 , kV$</td>
</tr>
<tr>
<td>Line resistance, $R_s$</td>
<td>$0.0722153 , \Omega$</td>
</tr>
<tr>
<td>Line inductance, $L_s$</td>
<td>$0.008962555 , H$</td>
</tr>
<tr>
<td>Load impedance, $Z_s$</td>
<td>$110.72 , \Omega$</td>
</tr>
</tbody>
</table>

6.4 Effect on harmonic distortion levels

Harmonic distortion is one of the primary concerns in terms of power quality [92, 101, 102]. Harmonic studies are typically carried out to investigate the effects of nonlinear devices and to analyse situations where harmonics are produced. The saturated core FCL, although is a nonlinear device, has been shown to have negligible influence on the power system under normal grid conditions from a harmonics perspective [103]. This is due to the saturation of the iron cores under normal grid conditions (resulting in a relative permeability of approximately unity). To verify this, simulations were carried out in PSCAD/EMTDC where a saturated core FCL application to a simple three-phase $132 \, kV$ system with a linear load (illustrated in Figure 6.5) was considered. Note that the nonlinear inductance model of the FCL was used to represent the behaviour of the saturated core FCL in PSCAD/EMTDC. The relevant parameters of this system are given in Table 6.2.

Figure 6.5: Saturated core FCL application to a three-phase $132 \, kV$ system
**Table 6.3:** Harmonic content of the load-side line voltage of Phase A - under normal operating conditions

<table>
<thead>
<tr>
<th>Harmonic order</th>
<th>Voltage harmonic as a percentage of fundamental (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>100</td>
</tr>
<tr>
<td>3</td>
<td>0.001485</td>
</tr>
<tr>
<td>5</td>
<td>0.000773</td>
</tr>
<tr>
<td>7</td>
<td>0.000170</td>
</tr>
<tr>
<td>9</td>
<td>0.000030</td>
</tr>
<tr>
<td>11</td>
<td>0.000072</td>
</tr>
<tr>
<td>13</td>
<td>0.000036</td>
</tr>
<tr>
<td>15</td>
<td>0.000028</td>
</tr>
<tr>
<td>THD</td>
<td>0.001685</td>
</tr>
</tbody>
</table>

Note that, the even harmonics of the signal, when approximated up to six digits, was 0% and hence are not included in the table.

Harmonic analysis of the system was carried out under normal operating conditions (no fault), with the computed harmonic content of the load-side line voltage summarised in Table 6.3. In determining the harmonic magnitudes, the input signals (voltage) were sampled in accordance with the Nyquist Criteria, and a Fourier analysis was then applied to the sampled data. As can be seen from Table 6.3, the computed harmonic voltage components are lower than the recommended limits of IEEE Standard 519 [104].

During fault conditions, as the AC fault current varies, the dynamic action of the FCL cores being driven in and out of saturation produces harmonics in the current waveform. The changes in flux also result in a back EMF waveform of unusual shape being generated across the FCL. However, with proper operation of protection schemes, the fault will be detected and isolated within 1-2 cycles. Hence, the harmonics produced by the limiting action will only be present in the system for a brief period of time. According to IEEE Standard 519, such short-duration harmonics produced intermittently by a device are usually tolerable [104].
Although unlikely, an interesting scenario to consider is the unexpected loss of DC magnetic bias of the saturated core FCL under normal grid conditions. This scenario will force the FCL to remain on-line in an unbiased condition [40,103]. The PSCAD/EMTDC model of Figure 6.5 was adapted to simulate such a scenario, by setting the DC source (energising the DC coil) to zero at time $t = 0.2\,s$. The resulting current in the DC coil decayed exponentially as shown in Figure 6.6, during which the AC line current alternately drove the cores deeper in and out of saturation. This dynamic behaviour of the cores during a DC bias collapse, results in an increase in FCL impedance. Consequently, a larger back EMF is developed across the device. The line current and the load-side line voltage observed during the DC de-energisation are illustrated in Figure 6.7a and Figure 6.7b respectively.

![Figure 6.6: DC de-energisation (occurring at $time = 0.2\,s$) and exponential DC current decay](image-url)
Figure 6.7: During the loss of DC energisation (a) line current (b) load-side line voltage (blue) and FCL terminal voltage (red). Note that DC de-energisation occurred at $time = 0.2 \text{s}$ and the waveforms are zoomed in for clarity.
It is evident from the waveforms shown in Figure 6.7 that an undesirable byproduct of this dynamic behaviour is the voltage harmonics generated by the device. To analyse the harmonic content of the voltage waveforms a Fourier analysis was undertaken at different levels of DC bias energisation, with the total harmonic distortion as well as individual harmonic magnitudes computed. The individual harmonics observed in the signal at different levels of DC bias energisation, are shown in Figure 6.8. As can be seen, as the DC bias collapses and the FCL is driven deeper into desaturation, the magnitudes of the odd harmonics are seen to increase. The total harmonic distortion of the load-side voltage is given in Table 6.4. At the end of DC current decay (DC bias energisation level = 0%) the maximum harmonic distortion levels are observed, which clearly exceed the IEEE Standard 519 recommended limits [104]. It must be noted that resonance in the system could also occur under these conditions, depending on the circuit parameters and the presence of capacitor banks in the vicinity of the FCL.

An automatic bypass switch that can switch out the FCL from the system,
Table 6.4: The harmonic distortion observed at different levels of DC bias

<table>
<thead>
<tr>
<th>Total Harmonic Distortion (%)</th>
<th>DC bias energisation level</th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Voltage harmonic (V)</td>
<td>0%</td>
<td>20%</td>
<td>40%</td>
<td>60%</td>
<td>80%</td>
<td>100%</td>
</tr>
<tr>
<td></td>
<td>33.59</td>
<td>5.31</td>
<td>0.31511</td>
<td>0.06304</td>
<td>0.01039</td>
<td>0.00169</td>
</tr>
</tbody>
</table>

immediately following the loss of DC bias, can prevent the occurrence of the above-discussed undesirable effects [40, 103].

6.5 Effects on Power System Transient Stability

Transient stability of a power system is defined as the ability of that system to reach a stable operating state following a large disturbance such as a short-circuit fault, switching of circuits, significant load change or an abrupt tripping of generators [105]. If the system is able to retain synchronism following a disturbance sustained for a reasonably long period of time, then the system is considered to be transiently-stable.

Considering the role of FCLs during short-circuit conditions, it is imperative to analyse the impact of the device on transient stability of the system. Several such studies have been carried out in the past to investigate the transient stability of a power system with FCL installations [106–110]. It has been shown that FCLs, in general, improve the transient stability of the system by absorbing the accelerating power during a fault. The resulting decelerating torque forces the rotor back toward the equilibrium point or reach an acceptable steady state operating point, following the fault. With saturated core FCL applications in particular, the ability of the FCL to return to its low impedance state immediately following fault interruption, is shown to have a positive impact on transient stability performance [106].

