ASPECTS OF CIRCUIT BREAKER PERFORMANCE DURING HIGH VOLTAGE SHUNT REACTOR SWITCHING

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A THESIS SUBMITTED IN PARTIAL FULFILLMENT OF

THE REQUIREMENTS FOR THE DEGREE OF

MASTER OF APPLIED SCIENCE

in

THE FACULTY OF GRADUATE STUDIES

ELECTRICAL ENGINEERING

We accept this thesis as conforming

to the required standard

THE UNIVERSITY OF BRITISH COLUMBIA

March 1989

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Abstract

High voltage shunt reactor switching is a difficult circuit breaker duty. Severe reactor network insulation stresses can occur on breaker current chopping and even more so on breaker reignition. Predicting reactor switching transients is fundamental to assessing insulation concerns, and evaluating circuit breaker performance.

This work demonstrates measurement of circuit breaker interruption characteristics relevant to reactor switching, and their use in computer simulation of reactor switching transients. A technique for predicting circuit breaker reactor switching performance through simulation is also introduced and tested.
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Acknowledgement

The author gratefully acknowledges the guidance of Dr. H.W. Dommel, Dr. L.M. Wedepohl, and Dr. J.R. Marti of the Department of Electrical Engineering of the University of British Columbia in preparing this thesis.

Special thanks are also due Messrs' D.F. Peelo, B.L. Avent, J.H. Sawada, and J.K. Drakos of B.C. Hydro for their assistance, and to NSERC for their financial support.
Chapter 1

Introduction to Shunt Reactor Switching

Operation of extra high voltage transmission systems during light load periods gives rise to an excess of reactive power since circuits will typically be operating well below surge impedance loading. Reactive power generated by distributed line capacitances exceed that absorbed by distributed series inductances, and system voltages tend to rise. Voltage control requires reactive power absorption from the network or reduced reactive power production. Heavy loading conditions in contrast, require increased reactive power production to balance a tendency toward reduced system voltage levels.

A combination of various equipment and operating methods are used in modern power systems to achieve acceptable voltage profiles during the wide reactive power swings which occur with a normal range of loading conditions. These could include,

- Operation of generators as synchronous condensers during light loads
- Application of dedicated synchronous condensers
- Power transformer on load tap changer operations
- Switching shunt reactor or capacitor banks
- Removal of lightly loaded transmission circuits from service
- Static var compensators

The exact approaches applied depend as much on utility planning and operating philosophy as the nature of the load characteristics. B. C. Hydro, at the time of writing, uses all the above techniques except static var compensation.
In recent years, shunt reactors have become progressively more important in the control of B.C.Hydro's 500 kV system voltage levels. Typically, shunt reactor schemes are bus connected at one or both line terminations to compensate 60 – 65% of the associated distributed circuit shunt capacitance. Reactors are sometimes connected to the transmission network indirectly via medium voltage or tertiary windings of 500 kV/230 kV power transformers as shown in figure 1.1 where typical power system shunt reactor configurations are depicted.

Where frequent transmission connected reactor switching is anticipated, dedicated breakers allow switching without forcing the associated line out of service. Such devices need only be capable of interrupting normal reactor current as reactor faults are cleared by the circuit breakers in the line position. Where dedicated reactor breakers are not provided, the associated transmission line is commonly removed from service during light load conditions and line reactors switched into or out of service using a single line breaker. Line breakers and dedicated reactor breakers (if applied) must be capable of interrupting normal reactor currents which are typically below 200 A.

 Interruption of small inductive currents can impose a severe breaker duty even though full rated interrupting current can be more than 100 times larger. Shunt reactor and unloaded transformer switching are examples of small inductive current interruption frequently encountered in the course of power system operation. Transmission line connected shunt reactors may be switched as often as several times a day in the normal course of voltage control and this duty must be given careful consideration when specifying breakers for such applications.

 Due to an effect called current chopping, reactor breakers can force inductive load currents to zero in advance of a power frequency zero crossing. An oscillation develops in the interrupted network at 10-100 times above system frequency, during which overvoltages well in excess of 2 p.u. can develop with respect to ground or between phases. Further, due to the high network oscillation frequency, a rapidly rising recovery voltage develops across the opening breaker contacts. Should the opening interrupter withstand voltage be exceeded, abrupt arc current reignition will occur, generating high frequency transients. Reignition transients can not only
Chapter 1. Introduction to Shunt Reactor Switching

TERTIARY REACTOR

BUS REACTOR

12 KV TERTIARY VINDING

12 KV BREAKER

3 PHASE REACTOR

330 kV BUS

CIRCUIT SWITCHER

3 SINGLE PHASE REACTORS

LINE REACTOR

LINE BREAKERS

5 CB1

5 CB2

500 kV TRANSMISSION LINE

CIRCUIT SWITCHER

3 SINGLE PHASE REACTORS

Figure 1.1: Common Shunt Reactor Configurations
severely stress insulation with respect to ground, but resulting travelling wave propagation into reactor windings causes inter-turn stresses as well. Clearly, the nature of transients related to circuit breaker current chopping and reignition must be understood and their potential severity predictable, before breaker and insulation ratings are specified for reactor switching applications.

Circuit breaker current chopping and open interrupter dielectric withstand capability are not constants, but rather, are functions of arcing time during the interruption process. Current chopping is further a function of the reactor network circuit parameters as well as arc cooling effectiveness within the interrupting breaker. Dielectric withstand is a function of contact acceleration, and the dielectric properties of the insulating medium applied within the interrupter. The likelihood of reignition depends not only on the interrupter recovery voltage withstand capability as a function of time but on the rate of rise of recovery voltage (RRRV) on the interrupted reactor network. Successful interruption involves complex interactions between the circuit breaker characteristics and the network in which it is applied. While a breaker may never be called on to interrupt its full rated fault current, it is exposed to unique stresses on each associated reactor network interruption. Further, depending on breaker performance, insulation of the reactor and associated network devices (capacitive voltage transformers, bus insulators, etc.) may also be uniquely stressed.

This thesis presents the factors making shunt reactor switching such an onerous duty through discussion of the associated transient phenomena. Theoretical considerations are reinforced through presentation and analysis of several 500 kV shunt reactor switching tests. A method of characterizing breaker behavior during reactor switching is suggested. A technique for incorporating these characteristics into EMTP simulations is then proposed and tested as a tool for predicting transients and breaker performance in existing or tentative reactor network switching applications.
Chapter 2

Essential Reactor Switching Theory

This chapter introduces the transient phenomena which make reactor interruption a unique switching duty. Concepts are presented initially with reference to the single phase case. Additional considerations in switching practical three phase reactor networks are addressed in the following chapter.

Interruption of an AC current ideally takes place at a natural current zero. Practical circuit breakers rarely behave this way when interrupting small inductive currents. Large voltages developed following interruption can result in abrupt restoration of current flow if opening breaker contacts cannot withstand dielectric stresses. As a result, conduction frequently continues beyond the initial current zeroes following contact separation. Reactor network interruption typically produces unique transient overvoltages which cannot be neglected in assessing insulation requirements.

2.1 Arc Quenching and Current Chopping

Excepting semiconductor devices, all circuit breakers and switches in practical use work with some type of gas discharge following contact separation. Current continues to flow in a conducting gas between the open contacts in the form of an arc, until quenched by some interrupting mechanism. Electrical conductivity of the gas is maintained by thermal ionization, where arc temperatures in the order of 10,000 °K cause the gas to behave as a conducting plasma [24],[7]. Voltage dropped across the arc inputs power, tending to support high plasma temperatures. As the current approaches a natural zero crossing, arc diameter shrinks as current density remains approximately constant. Due to thermal inertia, the arc cannot cool instantaneously,
and a channel of hot conducting gas remains for a time following the current zero. Figure 2.2 shows typical arc conductivity as a function of temperature. If arc temperature remains high enough, arc conductivity will be sufficiently high for voltage across the open contacts to initiate a new half cycle of arc current. This is referred to as thermal reignition and leads to a smooth re-instatement of conduction. If cooled to below 2000 °K, the arc behaves as an insulator, preventing further conduction beyond the natural current zero.

Circuit breakers are frequently able to force arc current to zero in advance of a natural zero crossing through an effect called current chopping. The degree of current chopping during any particular interruption depends heavily on the breaker arc cooling mechanism, as well as the nature of the network being switched. Current chopping levels have an important influence on reactor network overvoltages and the transient recovery voltage (TRV) opening breaker contacts must withstand for successful interruption. Current chopping is the result of unstable interactions between the arc and the network external to the circuit breaker. Arc cooling mechanisms specific to various breaker types influence the onset of instability by controlling the rate at which, and the degree to which arc plasma conductivity is reduced during an attempted interruption.

2.1.1 Arc Dynamics and Instability

During successful interruption, an arc is rapidly transformed from a good conductor to a good insulator through virulent cooling. As the arc is cooled, abrupt changes in conductivity occur, causing current oscillations due to arc interactions with the network being interrupted. If arc cooling is sufficiently intense, oscillations can become unstable, producing high frequency current zeroes in advance of a natural zero. Interruption can occur at such a zero essentially forcing or chopping the power frequency current to zero prematurely. This effect is depicted in figure 2.3. Interactions between an electric arc, arc cooling mechanisms, and the network being interrupted leading to current chopping through arc instability have been studied in detail by Rizk [23],[22],[24]. His work led to a better understanding of arc and circuit breaker phenomena.
Chapter 2. Essential Reactor Switching Theory

Figure 2.2: Arc Conductivity as a Function of Temperature [7]
and much of the theoretical material commonly accepted at this time.

The experiments of Rizk and efforts of many others have shown that for small currents, circuit breaker arcs exhibit a static characteristic of the form:

\[ V I^\alpha = \eta \]  

where: 
- \( V \) is instantaneous arc voltage  
- \( I \) is instantaneous arc current  
- \( \alpha \) is a positive constant  
- \( \eta \) is a positive statistically random variable

Static arc resistance \( R_{so} \) may then be defined as

\[ R_{so} = \left[ \frac{V}{I} \right]_{I=I_o} = \eta I^{-(\alpha+1)} \]  

and dynamic arc resistance \( R_{do} \) as
Figure 2.4: Static Arc Characteristic With Static and Dynamic Resistances

\[ R_{do} = \left[ \frac{dV}{dI} \right]_{I=I_o} = -\alpha R_{so} \tag{2.3} \]

The static characteristic and arc resistances described by equations 2.1 to 2.3 are plotted in figure 2.4 for \( \alpha = 0.5 \) and \( \eta = 10,000 \). The dynamic arc resistance \( R_{do} \), becomes increasingly negative as current decreases, promoting instability as arc current approaches a natural current zero.

Rizk found that if perturbed by a small current step, the arc approached a new point on the static characteristic exponentially with a thermal time constant \( \theta \). The thermal time constant exhibited by any arc is heavily dependent on the arc cooling mechanism applied in the breaker being considered. Rizk further proposed that for such small perturbations the arc behavior could be modelled by either of the equivalent circuits of figure 2.5. For the parallel equivalent circuit:

\[ L = \frac{\theta R_{so}}{1 + \alpha} \]
Chapter 2. Essential Reactor Switching Theory

PARALLEL EQUIVALENT

\[ R_{so} \]

\[ L = \frac{\Theta R_{so}}{1+\alpha} \]

SERIES EQUIVALENT

\[ -\alpha R_{so} \]

\[ (1+\alpha)R_{so} \]

\[ (1+\alpha)\Theta R_{so} \]

Figure 2.5: Arc Equivalent Networks for Small Current Perturbations

\[ R_i = \frac{-\alpha R_{so}}{1+\alpha} = \frac{R_{do}}{1+\alpha} \quad (2.4) \]

where: 
- \( R_{so} \) is static arc resistance
- \( R_{do} \) is dynamic arc resistance
- \( \Theta \) is arc thermal time constant
- \( \alpha \) is as equation 2.1

Appendix A provides a brief proof of exponential arc response to small current perturbations and justification of the Rizk arc equivalent circuits.

The equivalent circuits of figure 2.6 are frequently used in discussion of single phase arc interaction with the network external to the breaker during interruption of small inductive currents. In practice, supply and load inductances are large enough that rapid arc current perturbations do not flow through them. The circuit breaker is often close to the reactor network so \( L_b \) is small and can be neglected. \( C \) representing \( C_s \) in series with \( C_r \), frequently
Figure 2.6: Circuits for Study of Arc Interaction with the Network

resolves to simply $C_r$ since typically $C_s \ll C_r$ due to the large CVT, CT, circuit breaker bushing and bus capacitances on the source side of the breaker. These lead to the reduced equivalent of figure 2.6 which can be used to a first approximation to consider arc response to a small perturbing current step $i$. The resulting transient arc current $i_a$ may be evaluated by applying KCL:

$$i(t) = \left( \frac{1}{R_{so}} + pC + \frac{1}{R_i + pL} \right) e(t)$$

where $p$ and $\frac{1}{p}$ represent differentiation and integration with respect to time. By substituting
Chapter 2. Essential Reactor Switching Theory

e(t) = \frac{ie}{pc} and noting \( pi = 0 \), the arc current is given by:

\[
\frac{d^2i_a}{dt^2} + \frac{di_a}{dt} \left[ \frac{R_i}{L} + \frac{1}{R_{so}C} \right] + \frac{R_{so} + R_i}{R_{so}LC} = 0
\]  \tag{2.5}

Assuming solutions of the form \( i_a = Ke^{\lambda t} \) yields a characteristic equation with a pair of complex conjugate roots for \( \omega^2 > \beta^2 \). The solution of equation 2.5 then has the following general form:

\[
i_a(t) = I_o e^{-\beta t} \left[ \cos \omega_d t + \phi \right]
\]  \tag{2.6}

\[
\omega_d = \sqrt{\omega_o^2 - \beta^2}
\]

\[
\beta = \frac{1}{2} \left[ \frac{R_i}{L} + \frac{1}{R_{so}C} \right]
\]

\[
\omega_o^2 = \frac{R_{so} + R_i}{R_{so}LC}
\]

where: \( \omega_d \) is damped natural frequency

\( \omega_o \) is natural frequency

\( \beta \) is the damping coefficient

\( I_o \) and \( \phi \) are determined by initial conditions at the time of perturbation

Arc current oscillations become unstable if damping coefficient \( \beta \leq 0 \). That is:

\[
\frac{R_i}{L} + \frac{1}{R_{so}C} \leq 0
\]  \tag{2.7}

Substituting the Rizk equivalents of equation 2.4 this reduces to:

\[
\frac{1}{R_{so}} \left[ \frac{R_{do}}{\theta} + \frac{1}{C} \right] \leq 0
\]  \tag{2.8}
\[ \theta \leq -R_{do}C \]

\[ \leq R_{so}C\alpha \]

\[ = \frac{C\alpha \eta}{I_0 + 1} \]

Arc instability is hence more likely as a current zero approaches and \( R_{do} \) grows increasingly negative. Note that since \( R_{do} \) becomes increasingly negative as a current zero approaches, \( \beta \) progressively decreases so that \( \omega^2 > \beta^2 \) will eventually hold. It is thus perfectly justified to have assumed complex conjugate roots for equation 2.5. Rizk [24] observed that thermal time constant was about 100 times smaller for arcs cooled by an air blast than free burning arcs of similar magnitude. Hence where forced arc cooling mechanisms are applied, the instability threshold described by equation 2.8 will be brought on by the combined effects of:

- reduced time constant \( \theta \) through increased cooling
- larger negative dynamic arc resistance \( R_{do} \) as current decreases towards a natural zero.

At the stability threshold \( \beta = 0 \), oscillation frequency is simply:

\[ \omega_i = \sqrt{\frac{R_{so} + R_i}{R_{so}LC}} = \sqrt{\frac{1}{(1 + \alpha)LC}} \tag{2.9} \]

and the arc behaves like a pure inductance. Instability frequencies as high as 105 kHz were measured in the air blast breaker experiments of Gardner and Irwin [9]. If the circuit breaker is able to interrupt at a high frequency current zero produced by unstable arc oscillations, power frequency current will appear to have been chopped prematurely to zero.

### 2.1.2 The Current Chopping Number

From the arc instability leading to current chopping depicted in figure 2.3, the current at onset of instability \( i_i \), is not exactly the same as the apparently chopped current \( i_{ch} \) since a finite time is required for high frequency current zeroes to develop from the instability. Published
test results suggest \( i_i - i_{ch} \) is not normally large. Gardner and Irwin [9] for example found the ratio \( \frac{i_i}{i_{ch}} \) ranged from 1.0 to 1.4 for widely varied inductive networks switched with an air blast breaker. It is generally accepted [1] that errors are small in assuming \( i_{ch} \approx i_i \). Chopping current can then be predicted from equation 2.8 re-arranged to give current at the onset of arc instability:

\[
i_{ch} \approx \left[ \frac{C \alpha \eta}{\theta} \right]^n
\]

\[n = \frac{1}{1 + \alpha}\]

Experimental work supports a close proportionality between \( i_{ch} \) and the square root of apparent network capacitance \( C \) for oil, air blast and SF\(_6\) circuit breakers [1],[17],[14]. This corresponds to equation 2.10 for the case \( \alpha = 1.0 \) yielding a constant power static arc characteristic. A constant of proportionality, \( \lambda_{ch} \) called the chopping number, may then be used to describe the current chopping behavior of a device during a particular interruption as:

\[
i_{ch} = \lambda_{ch} C^{1/2}
\]

\[\lambda_{ch} = \sqrt{\frac{\alpha \eta}{\theta}}\]

Circuit breaker chopping number depends on arcing time and is normally distributed in switching experiments where arcing time is held constant. These effects are due to the influence of arcing time on arc cooling intensity and the statistically random behavior of static arc characteristics between switching operations represented by the random variable \( \eta \) in equation 2.1.

2.1.3 Current Chopping Overvoltages

Current chopping during interruption of small inductive currents triggers a load side oscillation as magnetic energy stored at the moment of chopping is released into the reactor network. The
first and possibly several successive voltage peaks can be well in excess of 1.0 pu and must be considered in assessing insulation concerns. Load network parameters influence the oscillation frequency typically ranging from 0.5 – 10.0 kHz. For the purposes of predicting load side overvoltages, it is usually sufficiently accurate to assume current chopping occurs as an abrupt step.

Figure 2.7 shows an example reactor load side oscillation following 20 A current chopping in advance of a natural zero for the network of figure 2.9. The initial overvoltage peak is called the suppression peak and always has the same polarity as load voltage at the instant just prior to chopping. The second is of opposite polarity and is called the recovery peak. Voltage across the open breaker contacts during interruption is called the breaker transient recovery voltage (TRV), and will normally be maximum at the recovery peak for single phase and solidly grounded three phase reactors.

Load side oscillation following reactor interruption is controlled by the chopping current $i_{ch}$, and the values of reactor network elements. The network of figure 2.9 may be used to study the load side oscillation following current chopping for a single phase reactor. Analysis outlined in Appendix B gives load side voltage after current chopping as:

$$V(t) = V_m e^{-\beta_L t} \cos(\omega_d t - \psi) \quad (2.12)$$

$$V_m = \sqrt{V_{ch}^2 + \left(\frac{\beta_L V_{ch} - \frac{i_{ch}}{C}}{\omega_d}\right)^2}$$

$$\beta_L = \frac{1}{2} \left[ \frac{R_1 R_2}{(R_1 + R_2) L} + \frac{1}{C(R_1 + R_2)} \right]$$

$$\omega_d = \sqrt{\frac{R_2}{(R_1 + R_2) LC}}$$

$$\psi = \arctan \frac{1}{\omega_d} \left[ \beta_L - \frac{i_{ch}}{V_{ch} C} \right]$$

$$i_{ch} = \text{magnitude of current chopped at } t = 0$$
Figure 2.7: Load Side Oscillation in Figure 2.9 Network for 20 A Chopped Current

Figure 2.8: Breaker TRV for the Load Side Oscillation Above
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\[ R_1 = 2.85 \Omega \quad \text{REACTOR COPPER LOSSES} \]
\[ R_2 = 1.5 \, \text{M}\Omega \quad \text{CORE LOSSES AT LSO FREQUENCY} \]
\[ L = 5.41 \, \text{H} \quad \text{REACTOR INDUCTANCE} \]
\[ C = 9.8 \, \text{nF} \quad \text{REACTOR NETWORK CAPACITANCE} \]

Figure 2.9: Circuit for Analysis of Load Side Oscillation on Current Chopping

\[ V_{ch} = \text{magnitude of load voltage at instant of chopping} \]

From equation 2.12, the load side suppression peak occurs when \( \omega_d t = \psi \) and is given by:

\[ V_p = V_m \exp \left( -\frac{\beta_L \psi}{\omega_d} \right) \quad (2.13) \]

In most practical reactor networks, the damping term \( \beta_L \ll \omega_0 \) and very little error results in predicting the suppression peak as simply:

\[ V_p = \sqrt{V_{ch}^2 + \frac{i_{ch}^2 L}{C}} \quad (2.14) \]

Some authors [1] have used an energy conservation approach to predict \( V_p \), arguing the suppression peak represents when magnetic energy stored at the time of chopping is transferred to the capacitance such that:

\[ \frac{1}{2} C V_p^2 = \frac{1}{2} C V_{ch}^2 + \frac{1}{2} L \eta_m i_{ch}^2 \quad (2.15) \]
Magnetic efficiency $\eta_m$ accounts for energy losses in the inductor core. The frequency dependent resistance $R_2$ in figure 2.9 representing core losses is large for shunt reactors so $\eta_m \approx 1.0$. This is equivalent to neglecting damping and yields the same result as equation 2.14 using network analysis. A further simplification is often imposed by assuming $V_{ch} \approx V_s$ (system peak voltage) since current chopping normally takes place near a load current zero. Then a per unit suppression peak overvoltage factor may be defined from equation 2.15 as follows:

$$k_p = \sqrt{1 + \left[ \frac{i_{ch}}{V_s} \right]^2 \frac{L\eta_m}{C}}$$

(2.16)

Taking advantage of equation 2.11, equation 2.16 can be expressed in terms of the chopping number if known:

$$k_p = \sqrt{1 + \left[ \frac{\lambda_{ch}}{V_s} \right]^2 \eta_m}$$

(2.17)

Little error results from assuming $V_{ch} \approx V_s$ over a reasonably large range of chopping currents. A difficulty in applying equations 2.13 or 2.16 is that $R_2$ and hence $\eta_m$ are frequency dependent [25] and must usually be determined from a switching test where $\beta_L$ may be measured.