Note that in the present study no detailed work has been undertaken considering the impact of the saturated core FCLs on power system stability.
6.6 Conclusions

Application of saturated core FCLs in electricity networks is likely to give rise to certain unintended consequences - both desirable and undesirable. Hence, before deploying this technology it is important to investigate and understand the potential impact of saturated core FCLs on power system switchgear, power quality and power system transient stability.

The key research contributions of this chapter can be summarised as follows:

1. It was demonstrated that the system TRVs in the presence of an FCL exhibit lower peak magnitudes (with high frequency oscillations) compared to the scenario with no current limiter, but with significantly higher RRRVs.

2. Despite a higher RRRV and the high frequency TRV imposed by the FCL-limited fault, the system TRV resulting from interrupting the FCL-limited fault current was still shown to be within the IEEE C37.06-2009 standard-defined TRV capability curve. In comparison, in a series-reactor limited case, the resulting RRRV was shown to exceed the standard TRV capability defined by both the T100 and T45 envelopes. Hence, circuit breaker TRV associated with FCL-limited current interruption is seen to be less severe than that of series-reactors.

3. In cases where the RRRV exceeds the standard TRV capability, it is recommended to add a capacitance in parallel with the FCL to modify the RRRV to an acceptable level or use a definite purpose circuit breaker for fast transient recovery voltage rise times.

4. It was demonstrated that the saturated core FCL reduces the voltage sag experienced both at the transmission and distribution voltage levels, for a transmission level fault. By mitigating the voltage sag to an acceptable level, the FCL improves the voltage quality and the reliability of the supply network, thus allowing some of the loads connected to the bus to ride through shallower sags.
5. Under normal grid conditions (no fault), the computed harmonic content of the line voltage in the presence of an FCL was shown to be well within the recommended limits of IEEE Standard 519. However, it was observed that unexpected loss of DC magnetic bias during steady state operation can lead to very high voltage harmonic distortion levels, clearly violating the recommended limits of IEEE Standard 519. Resonance in the system could also occur under these conditions, depending on the circuit parameters and the presence of capacitor banks in the vicinity of the FCL. An automatic bypass switch that can isolate the FCL from the system, immediately following the loss of DC bias, is recommended to prevent the occurrence of such a situation.

6. Potential impact on power system stability with regard to a saturated core FCL application was discussed. While FCLs in general, improve the transient stability of the system by consuming the accelerating power during a fault, the ability of the saturated core FCL to return to its low impedance state immediately following fault interruption, is seen to have a further positive impact on transient stability.

While some of these influences discussed in this chapter are specific to saturated core FCLs, they are aspects of the power system that need to be investigated for any type of FCL installation, to ensure quality of power, system stability and reliability of supply.
Chapter 7

Conclusions and Recommendations for Future Work

7.1 Introduction

This chapter consolidates the major findings and presents final conclusions on the work presented in this thesis as well as recommendations for future work.

7.2 Discussion

With the recent significant progress in the development of FCL technology and the interest that utilities are showing in adopting these devices, identifying and addressing issues that are likely to emerge due to FCLs in the grid is critical. In spite of the extensive scope of literature available on different fault current limiting technologies, research concerning the influence of these devices on the grid and the potential issues associated with system integration have been found to be limited. Due to the fact that operating principles and the limiting behaviour of the FCL technologies can be remarkably distinct, drawing generalised conclusions based on the behaviour of a particular type of FCL technology in a system is inefficacious
in relation to certain aspects. In this background, this thesis sought to establish a systematic procedure for investigating the transient behaviour of the saturated core FCL, utilising an accurate time-domain model to represent the device.

An extensive literature review of FCLs was given in Chapter 2 where the basic operation of a saturated core FCL was discussed. In essence, a saturated core FCL is a variable inductance iron core reactor, which is saturated under steady state un-faulted conditions and has an impedance similar to that of an air-core reactor. During a fault event the iron cores desaturate, resulting in an instant increase in impedance, thus limiting the fault current. Different saturated core FCL topologies, that have been the focus of research and commercial investigation, were also discussed. These include single-core topologies with bridge rectifiers, and dual-core topologies such as the open-core, closed-core and hybrid-core configurations.

The literature revealed that experimental measurements and FEM analysis have been the most prevalent techniques used to characterise the transient behaviour of these devices. Both these techniques, while being accurate, cannot be used to analyse the transient electrical behavior of FCLs in power systems, particularly on the impact on power system switchgear during fault events. FEM-based FCL modelling, despite its usefulness as a design verification tool, cannot be interfaced directly with all EMT programs that are in use today. Hence, there is an increasing necessity for accurate time-domain models of FCLs that can be confidently applied in relation to power system studies.

A review of several different time-domain models that have been proposed to describe the transient behaviour of dual-core saturated core FCLs, was presented in Chapter 2. These included analytical approaches as well as associated closed-form mathematical expressions. The closed form equations which have been presented in the literature have been developed from various techniques to approximate the B-H characteristics of the core material. These approximations for B-H characteristics were further extended to characterise the behaviour of FCLs by developing mathematical expressions to estimate magnetic linkage in the core sections due to both
AC and DC fields of the device. Subsequently, closed-form equations were derived to determine the induced back emf across the device. To estimate the parameters associated with closed-form equations, curve fitting procedures were utilised based on static FEM simulations. While the models performed reasonably well in predicting the FCL behaviour qualitatively, quantitative discrepancies in current and voltage magnitudes were observed in model validation exercises. Further analysis revealed that, although these models incorporate the magnetic linkage in core sections due to both AC and DC fields, they do not include the transformer coupling effect that exists between the AC and DC windings during a fault. While this effect is more prominent in some topologies than others, the “transformer” coupling between the AC and DC systems in saturated core FCLs can have a significant impact on the performance of the FCL. It was for this reason the magnetic circuit concept was chosen, which has the capacity to model the full nonlinear range of magnetic operation as well as the AC to DC coupling effects, for the modelling work in this thesis.

In spite of the extensive scope of literature available in the subject area concerning different fault current limiting technologies, research concerning the influence of these devices on the grid and the issues with system integration was found to be limited. Among the significant studies that have been undertaken to examine the behaviour of FCLs in utility grids, work conducted by the CIGRÉ Working Groups—A3.10, A3.16 and A3.23—were found to be of great interest. While some work has been undertaken to investigate potential grid integration issues of saturated core FCLs, based on the review of the existing literature, it was seen that further work is required to address key research gaps related to saturated core FCL applications and performance studies in utility grids.
Modelling saturated core FCLs: Nonlinear Reluctance Model and Nonlinear Inductance Model

Based on the literature related to modelling of electromagnetic systems, the magnetic circuit concept has often been used to derive analytical models with adequate accuracy. In recent work, this concept was applied to an open core saturated core FCL and the magnetic field of the device was represented by a magnetic circuit of lumped reluctances. The magnetic circuit utilised nonlinear reluctance elements to represent core flux paths, accounting for the effects of magnetic saturation in the cores, and linear reluctance elements to represent the leakage and linkage of the AC and DC coils of the device. The AC and DC windings were represented by mmf sources. The magnitude of each reluctance element is dependent on the geometry of the device and magnetic properties of the cores and is determined by analysing the magnetic flux distribution in the device using either FEA simulations or through experimental measurements.

The work presented Chapter 3, made advancements on this magnetic analysis of the open core saturated core FCL and proposed two modelling approaches to represent the FCL in transient network simulators.

The first modelling approach, termed the “Nonlinear Reluctance Model”, extended the magnetic analysis of the open core configuration, by directly coupling the magnetic circuit of the device to the electrical system of the current limiter. The model was implemented in PSCAD/EMTDC where the magnetic circuit and the AC and DC electric circuits of the device were solved simultaneously. The mmf produced in each winding was determined by relating the magnetic fields to electric currents that produce them (i.e. $F = NI$). Subsequently, the back emf generated across each coil (both AC and DC) was determined based on the magnetic flux linking each winding (i.e. $V_{coil} = N_{coil} \frac{d\phi_{coil}}{dt}$). Magnetostatic simulations carried out in CEDRAT FLUX3D package was used to analyse the flux distribution of the device and subsequently determine the reluctance elements in the magnetic circuit. An anhysteretic B-H relationship was utilised to represent the magnetic saturation of
the iron cores. The time step mismatch inherent in the model due to the interdependence between the electric and magnetic circuits, combined with the trapezoidal integration method used in EMTDC, were seen to cause numerical instabilities when the model was simulated using a solution time step as small as $10 \mu s$. To minimise these numerical artifacts and to ensure the accuracy of results, a very small solution time step (in the order of $1 \sim 10 \text{ns}$) was required to be used in the simulations.

The modelling approach was subsequently validated experimentally, with results obtained from standard short circuit tests undertaken on a single-phase prototype FCL. PSCAD/EMTDC simulations were shown to accurately reproduce the experimental results - the limited fault current, the back EMF generated across the FCL and the bias current in the DC circuit. The ripple in the DC current waveforms, during both the transient and steady state periods, was predicted exceptionally well by the model, confirming that the model was effective in predicting the “transformer coupling” effects of the device.

While the model produced accurate results, having to use a very small solution time step also entailed a lengthy processing time to run the EMTDC algorithm. Moreover, despite the use of a very small solution time step, numerical oscillations and instabilities were still observed when the FCL Reluctance Model was incorporated into a large network (such as a power system with rotating machines and power electronic devices where there are occurrences of other switching events in the system).

A second modelling approach was proposed, in order to overcome the above stated problem. This modelling approach, termed the “Nonlinear Inductance Model”, introduced a single equivalent electric circuit model of lumped inductors to represent the saturated core FCL. The initial electric circuit was derived from the magnetic circuit of the Nonlinear Reluctance Model, using the principle of electromagnetic duality. Each mmf source in the magnetic circuit, was represented by a driving current (current sources) in the electric circuit and each reluctance common to two meshes in the magnetic circuit was represented by an inductance connected between
the two corresponding nodes. The AC and DC windings and the associated circuits (including the resistance of each winding, AC grid-side circuit and the DC biasing arrangement), were coupled to this electric circuit of lumped inductors, by replacing the current sources with ideal coupling transformers. The actual currents and voltages in the windings were established by using appropriate turns ratios in these coupling transformers. The linear inductors in the equivalent electric circuit were derived based on the magnitudes of linear reluctance elements, while the nonlinear inductance elements were represented by a characteristic curve depicting the flux linkage-current \((\lambda - i; \text{ where } \lambda = N\phi)\) relationship. The model was incorporated into, PSCAD/EMTDC, with readily available elements. This model allowed for a larger solution time step to be used in the associated time domain simulations and eliminated the occurrence of numerical artifacts, compared to the method of directly interfacing the magnetic reluctance circuit to electric circuits in PSCAD/EMTDC. The model was validated by experimental results of a single-phase prototype saturated core FCL with an open core configuration, where it produced excellent predictions for AC current, FCL terminal voltage and DC bias current during both pre-fault and post-fault conditions. Compared to the case of Nonlinear Reluctance Model, the Nonlinear Inductance Model resulted in an improvement in solution speed, by a factor of 200, in the validation simulations. Hence, with the proposed modelling approach, a significant reduction in processing time can be achieved without compromising the accuracy of the results.

A key aspect of the proposed modelling approaches is that there is no unique magnetic circuit for all devices. The magnetic circuit and the subsequent equivalent electric circuit with the associated reluctance/inductance values are dependent on the FCL geometry and the magnetic properties of the FCL cores. Therefore, depending on the geometry and the flux distribution of a particular FCL device, the magnetic circuit may need to be adjusted to account for all substantial flux paths to produce an accurate model.

The applicability of the Nonlinear Inductance Model that was developed for the
open core centered DC coil arrangement was examined in relation to a few other selected saturated core FCL topologies. It was demonstrated that the equivalent electric circuit developed for the open core centered DC coil arrangement sufficiently models the behaviour of the Helmholtz type DC coil arrangement of an open core FCL. It was also shown that the basic magnetic circuit derived for the open core FCL sufficiently represents the closed core structure, and that the three tests identified for the open core case are still sufficient to determine all of the reluctance values in the closed core case. Both applications were experimentally validated with very good agreement between the experimentally measured and simulated quantities.

Saturated core FCLs: Steady-state and transient behaviour in power distribution and transmission networks

In Chapter 4, the potential performance of a saturated core FCL in an interconnected 11kV distribution system was examined, utilising the Nonlinear Inductance Model of the FCL proposed in Chapter 3. A potential FCL design was presented for the test network, and the efficacy of an FCL device placed on a bus-tie location of a meshed network, was investigated using PSCAD/EMTDC simulations. It was shown that while the bus-tie FCL limits the current that flows through it, the current contribution that is directly fed to the fault from the other side significantly rises. Consequently, the total effective current limiting achieved by the FCL was shown to be marginal. Through a numerical approach to fault analysis, the simulation results were theoretically verified, demonstrating the effects of the bus-tie FCL impedance on the fault current contributions by the adjacent un-faulted buses. It was also shown that, in such an interconnected system, in order to achieve the desired fault current reduction effect, application of multiple FCLs at critical locations of the network is necessary. However, cost might be a prohibitive factor in implementing this commercially and hence a suitable technique may need to be adopted when determining optimum placement for saturated core FCLs in interconnected systems.