### 2.2 Dielectric Reignition and Related Transients

At the onset of load side oscillation, opening breaker contacts are stressed by the difference between load side and system voltages. If recovery voltage exceeds the withstand capability of the opening contacts, the arc will abruptly reignite and conduction continues across the open contacts. Reignition can involve large transfers of energy between source and load networks and the resulting high frequency transients can be exceptionally severe. Reignition transients can propagate as travelling waves, resulting in appreciable reactor inter-turn stresses in addition to expected insulation stress with respect to ground. The impact of reignition transients coupling
into substation control cables, protection and control systems, alarm systems, and communications equipment observed by the author and others, can be very undesirable. Dielectric reignition occurs due to inadequate dielectric strength of the contact gap following successful arc quenching. This must not be confused with the very different thermal reignition mechanism where the arc, having been insufficiently cooled, remains conductive through the natural current zero. Arc voltage reheats the arc invoking a new half cycle of current. Thermal reignition results in a smooth, virtually transient free, restoration of arc current which was really not completely quenched. From this point on unless otherwise mentioned, reignition shall refer to dielectric reignition.

2.2.1 Transient Recovery Voltage

The transient recovery voltage (TRV) across interrupting breaker contacts is simply the difference between load side oscillation and system side voltages following interruption. Using equation 2.12 for the single phase case, TRV may be expressed as follows:

\[ V_{TRV} = V_s \sin(\omega_s t + \gamma) - V_m e^{-\beta_L t} \cos(\omega_d t - \psi) \]  (2.18)

where:
- \( V_s \) is peak system voltage
- \( \omega_s \) is system angular frequency
- \( \gamma \) is system voltage angle at the time of chopping
- \( V_m, \beta_L, \omega_d \) and \( \psi \) are as defined for equation 2.12

The circuit breaker TRV resulting from the load side oscillation following 20 A current chopping on interrupting the network of figure 2.9 is shown in figure 2.8. As expected, the first TRV maximum occurs at the load side suppression peak, while the second and largest coincides with the load side recovery peak. Dielectric strength grows with time as the interrupting contact gap widens, and conductive arc by products recombine or are removed [7], [3], [14]. If RRRV exceeds the rate at which dielectric strength is established between the opening breaker contacts, dielectric reignition will occur. Largest RRRV usually occurs between the time of
current chopping and the load side recovery peak making reignitions most common in this interval. Clearly, the larger the TRV at reignition, the greater the resulting energy transfer between source and load networks. The severity of the resulting reignition overvoltages is also accordingly increased.

Rate of rise of recovery voltage (RRRV) and maximum TRV, depend on chopping current and load side network natural frequency. Devices capable of high current chopping levels will be exposed to large RRRV and TRV and have greater chance of reignition unless TRV is limited in some fashion.

2.2.2 Reignition Transients

As reignition occurs, load side and source side voltages are quickly equalized in an oscillatory exchange of energy. In practice, travelling waves will result due to the distributed nature of the source impedance and reactor network, however elements may be lumped for analysis to gain an understanding of reignition transient phenomena. Effects of distributed impedances will be investigated in Chapter 6 where reactor switching simulation results are presented.

There are three mechanisms considered to be predominant during a reignition, occurring in the following sequence:

- First parallel oscillation
- Second parallel oscillation
- Main circuit oscillation

Though these oscillations all begin at the moment of reignition, their frequencies differ by at least an order of magnitude. It is hence acceptable to consider each separately from the others [1] using the circuits of figure 2.10.

Each pole of a high voltage breaker consists of a number of series connected interrupters with parallel grading capacitor networks to help distribute TRV evenly between them. $C_p$ and $L_p$ represent the equivalent capacitance and stray inductance of the interrupter grading network.
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The first parallel oscillation is a rapidly damped oscillatory discharge of the energy stored in $C_p$ through $L_p$ and $R_{cb}$ representing arc resistance. Frequency of the first parallel oscillation is given approximately by:

$$f_p = \frac{1}{2\pi \sqrt{L_p C_p}}$$

and is in the order of 1 – 10 MHz. An adequate general understanding of reignition transients can be gleaned by neglecting the first parallel oscillation and considering only the simplified network of figure 2.10. Provided conductivity of the arc path remains sufficiently high, the breaker will be unable to interrupt the first parallel oscillation and the second parallel oscillation...
develops.

During the second parallel oscillation, energy exchange between source and load side capacitances, \( C_s \) and \( C_r \), will ultimately reduce voltage across the breaker to almost zero. \( L_b \) and \( R_b \) represent the bus between the reactor and circuit breaker causing a damped oscillatory energy exchange. \( R_b \) may also represent arc resistance. Second parallel frequency is typically in the range 100 kHz – 500 kHz and the oscillation short-lived. Accordingly, initial inductor currents \( i_s(0) \) and \( i_r(0) \) at onset of reignition remain practically constant during the second parallel oscillation. Analysis based on this assumption in Appendix C shows the second parallel damped natural frequency is:

\[
\omega_{d2} = \sqrt{\frac{\omega_p^2 - \beta_p^2}{L_b}}
\]

\[
\omega_p = \sqrt{\frac{C_s + C_r}{L_bC_rC_s}}
\]

\[
\beta_p = \frac{R_b}{2L_b}
\]

Generally it is considered valid to neglect damping in estimating the second parallel oscillation frequency [1],[16] which is then given by:

\[
f_{p2} \approx \frac{1}{2\pi} \sqrt{\frac{C_s + C_r}{L_bC_rC_s}}
\]

As outlined in Appendix C, the breaker current and load side voltage have the following forms during the second parallel oscillation for reignition at \( t = 0 \):

\[
i_b(t) \approx \left[ \frac{C_r i_s(0) + C_s i_r(0)}{C_s + C_r} \right] \left[ 1 - e^{-\beta_p t} \cos \omega_{d2} t \right]
\]

\[
+ \sqrt{\frac{C_s C_r}{L_b(C_r + C_s)}} \left[ V_s(0) - V_r(0) \right] e^{-\beta_p t} \sin \omega_{d2} t
\]

(2.22)
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\[ V_r(t) \approx \frac{C_s V_s(o) + C_r V_r(o)}{C_s + C_r} + \left[ \frac{i_s(o) - i_r(o)}{C_s + C_r} \right] t + \frac{C_s e^{-\beta_p t}}{C_s + C_r} \left[ (V_r(o) - V_s(o)) \cos \omega_{d2} t + \frac{1}{\omega_p} \left( \frac{i_s(o)}{C_s} + \frac{i_r(o)}{C_r} \right) \sin \omega_{d2} t \right] \] (2.23)

where: \( i_s(o), i_r(o), V_r(o), \) and \( V_s(o) \) are initial currents and voltages as defined in figure 2.10.

Extremely large overvoltages may be generated during the second parallel oscillation but the complexity of equation 2.23 does not make this immediately clear. Consider a reignition occurring when \( V_r \) and \( V_s \) are at peak values of opposite polarity. Currents \( i_r \) and \( i_s \) would then be approximately zero. It is clear from equation 2.18 that \( V_r(t) - V_s(t) \) can be well in excess of 2.0 pu at that instant depending on current chopping at the previous interruption. Assuming that \( C_s \gg C_r \) as is generally the case, load side voltage becomes:

\[ V_r(t) \approx V_s(o) + \left[ V_r(o) - V_s(o) \right] e^{-\beta_p t} \cos \omega_{d2} t \] (2.24)

With \( V_s \approx 1.0 \text{ pu}, V_r(t) \) shortly after reignition could reach over 3.0 pu since \( \omega_{d2} \gg \omega_s \) and \( \beta_p \ll \omega_{d2} \). Time of reignition with respect to load side oscillation hence alters reignition severity, the worst case being reignition near a recovery peak where \( V_r(t) - V_s(t) \) is maximum. Equation 2.24 further illustrates \( V_r(t) \) approaching \( V_s(0) \), equalizing system and load side voltages as the oscillation progresses. After a time \( t_d \) when sinusoidal terms have decayed, \( i_b(t) \) reaches a quasi steady state following a number of zero crossings. If the breaker is not able to interrupt the second parallel current, a main circuit oscillation begins at \( t' = 0 \) for \( t' = t - t_d \) with the following initial conditions:

\[ i_b(t_d) \approx \frac{C_r i_s(o) + C_s i_r(o)}{C_s + C_r} \] (2.25)

\[ V_r(t_d) \approx \frac{C_s V_s(o) + C_r V_r(o)}{C_s + C_r} + \left[ \frac{i_s(o) - i_r(o)}{C_s + C_r} \right] t_d \]
All elements of the simplified network in figure 2.10 are involved in energy exchanges during the main circuit oscillation. \( L_b \) is usually neglected in the analysis since it is much smaller than \( L_s \) or \( L_r \). Neglecting damping, main circuit oscillation frequency is given as shown in Appendix C by:

\[
f_m \simeq \frac{1}{2\pi} \sqrt{\frac{L_s + L_r}{L_s L_r(C_s + C_r)}}
\]

(2.26)

The main circuit oscillation begins with the initial conditions of equation 2.25, and as shown in Appendix C, the load side voltage during this period is given by:

\[
V_r(t) \simeq V_0 \sin(\omega_s + \psi) + [V_r(td) - V_0 \sin \psi] e^{-\beta_m t} \cos \omega_m t
\]

(2.27)

\[
+ \frac{1}{\omega_m} \left[ \frac{i_s(o) - i_r(o)}{C_s + C_r} - \omega_s V_0 \cos \psi \right] e^{-\beta_m t} \sin \omega_m t
\]

where:

\[
\omega_m = \sqrt{\frac{L_s + L_r}{L_s L_r(C_s + C_r)}}
\]

\[
\beta_m = \frac{1}{2R_r(C_s + C_r)}
\]

As expected, equation 2.27 shows the load side voltage follows the 60 Hz source voltage once the main circuit oscillation damps.

Breaker current during the main circuit oscillation from Appendix C has the following form:

\[
i_b(t) \simeq \left[ \frac{C_r L_r \omega_s^2 - 1}{\omega_s L_r} \right] V_0 \cos(\omega_s t + \psi) + \left[ i_r(o) + \frac{V_o \cos \psi}{\omega_s L_r} \right] e^{-\beta_m t}
\]

(2.28)

\[
+ \omega_m C_r [V_0 \sin \psi - V_r(td)] e^{-\beta_m t} \sin \omega_m t
\]

\[
+ \frac{C_r}{C_r + C_s} [i_s(o) - i_r(o) - \omega_s(C_s + C_r)V_0 \cos \psi] e^{-\beta_m t} \cos \omega_m t
\]

Depending on network parameters and initial conditions, \( i_b(t) \) may not cross zero and the breaker may not be able to interrupt the main circuit oscillation. Steady state 60 Hz current
will be re-established, and interruption attempted at the next zero crossing. Test waveforms depicting this common behavior will be presented in Chapter 5.

While reignitions are more likely following the suppression peak, they can occur sooner. Reignition before the suppression peak is most likely when time between initial contact part and current chopping (arcing time) is short. Contact separation is then insufficient to withstand the TRV produced by the suppression peak. Figures 2.11 and 2.12 show load side voltage and breaker current for an 850 kV reignition near the recovery peak for the network of figure 2.9 following a 20 A current chopping interruption. These were calculated from the relationships derived in Appendices B and C using the network of figure 2.10 with the following parameters estimated for a single phase of a 500 kV reactor network tested by the author:

\[
\begin{align*}
C_a &= 70,000 \text{ pF} & L_a &= 14.4 \text{ mH} \\
R_b &= 75 \Omega & L_b &= 0.2 \text{ mH} \\
R_1 &= 2.85 \Omega & R_2 &= 1.5 \text{ M\Omega} \\
C_r &= 9800 \text{ pF} & L_r &= 5.41 \text{ H}
\end{align*}
\]

Peak overvoltage is about 722 kV with peak current approaching 4000 A. The result would have been considerably more severe without \( R_b \) chosen to account for the moderating influence of arc resistance. Steady state 60 Hz current begins to grow as the main circuit oscillation damps.

Reactor switching is a duty for which essentially all circuit breakers experience reignitions to some extent. The recovery voltage at which reignition occurs depends on:

- The rate of rise of the transient recovery voltage. This is controlled by the reactor network load side oscillation frequency and current chopping levels which are a characteristic of the circuit breaker.

- The rate at which dielectric strength is established across the opening breaker contacts as the arc is quenched. This is also a characteristic of the circuit breaker.
Figure 2.11: Oscillation Voltages for 850 kV Reignition

Figure 2.12: Oscillation Currents for 850 kV Reignition
2.2.3 Virtual Current Chopping

Breaker current interruptions at a current zero of either of these oscillations will appear to the rest of the network as if power frequency current has been chopped. This effect is one form of a phenomenon referred to as virtual current chopping. A load side oscillation results with suppression peak related to apparently chopped current by equation 2.16. The current chopping is not brought on by arc instability and is not a manifestation of the conventional current chopping mechanism. It is simply an instance of interruption at a high frequency current zero. Reignition followed by virtual current chopping via interruption of the second parallel oscillation, can be repetitive with several successive reignitions and interruptions, until either 60 Hz current is re-established or oscillation interruption is successful.

2.2.4 Multiple Reignitions and Suppression Peak Escalation

As in the case of conventional current chopping, recovery voltage following virtual current chopping on interruption of second parallel oscillation, may exceed what the circuit breaker contacts can withstand. If so, reignition will recur exhibiting the previously described oscillation mechanisms. Depending on the circuit breaker it is then possible that:

- High frequency interruption will recur with the breaker successfully withstanding the resulting recovery voltage.

- Sixty hertz current will be re-established and interruption attempted at the next zero crossing.

- High frequency interruption is followed by multiple reignition/interruption events until successful interruption or 60 Hz current is re-established.

Multiple reignitions can lead to a condition called voltage escalation [1],[26]. It is simply an escalation in suppression peak magnitude at each interruption in a multiple reignition sequence. Stored load side energy following reignition current interruption generally differs from that
stored before reignition due to a partial energy exchange. If stored load energy is greater at interruption of reignition current than before the previous reignition, the resulting suppression peak will be larger than its predecessor. Interruption of second parallel currents, multiple reignitions, and hence voltage escalation can only occur with breakers capable of quenching high frequency currents.

Multiple 60 Hz reignitions are also possible, occurring most commonly where:

- Contacts part so slightly in advance of a natural zero, that breaker recovery voltage withstand capability at both the first and second current zero is exceeded.

- Circuit breakers incapable of reliable reactor switching may reignite at several successive 60 Hz current zeroes before clearing if at all.

Breaker current chopping capability increasing with arcing time is frequently the cause of successively larger suppression peaks during multiple 60 Hz reignitions.
To this point, discussion has focused on single phase reactor switching. Current chopping, reignition, and associated transients are complex and are best understood by first considering the single phase case, and then extending principles to three phase networks.

Although similar to single phase phenomena already presented, transients related to three phase reactor network switching can be complicated by phase interactions. The extent of electrical coupling between phases depends heavily on the nature of the load network and in general, inter-phase coupling on the source side of the breaker has a relatively minor influence. Both capacitive and inductive coupling can affect reactor switching transients to varying extents and must be considered. As each phase of the load network is successively interrupted, phase interactions can include:

- Load side oscillation voltages and hence breaker recovery voltages are influenced by the load side oscillations or reignition transients of adjacent phases.

- Interruption processes on one or more adjacent phases are influenced by reignition in one particular phase. Virtual current chopping brought on by an adjacent phase reignition is a prime example of this interaction form.

The extent to which transient currents and voltages couple to adjacent phases is highly dependent on the ratios of zero sequence to positive sequence reactance and admittance ($X_0 / X_1$ and $Y_0 / Y_1$) in the reactor network. Analytical treatment of interruption transients is much more complicated than the single phase case due to the number of reactive network elements and the associated initial conditions required for solution. Further, solution must be performed in three
steps as each phase is successively interrupted. Analytic solutions for three phase network transient recovery voltage have been derived by Van Den Heuvel [26] in considerable detail using lumped parameter network models with both capacitive and inductive phase coupling and analytical solutions for the three phase case will not be considered here in detail. The origin and general nature of the most important phase interactions will be examined briefly and their practical effects outlined.

3.1 General Three Phase Reactor Load Side Oscillation

Where the phases of a reactor network are coupled, load side oscillation on any particular phase is affected by those of the other phases. The first interrupting phase produces a load side oscillation of the same form as equation 2.12 for the single phase case but of lower frequency. As the other phases interrupt, double frequency oscillations involving all phases and new natural frequencies result. The extent of energy transfer to adjacent phases depends on the type and degree of coupling as well as initial conditions at each successive phase interruption. As will later be shown, even small amounts of capacitive coupling can lead to double frequency load side oscillations. Chopping current is usually largest in the last phase to clear, leading to the largest chopping overvoltages in the interruption sequence. Figure 3.13 gives an example of load side oscillation interaction during interruption of a three phase network with small capacitive coupling tested by the author.

A general three phase reactor network is represented in figure 3.14. $L_\phi$ represents the self inductance of each phase reactor while $L_n$ represents a neutral reactor inductance when present. $M$ represents mutual inductance between the network phases including both reactor and associated busses. $C_g$ and $C_f$ represent the phase to ground and phase to phases capacitances contributed by busses, CVT, CT, breaker insulators, bus insulators and surge arresters primarily. This model assumes for simplicity that the network impedances and admittances are balanced though this is generally not the case.
Figure 3.13: Three Phase Reactor Load Side Oscillation with Light Capacitive Coupling

Figure 3.14: General Three Phase Reactor Network
Mutual inductance can be referred to the neutral to form the equivalent network of figure 3.15. From the analysis given in Appendix D, the referred mutual equivalent of figure 3.15, and solidly grounded equivalent representation of figure 3.16 behave identically if:

\[ L_g = 3L_N + L_p \]

\[ = L_\phi + 3L_n + 2M \]

\[ L_l = 3L_p + \frac{L_p^2}{L_N} \]

\[ = [L_\phi + 3L_n + 2M] \left[ \frac{L_\phi - M}{L_n + M} \right] \]

Load side oscillations may now be considered by including the effects of capacitances and damping resistances in the grounded equivalent network of figure 3.16. Assuming all phases have interrupted, no sources are connected and using operational notation, nodal analysis leads to:

\[
\begin{bmatrix}
  D_s & D_m & D_m \\
  D_m & D_s & D_m \\
  D_m & D_m & D_s
\end{bmatrix}
\begin{bmatrix}
  V_A \\
  V_B \\
  V_C
\end{bmatrix}
= \begin{bmatrix}
  I_A \\
  I_B \\
  I_C
\end{bmatrix}
= \begin{bmatrix}
  0 \\
  0 \\
  0
\end{bmatrix}
\]

(3.30)

Where \( D_s \) and \( D_m \) are given by:

\[ D_s = p(2C_l + C_g) + \frac{1}{p} \left( \frac{1}{L_g} + \frac{2}{L_l} \right) \]

\[ D_m' = -\left( pC_l + \frac{1}{pL_l} \right) \]

Since the system of differential equations is symmetrical, it may be decoupled by applying the
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3. Switching Three Phase Reactor Networks

\[ L_P = L_\phi - M \]
\[ L_N = L_n + M \]

Figure 3.15: Referred Mutual Equivalent Three Phase Reactor Network

\[ L_q = L_\phi + 3L_n + 2M \]
\[ L_l = L_q \left( \frac{L_\phi - M}{L_n + M} \right) \]

Figure 3.16: Solidly Grounded Equivalent Three Phase Reactor Network
Fortescue transformation\(^1\) to phase voltages:

\[
\begin{bmatrix}
V_0 \\
V_1 \\
V_2
\end{bmatrix}
= \frac{1}{3}
\begin{bmatrix}
1 & 1 & 1 \\
1 & a & a^2 \\
1 & a^2 & a
\end{bmatrix}
\begin{bmatrix}
V_A \\
V_B \\
V_C
\end{bmatrix}
\]

and identically to phase currents to decouple the system into positive, negative and zero modes as follows:

\[
\begin{bmatrix}
D_s + 2D_m & 0 & 0 \\
0 & D_s - D_m & 0 \\
0 & 0 & D_s - D_m
\end{bmatrix}
\begin{bmatrix}
V_0 \\
V_1 \\
V_2
\end{bmatrix}
= \begin{bmatrix}
I_0 \\
I_1 \\
I_2
\end{bmatrix}
= \begin{bmatrix}
0 \\
0 \\
0
\end{bmatrix}
\] (3.31)

Noting that the positive and negative mode differential equations are identical, two natural oscillation modes exist governed by the decoupled positive and zero mode differential equations:

\[
\frac{d^2V_1}{dt^2} + \frac{dV_1}{dt} \frac{1}{R_g(3C_l + C_g)} + \frac{(3L_g + L_l)V_1}{L_lL_g(3C_l + C_g)} = 0
\] (3.32)

\[
\frac{d^2V_0}{dt^2} + \frac{dV_0}{dt} \frac{1}{R_gC_g} + \frac{V_0}{C_gL_g} = 0
\] (3.33)

In practice, reactor network damping will be light and oscillatory solutions of the following forms are expected:

\[
V_1(t) = K_1 e^{-\beta_1 t} \cos(\omega_1 dt + \phi_1)
\] (3.34)