The potential performance of a saturated core FCL, installed at a critical feeder
of the 132 kV sub-transmission system was also analysed in Chapter 5. The PSCAD/EMTDC network model used for the study was developed and validated based on power system data provided by a transmission utility. An FCL design that would meet the performance specifications of the network under consideration was presented, and was shown to provide approximately 40% reduction in symmetrical fault current for a three phase to ground fault, when the FCL was placed in the network. The performance of the saturated core FCL in the system was compared with that of an equivalent air-core reactor, where the application of a saturable core FCL was shown to have significant merit over the conventional current-limiting series reactors. This is particularly true when considering the performance of the saturable core FCL compared with that of an equivalent air-core reactor, under normal steady state (un-faulted) conditions. Significant performance differences between the two could be observed in terms of their respective effects on steady state current and network power flows. In comparison to the saturable core FCL, the equivalent air-core reactor imposes greater constraints on the network power flows and has considerably larger steady state voltage drop under pre-fault conditions. Fault performance of both devices was consistent, with the highest fault current clipping achieved by both devices for the most severe fault scenario - single phase to ground fault. However, even for less severe fault scenarios, such as for a line to line fault, both the saturable core FCL and the equivalent reactor were shown to provide adequate level of current limiting.

Saturated core FCLs: Interactions with existing power system protection

A comprehensive analysis on the influence of a saturated core FCL installation on existing power system protection schemes was conducted in Chapter 5. A review of the three fundamental protection schemes—overcurrent, distance, differential protection—was presented and their general susceptibilities to current limiters in terms of the four protection subsystems—sensing, pickup, processing and
coordination—were discussed. Due consideration was also given to both the FCL location and the fault location with respect to the primary protection zone in the analysis.

Subsequently, a simulation-based investigation was carried out to identify the impact of a saturated core FCL application on existing feeder protection schemes (distance and differential) at a sub-transmission level utility substation. The existing feeder protection schemes—distance and differential—at a 132 kV utility substation were modelled based on the relay parameter settings provided by a utility. While in the real network each feeder has two protection systems enabled to account for redundancy, for the purpose of this study it was assumed that only one group was enabled at a time. The performance of the relays was compared with and without a saturated core FCL.

It was shown that, despite the added impedance due to the presence of the FCL, the distance relay operation was not affected for internal faults. Internal faults were detected as primary zone (Zone 1) faults, and the relay trip signal was shown to trigger immediately following such a fault, with no discernible effect on the Mho relay due to FCL. However, it was found that for a fault simulated within the backup zone of protection (Zone 2), the presence of an FCL within Zone 1 causes under-reach of Mho relay. Typically, the trip signal for Zone 2 is only triggered if the fault is still detected by the relay after the predefined delay time from its pickup time. While the fault was detected at the transient phase of the fault (first 4-5 cycles post fault), the FCL-limited follow current was shown to fall under the threshold pickup value, resulting in $Z_{\text{fault}} > Z_{\text{pickup}}$ as the fault current reach steady state conditions. Since the fault was not detected for the total duration of the preset delay time of the relay, the backup protection of the relay was not triggered. It was demonstrated that by adjusting the relay zone settings to accommodate the presence of the FCL impedance, proper operation of the distance relay can be achieved. Note that, this fault scenario only considered the operation of the Zone 2 protection of a particular relay. Provided that the primary protection of that particular line section
operates accurately, the fault will still be detected and cleared by the primary relay, immediately following the fault. However, for proper protection coordination it is necessary to ensure timely operation of both primary and backup protection of relays.

Several case studies were also conducted to identify possible implications of a saturated core FCL installation on transmission feeder current differential relays (enabled with the dual slope percentage biased restraint characteristic) protecting the line. The performance of the relay under each scenario was found to be consistent with the performance displayed without the current limiters in the system.

Based on literature and the observations from the study under consideration, the following is recommended:

1. To ensure proper operation of the relay, the FCLs must also allow the fault currents to flow at least for the duration of the processing time of the relay and any additional preset delay times.

2. In cases where the magnitude of the FCL-limited current is less than the pickup current setting of the overcurrent relay, the relay will fail to identify the fault conditions causing it to mal-operate. In such situations the operating characteristics of the relays need to be adjusted according to the new fault level of the system.

3. Where overcurrent devices using different techniques for measuring current magnitude (for an example RMS vs. peak of the fundamental frequency component) are set to coordinate with each other, the distortion in follow current can lead to relay mal-operation. In such cases it is recommended that the coordination of associated relays be re-examined to ensure the current waveforms are evaluated accurately.

4. In cases where the presence of an FCL in the fault loop increases the zone impedance, causing the impedance seen by the relay to fall outside of the relay operating zone, the operating characteristic of the relay should be adjusted to
account for the impedance associated with the FCL.

5. Severe distortion in current waveforms due to FCLs may result in phase angle shifts which could influence the impedance and direction determination process of the relay. In such cases the operating characteristic of the relay should be adjusted to account for the phase angle shifts caused by the distorted current waveforms.

6. If an FCL limits only one of the feed currents in a double-end feed system, for an internal fault, it may lead to a significant phase shift of the limited fault current depending on the degree of limitation and X/R ratio. This could lead to a reduction in differential current sensed by the relay and cause mal-operation of the relay. There is also risk of having insufficient sensitivity since the scalar sum of the secondary currents remain unaffected. This can be avoided by installing FCLs at both ends of the protected unit. This will also ensure protection against core saturation, for faults occurring at either end.

It is concluded that depending on the type of FCL and its characteristics as well as its location with respect to the protection zones defined, the existing protection schemes may have to be revised to ensure selectivity, proper coordination and operation. While the potential issues identified in the cases studied in this thesis and in literature gives an insight into potential grid integration issues of saturated core FCL, it is recommended that each FCL installation be evaluated on a case-by-case basis to identify the specific protection issues associated with the installation.

**Saturated core FCLs: Other application considerations**

Chapter 6 of the thesis addressed several other application considerations that network operators may need to take in to account when considering a potential FCL installation. The influence of a saturated core FCL on the interrupting duty of a circuit breaker was investigated in this chapter. It was demonstrated that the system TRVs in the presence of an FCL exhibit lower peak magnitudes (with high frequency
oscillations) compared to the scenario with no current limiter, but with significantly higher RRRV. Despite the higher RRRV and the high frequency TRV imposed by the FCL-limited fault, the system TRV resulting from interrupting the FCL-limited fault current was still shown to be within the IEEE C37.06-2009 standard-defined TRV capability curve. In cases where the RRRV exceeds the standard TRV capability, addition of a capacitance in parallel to the FCL is recommended to modify the RRRV to an acceptable level or use of a definite purpose circuit breaker for fast transient recovery voltage rise times.

It was also demonstrated that the saturated core FCL reduces the voltage sags experienced both at the transmission and distribution voltage levels, for a transmission level fault. By mitigating the voltage sags to an acceptable level, the FCL improves the voltage quality and the reliability of the supply network, thus allowing some of the loads connected to the bus to ride through the shallower sag.