\(^1\)Fortescue \(\frac{1}{3} \cdot \begin{bmatrix}
1 & 1 & 1 \\
1 & a & a^2 \\
1 & a^2 & a
\end{bmatrix}\) or Clark \(\frac{1}{3} \cdot \begin{bmatrix}
1 & 1 & 1 \\
2 & -1 & -1 \\
0 & \sqrt{3} & -\sqrt{3}
\end{bmatrix}\) transformations could equally well be applied as the rows of either are eigen vectors of the characteristic matrix in equation 3.30. Either will thus produce the decoupled system of equation 3.31.
\[ \beta_1 = \frac{1}{2R_g(3C_l + C_g)} \]

\[ \omega_1 = \left[ \frac{3L_g + L_l}{L_l L_g(3C_l + C_g)} \right]^\frac{1}{2} \]

\[ \omega_{1d} = \sqrt{\omega_1^2 - \beta_1^2} \]

where: \( \beta_1 \) is positive mode damping coefficient

\( \omega_1 \) is positive mode natural frequency

\( \omega_{1d} \) is positive mode damped natural frequency

\( K_1 \) and \( \phi_1 \) are determined by positive mode initial conditions. The negative mode solution form will be the same but with constants \( K_2 \) and \( \phi_2 \) depending on negative mode initial conditions. Then the zero mode solution has the following form:

\[ V_b(t) = K_0 e^{-\beta_0 t} \cos(\omega_{0d} t + \phi_0) \]  \hspace{1cm} (3.35)

\[ \beta_0 = \frac{1}{2R_g C_g} \]

\[ \omega_0 = \left[ \frac{1}{L_g C_g} \right]^\frac{1}{2} \]

\[ \omega_{0d} = \sqrt{\omega_0^2 - \beta_0^2} \]

where: \( \beta_0 \) is zero mode damping coefficient

\( \omega_0 \) is zero mode natural frequency

\( \omega_{0d} \) is zero mode damped natural frequency

\( K_0 \) and \( \phi_0 \) are determined by zero mode initial conditions.
Transforming back to phase quantities, voltages become weighted sums of the positive, negative, and zero mode voltages. The general form of a resulting phase voltage neglecting damping could be written:

\[
V(t) = A_0 \cos(\omega_0 t + \phi_0) + A_{12} \cos(\omega_1 t + \phi_{12})
\]

\[
= 2A_0 \cos \left[ \frac{(\omega_0 + \omega_1)}{2} t + \frac{\phi_0 + \phi_{12}}{2} \right] \cos \left[ \frac{(\omega_0 - \omega_1)}{2} t + \frac{\phi_0 - \phi_{12}}{2} \right]
\]

\[
+ (A_{12} - A_0) \cos(\omega_1 t + \phi_{12})
\]

where \( A_{12} \) and \( \phi_{12} \) have absorbed the positive and negative modes into one term. The first term of equation 3.36 produces a modulated load side oscillation, manifesting the sum and difference of the mode frequencies. Substituting into the mode frequency expressions for \( L_g \) and \( L_l \):

\[
\omega_0 = \left[ \frac{1}{C_g(L_0 + 3L_n + 2M)} \right]^{\frac{1}{2}}
\]

\[
= \left[ \frac{1}{L_0 C_0} \right]^{\frac{1}{2}}
\]

\[
\omega_1 = \left[ \frac{1}{(3C_l + C_g)(L_0 - M)} \right]^{\frac{1}{2}}
\]

\[
= \left[ \frac{1}{L_1 C_1} \right]^{\frac{1}{2}}
\]

where the positive and zero mode inductances and capacitances are given by:

\[
L_1 = L_0 - M
\]

\[
L_0 = L_0 + 3L_n + 2M
\]

\[
C_1 = 3C_l + C_g
\]

\[
C_0 = C_g
\]
The ratio of the mode frequencies may then be expressed as:

\[
\frac{\omega_1}{\omega_0} = \left[ \frac{L_0C_0}{L_1C_1} \right]^{\frac{1}{2}} = \left[ \frac{X_0Y_0}{X_1Y_1} \right]^{\frac{1}{2}}
\]  

(3.41)

The effects of phase to phase capacitance, mutual inductance, and neutral inductance on load side oscillation, all of which were not present in the single phase case, can be clearly demonstrated using equations 3.37, 3.38 and 3.41.

### 3.1.1 Three Single Phase Solidly Grounded Reactors

The grounded Y connection of three single phase reactors is commonly used in line shunt compensation applications by B.C. Hydro. In this case, the reactors are not magnetically coupled. Mutual inductance in the reactor network busses is generally small compared to the reactor phase inductance and \( M \) may be assumed zero. Since the bank is solidly grounded, \( L_n \) is zero and the following relationships result:

\[
\frac{X_0}{X_1} = 1.0
\]

\[
\omega_0 = \left[ \frac{1}{C_gL_\phi} \right]^{\frac{1}{2}}
\]

(3.42)

\[
\omega_1 = \left[ \frac{1}{(3C_l + C_g)L_\phi} \right]^{\frac{1}{2}}
\]

(3.43)

If air insulated busses connect the reactor to the associated circuit breaker, \( C_l \ll C_g \) due to relatively large instrument transformer and surge arrester capacitances. As a result, \( \frac{\omega_1}{\omega_0} \approx 1.0 \), and \( \frac{\omega_1}{\omega_0} \approx 1.0 \). Modulation of load side oscillation is accordingly slow and is entirely due to capacitive coupling via \( C_l \). As will be observed in Chapters 5 and 6, modulation can be pronounced even with \( C_l \ll C_g \). Slow phase interactions during load side oscillation means suppression and recovery peaks will not be significantly influenced by adjacent phases and it is generally accepted this case may be treated as three individual single phase reactors [1]. The suppression peak for each phase could then be predicted using equation 2.14 as:
\[ K_p \simeq \sqrt{1 + \left( \frac{i_{ch}}{V_a} \right)^2 \frac{L_\phi}{C_g}} \] (3.44)

where \( i_{ch} \) is the chopped current, and \( V_a \) the peak system voltage. The three phase reactor VA rating is:

\[ Q = \frac{3V_s}{\sqrt{2}} \left[ \frac{V_s}{\sqrt{2}\omega_sL_\phi} \right] \]
\[ = \frac{3V_s^2}{2\omega_sL_\phi} \]

and recalling from Chapter 2 that \( i_{ch} = \lambda_{ch} \sqrt{C_g} \), the suppression peak may be expressed as simply a function of breaker chopping number and reactor rating:

\[ K_p \simeq \sqrt{1 + \frac{3\lambda_{ch}^2}{2Q\omega_s}} \] (3.45)

Equations 3.44, and 3.45 provide a means of assessing suppression peak in the simplest solidly grounded three phase case. However, if \( C_l \ll C_g \), as where cables or very long air insulated busses connect the reactor network to a circuit breaker, validity of single phase treatment may fail. Further, single phase equations offer no insight into the effects of phase interactions beyond the first cycle of load side oscillation or during reignition.

### 3.1.2 Single Tank Three Phase Solidly Grounded Reactors

Three phase reactors are often constructed on a common core and housed in a single tank. Depending on core geometry and winding arrangement, mutual inductance can vary significantly. A knowledge of the mutual inductance is necessary to properly assess load side oscillation.

Core geometries may result in \( M \ll L_\phi \) and little error will result in treating the reactor as in section 3.1.1 after careful consideration. Common construction methods result in negative reactor mutual inductance for the sense of \( M \) shown in figure 3.14. Since the bank is solidly grounded:
\[
\frac{X_0}{X_1} = \frac{L_\phi + 2M}{L_\phi - M} \leq 1.0
\]

\[
\omega_0 = \left[ \frac{1}{C_g(L_\phi + 2M)} \right]^{\frac{1}{2}} \tag{3.46}
\]

\[
\omega_1 = \left[ \frac{1}{(3C_i + C_g)(L_\phi - M)} \right]^{\frac{1}{2}} \tag{3.47}
\]

With respect to the same network with \( M = 0 \), \( \omega_1 \) decreases, and load side oscillation modulation will be more rapid. TRV and RRRV may be larger than for \( M = 0 \) and equations 3.44 or 3.45 cannot be applied with confidence where \( M \) is not negligible.

### 3.1.3 Three Phase Reactor Networks With Neutral Reactor

Four reactor schemes with \( L_\phi > L_n \) are commonly applied in shunt compensation of transmission lines protected by single pole tripping relaying systems. Figure 3.16 demonstrates the equivalent phase to phase inductance \( L_i \) resulting intentionally from this connection to compensate the capacitive coupling between transmission line phases which hinders single phase fault clearing. With discrete phase reactor tanks mutual inductance \( M = 0 \) and:

\[
\frac{X_0}{X_1} = 1 + \frac{3L_n}{L_\phi} > 1.0
\]

\[
\omega_0 = \left[ \frac{1}{C_g(L_\phi + 3L_n)} \right]^{\frac{1}{2}} \tag{3.48}
\]

\[
\omega_1 = \left[ \frac{1}{(3C_i + C_g)L_\phi} \right]^{\frac{1}{2}} \tag{3.49}
\]

For a B.C. Hydro four reactor scheme considered by the author in Chapter 5, \( L_n \approx 0.5L_\phi \) and \( \frac{X_0}{X_1} \approx 2.5. \)

In effect, the neutral reactor generates positive inductive coupling between phases of the reactor network. Zero mode natural frequency is substantially reduced and \( \frac{\omega_1}{\omega_0} \) increased over solid grounding of the same network. The difference between mode frequencies \( |\omega_1 - \omega_0| \) will
be larger. More rapid load side oscillation modulation can thus be expected over the solidly grounded case. A significant increase in TRV and RRRV can result with a neutral reactor due to phase currents interrupting at different times as will be demonstrated in section 3.1.5. Equations 3.44 and 3.45 cannot be confidently applied.

### 3.1.4 Ungrounded Y Connected Three Phase Reactors

An ungrounded reactor connection corresponds to the case where \( L_n \) approaches \( \infty \) and \( C_g \) connects to the neutral bus of figure 3.16. In this instance, \( C_g \) will not include instrument transformer or bus capacitances since they exist with respect to ground. \( C_g \) will rather represent a potentially small effective capacitance to neutral of the reactor winding and busses. Phase to ground capacitances in the network will generate phase to phase capacitance contributions increasing \( C_l \) such that \( \frac{1}{\sqrt{3}} < 1.0 \) typically. Reactors of this type are frequently constructed on a common core and mutual inductance can be significant. Allowing \( L_n \) to approach \( \infty \):

\[
\frac{X_0}{X_1} = \infty
\]
\[
\omega_0 = 0
\]
\[
\omega_1 = \left[ \frac{1}{(3C_l + C_g)(L_\phi - M)} \right]^\frac{1}{2}
\]

Equations 3.50 and 3.51 imply that zero mode oscillation cannot exist.

An ungrounded connection imposes very strong inductive coupling between phases of the reactor network as indicated by setting \( L_n \to \infty \) in equation 3.29 yielding:

\[
L_l = 3(L_\phi - M)
\]
\[
L_g = \infty
\]

This represents in effect, the network of figure 3.16 with only phase to phase inductances and
hence strong inductive coupling. Phase interactions are very pronounced during interruption and equations 3.44 and 3.45 cannot be applied.

3.1.5 Neutral Offset Due to Staggered Clearing of Phase Currents

Due to steady state reactor currents being out of phase, each pole of the breaker interrupts at a different time. In the case of reactors which are not solidly grounded or where mutual inductance is not negligible, a neutral offset voltage is imposed as the first and second poles of the breaker interrupt.

Consider the simple network of figure 3.17 where the first phase has interrupted. By superposition the neutral voltage is:

\[ V_{N1} = -\tilde{V}_1 \frac{L_N}{2L_N + L_p} \]

\[ = -\tilde{V}_1 \frac{M + L_n}{2L_n + M + L_\phi} \tag{3.53} \]

where \( \tilde{V}_1 \) is the source phasor voltage of the first interrupted phase. When the second phase clears, as shown in figure 3.17, \( V_N \) is offset to a new value:

\[ V_{N2} = \tilde{V}_3 \frac{L_N}{L_N + L_p} \]

\[ = \tilde{V}_3 \frac{L_n + M}{L_\phi + L_n} \tag{3.54} \]

where \( \tilde{V}_3 \) is the source phasor voltage of the remaining uninterrupted phase.

Both the effective load side oscillations and recovery voltages of the first two interrupting phases are offset to levels dependent on the nature of the reactor network. For the two special cases considered in Chapters 5 and 6:

1. Solidly grounded reactors with \( M = 0, V_{N1} = V_{N2} = 0 \) and neutral offset is zero.
2. Four reactor schemes with $M \approx 0$, $V_{N1} = -V_1 \frac{L_n}{2L_n+L_p}$, $V_{N2} = V_3 \frac{L_n}{L_p+L_n}$, $V_{N1} < \frac{V_1}{2}$. First and second phase suppression peaks are reduced by the neutral offset, while recovery peaks are increased. Maximum breaker TRV is thus increased by neutral offset voltages for the first and second interrupting phases. Suppression peaks will no longer represent the largest phase to ground voltages during load side oscillation.

Figure 3.17: Neutral Voltage Offset on Staggered Phase Interruption
3.2 Phase Interactions on Reignition

Current chopping in the first or subsequent phase to interrupt frequently leads to reignition. As in the single phase case, reignition involves second parallel and possibly main circuit oscillations. An important difference from the single phase case is all capacitive and inductive circuit elements adjacent to the reigniting phase breaker pole are involved in energy exchange during reignition oscillations. Oscillation current in the reigniting phase may hence be partly sourced by adjacent phases which have not yet interrupted. Further a large reignition current may couple voltages onto, and alter currents in, adjacent phases. In either case, a high frequency component is superimposed on the 60 Hz currents of uninterrupted phases. Should either of the resulting phase currents pass through zero, they may be interrupted. To the network, this appears as though power frequency current has been chopped to zero. This is an alternate form of virtual current chopping to that described in the previous chapter and is only possible in poly-phase systems. Though not frequently observed, it is most likely to occur where interphase coupling is pronounced as in the case of ungrounded three phase reactor networks. Transient voltages coupled to adjacent phases on reignition were observed to some degree in field testing of a solidly grounded three phase reactor network with weak capacitive coupling.

3.3 Predicting Three Phase Reactor Network Switching Transients

It useful to be able to predict reactor switching transients in order to:

1. Estimate maximum phase to ground voltages during load side oscillation to assess insulation concerns.

2. Evaluate recovery voltages to determine whether the circuit breaker can withstand the reactor interruption duty.

3. Gain an appreciation of transient voltages and currents in both reigniting and adjacent phases.
4. Determine whether interphase coupling influences network transients significantly.

Considerable efforts have been directed at calculation of transients which might be experienced on interruption of three phase reactor networks. These can be broadly categorized as analytical approaches, or computer simulation using facilities such as EMTP. Each approach has merits and choice of methods will depend on the network being considered, and the phenomena of concern.

3.3.1 Considerations in Analytical Approaches

The complexities of approaching three phase reactor network switching analytically are well known and have not been covered here in detail. Considerable efforts have been expended in this area [26]. Most commonly, classical time domain or frequency domain solution methods have been applied using eigen vector techniques to decouple and simplify solution of three phase differential equations.

Application of analytical methods to the three phase reactor switching problem has several advantages including:

- Insight into effects of various coupling types, network grounding methods, or changes in network parameters can be readily gained by studying the form of the analytic solutions for various network configurations.

- Transients can be understood by simply choosing an appropriate network solution.

- No knowledge of simulation facilities such as EMTP is necessary.

However, there are clear limitations in many cases which must be understood before considering analytical solution:

- Interruption of each successive phase must be individually formulated and solved applying initial conditions which will be influenced by the solution for the previous interruption.
• Studying the effect of alternate coupling types or grounding methods requires derivation of separate solutions.

• Published literature primarily emphasizes solution only of load side oscillation transients following interruption. Analytical solution of reignition transients for three phase networks is very complicated and little seems to have been published on the subject.

• Treatment of constituent reactor network components as lumped elements introduces errors where long bus sections, cables, or other distributed elements are present. Errors will be even more significant when considering reignitions.

• Non-linear devices such as surge arresters, commonly applied in reactor network insulation protection, are not easily handled by analytical methods.

Van Den Heuvel [26] has provided concise lumped model analytical solutions for load side oscillations in grounded three phase reactors with various coupling forms. This work will be very useful in consideration of networks where lumped modelling is sufficiently accurate and non-linear elements need not be considered.

It would be useful to not only predict reactor switching transients, but also to consider the influence of circuit breaker characteristics on those transients and judge how well a breaker will perform in a specific reactor switching application. This requires consideration of breaker current chopping and reignition characteristics in concert with the transients generated by current chopping and reignitions. Analytical methods with their associated complexity, are not well suited to this problem.

3.3.2 Computer Simulation Considerations

Usefulness of the EMTP in simulation of power system transients has long been established. For the most part, EMTP accuracy is limited only by the validity of the network models to which it is applied. As a result of various research efforts, [1], [26], [18], guidelines regarding necessary modelling detail for digital computer simulation of reactor switching have been suggested.
There are several advantages in using simulation facilities such as EMTP to study reactor switching:

- Effects of altering the network in any way can usually be handled without completely reformulating the problem.
- Reignitions and resulting transient effects can be incorporated.
- Successive phase interruptions need not be considered in separate steps.

Use of the EMTP in this context is however not without limitations and consideration should be given to the following:

- Influence of different coupling types and grounding methods can only be confidently assessed by performing numerous simulations.
- Considerable care must be taken in assessing the required modelling detail. This may not be easy, especially when dealing with the high frequency transients associated with reignition.
- Influential parameters such as reactor core losses and bus impedances are frequency dependent and can be difficult to estimate.
- EMTP is constrained to use of a fixed time step for the full duration of a study. Since reignition transients are much higher frequency than the breaker recovery voltage causing reignition, a smaller time step must be used in simulating reignition than load side oscillation. This requires separate simulation of load side oscillation to determine initial conditions prior to reignition, followed by simulation of the reignition at a reduced time step, incorporating the initial conditions. This process can be cumbersome.

Methods for incorporating time dependent circuit breaker characteristics which influence switching transients and hence the likelihood of successful interruption into EMTP simulations,
would help to make results more realistic. Circuit breaker characteristics relevant to reactor switching are presented in the following chapter. A technique for incorporating empirical breaker characteristics into reactor switching computer simulations will be presented in Chapter 6 with emphasis on predicting circuit breaker performance.
Chapter 4

Breaker Characteristics Relevant to Reactor Switching

High voltage circuit interruption is a formidable task and ever increasing system voltage levels and protection speeds have called for application of progressively more sophisticated circuit breaker technologies. Today, available circuit breaker types include:

- Oil devices up to 550 kV.
- Air blast devices up to 1100 kV.
- Compressed gas (Sulphur Hexafluoride) devices up to 765 kV.

In considering circuit breaker performance, short circuit current breaking capacity, and maximum interrupting capacity are often referred to. However, in the case of reactor switching the following characteristics strongly influence the chances of successful interruption and the resulting transient overvoltages to which equipment will be exposed:

- Current chopping capability and its dependence upon arcing time. Chopping levels control overvoltages during load side oscillation and hence the maximum recovery voltage (TRV).

- Withstand voltage of the opening contacts following interruption and its dependence on arcing time. RRRV and maximum TRV must not exceed what the opening breaker contacts are capable of withstanding or reignition results.

Considerable testing is required to determine breaker current chopping numbers and opening contact recovery withstand voltage as functions of arcing time, but reactor switching performance may not be analysed in detail without these characteristics.
Successful interruption not only requires arc quenching to extinction by intense cooling, but also that the opening breaker contacts withstand TRV following extinction. Hence a suitable arc quenching medium must also be a good insulator to enhance the recovery withstand voltage capability of the interrupter. Figure 4.18 compares the insulating qualities of SF₆, oil, and air as functions of pressure. Aside from rapid contact separation, establishing and maintaining quenching medium pressure is essential to good dielectric performance during interruption. Circuit breakers well suited to reactor switching applications should have several desirable qualities:

- Low current chopping levels to avoid excessive chopping overvoltages and large RRRV.
- Fast dielectric recovery of the interrupters after arc quenching to withstand large RRRV and TRV associated with current chopping.
- Reduced tendency to interrupt high frequency current reducing the likelihood of multiple reignition and voltage escalation.

4.1 Contrasting Circuit Breaker Technologies

Rapid reduction of arc conductivity through intense cooling was discussed in section 2.1 with reference to arc quenching to extinction. It is clear from figure 2.2, that effective cooling between 5000 – 1500 °K can reduce arc conductivity from that of a good conductor to a good insulator [7]. Therefore superior arc quenching media must have good cooling properties in this temperature range. Figure 4.19 contrasts the thermal conductivities of SF₆, N₂, and H₂. Hydrogen is the principle by-product in the thermal breakdown of insulating oils while nitrogen is the main constituent of dry air. In the temperature range of interest, hydrogen has the highest thermal conductivity, followed by SF₆ and nitrogen.

To achieve suitable interrupting capacities, forced cooling of hot arc gases is mandatory since conduction alone provides inadequate heat transfer. With conventional circuit breakers,
energy provided externally or drawn from the arc itself, is used to force cooling of the arc. Cooling effectiveness hinges on unobstructed coolant flow and is usually enhanced by forcing the arc to burn through a smooth nozzle in which high coolant blast velocities can be achieved.

### 4.1.1 Oil Circuit Breakers

Bulk oil (dead tank) and minimum oil (live tank) breakers are in common use. The extinguishing chamber designs of each are basically the same so their interrupting mechanisms may be jointly described.

As moving contacts open an arc is drawn causing vigorous oil decomposition. Copious amounts of hydrogen are produced pressurizing the interrupting chamber. Continued contact travel causes a high pressure hydrogen blast to cool the arc as exhausting vents successively open. Since blast energy is derived solely from the arc, blast pressure does not build extremely rapidly. However, clearing times in the order of 40 - 50 ms can be achieved, which are adequate
for many applications. Due to the high thermal conductivity of hydrogen in the critical temperature range, fairly high RRRV withstand capability can be achieved with oil interrupters. Cooling blast intensities are lower than air blast breakers, and oil breaker chopping numbers tend to be smaller as shown in table 4.1. This causes a desirable limiting of chopping overvoltages. Cooling blast intensity in oil interrupters is a function of arc current magnitude as oil decomposes more rapidly at the higher arc power associated with elevated currents.

Oil breaker reactor switching experiments have shown good agreement with the theory presented in Chapter 2. The work of Murano et al [17] for example, showed minimum oil breaker current chopping was related to network capacitance as predicted by equation 2.10 with \( n \approx 0.47 \). This supports the validity of characterizing current chopping behavior of oil breakers using the chopping number \( \lambda_{ch} \) as defined in section 2.1.2 [1],[17],[26] and [2].