A harmonic analysis of a system with a saturated core FCL installation was carried out and it was shown that, under normal operating conditions (no fault), the harmonic content of the line voltage was well within the IEEE Standard 519 recommended limits. However, it was observed that unexpected loss of DC magnetic bias during steady state operation can lead to very high harmonic distortion levels, clearly violating the IEEE Standard 519 recommended limits. It must be noted that resonance in the system could also occur under these conditions, depending on the circuit parameters and the presence of capacitor banks in the vicinity of the FCL. An automatic bypass switch that can switch out the FCL from the system, immediately following the loss of DC bias is recommended to prevent the occurrence of such a situation.

A discussion based on literature was given on the impact of FCLs on power system transient stability. It was revealed that in general FCLs have the ability to improve the transient stability of the system by consuming the accelerating power during a fault. Additionally, the ability of the saturated core FCL to return to its low impedance state immediately following fault interruption, was noted to have a
further positive impact on transient stability.

While some of the influences discussed in Chapters 5 and 6 can be FCL-technology-specific or network-specific, they are aspects of the power system that need to be investigated for any type of FCL installation, to ensure quality of power, system stability and reliability of supply.

7.3 Recommendations for Future Research

While it is expected that the research presented in this thesis will make useful contributions to the general study of saturated core FCLs, there are a few other areas of interest that can be explored. Several of these potential areas are outlined in the following sections.

Model validation under different system conditions

Typically, FCL testing is restricted to investigating its performance during standard short circuit tests and under steady state un-faulted conditions in high power testing laboratories. The saturated core FCL models proposed in this thesis have also been validated against experimental results obtained from standard short circuit tests of a prototype FCL. The tests were performed with a 50 Hz fixed AC source for a bolted fault at varying levels of DC bias. While the validation process covered the two most significant operating conditions that are of interest from a utility perspective—i.e., current/voltage behaviour of the device during steady state un-faulted conditions and during a fault event at different fault levels—it may be of interest to investigate and validate the performance of the proposed model under different system conditions. Further work is required to establish the model performance during short duration transients or limiting behaviour at other power frequencies and partial faults. Model verification against actual data from saturated core FCLs that are operating in real electricity grids would be ideal.
Extending the Nonlinear Inductance Model for three-phase FCL devices and other FCL topologies

While substantial work has been carried out in this thesis to develop an accurate time-domain model of the saturated core FCL and to demonstrate the applicability of the modelling approach to several saturated core FCL topologies, the modelling only covered single-phase geometries. Extending the magnetic circuit approach to a three-phase FCL device, and subsequently developing an equivalent electric model to represent a three-phase FCL device could be useful. In a three-phase FCL device (where all three phases will be located in a single geometrical body) certain amount of coupling between the phases can be expected, which requires further studies. In particular, during a floating fault, the three phases are coupled electrically and hence certain amount of inter-phase mutual coupling has been observed. During a grounded fault, however, the phases are de-coupled electrically and hence the magnetic coupling between the phases has also been found to be very minimal. Since the FCL needs to be characterised under all fault conditions, the inter-phase coupling has to be accounted for when modelling a three phase FCL using the magnetic circuit concept.

It must be noted, however, that the use of three single-phase FCL devices, as opposed to a three-phase FCL device, is often preferred to avoid insulation complications in high voltage applications.

Development of future guidelines on the application of FCLs

Much of the application considerations discussed in Chapter 6 are aspects of the power system that need to be investigated in relation to any type of FCL installation. While the issues identified and the extent of their implications may be FCL technology-specific, network-specific or location-specific, they provide a general framework to establish standard guidelines for the application of FCLs to power systems. The comprehensive transient based saturated core FCL models that were proposed in this thesis offer the potential for considerable further investigation into
a variety of other transient phenomena associated with integrating a saturated core FCL. Findings from these investigations could be useful in establishing guidelines for FCL applications in utility grids.

**FCL applications in decentralised systems**

The increased penetration of renewable and decentralised generation has led to a considerable increase in the system short-circuit current levels in distribution systems. In particular, the advent of wind-turbine power generation (WTPG) has created potential applications for FCLs that require further investigation. The transient based saturated core FCL models proposed in this thesis can be utilised to investigate the impact of a saturated core FCL installation on a WTPG system, focusing on aspects such as effects on short-circuit currents, generator stability, generator decoupling protection and power quality.
References


[77] F. Darmann, R. Lombaerde, F. Moriconi, and A. Nelson, “Design, test and demonstration of saturable reactor high-temperature superconductor fault cur-