Recovery voltage withstand strength following extinction in an oil interrupter is provided by contact separation and the insulating qualities of the oil itself. Good dielectric performance
requires rapid evacuation of gas bubbles and decomposition by-products following arc quenching. In modern oil breakers this is often achieved and hence the likelihood of reignition reduced by:

- Increasing contact opening acceleration.

- Permanently pressurizing the interrupting chambers.

- Use of forced oil injection to flush decomposition by-products out of the interrupter as they are produced.

Opening energy for an oil breaker is often provided by a spring which is pre-charged during the previous closing stroke. Since arc quenching energy is produced by the arc itself, only modest operating energy need be provided externally.

4.1.2 Air Blast Circuit Breakers

Both operating and arc quenching energy is provided externally with compressed air in an air blast breaker. Substation compressors maintain a constant supply of dry pressurized operating air in a central reservoir. Supply lines are then routed to small dedicated storage tanks for each air blast device.

At the start of and throughout the opening stroke, the moving contacts are accelerated by high pressure air. A blast of high pressure air also cools and quenches the arc. Since air is less thermally conductive than hydrogen or $SF_6$ in the critical temperature range (5000 - 1500 °K), a higher air blast velocity must be used to dissipate an equivalent arc energy. Air blast duration is usually more than five cycles following contact separation and catastrophic failure is likely if arcing extends beyond this time. Modern air blast breakers use continuously pressurized interrupters to enhance dielectric performance.

Due to their high short circuit current interrupting capacity, air blast breakers are in common use. Breaking capacity in excess of 100,000 A rms is available at low voltages. Short breaking
times, \((\approx 2 \text{ cycles at } 60 \text{ Hz})\), also make air blast devices attractive where rapid fault clearing is desirable.

Requirements for expensive and maintenance intensive compressor, air drying, and storages systems are a general disadvantage of air blast breaker application. Where reactor switching is involved, the following qualities are more of a concern:

- High blast intensities result in air blast breakers having a well documented higher current chopping tendency than \(SF_6\) devices \([1],[17],[7],[14]\). This is largely due to the higher thermal conductivity of air over \(SF_6\) between 5000 - 10,000°K as shown in figure 4.19. Arc thermal time constant \(\theta\) is smaller with increased cooling, causing arc instability and current chopping to occur at higher levels. Larger current chopping overvoltages result.

- Cooling in the critical range 5000 - 1500°K, where the arc must remain a good insulator to ensure successful interruption, is not as effective with air as oil or \(SF_6\). As a result, air blast breakers have a higher sensitivity to RRRV and a correspondingly higher tendency for dielectric reignition during reactor switching.

A common method of dealing with undesirable effects of high current chopping tendencies in air blast breakers during reactor switching, is to use opening resistors. Opening resistors are connected in parallel with the main interrupters and an auxiliary interrupter added to break resistor current some time after main contacts have opened. RRRV and resulting TRV across the breaker are reduced over those for an identical interrupter without opening resistors because:

- Increased resistance lowers the current at which the arc becomes unstable hence reducing chopping current.

- The new steady state load side voltage following current commutation to the opening resistor, is smaller than and phase advanced with respect to the system voltage. This results in a reduced RRRV when the resistor switch interrupts.
These points are demonstrated analytically in Appendix E.

The delay between main interrupter contacts parting and parting of the associated resistor switch is called insertion time. Insertion times in the order of 20 ms are commonly chosen to exceed maximum anticipated main interrupter arcing times. Load side oscillation resulting from interruption of the reference network of figure 2.9 with a 5000 Ω opening resistor is shown in figure 4.20. A 20 A resistor switch chopping level is assumed and the response calculated using expressions derived in Appendix E. Results may be contrasted to 4.21 depicting the same interruption with no opening resistor. Reduced load side overvoltages are apparent with the opening resistor. Figure 4.22 shows the resulting circuit breaker recovery voltage without resistor, compared to those with 2000 and 5000 Ω resistors, where reduced maximum TRV and RRRV are apparent. With an opening resistor inserted, source voltage lags the reactor voltage phasor at the moment of interruption. This results in breaker TRV recovery peaks occurring later, giving breaker contacts more time to establish withstand capability. For the same reason, TRV suppression peaks are larger with an opening resistor even though absolute suppression peak voltages with respect to ground are smaller. The increased risk of suppression peak reignition is a small drawback contrasted to the advantages of reduced current chopping overvoltages, RRRV and maximum breaker TRV. Air blast breakers expected to switch shunt reactors are frequently equipped with opening resistors to reduce the severity of the interruption duty. Another approach, is the use of metal oxide surge arresters in parallel with interrupter contacts to limit breaker TRV [1]. This approach has been used successfully in SF6 circuit breakers for 756 kV shunt reactor switching applications [21]. More commonly, reactor surge arresters applied for insulation protection, assist the breaker by limiting chopping overvoltages, with resulting limitation of breaker TRV [4]. Many B.C. Hydro shunt reactor compensated line terminals use air blast breakers without opening resistors, which are frequently used for reactor switching when the associated lines are out of service. Chopping overvoltages are exceptionally severe in these cases, and surge arresters are essential to successful interruption and preventing insulation damage.
Chapter 4. Breaker Characteristics Relevant to Reactor Switching

Figure 4.20: Interruption at 20 A Current Chopping with 5 kΩ Opening Resistor

Figure 4.21: Interruption at 20 A Current Chopping with No Opening Resistor
Reactor switching experiments with air blast circuit breakers have shown current chopping performance in good agreement with Chapter 2. Murano et al for instance [17], found current chopping dependence on network capacitance as described be equation 2.10 with \( n = 0.49 \). It is hence well accepted that air blast breaker current chopping performance may be described with a chopping number \( \lambda_{ch} \) according to equation 2.10 [24],[1],[17],[26]. Tests performed by the author and others [17], [14], confirm the chopping numbers of typical air blast breakers to be considerably larger than \( SF_6 \) or oil devices as described in table 4.1.

Recovery voltage withstand capability following arc extinction in an air blast interrupter is dependent on contact separation and continued cooling. The well documented sensitivity of air blast breakers to RRRV [1],[7] is primarily due to lesser post arc cooling effectiveness of an air blast below 3500 °K. Reignitions are hence quite common in air blast breaker reactor switching. Increasing air blast intensity provides a poor remedy since current chopping would be more pronounced, further aggravating RRRV and maximum TRV. The author and others [1],[3], have routinely observed multiple reignitions during air blast breaker shunt reactor
switching. This has also been attributed to the high thermal conductivity of air in the range $5000 - 10,000 \, ^\circ K$.

Due to higher current chopping capability and lesser RRRV tolerance, air blast breakers frequently reignite prior to the load side oscillation suppression peak. This has the effect of limiting load side overvoltages by reconnecting the interrupted reactor phase to the power system which is at a lower potential. Conversely, reignitions following the suppression peak where breaker TRV approaches maximum values, may generate second parallel oscillation overvoltages sufficient to invoke surge arrester operation. Both effects have been observed by the author and will later be shown in field test results presented in Chapter 5.

### 4.1.3 SF$_6$ Gas Circuit Breakers

Sulphur Hexafluoride ($SF_6$) has 2.5 to 3 times the dielectric strength of dry air at the same pressure as may be seen in figure 4.18. Figure 4.19 previously showed the superior thermal conductivity of $SF_6$ below $3000 \, ^\circ K$ giving good arc quenching performance near and following extinction. Further, $SF_6$ exhibits exceptionally fast recombination of arc dissociation products reforming $SF_6$. The result is good dielectric performance in withstanding large RRRV making reignitions much less common in $SF_6$ versus air blast devices.

Available $SF_6$ circuit breaker types include:

- Two Pressure Blast
- Puffer

Two pressure blast function is similar to an air blast breaker. Interrupters are enclosed within a second $SF_6$ chamber at a pressure well below that of the interrupters themselves. During interruption, $SF_6$ is blasted across the arc at high pressure, venting into the outer low pressure chamber where it is recovered and compressed for reuse. Operating energy is externally supplied by compressed air or $SF_6$.

Puffer $SF_6$ device operating energy is provided externally at first, and later from the arc itself. As contacts part, an arc is drawn which rapidly dissociates the $SF_6$ gas. The opening
moving contact also serves as a piston to compress fresh $SF_6$, and blow a quenching puff of gas over the arc. While initial contact/piston movement is forced by an external mechanism, pressure increase due to rapid gas dissociation later significantly assists the motion. As a result, puff intensity is determined to a large extent by the arc current magnitude. Design of the vents through which the $SF_6$ puff is expelled can be used to control how abruptly the arc will be quenched.

Two pressure blast devices are considerably complex, requiring expensive compressors and gas storage facilities. Current chopping capability is higher than puffer devices due to higher coolant velocity. Accordingly, chopping overvoltages during reactor switching with two pressure blast $SF_6$ devices can be large. Kobayashi et al [14] tested 275 kV reactor switching with air blast, gas blast and puffer devices. The air blast breaker tested was fitted with 15.8 kΩ opening resistors and produced 1.71 pu maximum chopping overvoltages. In contrast, the gas blast device tested was not equipped with opening resistors and produced overvoltages up to 2.27 pu switching the same reactor network.

Various research efforts have established $SF_6$ device current chopping behavior to be in accordance with equation 2.10. Murano et al [17] found in their $SF_6$ gas blast breaker experiments, that equation 2.10 was satisfied for $n=0.48$. Kobayashi et al [14] deduced $n=0.47$ in experiments with an $SF_6$ puffer breaker. Both results support $n=0.5$ arrived at in Chapter 2 using arc stability criterion assuming a constant power arc characteristic ($\alpha = 1.0$). Puffer $SF_6$ designs are known to provide very gentle reactor interruption characterized by low current chopping levels [1][17][13], and [11]. Chopping overvoltages are accordingly lower than air blast or $SF_6$ blast devices and tripping resistors are not necessary to aid in the reactor switching duty. Chopping numbers are significantly less for $SF_6$ versus air blast devices due to the reduced cooling effectiveness of $SF_6$ in the range 5000 - 10000 °K compared to air. Murano et al [17] measured chopping numbers in the order of $3 AF^{-\frac{1}{2}} \times 10^4$ as opposed to $15 AF^{-\frac{1}{2}} \times 10^4$ for the air blast breaker tested. Kobayashi et al [14] measured in the order of $18 AF^{-\frac{1}{2}} \times 10^4$ and $7.8 AF^{-\frac{1}{2}} \times 10^4$ respectively for $SF_6$ blast and $SF_6$ puffer breakers switching the same network.
Table 4.1: Single Interrupter Chopping Numbers for Various Breakers

<table>
<thead>
<tr>
<th>Breaker Type</th>
<th>$\lambda_{ch1} \ (AF^{-\frac{1}{2}} \times 10^t)$ [1]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Oil</td>
<td>7 - 10</td>
</tr>
<tr>
<td>Air Blast</td>
<td>12 - 33</td>
</tr>
<tr>
<td>$SF_6$ Puffer and Gas Blast</td>
<td>3 - 18</td>
</tr>
</tbody>
</table>

Reduced current chopping tendency and increased dielectric capability following arc quenching make $SF_6$ circuit breakers, in particular puffer devices, a good choice for dedicated reactor switching applications. $SF_6$ puffer load break switches are used in all B.C. Hydro shunt reactor line compensation schemes where the reactor network must be switched independently of the associated circuit.

4.2 Current Chopping and Recovery Voltage Withstand Characteristics

Current chopping depends to a large extent on intensity of arc cooling in the temperature range 5000 - 10000 °K. Arc cooling intensity is in turn a function of both thermal conductivity and flow rate of the arc quenching medium. Because static arc characteristics depend on the random variable $\eta$, current chopping would also vary between interruptions, even if all other influential parameters could be held constant. These effects were in fact summarized in equation 2.10 which for a constant power arc characteristic may be written as:

$$i_{ch} = \lambda_{ch} \sqrt{C}$$

$$\lambda_{ch} = \sqrt{\frac{\eta \alpha}{\theta}}$$
Since the thermal time constant $\theta$ becomes smaller with increased cooling intensity, chopping number $\lambda_{ch}$ is increased. Because maximum blast velocity cannot be instantaneously established on trip initiation, thermal time constant and hence chopping number are arcing time dependent. Because $\eta$ is a random variable, chopping number will be normally distributed for test interruptions with constant arcing time.

Recovery voltage withstand capability across opening breaker contacts depends on:

- Contact acceleration and final separation.
- Effective arc cooling below 5000 $^\circ$ K.
- Establishing and maintaining interrupter insulating medium pressures.
- Rapid evacuation of conductive arc by-products or gas dissociation products.

To differing degrees, these factors are all functions of arcing time. However contact separation changes the most profoundly during a tripping operation, and has the largest single influence on recovery voltage withstand capability. Rizk found in his experiments with an air blast circuit breaker [24] interrupting a 400 A arc, that within 100 $\mu$s of arc quenching, withstand voltage simply approached that of the increasing contact gap in dry air. Hermann and Ragaller [12] suggest that within 100 $\mu$s of arc quenching in an $SF_6$ interrupter, breakdown strength is determined solely by contact separation.

Variations in breaker recovery voltage withstand capability and current chopping are highly likely between trip operations as complex interactions between arc, quenching medium, and opening contacts are unlikely to be identically repeatable. Accordingly a full theoretical representation of the interacting factors affecting current chopping and recovery voltage withstand would be extremely difficult. Circuit breaker design and development efforts hence rely extensively on empirical relationships derived through careful experiment.
4.2.1 Measuring Current Chopping and Reignition Characteristics

Current chopping and reignition (recovery voltage withstand) characteristics in particular, can and have been measured by the author and others using reasonably simple techniques. These characteristics are essential to assessing the suitability of a device to reactor switching applications.

To acquire these data, instrumentation must be applied to record breaker currents, load and source side phase to ground voltages, and breaker tripping command as functions of time. By having previously measured delays between trip application and phase interrupter contact parting times, arcing time may be determined for each phase from interruption test traces. Times between trip application and contact parting vary somewhat between operations due to differing starting pressures and non-linearities in breaker actuating mechanisms. For 500 kV air blast breakers tested by the author, these times were generally repeatable to within ± 0.5 ms. Chopping currents may then be measured directly from current traces. Where network electrical parameters have been accurately estimated, calculating chopping currents from suppression peaks gives good agreement with direct measurement if reactor network interphase coupling is small.

Current and voltage transformers used must have suitable frequency response to ensure accurate capture of the phenomena of concern. This may be especially difficult when high frequency events such as reignitions are to be studied. Instrument transformers provided for normal operation of protective relaying and metering equipment perform well at system frequency but their response at higher frequencies can be unacceptable. Sophisticated reactor switching tests in the substation environment may as a result require temporary installation of higher quality instrument transformers. This is an expensive, time consuming step, and benefits must be weighed against use of existing devices which may give adequate results if lower frequency events such as load side oscillations or current chopping are being studied.

Control facilities must be incorporated to allow predictable variation of the point on wave of interrupter contact parting. By adjusting the point on wave at which the trip command is
applied, arcing time is controlled and its influence on current chopping may be measured. Due to uncertainty in estimating the instant of contact parting and the random nature of a device chopping number, data are likely to be somewhat scattered as will be seen on presentation of field measurements. If desired, curve fitting techniques may be applied to determine formulae describing the current chopping characteristics with some specified degree of confidence.

Reignition (recovery voltage withstand) characteristics may be estimated in similar fashion, through analysis of device reignitions. By measuring the arcing time and voltage across the opening breaker contacts at the point of reignition, a reignition characteristic may be estimated. The process is more difficult than measuring current chopping performance since:

- The test breaker may not reignite over a practical range of arcing times. SF₆ devices in particular are likely to exhibit little or no reignition tendency during reactor switching tests due to their superior dielectric characteristics and lower current chopping. Even with air blast breakers, reignitions tend to occur for shorter arcing times at breaker TRV less than 50% of ultimate withstand levels.

- The test reactor network may not generate especially large TRV or RRRV even with the larger current chopping levels associated with air blast breakers. Reignitions may occur only for a limited range of small arcing times and data over a practical range of arcing time may hence be unattainable using this method. Extrapolation between measurements and rated ultimate withstand levels may be necessary to fill voids in the reignition characteristic.

A laboratory test facility where load network parameters could be altered to control TRV and RRRV would offer better chances of invoking circuit breaker reignitions over a full range of arcing times. Performing such tests at full rated voltage requires unique facilities of which only a few exist in the world. As a result, measurements of this type are commonly performed at lower voltages using a reduced number of interrupters. Performance of the full scale device is then extrapolated using statistical arguments. It is absolutely essential that laboratory tests
duplicate within acceptable limits the interactions between network and circuit breaker which will occur in a practical network. Damping and natural frequencies of the test network must be carefully chosen to duplicate potential field conditions to which the circuit breaker may be applied.
Chapter 5

Reactor Switching Field Tests

In recent years B.C. Hydro experienced a number of interruption failures while switching out 500 kV 3x45 MVar shunt reactor banks with two different varieties of switchgear. A series of reactor switching test programs were performed to explore the cause of the failures. A better understanding of the reactor switching duty and circuit breaker performance was also sought.

Due to system load being concentrated in the southwest, and large hydro electric plants located in the northern and eastern reaches of the province, 500 kV circuits are employed in the B.C. Hydro system. Banks of 525 kV, three single phase 45 MVar reactors are used throughout the system to compensate 60 – 65% of associated line shunt capacitance. These are usually located in the line terminals as shown in the station one line diagram of figure 5.23. Dedicated load circuit switchers are provided if the reactor is to be switched separately from the line for voltage control flexibility. Although routinely used for reactor switching, circuit switchers cannot interrupt fault current, and line breakers must be capable of reactor switching as well. This chapter presents highlights of several field tests which the author initiated or was directly involved with. Practical manifestations of previously described phenomena will be discussed and the severity of the reactor switching duty highlighted. Strengths and shortcomings in the reactor switching performance of several practical pieces of switchgear will be analysed and outlined.

The site of the reactor switching failures and subsequent testing was Nicola substation previously shown in figure 5.23. Nicola is a critical 500 kV transmission hub providing interconnection between major generation, and central load centers in the B.C. Hydro network via eight 500 kV lines. Five 135 MVar shunt reactor banks are provided at Nicola line terminal
for system voltage control. Of these, four are grounded through 1000 Ω neutral reactors to assist single pole line ground fault clearing, and the fifth is solidly grounded. Reactor switching failures were experienced with an air blast breaker and an SF₆ puffer type circuit switcher. Reactor interruption tests were performed on three air blast breakers of different manufacture, and the circuit switcher of concern. Significant results of each test program are presented in the following sections.
Chapter 5. Reactor Switching Field Tests

5.1 Nicola 5CB25 Testing

5CB25 is a four interrupter air blast breaker with 400 Ω opening resistors which also serve as closing resistors. The ohmic value was selected for the suppression of energizing switching surges. Main contacts open 20 ms after trip command application. Current then commutates to the resistor switch whose contacts open 21 ms after main contact separation. Currents generally interrupt within 2 cycles of resistor switch contact separation. While de-energizing 5RX11 with 5L98 out of service, 5CB25 experienced incidents of prolonged arcing and in one case a single phase failed catastrophically.

5.1.1 5CB25 Test Instrumentation

A test program was arranged to monitor 5CB25 switching 5RX11 under controlled conditions. A zero crossing detector was used to ensure consistent point on wave trip application. 5D51 was tripped each time as backup to avoid equipment damage in the event of 5CB25 failure. The sequence of events consisting of zero crossing detector initiation, breaker and backup device trip application, and start/stop of recording equipment was controlled by a desk top computer as shown in figure 5.24. All signals were recorded on magnetic tape for later analysis and on light beam chart recorders for immediate assessment. 5CVT25 voltages were monitored to observe load side voltage as measured by a capacitive voltage transformer tuned for 60 Hz operation. To ensure higher accuracy, load side voltage was also monitored on C phase by erecting a fast capacitive voltage divider with 1 MHz frequency response, adjacent to 5CVT25. The voltage divider signal was transmitted about 200 m to the station control room via optical fibre to reduce noise. All instrument connections to current, voltage, and tripping signals were made in protection and control cabinets in the station control room.
5.1.2 5CB25 Test Observations

5CB25 Test Trip 1 in figure 5.25 shows a normal interruption. Resistor switch current chopping in the order of 15 A and 20 A can be seen in the B and C phase current traces respectively. Commutation from main contacts to resistor switch is most evident in the C phase CVD (capacitive voltage divider) trace approximately 35 ms from trace initiation. Current chopping precipitates modulated load side voltage oscillations as predicted by equation 3.36. Since 5RX11 is made up of three individual phase reactors, inductive coupling does not exist within the reactor bank. An elevation of the actual equipment arrangement involved in the 5CB25/5RX11 appears in
Chapter 5. Reactor Switching Field Tests

Figure 5.26. Simulation work performed in Chapter 6 confirms the phase interactions during load side oscillation to be due almost entirely to bus capacitive coupling for this network. Dual frequency oscillation is observed most clearly on C phase voltage and prominent frequencies are 660 Hz modulated by 20 Hz. From equation 3.36 zero and positive mode natural frequencies are hence 680 Hz and 640 Hz respectively. Distortion due to limited bandwidth and protective spark gap operation is very obvious when comparing the CVT and CVD derived voltage signals. The CVT is unsuitable for highly accurate measurements at the frequencies of concern to reactor switching, but some general information can usually be gleaned from its signals all the same.

Of further interest in figure 5.25 is B phase reignition about 0.8 ms following current chopping which occurred very near to B phase load side voltage recovery peak. Reignition is manifested by both the sudden appearance of a high frequency current oscillation, and abrupt translation of the load side voltage to match the source side. In this instance, the second parallel oscillation current was interrupted before a new 60 Hz half cycle of resistor switch current was established.

Point on wave of trip initiation was varied, and an instance of prolonged interruption immediately observed in 5CB25 Test Trip 2 given in figure 5.27. When a breaker is capable of reactor interruption, arcing times of less than one 60 Hz cycle are expected, and usually not more than one reignition occurs. In the test of figure 5.27, A phase interrupted about one half cycle after three reignitions, the last of which was interrupted during the second parallel oscillation. Several incidents of second parallel current interruption were observed suggesting 5CB25 is capable of high frequency current interruption. Breakers with higher blast strength tested later showed this behavior more frequently. Due to the frequency response of station current transformers being limited to about 10 kHz, second parallel oscillation frequency could not be measured from current signals. B phase interruption in figure 5.27 was very prolonged with resistor switch arcing in excess of 60 ms as a result of multiple 60 Hz recovery voltage reignitions, each leading to renewed 60 Hz current flow. Reignition voltage increased with the first three B phase reignitions, and fell back significantly at the fourth and sixth. This is a symptom of instability in the breaker reignition characteristic to be examined in more detail.
Figure 5.25: 5CB25 Test Trip No. 1
later. C phase load side oscillation voltage showed no modulation until A phase cleared at which point significant modulation began.

Figure 5.28 shows 5CB25 Test Trip 24 where prolonged arcing was so severe, A phase was actually interrupted by the backup device 5D51. C phase interrupted 10 ms before 5D51 opened. A and C phase arcing times of 106 ms and 98 ms respectively are complete reactor interruption failures. As in figure 5.27, unstable variation in reignition voltages was observed on both phases following the first several reignitions.

The largest 5CB25 resistor contact current chopping noted throughout the test program was 24 A with a corresponding suppression peak of 1.6 pu. Suppression peak was calculated from equation 3.44, with the following estimated network parameters:

\[ L = 5.41 \, \text{H} \quad C = 9800 \, \text{pF} \]

\[ R_1 = 2.85 \, \Omega \quad R_2 = 1.5 \, \text{M\Omega} \]

This yielded 1.63 pu for 525 kV operation with 24 A current chopping, agreeing well with the measured suppression peak. The network capacitance to ground consists of 5CVT25 (5000 pF), reactor bushing and surge arrester (2800 pF), and 2000 pF estimated for bus conductors and support insulators. Surge arresters applied at Nicola reactors are intended to limit switching surges to 950 kV (2.1 pu on a 550 kV base). Arrester operations during 5RX11 switching
Figure 5.27: 5CB25 Test Trip No.2
Figure 5.28: 5CB25 Test Trip No. 24
Chapter 5. Reactor Switching Field Tests

5.1.3 Characterizing 5CB25 Performance of 5RX11 Switching

A 5CB25 resistor switch arcing characteristic was constructed by plotting arcing time against contact point on wave parting time shown in figure 5.29. Point on wave time was measured from contact separation to the previous current zero on the phase of concern. Hence a point on wave time of zero represents contact separation exactly at a current zero. For stable 5RX11 interruption, the breaker should consistently follow the solid characteristic with a resulting 12 ms operations with 5CB25 were hence rare.

While modulation of the load side oscillation was the most pronounced phase interaction observed, effects were also apparent during reignitions of adjacent phases. For example the C phase load side oscillation in Test Trip 1 of figure 5.25 shows a small coupled transient voltage at the final A phase reignition. Generally for 5CB25 tests, adjacent phase reignitions only had noticeable influence during load side oscillation on the phase of concern and effects were never significant.

Figure 5.29: 5CB25 Resistor Switch Arcing Characteristic
maximum resistor switch arcing time. Dashed lines represent unstable characteristics which the breaker follows during extended interruptions jumping from one unstable characteristic to the next until finally clearing or failing to interrupt. Interestingly, extended arcing and interruption failures only occurred with point on wave time between 4 to 6 ms and was clearly not a random occurrence. In the course of one 60 Hz cycle, this critical 2 ms window occurs 6 times amongst the three phases. The chance of prolonged arcing on at least one phase is then in the order of 72%. If contact parting falls in the critical window overlap between phases, prolonged arcing will occur on both as in figures 5.27 and 5.28.

Chopping current dependence on arcing time was clearly noted in individual switching traces. Figure 5.30 for example, shows prolonged C phase interruption where chopping level increases with each successive attempted interruption. The trend is evident in both the C phase current and in the escalating suppression peaks of the C phase voltage.

Figure 5.31 shows a 5CB25 resistor switch chopping characteristic specific to 5RX11 derived by analysis of each tripping test. Each point could be converted to a chopping number using equation 2.11 and the resulting chopping number characteristic would not be network specific. Using equation 2.10 the maximum 5CB25 chopping number measured was $24 \text{ AF}^{-\frac{1}{4}} \times 10^4$ or $12 \text{ AF}^{-\frac{1}{4}} \times 10^4$ per interrupter. For constant arcing time, a normally distributed variation in chopping current is expected and hence the chopping characteristic is somewhat scattered. Chopping levels increase with arcing time as cooling blast intensity rises. The largest chopping level observed was 24 A at 26 ms arcing time. With prolonged arcing, chopping levels dropped off possibly due to:

- Cooling intensity diminishing as the limits of normal blast duration approached.
- Reignition and subsequent current chopping were actually occurring outside the main stream of the air blast.
- Blast flow deficiencies within the breaker mechanism.

Reduced chopping at prolonged arcing time may have made the difference between ultimate
Figure 5.30: 5CB25 Test Trip No. 4
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Figure 5.31: 5CB25 Resistor Switch Current Chopping Characteristic

Figure 5.32: 5CB25 Resistor Switch Reignition Characteristic
interruption or complete failure. Extended arcing may otherwise have consistently lead to complete failure if maximum chopping had persisted.

The 5CB25 resistor switch reignition characteristic shown in figure 5.32 was prepared by plotting reignition recovery voltage against reignition arcing time for each test trip. The recovery voltage withstand capability rises sharply to 1000 kV (1.8 pu on a 550 kV base) in about 6 ms following contact separation. Provided pressure is maintained in the interrupting chamber, reignition voltage for an air blast breaker is primarily a function of contact separation and the withstand voltage should plateau once contacts have fully opened. Rather than rising to a plateau, the 5CB25 reignition characteristic is unstable beyond about 12 ms arcing time after which reignition voltage rises and falls unpredictably. This behavior is obvious in figures 5.26 and 5.28 where successive reignitions during prolonged arcing occur at random rather than progressively increasing recovery voltages.

Both the random reignition voltages and curtailed current chopping associated with extended 5CB25 arcing times may be suggestive of dielectric failure outside the main air blast stream, but this was never proven. The arcing characteristic alone shows 5CB25 to be unsuitable for the 5RX11 reactor switching duty. Ultimately, this breaker and all others of the same manufacture applied to reactor switching in B.C.Hydro, were relocated to uncompensated line terminals.

5.2 Nicola 5CB15 Testing

5CB15 is the partner breaker to 5CB25 in the Nicola 5L98 line position. It is a six interrupter air blast device without opening resistors and relative to 5RX11 has an identical electrical location as 5CB25. Each pole of the breaker consists of three heads in series with two series interrupters per head.

Control and instrumentation were similar to 5CB25 tests except a high speed voltage divider and magnetic tape recorder were not available. Light beam oscillographic recorders were used for signal recording and analysis. Current signals were obtained from both 5CT15 and high
voltage bushing CT’s of 5RX11 while load side voltages were recorded from 5CVT25.

5.2.1 5CB15 Test Observations

5CB15 chopped a maximum of 70 A during the test program exhibiting much higher chopping numbers than 5CB25 (70.7 $AF^{-\frac{1}{2}} \times 10^4$ overall or 28.9 $AF^{-\frac{1}{2}} \times 10^4$ for a single interrupter). This is due to the combination of higher air blast intensity and the absence of an opening resistor switch. Surge arresters operated during each test and played an important role in successful interruption. At 520 kV, the suppression peak calculated for 70 A chopping on 5RX11 interruption is 3.93 pu. Such levels were never realized as 5RX11 surge arresters operated at 2 pu or less.

Figure 5.33 is a typical 5CB15 interruption showing 5CT15 breaker phase currents ($I_{AC}$, $I_{BC}$ and, $I_{CC}$), 5RX11 reactor currents ($I_{AB}$, $I_{BC}$ and, $I_{CA}$), and 5CVT25 voltages ($V_A$, $V_B$ and, $V_C$). Point wave time was sufficient to produce about 70 A current chopping on A phase. The $V_A$ trace clearly shows immediate surge arrester operation and suppression peak is limited to about 1.7 pu. The resulting breaker recovery voltage was hence not large enough to cause reignition and interruption was successful. As the arrester operates, reactor energy discharges as noted by the exponential decay of $I_{AR}$. Without surge arresters, such high levels of current chopping would most assuredly cause reignition. Phase B and C interruptions are somewhat more eventful. B phase point on wave time is large enough that arcing time to the first zero is too short for the opening contacts to withstand the suppression peak. Following 25 A current chopping, suppression peak reignition occurs at 100 kV recovery voltage. The resulting second parallel oscillation current is interrupted only to be followed by double recovery voltage reignitions at about 300 kV re-establishing 60 Hz current. B phase subsequently chopped about 65 A causing surge arrester operation and thereby interrupting successfully. C phase interruption is similar with arcing time to the first zero being even less. Initial current chopping of 5 A causes a suppression peak reignition at 60 kV which is interrupted and followed by a single recovery voltage reignition at 80 kV restoring 60 Hz current. The suppression peak reignition is a case of
the breaker limiting the suppression peak overvoltage by reigniting and reconnecting the reactor to the system. Interruption is successful at the subsequent zero where 65 A current chopping causes C phase surge arrester operation. Arcing times to initial B and C phase current zeroes were too short for resulting current chopping to operate surge arresters or for sufficient contact separation to prevent reignition.

Phase interactions during load side oscillation were not observed since arrester operation or reignition truncated all but the initial quarter cycle of oscillation at each interruption. Phase interactions at reignition were on the contrary quite visible. Reignition represents an abrupt high magnitude current injection into the reactor network and substation ground grid. Transient currents are hence expected in adjacent phases during reignition if any degree of coupling exists. Transient currents appear on A and B phases during the C phase suppression peak reignition and subsequent recovery voltage reignition. Later suppression peak and multiple recovery
voltage reignitions on B phase couple sizable transient currents into A and C phases. These transients do not appear in the measured reactor currents. Several factors must be considered in interpreting these observations:

- Second parallel oscillation currents associated with reignition cannot flow appreciably in the large reactor inductance but may be shunted past via bushing capacitance.

- Current flows in the ground mat directly beneath the reigniting breaker, elevating ground potential and introducing high frequency currents into buried cables including those associated with adjacent current transformers (5CT15).

- Absence of noise in the reactor current traces may in part be due to the the distance of reactor cables from the reignition source. 5RX11 cable trenches are about 100 m from 5CB15 and run perpendicular to reactor busses over head.

- High frequency response of station current transformers is in the order of 10 kHz and their transient response is completely unknown. They cannot be expected to accurately reproduce the fast transient currents associated with reignition.

In summary the extent of interphase coupling at reignition cannot be concluded with confidence except to say it appears to be significant. Simulation work in Chapter 6 suggests coupled currents during adjacent phase reignitions can in fact be substantial, especially when the breaker does not have opening resistors.

### 5.2.2 Characterizing 5CB15 Performance During 5RX11 Switching

5CB15 arcing and current chopping characteristics plotted from point on wave, arcing time and current chopping measurements are shown in figures 5.34 and 5.35. The arcing characteristic is determined largely by whether the reactor network response to current chopping results in surge arrester operation. Increasing from point on wave of 0 ms, arcing time is sufficient that resulting chopping levels cause arrester operation leading to successful interruption. As point on wave
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Figure 5.34: 5CB15 Arcing Characteristic

Figure 5.35: 5CB15 Current Chopping Characteristic
increases to around 5 ms, arcing time is too short for the consequently smaller chopping levels to operate surge arresters. Reignitions translate the breaker to the second characteristic where longer arcing times increase chopping levels and ensure successful operation through surge arrester operation. For the characteristic shown, minimum arcing time for assured arrester operation is about 3 ms corresponding to approximately 30 A current chopping. Because gapped surge arrester operating voltage is statistical, minimum arcing time will vary and might be better defined as the longest arcing time for which arresters consistently do not operate on suppression peaks.

5CB15 current chopping capability rises rapidly with time to the maximum 70 A measured. The characteristic does not appear to have reached a plateau and had arresters not assured interruption, longer arcing times may have yielded still higher chopping levels. The reignition characteristic of figure 5.36 indicates a rather slowly rising recovery voltage withstand capability. At an arcing time of 4 ms for example, 5CB15 could chop up to 40 A but withstand 350 kV at best across its opening contacts. 5CB25 by contrast, would chop only 5 to 8 A
and withstand at least 400 kV thus having a much higher chance of successful interruption. This difference may be due to lower contact acceleration or an increased likelihood of timing spread between the six 5CB15 series interrupters versus four in 5CB25. Either effect would delay 5CB15 contacts reaching their maximum separation. High current chopping levels with resulting arrester operations made it impossible to fix points on the reignition characteristic beyond 4 ms. Surge arresters are mandatory in this 5CB15 reactor switching application not only for insulation protection, but also to ensure expedient interruption. In the presence of surge arresters, 5CB15 handles the 5RX11 switching duty easily.

5.3 Nicola 5CB3 Testing

5CB3 is a line breaker associated with 5L72 which is shunt compensated by 5RX3. An air blast device, 5CB3 has six series interrupters per phase with each phase consisting of three dual interrupter heads. Opening resistors are not incorporated in the design of this breaker. Though of a different manufacture, the electrical location of 5CB3 relative to 5RX3, is identical to that of 5CB25 relative to 5RX11. 5RX3 is a four reactor scheme with a 1000 \( \Omega \) neutral reactor, and phase reactors identical to 5RX11. 5L72 line protection initiates single pole tripping for single phase faults and 5RX3 must compensate phase to phase line capacitance to ensure successful fault clearing.

Originally, this test program was initiated to investigate misoperation of a 5RX8 pressure relief micro-switch causing false protection system tripping during a 5RX3 interruption with 5CB3. The study confirmed the misoperation was due to 5RX8 control cable transients during 5CB3 reignitions.

Control and instrumentation were the same as applied in 5CB15 testing as a magnetic tape recorder and high frequency capacitive divider were unavailable. 5CT3 currents together with 5CVT12 and 5CVT13 voltages were monitored and recorded on light beam oscillographs.
5.3.1 5CB3 Test Observations

5CB3 current chopped up to a maximum of 80 A during the test program. Since the phase reactor tanks of 5RX3 are grounded, capacitance of the neutral reactor bushing and winding would not significantly affect the load network capacitance observed at the breaker. Maximum chopping number calculated using 9800 pF as estimated for 5RX11 is $80.8 \times 10^4$ overall or $33 \times 10^4$ for a single interrupter.

Figure 5.37 shows 5CB3 Test Trip 4 where all phases interrupted successfully with the aid of surge arrester operation clamping recovery voltage following current chopping. As with 5CB15, surge arresters operated in conjunction with each successful current interruption.

Suppression peak reignitions were common for short arcing times due to the rapidly rising current chopping characteristic of 5CB3. However, as with 5CB15, second parallel oscillations were frequently interrupted successfully. The recovery voltage reignitions observed with 5CB3 occurred at higher voltages than 5CB15. For example in figure 5.38 showing 5CB3 Test 7, a suppression peak reignition at 150 kV is interrupted only to be followed by a 780 kV recovery voltage reignition at 4.2 ms arcing time. By contrast, the withstand voltage at 5 ms arcing time for 5CB15 is only about 300 kV. The rapidly rising reignition characteristic of 5CB3 resulted in higher reignition voltages and hence more severe reignition transients than observed with other breakers tested. This is predicted by equation 2.22 where larger second parallel oscillation voltages result if reignition voltage $V_r(o) - V_s(o)$ is large. In numerous 5CB3 tests, surge arrester operations actually occurred at reignition. The A phase recovery voltage reignition of figure 5.38 for example, caused surge arrester operation during second parallel oscillation. Arrester operation produced a $-370$ A current impulse of 0.6 ms duration before resealing after which 60 Hz current flow was re-established. In contrast to arrester operating at a suppression peak, operation on a recovery voltage reignition does not aid interruption as flashover of the opening contacts has already occurred. Under these circumstances, interruption is less likely to occur until the next 60 Hz zero approaches. Final current chopping at about 65 A ensures arrester operation leading to successful interruption in figure 5.38.
Figure 5.37: 5CB3 Test Trip No. 4
Figure 5.38: 5CB3 Test Trip No. 7
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Figure 5.39: 5CB3 Arcing Characteristic

Figure 5.40: 5CB3 Current Chopping Characteristic
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The QUAL and SYP signals are from the two conductors associated with the 5RX8 pressure relief micro-switch device which had previously caused false tripping during 5RX3 switching with 5CB3. Transient voltages are clearly induced on these conductors at the moment of 5CB3 reignitions even though 5RX8 is some 160 m from 5RX3. D.C. offsets suggesting ground plane shift also appear and decay slowly away following reignment. Peak voltages of 4.2 kV were measured at 5RX8 on these test conductors using shielded high speed memory voltmeters. During several of the severest recovery voltage reignitions where arresters operated, the 5RX8 pressure relief device operation recurred, confirming 5CB3 reignitions as the original cause. A clear proximity effect was noted in that reignitions in the 5CB3 phase closest to station control cable trenches, produced the largest transient voltages on 5RX8 control wiring. Reignition produces a very steep current impulse as capacitive charges on either side of the breaker equalize during the second parallel oscillation. This impulse undoubtedly propagates in part through the station ground mat, introducing noise in control cables and causing localized shifts in ground
potential as it passes. At 5RX8, potential differences resulting between pressure relief micro-
switch conductors and the reactor tank following severe 5CB3 reignitions were sufficient to
cause micro-switch dielectric failure having the same effect as contact closure.

As with 5CB15 testing, reignition on one phase appeared to couple currents to adjacent
phases. The comments given in section 5.2.1 regarding the nature of these currents apply here
as well. Further insight will be given into currents coupled to adjacent phases during reignition
in the simulation work presented in Chapter 6.

5.3.2 Characterizing 5CB3 Performance During 5RX3 Switching

The arcing, chopping and reignition characteristics of 5CB3 are shown in figures 5.39, 5.40,
and 5.41 respectively. As with 5CB15 for small point on wave times, arcing time is sufficiently
long to ensure arrester operation through substantial current chopping levels. As point on wave
increases to about 6 ms, smaller arcing time with reduced current chopping prevents arrester
operations. Current chopping however invokes reignitions, translating the breaker to the second
arcing characteristic. Minimum arcing time will vary due to the statistical nature of gapped
arrester operating voltages. Overall, the 5CB3 arcing characteristic is very similar to 5CB15
with the exception of a smaller maximum arcing time due to higher current chopping capability.

Although 5CB3 current chopping rises slightly more rapidly and to a higher maximum than
5CB15, the forms of the chopping characteristics are much the same. The most significant
difference between the breakers is a more rapid rise of reignition voltage on the part of 5CB3.
Three ms following contact separation 5CB3 withstands in the order of 800 kV compared to
150 kV in the case of 5CB15. This is reflected in a reduced incidence of multiple reignitions of
5CB3 compared to 5CB15. Further, recovery voltage reignitions occurred at much higher levels
with 5CB3, increasing the second parallel oscillation voltage severity. Surge arrester operations
on recovery voltage reignitions were hence common during 5CB3 reactor switching tests. Due
to the high chopping capability with resulting arrester operations, ultimate recovery voltage
withstand level could not be determined. In spite of the relative superiority of its reignition
characteristic, 5CB3 in this application requires reactor surge arresters for both insulation protection as well as reliable interruption.

5.4 Nicola 5D44 Testing

5D44 is one of five SF₆ puffer type load interrupting switches applied at Nicola substation for dedicated reactor switching. Each phase consists of two SF₆ interrupters and a series connected disconnect switch. Within each interrupter are three contact sets in series, each consisting of a main contact and parallel arcing contact. During interruption, the main contacts part, transferring current to the arcing contacts where it is eventually quenched in an SF₆ puff and interrupted.

Electrically, 5D44 is situated with respect to 5RX4 in identical fashion to 5D51 with respect to 5RX11 in figure 5.26. Following a 5RX3 switching failure, the manufacturer confirmed 5D44 was not rated for switching four reactor schemes. This was resolved by adding a switch to automatically bypass the neutral reactor during 5RX3 switching. Earlier models of the SF₆ switch behaved somewhat erratically as shown in the arcing characteristic of figure 5.42. The device operated primarily on characteristics (ii) and (iii) making arcing time quite unpredictable. The fourth characteristic actually represents a complete failure which occurred during testing. This unstable behavior was determined by the manufacturer to be due to an uneven division of recovery voltage between the two series interrupters per phase. Once modified to correct the unbalance, the device was tested on site to determine its suitability for three reactor grounded bank switching duty. The neutral reactor 5NR4 was by-passed for the test program.

Individual phases of the load interrupter are coupled with drive shafts and gear reduction units to actuate the entire assembly from a single motor drive. Timing spread between the phases can be significant due to mechanical dead band in elements of the switch drive mechanism. Arcing times can hence be difficult to deduce because time between trip command application and contact parting varies significantly between operations. To overcome this problem, B and C phases of the switch were fitted with opto-mechanical transducers providing
contact travel traces from which contact separation could be deduced.

5.4.1 5D44 Test Observations and Characteristics

A typical interruption with the modified circuit switcher is shown in figure 5.43 exhibiting essentially no current chopping and no suppression peak as a result. The load side oscillation is virtually unaffected by adjacent phases since the bus between switcher and reactor is less than 15 m. Phase to Phase capacitance $C_l$ is hence negligible and the positive mode oscillation described by equation 3.34 is non-existent. Oscillation frequency is 1360 Hz fixed by single phase reactor inductance and the combined effective capacitances of reactor bushing, winding and surge arrester.

Although soft interruptions void of current chopping were mostly the case, recovery voltage reignitions were frequently observed. Figure 5.44 is an example of an interruption with no current chopping followed by a recovery voltage reignition at 400 kV some 0.4 ms later. The resulting modified switch arcing characteristic of figure 5.45 shows a minimum arcing time in
Chapter 5. Reactor Switching Field Tests

Figure 5.43: 5D44 Typical Test Interruption

Figure 5.44: 5D44 Test Interruption with Recovery Voltage Reignition
excess of 8.33 ms in contrast to all air blast breakers tested whose minimum arcing times were less. Two consecutive reignitions before successful interruption were hence common during 5RX3 switching. With more appropriate division of recovery voltage between interrupters, 5D44 operates on the single stable characteristic (ii) in figure 5.41 prior to modification. The reignition characteristic of 5D44 of figure 5.46 suggests recovery voltage withstand capability builds more slowly than any of the air blast breakers tested, accounting for the larger minimum arcing time. The stable arcing characteristic clearly shows the withstand capability is eventually sufficient for successful interruption even though higher load side oscillation frequency produces larger RRRV. With little or no current chopping, maximum TRV is effectively controlled and the switcher reignition characteristic is more than satisfactory for reliable reactor switching in this application.
5.5 Switchgear Field Testing Summary

In presenting these test results, the transient phenomena of current chopping, load side oscillation, reignition, and interaction of natural reactor network modes due to interphase coupling, have been graphically demonstrated. While station current transformers were not fast enough to show arc current instability oscillations, or the details of second parallel oscillation currents, valuable information on air blast breaker current chopping behavior was derived.