Nomenclature

\( A_{\text{coil}} \)  
Coil cross-sectional area

\( A_{\text{core}} \)  
Core height

\( B_{\text{sat}} \)  
Maximum flux density at saturation

\( F_{\text{ac}} \)  
Magnetomotive force due to AC winding

\( F_{\text{dc}} \)  
Magnetomotive force due to DC winding

\( H \)  
Magnetic field intensity

\( I_{OP} \)  
Operating current

\( I_{RT} \)  
Restraint current

\( I_{S1} \)  
Differential current threshold setting

\( I_{S2} \)  
Bias current threshold setting

\( i_{\text{ac}} \)  
AC line current

\( I_{dc} \)  
DC current

\( I_{\text{fault}} \)  
Limited rms fault current

\( I_{\text{max}} \)  
Line current at which the core is fully saturated

\( I_{o} \)  
Zero sequence current

\( I_{\text{phase}} \)  
Phase current

\( \tilde{L} \)  
Differential inductance

\( L_{\text{air}} \)  
Air core inductance

\( L \)  
Inductance

\( n_{\text{ac}} \)  
Number of AC coil turns

\( N_{\text{dc}} \)  
Number of DC coil turns

\( R_{\text{ac-coil}} \)  
AC coil resistance
\( R_{dc-coil} \) \( \rightarrow \) DC coil resistance
\( \mathcal{R} \) \( \rightarrow \) Reluctance
\( V_{ac-coil} \) \( \rightarrow \) Voltage induced in the AC winding
\( V_{dc-coil} \) \( \rightarrow \) Voltage induced in the DC winding
\( V_{fcl} \) \( \rightarrow \) Voltage across the FCL
\( V_i \) \( \rightarrow \) Pre-fault bus voltage
\( V_{phase} \) \( \rightarrow \) Phase voltage
\( Z_{FCL} \) \( \rightarrow \) FCL fault impedance
\( Z_R \) \( \rightarrow \) Impedance reach
\( Z_{ins} \) \( \rightarrow \) Insertion impedance
\( Z_{bus} \) \( \rightarrow \) Bus impedance matrix
\( Z_{fault} \) \( \rightarrow \) Fault impedance
\( Z_{pickup} \) \( \rightarrow \) Threshold pickup value of Mho relay
\( \beta \) \( \rightarrow \) Variable dependent on the AC coil geometry
\( h \) \( \rightarrow \) Height of coil
\( l \) \( \rightarrow \) Length of the flux path around the core
\( \lambda \) \( \rightarrow \) Flux linkage
\( \mu_0 \) \( \rightarrow \) Permeability of air
\( \omega \) \( \rightarrow \) Frequency in radians per second
\( \phi \) \( \rightarrow \) Relay characteristic angle
\( \phi_{ac} \) \( \rightarrow \) Magnetic flux through AC each winding
\( \phi_{dc} \) \( \rightarrow \) Magnetic flux through DC each winding
\( k \) \( \rightarrow \) Residual compensation factor
CIGRÉ \( \rightarrow \) Conseil International des Grands Réseaux Électriques
CT \( \rightarrow \) Current Transformer
EMF \( \rightarrow \) Electromotive force
EMT \( \rightarrow \) ElectroMagnetic Transient
EPRI \( \rightarrow \) Electric Power Research Institute
FCL \( \rightarrow \) Fault Current Limiter
<table>
<thead>
<tr>
<th>Acronym</th>
<th>Description</th>
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<tbody>
<tr>
<td>FEA</td>
<td>Finite Element Analysis</td>
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<tr>
<td>FE</td>
<td>Finite Element</td>
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<tr>
<td>FEM</td>
<td>Finite Element Method</td>
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<tr>
<td>FFT</td>
<td>Fast Fourier Transform</td>
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<tr>
<td>GTO</td>
<td>Gate-turn-off</td>
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<tr>
<td>HTS</td>
<td>High Temperature Superconductors</td>
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<tr>
<td>HV</td>
<td>High Voltage</td>
</tr>
<tr>
<td>IEC</td>
<td>International Electrotechnical Commission</td>
</tr>
<tr>
<td>IEEE</td>
<td>Institute of Electrical and Electronics Engineers</td>
</tr>
<tr>
<td>IPP</td>
<td>Independent Power Producer</td>
</tr>
<tr>
<td>L-G</td>
<td>Line-to-ground</td>
</tr>
<tr>
<td>L-L</td>
<td>Line-to-line</td>
</tr>
<tr>
<td>LTS</td>
<td>Low Temperature Superconductors</td>
</tr>
<tr>
<td>MMF</td>
<td>Magnetomotive force</td>
</tr>
<tr>
<td>MV</td>
<td>Medium Voltage</td>
</tr>
<tr>
<td>PI</td>
<td>Proportional-Integral</td>
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<tr>
<td>RMS</td>
<td>Root Mean Square</td>
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<tr>
<td>RRRV</td>
<td>Rate-of-Rise-of-Recovery Voltage</td>
</tr>
<tr>
<td>RTDS</td>
<td>Real-time digital simulator</td>
</tr>
<tr>
<td>SCFCL</td>
<td>Superconducting Fault Current Limiter</td>
</tr>
<tr>
<td>SSSFCL</td>
<td>Solid State Fault Current Limiters</td>
</tr>
<tr>
<td>TNSP</td>
<td>Transmission Network Service Provider</td>
</tr>
<tr>
<td>TRV</td>
<td>Transient Recovery Voltage</td>
</tr>
<tr>
<td>VT</td>
<td>Voltage Transformer</td>
</tr>
<tr>
<td>WTPG</td>
<td>Wind Turbine Power Generation</td>
</tr>
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</table>
Appendix A

Derivation of Analytical Nonlinear Reluctance Model of FCL

As described in Chapter 3 reluctance values of the magnetic circuit (shown in Figure A.1) representing an open core FCL geometry, can be derived by determining the flux linkage of each coil under three different test conditions [66]:

- Test 1 - $NI_{dc}$ is varied and $NI_{ac1} = NI_{ac2} = 0$
- Test 2 - $NI_{dc} = 0$ and $NI_{ac1} = NI_{ac2}$ (varied)
- Test 3 - $NI_{dc} = 0$ and $NI_{ac1} = -NI_{ac2}$ (varied)

![Figure A.1: Equivalent magnetic circuit of open core FCL adapted from [66]](image)

Test 1 and Test 2 are used to determine the values of $R_i$ and $R_o$, along with the series combination of $R_c + R_a$. Note that for Test 1 and Test 2 the resulting
Figure A.2: Equivalent circuit for (a) Test 1 (b) Test 2 (c) Test 3

Circuits are completely symmetrical and hence can be reduced to two separate sides that have identical reluctance and flux in each corresponding path (as shown in Figure A.2a and A.2b).

By applying standard circuit analysis techniques to the circuits in Figure A.2a and A.2b:

\[ R_i (\phi_{o1} - \phi_{c1}) = N I_{dc} - R_o \phi_{o1} \]  
(A.1)

and \( (R_c + R_a) = \frac{R_i (\phi_{o1} - \phi_{c1})}{\phi_{c1}} \)  
(A.2)

\[ R_i (\phi_{o2} - \phi_{c2}) = -R_o \phi_{o2} \]  
(A.3)

Where \( \phi_{o1} \) and \( \phi_{o2} \) are equal to half of the measured flux linkage of the DC coil for Test 1 and Test 2 respectively. \( \phi_{c1} \) and \( \phi_{c2} \) are equal to the measured flux linkage of one of the AC coils in Test 1 and Test 2 respectively.

Simultaneous solving of (A.1) and (A.3) results in:

\[ R_i = \frac{N I_{dc}}{\phi_{o1} - \phi_{c1} - \frac{\phi_{o2}}{\phi_{o2} - \phi_{c2}}} \]  
(A.4)

\[ R_o = \frac{N I_{dc}}{\phi_{o1} - \phi_{o2} \frac{\phi_{c1} - \phi_{c2}}{\phi_{o2} - \phi_{c2}}} \]  
(A.5)

Substituting \( R_i \) from (A.4) into (A.2):
Note that the magnitudes of the $\Re_i$ and $\Re_o$ are constant over the range of applied mmf, while the magnitude of $\Re_c$ and the series combination of $\Re_c + \Re_a$ are nonlinear.

Test 3 is then used to determine the individual values of $\Re_c$ and $\Re_a$, along with $\Re_y$. Similar to the previous two tests, the analysis can be limited to a single side of the Test 3 circuit (as shown in Figure A.2c) based on the symmetry of the circuit. In Figure A.2c, $\Re_p$ is the parallel combination of $\Re_i$ and $\Re_o$ (magnitudes of which were derived via Tests 1 and 2) and $\Re'_y$ is equal to one half of $\Re_y$.

By applying standard circuit analysis techniques to the circuit in Figure A.2c:

$$\Re_a = \frac{NI_{dc} (\phi_{o1} - \phi_{c1})}{\phi_{c1} [\phi_{o1} - \phi_{c1} - \frac{\phi_{o1}}{\phi_{c2}} (\phi_{o2} - \phi_{c2})]} \quad (A.6)$$

and can be re-arranged as:

$$\Re_a = \frac{(\Re_c + \Re_a) \phi_{c3} + \Re_p (\phi_{c3} - \phi_{y3}) - NI_{ac1}}{\phi_{y3}} \quad (A.7)$$

and

$$\Re'_y = \frac{(\phi_{c3} - \phi_{y3})}{\phi_{y3}} (\Re_a + \Re_p) \quad (A.9)$$

The previously determined value of $(\Re_c + \Re_a)$ from Test 1 is linear below the saturation region, and hence, at very low values of applied mmf, (A.6) and (A.8) can be used to calculate the magnitude of $\Re_a$.