To contrast current chopping ability, single unit maximum chopping numbers are listed in table 5.2 for these tests and for tests by others. Maximum chopping numbers are of prime concern since they govern worst case load side oscillation overvoltages and hence the likelihood of reignition. The single unit chopping numbers measured for Nicola breakers are in line with those published by others for breakers with and without opening resistors. 5CB25 resistor switch chopping was markedly reduced over 5CB3 and 5CB15 as predicted theoretically in
Table 5.2: Contrasting Air Blast Breaker Current Chopping Measurements

<table>
<thead>
<tr>
<th>System Voltage kV</th>
<th>Interrupters n</th>
<th>( I_{ch} ) A</th>
<th>( C_L ) nF</th>
<th>( \lambda_1 ) ( AF^{-\frac{1}{2}} \times 10^4 )</th>
<th>( R_b ) Ω</th>
<th>Reference</th>
</tr>
</thead>
<tbody>
<tr>
<td>500</td>
<td>6</td>
<td>70</td>
<td>9.8</td>
<td>28.9</td>
<td>0</td>
<td>Present Work</td>
</tr>
<tr>
<td>500</td>
<td>6</td>
<td>80</td>
<td>9.8</td>
<td>33</td>
<td>0</td>
<td>Present Work</td>
</tr>
<tr>
<td>500</td>
<td>6</td>
<td>35</td>
<td>4.5</td>
<td>21.3</td>
<td>0</td>
<td>[15]</td>
</tr>
<tr>
<td>750</td>
<td>8</td>
<td>70</td>
<td>8.5</td>
<td>26.8</td>
<td>0</td>
<td>[15]</td>
</tr>
<tr>
<td>500</td>
<td>4</td>
<td>24</td>
<td>9.8</td>
<td>12</td>
<td>400</td>
<td>Present Work</td>
</tr>
<tr>
<td>735</td>
<td>8</td>
<td>10.5</td>
<td>2.6</td>
<td>7.2</td>
<td>1050</td>
<td>[6]</td>
</tr>
</tbody>
</table>

Appendix E and reported by others. 5CB25 was however unsuitable for reactor switching for dielectric reasons manifested in unstable arcing and reignition characteristics. 5CB25 test results illustrate the effectiveness of opening resistors in reducing chopping currents.

5CB3 and 5CB15 field tests clearly show the usefulness of gapped surge arresters in limiting both suppression peak and reignition overvoltages while ensuring successful interruption by limiting breaker TRV. In spite of high chopping currents, surge arresters made reactor switching a stable duty ensuring maximum arcing times under one cycle. By contrast, the subdued current chopping behavior of 5D44 demonstrated how well suited SF₆ puffer devices are to shunt reactor switching applications if acceptable reignition characteristics can be achieved.

Arcing, current chopping and reignition characteristics are significant results of the field testing extracted through analysis of the recorded waveforms. These characteristics defy tidy mathematical description due to the following:

- Time to contact separation from trip initiation generally varies ± 0.5 ms about a mean value, adding variability to estimation of arcing time.

- Current chopping number is statistically random along with being significantly arcing time dependent.
• Flashover voltage of an opening contact air gap is statistically random.

• Air blast pressure and velocity and consequentially arc quenching effectiveness, will not be precisely repeatable.

These characteristics will nevertheless be very useful in accounting for switchgear behavior in computer simulation of reactor switching transients to be considered in the following chapter.

Arcing characteristics derived for each device show clearly the stability with which the reactor network interruption duty is performed as well as the shortest and longest clearing times which can be expected in the application studied.
Chapter 6

Simulating Reactor Switching to Predict Circuit Breaker Performance

This chapter examines some essential aspects of reactor switching simulation. Various approaches are considered and chosen for studying the important transients associated with current chopping, load side oscillations and breaker reignitions. These are presented in the contexts of:


2. Predicting circuit breaker performance during reactor interruption.

Cases chosen for simulation pertain directly to tests discussed in the previous chapter. In particular, test interruptions will be reconstructed to verify a circuit breaker performance prediction technique.

The analyses of load side oscillation in Appendix B and reignition in Appendix C, were based on simple lumped reactor network models. While this gave general ideas of what may be expected, the distributed nature of the reactor network and source side station busses can significantly alter lumped parameter results especially in the case of reignition. Where interphase coupling is not negligible, analytical methods can be very difficult to apply. If the network of concern can be modelled adequately, simulation will predict load side oscillation, breaker recovery voltage, and reignition transients where lumped parameter analysis is cumbersome or cannot be justified.

6.1 A Method for Predicting Breaker Performance During Reactor Interruption

In Chapter 4, a practical method of measuring circuit breaker current chopping and reignition characteristics was outlined. By application to field test data, characteristics were derived in
Chapter 5 for four devices applied to 500 kV reactor switching in a B.C. Hydro substation.

Armed with these data, and a detailed knowledge of the reactor network, a simulation facility such as EMTP may be used to estimate or predict breaker performance on reactor interruption. First, the reactor network must be modelled to offer an accurate representation at expected load sided oscillation frequencies. Performance for a particular contact parting time (point on wave) may then be predicted as follows:

1. A.C. Voltage sources are shifted such that the start of the simulation correspond to the instant of breaker contacts parting in order that study time is the simulated arcing time. Currents at the start then correspond to the point on wave time to be studied.

2. The first phase current to approach zero in the simulation is superimposed on the breaker current chopping characteristic, and intersection determines current chopping for the first interruption. If the capacitance of the reactor network being considered is not the same as where the chopping characteristic was measured, chopping levels must be adjusted using equation 2.10.

\[ i_{ch2} = i_{ch1} \sqrt{\frac{C_2}{C_1}} \]

3. Simulating the estimated first phase current chopping, resulting recovery voltage is superimposed on the breaker reignition characteristic. If intersection occurs, interruption fails and reignition voltage can be estimated at the point of intersection.

4. Simulation continues with the first phase interrupted if successful or representing it's reignition if indicated until the next phase current approaches zero. By superimposing on the chopping characteristic as the second current approaches zero, the second interruption chopping level is estimated.

5. Steps 2 through 4 are then repeated until all phases have successfully interrupted or one or more phases have reignited several times. In the later case, interruption must be deemed unsuccessful.
6. If the details of reignition are of interest, they should be simulated using a reactor representation suited to the high frequencies associated with second parallel oscillation. If only the fact reignition has occurred is to be represented, the low frequency model is sufficient.

Ultimately, this approach should be useful in predicting expected transients and circuit breaker performance if the reactor network were altered, or the same breaker applied elsewhere. Chapter 5 graphically demonstrated the very different results obtained switching the same reactor network (5RX11) with two different breakers (5CB15 and 5CB25). Clearly, circuit breaker characteristics must be incorporated into simulations if results are to reflect such differences.

In simulating interruption to estimate breaker performance, reactor network and source representations must be reasonably accurate at expected load side oscillation frequencies. Since these are typically less than 5 kHz, fairly simple representations can be used [6]. Realistic simulation of load side oscillation after current chopping is vital in order to:

1. Accurately predict reignition or successful interruption of a breaker pole.
2. Correctly invoke surge arrester operations at higher current chopping levels. Surge arresters can limit recovery voltage to levels which a breaker can comfortably withstand. Failing to properly invoke surge arrester operation in interruption simulations can mean the difference between correct and erroneous breaker performance prediction.

Accurately determining reignition transients in multi-phase reactor networks, requires significant modelling detail. High frequency reactor and bus representation are mandatory as is detailed representation of the substation on the source side of the breaker [1]. Fortunately, such detail is not essential if the goal is simply predicting whether reignition occurs. Several approaches to modelling for load side oscillation and reignition study are discussed in the following sections.
6.2 Modelling for Current Chopping and Load Side Oscillation Simulation

The most important aspects of modelling for current chopping and load side oscillation were found to be the reactor and load side bus representations. Of the breakers tested, only 5CB25 exhibited chopping levels low enough to preclude arrester operations allowing load side oscillation to be readily observed. 5CB25 interruptions were hence used as initial simulation examples to refine reactor and load side bus representations.

6.2.1 Three Phase Grounded Reactor (5RX11) Modelling for Load Side Oscillation Study

The experience of others [1] suggests that a reasonably simple reactor model should suffice in the study of load side oscillation. Accordingly, the circuit of figure 6.47 was chosen with component values as follows:
Chapter 6. Simulating Reactor Switching to Predict Circuit Breaker Performance

Figure 6.48: Load Side Bus Model Geometry

\[ C_{RA} \quad 2800 \text{ pF} \quad \text{Reactor Winding, Bushing, and Arrester Capacitance} \]
\[ R_1 \quad 2.85 \Omega \quad \text{Reactor Copper Losses} \]
\[ R_2 \quad 1.5 \text{ M\Omega} \quad \text{Damping due to Core Losses} \]
\[ L \quad 5.41 \text{ H} \quad \text{Reactor Inductance} \]

\( R_1 \) and \( L \) were calculated from the reactor nameplate ratings at 60 Hz while \( C_{RA} \) and \( R_2 \) were estimated from the 5D44/5RX4, switching tests of Chapter 5 since 5RX4 and 5RX11 phase reactors are identical and short bus lengths ensured no phase interactions. Damping could hence be estimated using equation B.73 without distortion due to phase interactions. From figure 5.44, application of Appendix B equations to load side oscillation damping yielded \( C_{RA} \) and \( R_2 \).

This approach coupled with the bus modelling detailed in the next section gave very good simulation agreement with load side oscillations observed during 5CB25 testing.
6.2.2 Buss Representation for Load Side Oscillation Study

An elevation view of the load side buswork was given in figure 5.26. The bus between breaker and reactor is made up of an overhead strain bus and two low bus sections. Three general methods of modelling the load side bus sections were initially identified:

- Balanced Bus Representation
- Flat Line Bus Representation
- Six Phase Flat Line Bus Representation

Balanced modelling assumes mutual impedances and admittances are equal amongst the bus phases. Flat line modelling assumes conductors lie in the same plane and mutuals are not equal. Coupling between strain and low bus sections is ignored in both instances. Six phase flat line modelling incorporates unequal mutuals and accounts for coupling between strain and low busses by using two six phase line sections. Load bus model geometry is then as shown in figure 6.48.

From nameplate information, 5CVT25 capacitance was assigned a value of 5000 pF and located as shown in figure 6.48. Disconnect switch support stack insulators of approximately 50 pF each, were lumped at the ends of each bus section. Using a station ground resistivity of 88.8 Ω - m, the UBC Line Constants Program was used to calculate load bus data for the three representations at 60 Hz. Coupled π models were used to represent each discrete bus section. To attempt to account for the station ground grid, ground conductors spaced 2 meters apart were placed 0.01 m above the ground beneath each bus section in the line constants calculation. To contrast the bus modelling methods, load side oscillation was simulated for 5CB25 Test 5. Unfortunately a high quality reproduction was not available for this test, so a copied field record is shown in figure 6.49. As with all other 5CB25 tests, predominant load side oscillation frequencies in test 5 were 660 Hz modulated by 20 Hz corresponding to zero and positive mode natural frequencies of 680 and 640 Hz respectively.
Figures 6.50, 6.51 and 6.52 respectively show C phase load side oscillation simulated using each of the three bus modelling techniques. All yield natural frequencies of 660 Hz and 20 – 22 Hz as observed in field tests confirming network capacitances have been estimated quite accurately. The six phase flat line model gave the strongest modulation and agreed most closely with the field results in later simulations. Each method yielded the same suppression and recovery peaks, so breaker TRV would not be significantly affected by the choice of bus models for the solidly grounded case. Based on this comparison, six phase flat line load bus modelling was applied for all subsequent load side oscillation simulations.

6.2.3 Source Representation for Load Side Oscillation Simulation

As expected, source representation for load side oscillation was not found to be critical. Beginning with fairly detailed transmission interconnections to adjacent stations, the source representation was progressively simplified to that shown in figure 6.53.

Use of the equivalent characteristic impedance of adjacent circuits in parallel with a 60 Hz
Figure 6.50: 5CB25 Test 5: C Phase Balanced Bus Simulation

Figure 6.51: 5CB25 Test 5: C Phase Flat Line Bus Simulation
Chapter 6. Simulating Reactor Switching to Predict Circuit Breaker Performance

Thevenin equivalent source representation is supported by CIGRE Working Group 33.02 [5] with the qualifying assumption that reflections returning on adjacent interconnections cannot affect results. Such is certainly true for load side oscillation transients since breaker poles will have opened. Characteristic impedances were calculated from B.C. Hydro line data for the seven 500 kV circuits adjacent to 5L98. Source capacitance $C_s$ includes CVT’s, CT’s, surge arresters, stack insulators and bus work on the source side of 5CB25. The four reactor network represents NIC four reactor schemes in service during 5CB25 tests. This source model was used for the load side oscillation simulations contrasting bus models in the previous section.

6.3 Modelling for Reignition Simulation

Second parallel oscillation frequencies observed during reignition are typically in the range 100 kHz – 500 kHz. 5CB25 reignition oscillations were measured from high speed voltage divider signals to be 350 kHz – 420 kHz. These are high frequency events which cannot be
properly simulated with 60 Hz buss and reactor representations. Source representations must also be carefully re-evaluated.

As described in Chapter 2, reignition overvoltages can be substantial and arrester operations as observed during 5CB3/5RX3 tests are not unusual. While detailed simulation of reignition is not essential to judging successful interruption, it is useful for assessing insulation stresses and surge arrester duty.
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6.3.1 High Frequency Load Bus Modelling

Load bus data was calculated at 350 kHz for the three bus modelling approaches using the UBC Line Constants Program. Load bus representations significantly affected simulated second parallel oscillation frequencies. The six phase flat line model gave the best agreement with field tests as will be shown in subsequent sections. Choice of bus model also affected the magnitudes of currents coupled to adjacent phases during second parallel oscillation. Due to the encouraging results obtained with initial 5CB25 reignition simulations, the 6 phase 350 kHz load bus representation was retained for subsequent reignition study.

6.3.2 High Frequency Reactor Modelling

At high frequencies, the reactor winding behaves as a distributed element exhibiting propagation delay for steep wave fronts [1]. This can result in substantial turn to turn stresses besides conventional phase to ground insulation stresses. In order to represent this tendency the reactor
Figure 6.55: Distributed High Frequency Substation Source Model

ALL DIMENSIONS ARE METERS (NTS)

Zc REPRESENTS LINE CHARACTERISTIC IMPEDANCE
MATRIX IN SERIES WITH 3 PHASE SOURCES

CTR* 600 pF TRANSFORMER BUSHING

C rx 2800 pF REACTOR AND SURGE ARRESTER

CV 6000 pF CVT, CB BUSHINGS, STACK INSULATORS

S 50 pF STACK INSULATOR

STRAIN BUSS

LOW BUSS

HIGH BUSS
winding inductance and capacitance were distributed as shown in the model of figure 6.54. Incorporating the high frequency reactor model increased second parallel oscillation frequency but did not affect the magnitudes of second parallel oscillation voltages or currents. This will be demonstrated in subsequent sections during simulation of several field test reignitions.

6.3.3 High Frequency Distributed Source Representations

Reignition simulation by others suggests that distributed modelling of the substation network behind the reigniting breaker is necessary [6]. To test this principle, the distributed source model of figure 6.55 was used to represent the Nicola switch yard behind 5CB25. Low, high and strain bus sections were treated as equivalent π circuits with ground mat represented as in section 6.2.2. The high bus conductor is identical to that previously described for low bus with 6 m phase spacing and 15 m elevation above ground.

Source side bus data was calculated using the UBC Line Constants Program at 350 kHz and station ground resistivity of 88.8 Ω·m. The following arguments were then applied to complete the distributed station model:

- Line characteristic impedances were connected in series with sources at each line position as returning reflections were not expected to influence reignition simulations of 50 μs duration.

- Line shunt reactors were represented by winding, bushing and surge arrester capacitances only as their inductances had no influence on reignition simulation results.

- Transformers were represented by bushing and surge arrester capacitances only.

- Source voltages at identical angles were used at each line position to avoid large bus circulating currents during reignition simulation.

- Breaker bushing and disconnect switch stack insulator capacitances were lumped at each line position.
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- Low and High busses are supported by 50 pF stack insulators roughly every 20 m. These were lumped at the ends of each bus section.

- All line CVT’s were assumed identical to 5CVT25 (5000pF).

Use of the distributed source model increased second parallel oscillation frequency to as high as 393 kHz, agreeing well with 5CB25 tests. Peak reignition voltage and current were reduced, presumably due to reflections within the substation source bus network.

Reignition simulations were necessarily performed with a very small time step (20 ns). To avoid very lengthy studies, simulations using 20 μs time step were stopped just short of reignition, and initial conditions passed to new studies at 20 ns time step, to simulate reignition details. EMTP does not support use of initial conditions with distributed parameter lines at this time, and effects of replacing bus T sections with distributed parameter lines could not be tested.

6.4 Verification of Breaker Performance Prediction for Three Phase Grounded Reactor Switching

The breaker performance prediction approach outlined in section 6.1 will be verified for solidly grounded networks by re-constr.ucting several 5CB15 and 5CB25 interruptions recorded during field tests. Several reignitions will also be considered in detail to highlight the modelling principles and observations mentioned briefly in section 6.3.

6.4.1 5CB25 Test 5 Reconstruction

This was chosen as an example of a normal 5RX11 interruption with 5CB15. Using the proposed technique, Test 5 interruption was to be reconstructed for contrast to the actual interruption test results. Test 5 point on wave timing has breaker resistor contacts parting as A phase current passes through a positive going zero such that A phase point on wave time is 0 ms. Reconstruction progressed in the following steps:
Chapter 6. Simulating Reactor Switching to Predict Circuit Breaker Performance

Figure 6.56: 5CB25 Test 5 Reconstruction: Estimating Current Chopping

Figure 6.57: 5CB25 Test 5 Reconstruction: Predicting Reignition
Figure 6.58: 5CB25 Test 5 Reconstruction: Simulated Voltages

Figure 6.59: 5CB25 Test 5 Reconstruction: Simulated Currents
1. Estimated C phase current chop by initial C phase current intersection with 5CB25 current chopping characteristic was between 3.6 - 8.6 A. The higher value was chosen as a worst case.

2. Superimposing the resulting C phase recovery voltage on 5CB25 reignition characteristic, C phase reignition 1 was predicted between 180 - 420 kV and the higher level chosen as a worst case reignition.

3. Incorporating C phase reignition 1, simulation continued to estimate B phase current chop 1 between 7.8 - 15.7 A. The higher value was chosen as a worst case.

4. The resulting B phase recovery voltage superimposed on 5CB25 reignition characteristic, predicted successful B phase interruption.

5. Simulation continued to estimate A phase current chop 1 between 12.0 - 22.5 A choosing the higher value as a worst case.

6. The resulting A phase recovery voltage superimposed on 5CB25 reignition characteristic predicted successful A phase interruption.

7. Simulation continued to estimate C phase current chop 2 between 16.0 - 24 A choosing the higher value as a worst case.

8. The resulting C phase recovery voltage superimposed on 5CB25 reignition characteristic predicted marginal interruption success as this second attempt occurred in the unstable region. The interruption was deemed successful since TRV was just under the unstable characteristic, and that a lower chopping current would have been more probable.

Reconstruction steps are summarized in figures 6.56 and 6.57 predicting successful interruption of A and B phases with a somewhat marginal C phase interruption after one reignition. Figure 6.56 shows superimposing phase currents nearing zero at each attempted interruption to estimate likely current chopping levels for each. Resulting breaker TRV results plotted to
just beyond recovery peaks are shown in figure 6.57 where successful interruption or reignition was deduced. Absolute values of current and recovery voltage are plotted in each case. An overall simulation of the reconstructed interruption is shown in figures 6.58 and 6.59. This is not intended to show reignition details which will be studied in the next section.

Unfortunately a high quality plot of field waveforms was not available for 5CB25 Test 5 but the C phase results of figure 6.49 copied from records show the interruption sequence for that phase. From careful scrutiny of Test 5 traces actual interruption proceeded as follows:

1. C phase interrupted chopping 5 A and reignited at 420 kV.

2. B phase interrupted chopping 12 A and withstood the resulting recovery voltage successfully.

3. A phase interrupted chopping 18 A and withstood the resulting recovery voltage successfully.

4. C phase interrupted a second time chopping 22 A and withstood the resulting recovery voltage successfully.

Hence the performance prediction technique correctly reconstructed the test 5 interruption even though worst case current chopping levels, offering the highest chance of reignition, were assumed. Having demonstrated the performance prediction method, the 5CB25 Test 5 C phase reignition will be considered in more detail.

**6.4.2 Considering 5CB25 Test 5 C Phase Reignition**

In both field test and simulated reconstruction results, a 420 kV reignition occurred on C phase following the first current chopping attempt. Simulations of the reignition were attempted using six phase 350 kHz flat line load busses and high frequency reactor model previously discussed. Initially, a lumped source representation was tested and later replaced with the distributed substation model of figure 6.55.
Figure 6.60: 5CB25 Test 5 C Phase Reignition – Lumped Source Simulated Voltages

Figure 6.61: 5CB25 Test 5 C Phase Reignition – Lumped Source Simulated Currents
Results with lumped source representation are given in figures 6.60 and 6.61. Dominant second parallel oscillation frequencies are 333 kHz modulated by 33 kHz suggesting natural mode frequencies of 366 kHz and 300 kHz due to phase interaction. Figure 6.62 contrasts the C phase voltage at 5CVT11 (DROPC) to the higher 5RX11 (RXC) voltage due to reflections on the bus between reactor and CVT. The C phase current impulse at reignition peaked at about 1060 A and approximately 120 App coupled to adjacent phases as shown in figure 6.61. Second parallel oscillations clearly involved adjacent phases even with the weak coupling presented by the approximately 150 m of air insulated load bus in the 5CB25/5RX11 configuration.

Results of stimulating the C phase reignition using the high frequency distributed source model are shown in figures 6.63 and 6.64. Voltage oscillation at the reactor does not differ significantly from lumped source results. Dominant second parallel oscillation frequencies are 348 kHz and 30 kHz suggesting natural mode frequencies of 378 kHz and 318 kHz increased over lumped source results due to distribution of source capacitance. Peak reignition impulse current was reduced to approximately 820 A and coupled adjacent phase currents reduced to 120 App.
Figure 6.63: 5CB25 Test 5 C Phase Reignition – Distributed Source Simulated Voltages

Figure 6.64: 5CB25 Test 5 C Phase Reignition – Distributed Source Simulated Currents
Reduction of peak currents and the numerous small steps observed in the simulated current records were assumed to be the result of reflections returning from within the distributed source representation.

Clearly, use of the distributed source model has a significant effect on the peak reignition current. In either case, second parallel frequencies observed agreed very well with the 350 - 420 kHz observed in 5CB25 field tests but distributed source results were better in this regard. The 400Ω opening resistors on each phase significantly damped energy exchange giving reignition the appearance of an exponential voltage equalization with second parallel oscillation superimposed. Since, oscillation was so heavily suppressed, current zero crossings and hence second parallel oscillation interruption would be unlikely. In fact few second parallel oscillation interruptions were observed during 5CB25 tests in contrast to 5CB15 or 5CB3 supporting this simulation result.

Significant overvoltages were not observed on 5CB25 reignitions during field testing. This is supported by simulation results of figures 6.60 and 6.63 which revealed minimal (1.04 pu) reignition overvoltages.

6.4.3 5CB25 Test 4 Reconstruction

To further evaluate the performance prediction technique, 5CB25 Test 4 was chosen as an example of the breaker failing to interrupt correctly. Test 4 point on wave timing has resistor contacts parting just before a positive going A phase zero crossing such that A phase point on wave time is 8 ms. In effect, Test 4 point on wave is 0.33 ms in advance of the Test 5 point on wave of the previous section.

Reconstruction progressed in the following steps:

1. Estimated A phase current chop 1 as 0 A by initial A phase current intersection with 5CB25 chopping characteristic.

2. Superimposing the resulting A phase recovery voltage on 5CB25 reignition characteristic, predicted A phase reignition 1 between 0 and 20 kV. The higher value was selected as a
Figure 6.65: 5CB25 Test 4 Reconstruction: Estimating Current Chopping

Figure 6.66: 5CB25 Test 4 Reconstruction: Predicting Reignition
Figure 6.67: 5CB25 Test 4 Reconstruction: Simulated A Phase Voltage

Figure 6.68: 5CB25 Test 4 Reconstruction: Simulated B Phase Voltage
Figure 6.69: 5CB25 Test 4 Reconstruction: Simulated C Phase Voltage

Figure 6.70: 5CB25 Test 4 Reconstruction: Simulated Currents
worst case.

3. Incorporating A phase reignition 1, simulation continued to estimate C phase current chop 1 between 4.5 – 10 A. The higher value was selected as a worst case.

4. Superimposing the resulting C phase recovery voltage on the 5CB25 reignition characteristic, predicted C phase reignition 1 between 200 – 450 kV. The higher value was selected.

5. Incorporating C phase reignition 1, simulation continued to estimate B phase current chop 1 between 7.5 – 19 A. The higher value was selected as a worst case.


7. Simulation continued to estimate A phase current chop 2 between 12.5 – 22 A. The larger value was chosen as a worst case.

8. Superimposing the resulting A phase recovery voltage on 5CB25 reignition characteristic predicted successful interruption.

9. Simulation continued to estimate C phase current chop 2 between 16.5 and 24 A. The higher value was selected as a worst case.

10. Superimposing the resulting C phase recovery voltage on 5CB25 reignition characteristic, predicted C phase reignition 2 between 1000 – 1100 kV. The higher value was chosen. This reignition occurred in the unstable region of the reignition characteristic indicating strong likelihood of further failures.

11. Incorporating C phase reignition 2, simulation continued to estimate C phase current chop 3 between 20 – 22 A. The higher value was selected as a worst case.

12. Resulting C phase recovery voltage superimposed on 5CB25 reignition characteristic predicted C phase reignition 3 between 800 – 1050 kV. The higher value was selected.
13. Incorporating C phase reignition 3, simulation continued to estimate C phase current chop 4 between 20 – 24 A. The higher value was selected as a worst case.

14. Resulting C phase recovery voltage superimposed on 5CB25 reignition characteristic predicted C phase reignition between 850 – 1000 kV. Further simulation was pointless as interruption failure was clearly predicted.

Reconstruction steps are shown in figures 6.65 and 6.66 demonstrating superimposition on breaker characteristics to determine current chopping levels and predict successful interruption versus reignition. Reconstruction correctly predicted failure to interrupt C phase as shown in the actual test 4 waveforms of figure 5.30. A minor discrepancy is that C phase current chop 4 led to successful interruption in the field test. This was due to the actual chopping being less than the 24 A chosen in the reconstruction, and that reignition voltages are difficult to ascertain in the unstable region beyond 12 ms arcing time. [p]

Overall reconstruction assuming C phase survives the current chop 4 recovery voltage is shown in figures 6.67, 6.68, 6.69 and 6.70. Aside from predicting C phase failure correctly, the C phase load side oscillation following final clearing agrees very well with the test results of figure 5.30. Predominant load side oscillation frequencies are 660 Hz and 20 Hz as observed throughout 5CB25 testing. Chopping currents and reignition voltages selected in the reconstruction differed slightly from the actual test due to scatter in the breaker characteristics. This in no way detracts from the accuracy of the overall performance prediction which was reconstructed without regard to the actual test interruption. In practice there would be no prior results to consult and the prediction method would provide a means of estimating worst and best case circuit breaker performance.

6.4.4 5CB15 Test 4 Reconstruction

5CB15 Test 4, a typical 5CB15 interruption with surge arresters assisting due to extremely high chopping levels, was given in figure 5.33. The reignition characteristic given in figure 5.36 shows 5CB15 recovery voltage withstand capability rises rather slowly. Together with high
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Figure 6.71: 5CB15 Test 4 Reconstruction: Estimating Current Chopping

Figure 6.72: 5CB15 Test 4 Reconstruction: Predicting Reignition
current chopping levels, 5CB15 is prone to multiple reignitions and second parallel current interruptions as captured in figure 5.33. This behavior is often observed with air blast breakers, but cannot be predicted confidently since it is the result of complex interactions between an intense cooling mechanism and the high frequency second parallel oscillation current. Whether interruption will occur at any particular second parallel oscillation current zero is impossible to judge although resulting transients can be simulated with a suitable high frequency network representation. However, general interruption performance can still be accurately predicted since in most cases, multiple reignitions lead to re-established 60 Hz current. By assuming largest current chopping levels, and that reignition always leads to renewed 60 Hz current flow, a worst case prediction of breaker arcing time results. In practice, arcing time may be reduced if second parallel oscillation current is interrupted. However, if the assumption that reignition always restores 60 Hz current leads to unacceptable predicted arcing times, it would be unwise to consider the breaker for the application under study.

The current limiting gapped surge arresters applied to Nicola reactors are very difficult to model. Since the main intention was to observe their effects on recovery voltage, arresters were represented as a voltage controlled switch in series with $433 \ \Omega$ derived from manufacturers specifications. Maximum spark over voltage is specified at $885 \ \text{kV}$ but gapped arresters are known to operate at lower levels for rapidly rising wavefronts such as reignition. For the purposes of this study, $885 \ \text{kV}$ operation was assumed.

In 5CB15 Test 4 contacts separate 0.5 ms prior to a positive going C phase zero crossing such that C phase point on wave time is 7.8 ms. The interruption was reconstructed as follows:

1. Estimated C phase current chop 1 by initial C phase current intersection with 5CB15 chopping characteristic was between $8.5 - 11 \ \text{A}$. The higher value was selected as a worst case.

2. Superimposing the resulting C phase recovery voltage on 5CB15 reignition characteristic, suppression peak reignition was predicted between $30 - 40 \ \text{kV}$. The higher value was selected.
3. Simulation continued to estimate B phase current chop 1 between 30 - 39 A. The higher value was chosen as a worst case.

4. The resulting B phase recovery voltage was superimposed on the 5CB15 reignition characteristic to predict suppression peak B phase reignition 1 between 95 - 150 kV. The higher value was selected.

5. Simulation continued to estimate A phase current chop 1 between 48 - 56 A. The higher value was chosen as a worst case.

6. The resulting A phase recovery voltage superimposed on the 5CB15 reignition characteristic, predicted suppression peak reignition without an arrester. With an arrester in place, its operation ensured successful interruption by significantly limiting the recovery voltage.

7. Simulation continued to estimate C phase current chop 2 between 63 - 68 A. The higher value was selected as a worst case.

8. Simulating C phase current chop 2, the surge arrester operated before suppression peak. Superimposing resulting recovery voltage on the reignition characteristic showed successful interruption was easily achieved.

9. Simulation continued to estimate B phase current chop 2 between 68 - 71 A. The higher value was chosen as a worst case.

10. Simulating B phase current chop 2, the surge arrester operated before the suppression peak, limiting recovery voltage and securing successful interruption.

These steps are depicted in figures 6.71 and 6.72 showing superimposition of currents and recovery voltages on 5CB15 characteristics to estimate current chopping and predict success of interruption. Figure 6.72 shows clearly the assisting role of surge arresters in successful interruption. Recovery voltage A1 plotted with and without arrester, shows how the fully open
Figure 6.73: 5CB15 Test 4 Reconstruction: Simulated Voltages

Figure 6.74: 5CB15 Test 4 Reconstruction: Simulated Currents
withstand voltage specification (1600 kV) of the breaker would have been exceeded without an arrester operation.

Performance prediction effectively forecasted correct overall performance as observed in figure 5.33 even though the second parallel current interruption issue was avoided by worst case assumptions. The simulated reconstruction is shown in figures 6.73 and 6.74.

Substantial transient currents appeared in the simulation on adjacent phases during current chopping and reignition. This agrees with field tests to a degree but appears to the author to be somewhat excessive. In the case of reignitions for example, transient current zeroes occurred on both adjacent phases during simulation sufficient to cause virtual current chopping. Since virtual chopping was not observed in field tests, simulated adjacent phase transient currents seem excessive possibly due to:

1. Step size being too large in the overall reconstruction to capture high frequency events accurately.

2. Distributed source and high frequency reactor bus model are needed to simulate reignition transients accurately.

3. Arc resistances may have been larger than the 40 Ω assumed per phase, effectively damping and reducing coupled current oscillation magnitudes in the same fashion as the 400 Ω opening resistor in 5CB25 simulations.

4. Mutual load bus inductances calculated, may be larger than actual due to difficulties representing the three dimensional conductor spatial relationships.

5. The station ground grid was treated as a single node when constructing network models but it behaves more as a distributed element at high frequencies. This is a complicated problem, which was not considered in the present work.

Due to the limited frequency response of station current transformers, it is difficult to judge what adjacent phase transient currents might actually have been during reignition. Current
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Figure 6.75: 5CB15 Test 4 B Phase Reignition: Simulated Voltages

Figure 6.76: 5CB15 Test 4 B Phase Reignition: Simulated Currents
impulses observed during field tests may have been coupled to the CT secondaries via winding capacitance rather than true transformer action, masking real primary currents at those instants. Extensive precautions were taken to avoid instrumentation ground loops, but noise may still have been partially responsible for the large current impulses observed. However noise cannot be completely responsible, since the observed impulses appeared on current recording channels only.

6.4.5 Considering 5CB15 Test 4 B Phase Reignition

Using high frequency reactor and bus models together with distributed source modelling, 5CB15 Test 4 B phase reignition was simulated. Both the form of the second parallel oscillation and adjacent phase transient currents were of interest. Figures 6.75 and 6.76 show simulated reignition voltages and currents respectively. The predominant second parallel frequency is 375 kHz, in agreement with 5CB25 field tests. Voltage excursions beyond 1.0 pu are small as predicted by equation 2.23 for a small difference between source and load side potential at reignition.
Currents coupled to adjacent phases are somewhat smaller than Figure 6.74, supporting the point that modelling used for performance prediction and interruption reconstruction is not sufficiently detailed for reignition simulation. Though coupled currents still seem large (320 App), implied risk of virtual chopping is reduced.

6.5 Predicting Four Reactor Scheme Switching Performance

In subsequent sections the proposed breaker performance prediction technique will be tested and verified for four reactor scheme switching by reconstruction of a 5CB3/5RX3 interruption. Clear differences in load side oscillation phase interactions compared to solidly grounded schemes are expected with the inductive coupling introduced by the neutral reactor. This will be considered before attempting four reactor network interruption reconstructions.

6.5.1 Considering 5RX3 Load Side Oscillations

Figure 5.37 depicts 5CB3 Test 4 which is a case of 5RX3 interruption without reignition. Current chopping levels were 40 A, 65 A and 29 A for A, B and C phases respectively. In each case, current chopping invoked surge arrester operation, and the differences in load side oscillation with a neutral reactor were effectively masked. To examine the difference with simulations, the 3 reactor model given in figure 6.47 was replaced with the 4 reactor representation of figure 6.77. Further, to avoid arrester operations, chopping levels were reduced to 30% of those measured in Test 4 and the Test 4 point on wave used in simulation. Figures 6.78, 6.79 and 6.80 show the resulting 3 phase load side oscillation voltages. As the first pole (A phase) interrupts, the load side oscillation is offset in a direction opposite to the polarity of $V_A$ at the instant of chopping in keeping with equation 3.53. As the second phase clears (C phase), the resulting load side oscillation is offset in the same sense as the instantaneous polarity of $V_B$ as predicted by equation 3.54.

Neutral offset alters the first and second phase breaker TRV dramatically over the same point on wave and current chopping without a neutral reactor. This is demonstrated in figure 6.81
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Figure 6.78: 5CB3 Test 4 Load Side Oscillation: Simulated A Phase Voltage

Figure 6.79: 5CB3 Test 4 Load Side Oscillation: Simulated B Phase Voltage
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Figure 6.80: 5CB3 Test 4 Load Side Oscillation: Simulated C Phase Voltage

Figure 6.81: 5RX3 Test 4: Simulated Breaker TRV with 5NR3 in and Bypassed
where the breaker recovery voltages for both cases are shown. Recovery peak magnitudes are increased in the first two phases to clear and reduced in the last in contrast to interruption at the same chopping levels with 5NR3 by-passed. For the second phase, recovery peak occurs later since first phase neutral offset voltage shifts the second current phase angle somewhat. The first interrupting breaker phase, thus must endure the most severe increase in recovery voltage and has significantly greater likelihood of reignition over the solidly grounded case. Increased TRV is also due in part to an effective increase in the inductance being interrupted with the neutral reactor in place.

Modulation in the simulated load side oscillation is more rapid than observed with solidly grounded reactor switching tests as predicted by equations 3.48 and 3.49. The dominant frequencies are 650 Hz and 107 Hz respectively.
6.5.2 5CB3 Test 11 Reconstruction

5CB3 Test 11 contact parting occurred 1.83 ms before a negative going A phase zero crossing such that point on wave time for A phase was 6.2 ms. This represents contact separation about 0.3 ms sooner than Test 7 shown in figure 5.38. Since a high quality plot of Test 11 was not available, a copy of A phase current and voltage light beam oscillograph traces are shown in figure 6.82. The two interruptions are very similar in that both lead to a surge arrester operation on recovery voltage reignition. Test 11 was chosen as more unique since the breaker withstood the suppression peak on initial A phase chopping, then reignited as load side oscillation voltage rose towards recovery peak. Reconstructing this interruption was hence a good test of both network modelling and how discerning the breaker performance prediction could be regarding reignitions.

5CB3 Test 11 was reconstructed as follows:

1. Estimated A phase current chop 1 by initial A phase current intersection with 5CB3 current chopping characteristic between 18 - 34 A. The lowest value was selected to test whether the technique could discern suppression peak survival in the best case.

2. Resulting A phase recovery voltage superimposed on the 5CB3 reignition characteristic suggested the breaker would easily withstand the suppression peak but recovery voltage reignition would occur between 500 - 800 kV. Choosing 34 A chopping as a worst case predicted suppression peak reignition. This illustrates the need to consider both best and worst cases if using the technique to predict breaker performance in practice. Best and worst case interruption scenarios could then be predicted for a range of point on wave times. Arrester operation at reignition was purposely not simulated in the reconstruction, as it would be studied in more detail later.

3. Incorporating A phase reignition 1, simulation continued to estimate C phase current chop 1 between 53 - 67 A. The higher value was chosen as a worst case.

4. C phase arrester operated before the simulated suppression peak ensuring successful C
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Figure 6.83: 5CB3 Test 11 Reconstruction: Estimating Current Chopping

Figure 6.84: 5CB3 Test 11 Reconstruction: Predicting Reignition
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phase interruption as determined by superimposing resulting TRV on 5CB3 reignition characteristic.

5. Continued simulation estimated B phase current chop 1 between 65 - 78 A. The higher value was chosen as a worst case.

6. B phase arrester operated before the simulated suppression peak ensuring successful B phase interruption as determined by superimposing resulting TRV on 5CB3 reignition characteristic.

7. Continued simulation estimated A phase current chop 2 between 70 - 80 A. The higher value was selected as a worst case.

8. A phase arrester operated before the simulated suppression peak ensuring successful A phase interruption as determined by superimposing resulting TRV on 5CB3 reignition characteristic.

The reconstruction steps are shown in figures 6.83 and 6.84 and the simulated reconstruction in figures 6.85 and 6.86. As was previously noted in figure 6.82, the second phase interruption is distinctly delayed due to current phase shifting by neutral offset voltage which appears as the first phase clears. Overall reconstruction agrees nicely with actual test results. Best case chopping current was initially selected to test technique discernment, as failure to predict the breaker withstanding suppression peak after best case current chopping, would constitute prediction method failure. Since actual A phase results lie between between the scenarios predicted by worst and best case current chopping, the prediction technique passed this test. Subsequent correct prediction of reignition on recovery voltage further emphasizes that breaker performance during four reactor interruption can be suitably predicted using the proposed method.
Figure 6.85: 5CB3 Test 11 Reconstruction: Simulated Voltages

Figure 6.86: 5CB3 Test 11 Reconstruction: Simulated Currents
6.5.3 Considering 5CB3 Test 11 A Phase Reignition

A phase reignitions during 5CB3 Test 11 and Test 7 were perhaps the most interesting observed throughout the Nicola breaker tests since arrester operation occurred during second parallel oscillations. These were not isolated incidents, as reignition invoked surge arrester operations occurred 9 times out of 12 interruptions for the point on wave timing spread bounded by Tests 7 and 11. As a measure of effectiveness of the distributed source, high frequency six phase flat line bus and high frequency reactor modelling, the Test 11 A phase reignition was examined in more detail. The high frequency four reactor representation of figure 6.87 for 5RX3 was developed and used to study the reignition. Reconstruction had shown reignition voltage to lie between 500 – 800 kV. For purposes of simulation, reignition voltage was taken to be 700 kV as indicated in Test 7 records. Since a high speed voltage divider was not available for 5CB15 or 5CB3 tests, field reignition waveforms were not available for comparison to simulations. However, if simulations did not generate reignition overvoltages sufficient to operate a surge
This section has shown the validity of applying the proposed breaker performance prediction technique to four reactor schemes by correctly reconstructing a known interruption. By confirming simulated reignition correctly predicts surge arrester operation, some measure of confidence has been established in the high frequency network representations chosen.

Figure 6.88 shows the simulated Reignition overvoltage reaches over 900 kV at the reactor. Second parallel oscillation frequency is 393 kHz. Manufacturers specifications for 5RX3 surge arresters give 885 kV as a maximum switching surge spark over level. High frequency modelling definitely predicts arrester operation at this simulated reignition, in full agreement with field test results.

Figure 6.88: 5CB3 Test 11 A Phase Reignition: Simulated Reactor Voltage

arrester, doubt would be cast on the high frequency modelling suitability.
Conclusions

In the early chapters, shunt reactor switching was shown to be an onerous circuit breaker duty through analysis of the single phase case and extending principles to three phase reactor switching. Large breaker RRRV is generated following interruption by load side oscillation voltage whose frequency is typically 5 – 30 times that of the power system and is reactor network dependent. Maximum breaker TRV depends on load side oscillation amplitude governed partly by the reactor network, but more heavily by circuit breaker current chopping. For successful interruption, circuit breaker contacts must withstand the large TRV associated with load side oscillation suppression and recovery peaks. Otherwise reignition results, exposing the network to potentially severe transient overvoltages and renewing 60 Hz current flow.

In the case of three phase reactors, breaker TRV is complicated by phase interactions arising from capacitive and inductive coupling in the reactor network. Where capacitive and inductive coupling are small, little error results in applying the single phase equations to prediction of suppression peaks following current chopping. However, even small amounts of coupling can generate a second oscillation mode, manifested by a modulated load side oscillation. Suppression peaks may then no longer represent the largest phase to ground reactor network voltage stresses during load side oscillation. As a result, single phase equations cannot be confidently applied to predict suppression peaks or circuit breaker TRV in all but the simplest solidly grounded cases. Natural load side oscillation modes are controlled by the reactor network parameters and grounding method. Addition of a neutral reactor for example, was found to significantly increase breaker TRV due to phase interactions during load side oscillation, and neutral offset voltages due to staggered phase interruption. Analytical treatment of the three phase case
is complex and was not covered except to highlight the origin of natural load side oscillation modes.

Ideal reactor circuit breakers will have a subdued current chopping characteristic, and rapidly rising reignition characteristic. Air blast breakers though commonly applied to reactor switching, exhibit high current chopping levels and are prone to reignition. Opening resistors can be added to reduce breaker TRV and effectively lower current chopping levels by enhancing arc stability. Otherwise, reactor surge arresters must play a key role in successful reactor interruption at typical air blast breaker chopping levels. This was confirmed by both field testing and simulations of Nicola 5CB15 and 5CB3 reactor switching. Measured chopping numbers for these devices were as large or larger than those published by others. SF₆ breakers and puffer types in particular, are well suited to reactor switching, providing virtually current chopping free interruption as observed in Chapter 5. This is due to the dynamic cooling performance of SF₆, and its superior insulating qualities.