The magnitude of $\Re'_y$ can then be determined using:

$$\Re'_y = \frac{(\phi_{c3} - \phi_{y3})}{\phi_{y3}} (\Re_a + \Re_p) \quad (A.9)$$

Note that the mmf acting on the series combination of $\Re_c + \Re_a$ is different in each of Tests 1, 2 and 3. Hence curves for $\Re_c$, over a range of applied mmfs, are
determined for each of the Tests 1, 2 and 3 using (A.10).

\[ R_c = R_{ca} - R_a \quad (A.10) \]

It was shown in [66] that, while \( R_c \) appears to have different saturation points when plotted against the source mmf of each test, when it was plotted against the component of mmf directly acting on the cores the resulting \( R_c \) values for each test were uniform.
Appendix B

Overview of Saturated Core FCL Design

As discussed in Chapter 4 of this thesis, saturated core FCL design process is a multi-variable optimisation problem that involves the use of both FEA and optimisation software to determine the optimal FCL design that would meet the performance specifications of the network under consideration.

The main electrical parameters used for the design are as follows:

- Line to line voltage ($V_{LL}$)
- Number of phases ($Ph$)
- Line frequency ($f$)
- Steady-state current ($I_{SS}$)
- Prospective unlimited peak (asymmetrical) fault-current ($I_{\text{fault-peak}}$)
- Prospective unlimited symmetrical fault-current ($I_{\text{fault-symm}}$)
- Maximum allowable voltage drop at steady-state ($V_{\text{drop-max}}$)
- Required peak limited current ($I_{\text{FCL-peak}}$)
- Required symmetrical limited current ($I_{\text{FCL-symm}}$)
The clipping parameters for the symmetrical and asymmetrical fault are estimated using (B.1) and (B.2), respectively.

Percentage clipping of symmetric fault,

\[
P_{\text{clip-symm}} = \frac{I_{\text{fault-symm}} - I_{\text{FCL-symm}}}{I_{\text{fault-symm}}} \quad (B.1)
\]

Percentage clipping of asymmetric peak fault,

\[
P_{\text{clip-peak}} = \frac{I_{\text{fault-peak}} - I_{\text{FCL-peak}}}{I_{\text{fault-peak}}} \quad (B.2)
\]

The required FCL fault impedance can be calculated using (4.3) where \( V_{LG} \) is the line to ground system voltage and \( Z_s \) is the source impedance.

\[
Z_{\text{FCL-fault}} = \frac{V_{LG}}{I_{\text{FCL-symm}}} - Z_s \quad (B.3)
\]

Similar to most FCL technologies, the actual FCL impedance during a fault event is not a constant for saturated core FCLs. Hence, the fault impedance of an FCL is typically defined as the equivalent steady-state impedance that would result in the same fault current limiting effect [41]. Based on this definition, the fault impedance of the FCL device can be expressed in terms of the percentage clipping of symmetric fault using (B.1):

\[
P_{\text{clip-symm}} = \frac{V_{LG}}{Z_s} - \frac{V_{LG}}{Z_s + Z_{\text{FCL-fault}}} \quad (B.4)
\]

Rearranging equation (B.4):
\[ P_{\text{clip-symm}} = \frac{1}{Z_s} - \frac{1}{Z_s + Z_{\text{FCL-fault}}} \]  
(B.5)

\[ \frac{1}{Z_s}(1 - P_{\text{clip-symm}}) = \frac{1}{Z_s + Z_{\text{FCL-fault}}} \]  
(B.6)

Therefore, if the required percentage clipping of symmetric fault and the system impedance is known, the fault impedance of the FCL device can be calculated using,

\[ Z_{\text{FCL-fault}} = Z_s \left( \frac{P_{\text{clip-symm}}}{1 - P_{\text{clip-symm}}} \right) \]  
(B.7)

The next step is estimating the number of AC coil turns and the core cross section area. According to Faraday’s law, the induced EMF is equal to the flux linkage variation in the AC coil as given in (B.8), where \( n_{ac} \) is the number of turns in each AC coil.

\[ EMF = -n_{ac} \frac{d\varphi}{dt} \]  
(B.8)

For the FCL to achieve the desired symmetrical clipping the EMF must be,

\[ EMF = V_{LG} \times P_{\text{clip-symm}} \]  
(B.9)

Assuming that a sinusoidal EMF is induced on each AC coil in one quarter cycle, the maximum Volts-seconds necessary to drive the cores out of saturation can be calculated from (B.10).
Volts – seconds \[ t = \frac{\pi}{4} \]
\[ V_{FCL} \int_{t=0}^{t=\frac{\pi}{2}} V \cos(\omega t) d\theta = \frac{EMF_{peak}}{\omega} \] (B.10)

Assuming the maximum EMF is induced by fully de-saturating the magnetic cores, and assuming that most of the flux changes in the steel core cross section, the Volts-seconds can also be given as (B.11),

\[ Volts – seconds = n_{ac} \int d\varphi = n_{ac} A_{core} \int dB \] (B.11)

From (B.10) and (B.11), \( n_{ac} A_{ac} \) can be estimated as,

\[ n_{ac} A_{core} = \frac{EMF_{peak}}{\omega \int dB} \] (B.12)

Using (B.12), a number of possible \( n_{ac} A_{core} \) combinations possible can be determined. The value of \( A_{core} \) however, is constrained by the mass and size considerations.

Another important consideration is determining the DC bias required to achieve the limiting specifications. FEA magnetostatic simulations are used to determine the ideal level of DC bias required based on insertion impedance curves. If the DC bias level is too large, it will deter the de-saturation of the iron cores during fault conditions. On the other hand, if the DC bias level is too small, it will increase the steady-state impedance leading to a higher voltage drop across the device during un-faulted conditions.

While it is possible to determine the FCL design parameters based on these calculations and FEA simulations, it is a tedious process and the chosen FCL design may not be the optimal solution. Therefore, to optimise the designs an optimisation software coupled with FEA is used to automate small changes in the FCL design.
parameters, with an overall goal of minimising both the steady state (pre-fault) voltage drop across the FCL and the fault current through the FCL.

The optimisation software processes several hundred iterations of pre-calculations and FEA simulations to generate a Pareto Frontier of possible design solutions. The Pareto Frontier is a curve that describes the most Pareto efficient solutions in terms of minimum voltage drop and minimum fault current. The geometric design parameters that are considered in the optimisation process included the height of the cores, the area of the cores, the number of turns of the AC coils and the DC bias requirements. While the optimisation process focuses on minimising the steady state voltage drop and the fault current, several other FCL parameters are calculated in each design iteration – including the mass, footprint and power consumption of the FCL. These calculations are also used to select the optimal FCL for a given set of specifications.
Appendix C

132 kV FCL Model and System Modelling

C.1 132 kV FCL model parameters

As discussed in Section 4.3, following the saturated core FCL design process described in Appendix B, an optimal FCL design that would meet the performance specifications of the network under consideration was determined. Once the FCL design was chosen, an FE model of the prototype FCL was developed in the CEDRAT Flux3D package using the geometric parameters of the device determined from the optimisation process as well as the electromagnetic properties of each material. CEDRAT Flux3D was subsequently used to perform magnetostatic simulations and to determine the equivalent electric circuit parameters of the FCL model. The resulting FE FCL model is shown in Figure C.1 with corresponding linear inductance values given in Table C.1. The $L - i$ characteristic curve that was used to represent the nonlinear core inductances are given in Figure C.2.
Figure C.1: Finite element model of 132 kV FCL ($A_{\text{core}} = 0.32 \text{m}^2$ and $H_{\text{core}} = 3.1 \text{m}$)

Table C.1: FCL model parameters

<table>
<thead>
<tr>
<th>Inductance</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$L_y$</td>
<td>353.12 $H$</td>
</tr>
<tr>
<td>$L_a$</td>
<td>91.85 $H$</td>
</tr>
<tr>
<td>$L_i$</td>
<td>16.13 $mH$</td>
</tr>
<tr>
<td>$L_o$</td>
<td>18000 $H$</td>
</tr>
</tbody>
</table>
Figure C.2: Inductance - current characteristic curve representing the nonlinear core inductances $L_{c1}$ and $L_{c2}$
C.2 System Modelling

C.2.1 Line Models

There are four line models available in the PSCAD/EMTDC master library to represent transmission line segments: equivalent $\pi$-section model, Bergeron model, Frequency Dependent (Mode) and Frequency Dependent (Phase). The Bergeron and frequency dependent models are based on travelling wave theory. Selection of an appropriate line model is dependent on the study requirements and the availability of data. Equivalent $\pi$-section models are typically used to represent transmission line segments of short lengths (i.e. less than 15 km for a 50 $\mu$s simulation time step). For transmission distances of longer lengths (where the travelling time is greater than the solution time step) use of an appropriate travelling wave line model is recommended by the PSCAD/EMTDC vendor. While the Frequency Dependent line models are the most accurate, these models are only necessary when frequency domain characteristics of the lines are of primary interest. For studies where the frequency dependency of the line models is not critical, and only the accurate representation of steady state impedance/admittance of the lines are of importance (i.e. transient or the harmonic behaviour of the lines are not the primary interest) the constant frequency Bergeron line model can provide adequate representation.

Since the switching studies to be performed within the scope of this research focus on low-frequency effects that are mostly localised, the Bergeron model was used to represent the 275 kV and 132 kV transmission lines of the Powerlink system.

C.2.2 Transformers

Transformers were modelled using three phase two-winding and three-winding transformer models available in the PSCAD/EMTDC and E-TRAN Libraries. PSS/E load flow data for transformer impedance, winding MVA, magnetising conductance
and susceptance were used for transformer models. The PSCAD/EMTDC transformer models also require an air core reactance, a knee point and a magnetising current for each transformer to represent saturation characteristics of the transformer. The air core reactance is the slope of the asymptote to the saturated part of the V-I characteristic, the knee point is the V-axis intercept to that asymptote, and the magnetising current sets a point on the curve at 1.00 pu voltage, as per Figure C.3. In the absence of transformer saturation data, typical values were assumed.

Figure C.3: Reproduced PSCAD saturation characteristic [73]
Appendix D

Circuit breaker TRV studies

D.1 Circuit Breaker modelling

The specific TRV characteristics for the Columboola 170 kV circuit breakers were unavailable. Instead, standard values for prospective TRV characteristics for breakers with a rated voltage of 170kV and a rated interrupting current of 40kA circuit breaker specified by IEEE Standard C37.06-2009 [98] were used for the studies.

D.2 Effective Capacitances of Station Equipment

As discussed in Section 6.2, circuit breaker TRV is predominantly a localised phenomenon. Hence, in EMT simulations a detailed representation of the substation of interest is necessary. This involved accounting for the effective stray capacitances and inductances of various substation equipment such as circuit breakers, disconnector switches, surge arrestors, transformers, bus VTs and series reactors. Where the capacitance values of station equipment were not available, the minimum recommended values in IEEE Standard C37.011-2011 [96] were adopted as given in Table D.1.
Table D.1: Effective capacitance of station equipment

<table>
<thead>
<tr>
<th>Device</th>
<th>Rating</th>
<th>Type</th>
<th>Stray Capacitance</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Min ($\rho F$)</td>
</tr>
<tr>
<td>Circuit Breakers</td>
<td>170 kV</td>
<td>Outdoor, Live, SF6</td>
<td>50</td>
</tr>
<tr>
<td>Disconnector switch</td>
<td>132 kV</td>
<td>Outdoor, Closed</td>
<td>60</td>
</tr>
<tr>
<td>Surge Arrester</td>
<td>132 kV</td>
<td>Outdoor, Closed</td>
<td>80</td>
</tr>
<tr>
<td>Voltage Transformer</td>
<td>132 kV</td>
<td></td>
<td>150</td>
</tr>
<tr>
<td>Transformer*</td>
<td>100 MVA</td>
<td></td>
<td>2000</td>
</tr>
</tbody>
</table>

*Note that these transformers at the station are of various voltage ratings at 100 MVA

Table D.2: TRV capabilities of circuit breakers at various interrupting levels for terminal faults

<table>
<thead>
<tr>
<th>Percent of interrupting capability</th>
<th>Multipliers for rated parameters</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$K_{u_1}$</td>
</tr>
<tr>
<td>100</td>
<td>1</td>
</tr>
<tr>
<td>60</td>
<td>1</td>
</tr>
<tr>
<td>30</td>
<td>2.05</td>
</tr>
<tr>
<td>45</td>
<td>1.525</td>
</tr>
</tbody>
</table>

D.3 Calculation of TRV capability at 45% test duty

The circuit breaker TRV capability envelope at 45% short-circuit interrupting current, was derived using a method of interpolation. This was achieved by deriving the multipliers for terminal fault at 45% test duty using the multipliers for 30% and 60% interrupting capabilities that were defined in [96]. The standard multipliers defined in [96] and the multipliers derived for 45% test duty are given in Table D.2.
The four parameters defining the T45 envelope can then be obtained as follows:

\[ u_{1-45\%} = K u_{1-45\%} \times u_{1-100\%} = 206 \text{kV} \]
\[ t_{1-45\%} = K t_{1-45\%} \times t_{1-100\%} = 51 \mu s \]
\[ u_{c-45\%} = K u_{c-45\%} \times u_{c-100\%} = 275 \text{kV} \]
\[ t_{2-45\%} = K t_{2-45\%} \times t_{2-100\%} = 97 \mu s \]