Analysis of field tests showed the usefulness of breaker arcing characteristics to summarize device performance of a reactor switching duty. Current chopping and reignition characteristics derived from these measurements, are essential to the realistic simulation of reactor interruption with different circuit breakers. Further, these characteristics are central to the breaker performance prediction technique proposed and tested. Through reconstruction of several field test interruptions, the prediction technique was verified for solidly grounded and four reactor schemes. This method could prove useful for assessing breaker suitability to particular reactor switching applications and could serve as a design tool to assess reactor network alterations, or breaker modifications such as the addition of opening resistors.

Though fairly simple lumped element representations of source and reactor were acceptable, air insulated busses connecting breaker and reactor had to be modelled quite carefully to obtain the degree of load side oscillation phase interaction observed in field tests. In the author's opinion, this would be even more important where cables or more tightly coupled busses are employed. Careful modelling for load side oscillation simulation will ensure acceptable application
of the performance prediction method suggested.

Reignition simulations with distributed models of reactors and the substation, and high frequency representations of busses, gave good second parallel oscillation frequency agreement with field tests. The distributed substation model substantially reduced simulated peak reignition currents over those simulated using a lumped source model. The levels of transient current coupled to adjacent phases during reignition simulation were excessive in the author's opinion. Although various modelling refinements were attempted to address this problem, a solution was not found. This is not a drawback in terms of predicting breaker performance but presents a concern if three phase interactions at reignition are to be studied in detail.

7.1 Possible Avenues for Further Research

Other areas of study which could relate directly to the present work include:

1. Load side oscillation simulation and breaker performance prediction for ungrounded reactor applications.

2. Methods of deducing multiple interrupter breaker current chopping and reignition characteristics from single interrupter laboratory tests.

3. Techniques for calculating substation bus electrical parameters incorporating the complex geometries often encountered to confidently deduce adjacent phase coupling in particular.

4. Station ground grid modelling to better determine through simulation, the effects of reignition on substation control cables and grounding networks.
Appendix A

Arc Thermal Time Constant and Equivalent Circuits

Rizk [23] deduced an arc would reach a new steady state on its static characteristic exponentially with a thermal time constant. Further, he showed the arc could be replaced with an equivalent circuit to represent this behavior in analysis of arc interactions with the network to which the switchgear was applied. The following briefly highlights these arguments.

A.1 Exponential Response on Arc Perturbation

An exponential incremental arc voltage $e$ was observed and measured by Rizk when test arcs were perturbed by small current steps. The time constant was thermal in nature, becoming smaller as the arc was cooled more intensely. Rizk noted that:

- The arc behaved initially as a static resistance when perturbed by the small current step $i$, the initial value of $e$ being $e(o) = \frac{E_p}{I_o} i = R_o i$.

- For small current deviations about an initial operating point on the static arc characteristic, the final value of the incremental voltage $e$ could be predicted as $e(f) = \left(\frac{dE}{di}\right)_{I=I_o} i = R_{do} i$.

Given this behavior was governed by a thermal time constant $\theta$ a general solution for the incremental arc voltage as a function of time $e(t)$ could be written as:

$$e(t) = e(f) - [e(f) - e(o)] e^{-\frac{t}{\theta}}$$

By substituting the above expressions for $e(o)$ and $e(f)$ the incremental arc voltage may be written:
Recalling the definitions of static and dynamic arc resistances from Chapter 2:

\[ R_{so} = \frac{E_o}{I_o} \]
\[ R_{do} = \left( \frac{dE}{dI} \right)_{I=I_o} \]

Then equation A.55 can be written in terms of arc resistances:

\[ e(t) = i \left[ R_{do} + (R_{so} - R_{do}) e^{-\frac{t}{\tau}} \right] \] (A.56)

Total arc voltage as a function of time may then be expressed as the sum of the initial voltage \( E_o \) and the increment associated with the current step \( e(t) \):

\[ E(t) = E_o + e(t) \]
\[ = E_o + i \left[ R_{do} + (R_{so} - R_{do}) e^{-\frac{t}{\tau}} \right] \] (A.57)

If the arc remains stable, the final value of the arc voltage may then be expressed as:

\[ E(f) = E_o + R_{do}i \] (A.58)

### A.2 Arc Equivalent Circuits

Rizk [23] observed that when perturbed by a small current step the initial and final incremental arc voltages were given by \( R_{so}i \) and \( R_{do}i \) respectively, and noted this behavior could be represented by either of the networks given in figure 2.5. The following discussions are presented to justify equivalent behavior of these networks to that of an arc perturbed by a small current step.
A.2.1 Parallel Arc Equivalent Network

Response of the parallel equivalent network to a small current step can be studied using the circuit of figure A.89. If the switch closes at $t = 0$, $i_2 = 0$ since current through $L$ cannot change instantaneously. The initial incremental arc voltage is then $e(0) = iR_1$. Applying KCL;

$$i = i_1 + i_2$$

$$= e \left[ \frac{1}{R_1} + \frac{1}{R_2 + pL_1} \right]$$

where $p$ represents the differential operator $\frac{d}{dt}$. The final form of the differential equation becomes:

$$\frac{de}{dt} + e \left( \frac{R_1 + R_2}{L_1} \right) = \frac{iR_1R_2}{L_1} \tag{A.59}$$

Assuming a homogeneous solution of the form $e_h(t) = C_1 e^{-\frac{t}{\theta}}$ and a constant particular solution $e_p = C_2$ yields via substitution;

$$\theta = \frac{L_1}{R_1 + R_2}$$

$$C_2 = \frac{iR_1R_2}{R_1 + R_2}$$

The total solution thus may be written as:

$$e(t) = C_1 e^{-\frac{t}{\theta}} + \frac{iR_1R_2}{R_1 + R_2} \tag{A.60}$$

Applying the initial voltage condition $e(0) = iR_1$ yields $C_1 = \frac{iR_1^2}{R_1 + R_2}$ such that the final solution is then given by:

$$e(t) = \frac{iR_1}{R_1 + R_2} \left[ R_1 e^{-\frac{t}{\theta}} + R_2 \right] \tag{A.61}$$

By comparison to equation A.55, in order for the incremental voltage solutions to be equivalent:
Appendix A. Arc Thermal Time Constant and Equivalent Circuits

RESPONSE OF PARALLEL EQUIVALENT TO A CURRENT STEP

RESPONSE OF SERIES EQUIVALENT TO A CURRENT STEP

Figure A.89: Response of Arc Equivalent Circuits to a Current Step

- $R_1 = \frac{E_o}{I_o} = R_{so}$
- $\frac{R_1 R_2}{R_1 + R_2} = \left(\frac{dE}{dI}\right)_{I=I_o} = R_{do}$

From which the following must hold:

\[
R_1 = R_{so} \\
R_2 = \frac{R_{so} R_{do}}{R_{so} - R_{do}} \\
L_1 = \theta(R_1 + R_2)
\]

Then for a static arc characteristic of the form $EI^\alpha = \eta$, $R_{do} = -\alpha \frac{E_o}{I_o} = -\alpha R_{so}$ and the parallel network will behave equivalently to the arc for a step current perturbation if:

\[
R_1 = R_{so} \\
R_2 = -\frac{\alpha R_{so}}{1 + \alpha}
\]
Appendix A. Arc Thermal Time Constant and Equivalent Circuits

\[ L_1 = \frac{\theta R_{so}}{1 + \alpha} \]

A.2.2 Series Arc Equivalent Network

Response of the series arc equivalent network to a small perturbing current step can be studied using the circuit of figure A.89. Since the current through \( L \) cannot change instantaneously, the initial arc voltage assuming the switch closes at \( t = 0 \) is:

\[ e(o) = i(R_3 + R_4) \]

By KVL, \( e(t) = iR_3 + v(t) \) and using operational notation where \( p = \frac{d}{dt}; \)

\[
\begin{align*}
    v(t) &= i_2R_4 \\
    &= pL_2i_1 \\
    &= pL_2(i - i_2) \\
    &= pL_2i - \frac{pL_2v(t)}{R_4} \\
    &= \frac{R_4pL_2i}{R_4 + pL_2}
\end{align*}
\]

hence,

\[ e(t) = i \left[ R_3 + \frac{R_4pL_2}{R_4 + pL_2} \right] \]

This expression reduces to the following first order differential equation in \( e(t); \)

\[ \frac{de}{dt} + \frac{R_4}{L_2}e = i \frac{R_3R_4}{L_2} \]  \hspace{1cm} (A.63)

Assuming homogenous and particular solutions of the form \( e_h = C_1e^{-\frac{t}{\theta}} \) and \( e_p = C_2 \) respectively, yields;

\[
\begin{align*}
    e(t) &= C_1e^{-\frac{t}{\theta}} + iR_3 \\
    \theta &= \frac{L_2}{R_4}
\end{align*}
\]
Appendix A. Arc Thermal Time Constant and Equivalent Circuits

which through application of the initial condition reduces to;

\[ e(t) = R_4 i e^{-\frac{t}{\theta}} + i R_3 \]  \hspace{1cm} (A.64)

By comparison to equation A.55, in order for the series network to behave equivalently to an arc, the following must hold:

- \( R_4 = R_{so} - R_{do} \)
- \( R_3 = R_{do} \)
- \( L_2 = \theta R_2 \)

Equivalent response to an arc perturbing current step for a static arc characteristic is then satisfied if:

\[ R_3 = -\alpha R_{so} \]  \hspace{1cm} (A.65)
\[ R_4 = R_{so}(1 + \alpha) \]
\[ L_2 = \theta R_{so}(1 + \alpha) \]  \hspace{1cm} (A.66)

The parallel and series arc equivalent networks proposed by Rizk have been used extensively in various investigations [23],[22],[9], [1] and their validity is well accepted.
Appendix B

Reactor Load Side Oscillation Following Current Chopping

Load side oscillation on interruption of a single phase reactor may be considered analytically using the network of figure 2.9. $R_1$, $R_2$ and $L$ represent reactor winding resistance, reactor damping (core losses) and reactor inductance. Prior to interruption, the load side voltage and breaker current will be:

$$V(t) = V_s \sin \omega_s t$$  \hspace{1cm} (B.67)

$$i_b(t) \approx \frac{V_s}{\sqrt{R_1^2 + (\omega_s L)^2}} \sin(\omega_s t - \epsilon)$$

$$\epsilon \approx \arctan \frac{\omega_s L}{R_1}$$

where: $\omega_s$ is power system frequency
$V_s$ is system peak voltage

The approximation for $i_b(t)$ assumes current through load capacitance is negligible and $R_2 \gg \omega_s L$. In practice this is valid since reactor inductive reactance is at least one hundred times larger than load network capacitive reactance and one hundred times smaller than $R_2$ at power system frequencies.

Consider the network of figure 2.9 to determine the load side transient voltage following current chopping. Assuming current chopping occurs at time $t = t_{ch}$, $i(o) \approx i_{ch}$ since capacitive current is much smaller than the reactor current. $V(o) = V_{ch}$ can be calculated from a knowledge of the chopping current itself:
\[ V_{ch} = V_s \sin \omega_s t_{ch} \]  
\[ \omega_s t_{ch} = \arcsin \left[ \frac{I_{ch}}{V_s} \sqrt{R_1^2 + (\omega_s L)^2} \right] + \epsilon \]

Using the \( p \) operator to denote differentiation with time, KCL may be applied to the network:

\[
\left[ pC + \frac{1}{R_1 + \frac{R_2 pL}{R_1 + pL}} \right] V(t) = 0
\]

which reduces to:

\[
\frac{d^2 V}{dt^2} + \left[ \frac{R_1 R_2}{(R_1 + R_2) L} + \frac{1}{C(R_1 + R_2)} \right] \frac{dV}{dt} + \frac{R_2 V}{LC(R_1 + R_2)} = 0
\]  
\( \text{(B.69)} \)

Assuming solutions of the form \( Ke^\gamma t \) yields the characteristic equation:

\[
\gamma^2 + 2\beta \gamma + \omega_0^2 = 0
\]  
\( \text{(B.70)} \)

For practical reactor networks tested by the author and others \cite{1} \cite{17}, the solution of equation B.69 is a damped sinusoidal response. In terms of the characteristic equation, the physical evidence implies \( \beta^2 < \omega_0^2 \) such that its roots are complex:

\[
\gamma_{1,2} = -\beta \pm \sqrt{\omega_0^2 - \beta_L^2}
\]  
\( \text{(B.71)} \)

\[
\beta_L = \frac{1}{2} \left[ \frac{R_2}{R_1 + R_2} \right] \left[ \frac{R_1}{L} + \frac{1}{R_2 C} \right]
\]

\[
\omega_0^2 = \frac{R_2}{(R_1 + R_2) LC}
\]

\[
\omega_d = \sqrt{\omega_0^2 - \beta_L^2}
\]

The general solution of equation B.69 then takes the form:

\[
V(t) = K_1 e^{-\beta_L t} \cos \omega_d t + K_2 e^{-\beta_L t} \sin \omega_d t
\]  
\( \text{(B.72)} \)
where: $\omega_d$ is the damped natural frequency of the load side oscillation.

$\beta_L$ is the damping constant as previously defined.

$K_1$ and $K_2$ are constants be fixed by initial conditions at the moment of current chopping.

$\omega_0$ is the undamped natural frequency of the network.

The solution can be alternately expressed as:

$$V(t) = Ke^{-\beta_L t} \cos(\omega_d t - \psi)$$  \hspace{1cm} (B.73)

$$K = \sqrt{K_1^2 + K_2^2}$$

$$\psi = \arctan \frac{K_2}{K_1}$$

Applying the initial voltage condition to equation B.72 yields $V(0) = V_{ch} = K_1$. Solving for $K_2$ requires consideration of initial current conditions. Noting that $i(t) = -C \frac{dV}{dt}$, equation B.72 may used to derive a general solution for the current:

$$i(t) = -C \frac{dV}{dt}$$  \hspace{1cm} (B.74)

$$= Ce^{-\beta_L t} \left[ (\beta_L K_1 - \omega_d K_2) \cos \omega_d t - (\omega_d K_1 + \beta_L K_2) \sin \omega_d t \right]$$

Initial current conditions may then be applied:

$$i(0) = i_{ch}$$  \hspace{1cm} (B.75)

$$= C(\beta_L K_1 - \omega_d K_2)$$

$$K_2 = \frac{\beta_L V_{ch} - \frac{i_{ch}}{\omega_d}}{\omega_d}$$

Using equation B.73, a total solution for the load side voltage may then be written as:

$$V(t) = \sqrt{V_{ch}^2 + \left( \frac{\beta_L V_{ch} - \frac{i_{ch}}{\omega_d}}{\omega_d} \right)^2} e^{-\beta_L t} \cos(\omega_d t - \psi)$$  \hspace{1cm} (B.76)
Appendix B. Reactor Load Side Oscillation Following Current Chopping

\[ \psi = \arctan \left( \frac{V_{ch} \beta_L - \frac{i_{ch}}{C}}{\omega_d V_{ch}} \right) \]

where \( \beta_L \) and \( \omega_d \) are as previously defined. By inspection, it is clear the initial voltage peak (suppression peak) occurs at a time \( t_p \) where \( \omega_d t_p - \psi = 0 \). That is:

\[ t_p = \frac{1}{\omega_d} \arctan \left( \frac{V_{ch} \beta_L - \frac{i_{ch}}{C}}{\omega_d V_{ch}} \right) \]  

(B.77)

The magnitude of the suppression peak voltage is then given by:

\[ V_p = V(t_p) \]

(B.78)

\[ = \sqrt{V_{ch}^2 + \left( \frac{\beta_L V_{ch} - \frac{i_{ch}}{C}}{\omega_d} \right)^2} e^{-\frac{\beta_L}{\omega_d} \arctan \left( \frac{V_{ch} \beta_L - \frac{i_{ch}}{C}}{\omega_d V_{ch}} \right)} \]

In practical reactor networks the damped natural frequency \( \omega_d \) is usually sufficiently larger than the damping factor \( \beta_L \), that it is acceptable to neglect damping in predicting the suppression peak \( V_p \) [1], [17]. Neglecting damping is equivalent to simultaneously allowing \( R_1 \) approach zero and \( R_2 \) to approach \( \infty \) in equation B.71:

\[ \lim_{R_1 \to 0, R_2 \to \infty} \beta_L = 0 \]  

(B.79)

\[ \lim_{R_1 \to 0, R_2 \to \infty} \omega_d^2 = \frac{1}{LC} \]

Application of these conditions to equation B.78 yields:

\[ V_p \simeq \frac{\sqrt{V_{ch}^2 + \frac{i_{ch}^2 L}{C}}}{L} \]

(B.80)

\[ \simeq \frac{\sqrt{V_{ch}^2 + (i_{ch} \omega_d L)^2}}{i_{ch} \omega_d C} \]

\[ \simeq \sqrt{V_s^2 + \frac{(i_{ch} \omega_d L)^2}{\omega_d C}} \]

Usually, current chopping occurs close enough to a natural current zero that \( V_{ch} \simeq V_s \) where \( V_s \) is system peak voltage. Then equation B.80 may be used to define a per unit suppression peak overvoltage factor:
The reactor current expression of equation B.74 may be manipulated into an alternate form:

\[ i(t) = Ce^{-\beta t}\sqrt{D_1^2 + D_2^2}\cos(\omega_d t + \theta_1) \] (B.82)

\[ D_1 = (\omega_d K_2 - \beta K_1) \]
\[ D_2 = (\omega_d K_1 + \beta K_2) \]
\[ \theta_1 = \arctan\frac{D_2}{D_1} \]

Substituting the previously derived values of \( K_1 \) and \( K_2 \), \( D_1 \) and \( D_2 \) reduce to:

\[ D_1 = \frac{i_{ch}}{C} \]
\[ D_2 = \frac{1}{\omega_d} \left[ \omega_o^2 V_{ch} - \beta L \frac{i_{ch}}{C} \right] \]

A complete current solution may then be written as:

\[ i(t) = \sqrt{i_{ch}^2 + \left( \frac{C\omega_o^2 V_{ch} - \beta L i_{ch}}{\omega_d} \right)^2} e^{-\beta L t}\cos(\omega_d t + \theta_1) \] (B.83)
\[ \theta_1 = \arctan \left[ \frac{C\omega_d V_{ch}}{i_{ch}} - \frac{\beta L}{\omega_d} \right] \]

When studying breaker reignition, equations B.76 and B.83 may be used to deduce initial reactor voltage and current conditions just prior to reignition.
Appendix C

Analysis of Reignition Oscillations

As described in chapter 2 the first parallel oscillation is a short lived high frequency transient. A good general understanding of reignition phenomena may be obtained by neglecting the first parallel oscillation and it will not be considered here.

C.1 The Second Parallel Oscillation

Potentially the highest transient voltage at reignition will occur during the second parallel oscillation during the oscillatory energy exchange between source and load side capacitances. The currents in source and reactor inductances cannot change rapidly enough to influence the second parallel oscillation and to a good approximations may be treated as constants. Treating the reigniting breaker as simply a switch closing at $t = 0$, analysis can proceed using the second parallel oscillation network of figure C.90 derived from figure 2.7. $C_s$ and $C_r$ represent source and load side capacitances respectively while $L_b$ and $R_b$ represent the bus impedance between the breaker load side capacitance. $R_b$ can also incorporate arc and resistor switch resistances where appropriate. Applying KCL to the network with operational notation $p$ and $\frac{1}{p}$ to represent differentiation and integration with time;

$$
\begin{bmatrix}
 pC_s + \frac{1}{R_b+pL_b} & -\frac{1}{R_b+pL_b} \\
 -\frac{1}{R_b+pL_b} & pC_r + \frac{1}{R_b+pL_b}
\end{bmatrix}
\begin{bmatrix}
 V_s \\
 V_r
\end{bmatrix}
= 
\begin{bmatrix}
 i_s(0) \\
 -i_r(0)
\end{bmatrix}
$$

Since the current through the breaker cannot change instantaneously, and $V_b = R_b i_b + L_b i'_b$, initial conditions may be expressed as:

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SECOND PARALLEL OSCILLATION NETWORK

MAIN CIRCUIT OSCILLATION NETWORK

TRANSFORMING SOURCE TO NORTON EQUIVALENT

Figure C.90: Networks for Analysis of Reignition Oscillations
Appendix C. Analysis of Reignition Oscillations

\( i_b(o) = 0 \) \ \ \ \ \ (C.84) \\
\( V_b(o) = V_s(o) - V_r(o) \) \\
\( i'_b(o) = \frac{V_s(o) - V_r(o)}{L_b} \)

Using Cramer's rule, \( V_s(t) \) and \( V_r(t) \) may be individually solved for to yield:

\[
V_s(t) = \frac{i_s(o) pC_r + \frac{i_s(o) - i_r(o)}{R_b + pL_b}}{\text{Det } A}
\]
\[
V_r(t) = \frac{-i_r(o) pC_s + \frac{i_s(o) - i_r(o)}{R_b + pL_b}}{\text{Det } A}
\]
\[
V_b(t) = V_s(t) - V_r(t) = \frac{i_s(o) pC_r + i_r(o) pC_s}{\text{Det } A}
\]

where:

\[
\text{Det } A = \begin{vmatrix}
    pC_s + \frac{1}{R_b + pL_b} & -\frac{1}{R_b + pL_b} \\
    -\frac{1}{R_b + pL_b} & pC_r + \frac{1}{R_b + pL_b}
\end{vmatrix} = p^2 C_s C_r (R_b + pL_b) + p(C_r + C_s) \frac{R_b + pL_b}{R_b + pL_b}
\]

Substituting for \( \text{Det } A \) and noting \( i_b(t) = \frac{V_b(t)}{R_b + pL_b} \) yields the following differential equation in \( i_b \):

\[
i_b(t) \left[ p^2 + p \frac{R_b}{L_b} + \frac{C_r + C_s}{C_s C_r L_b} \right] = \left[ \frac{i_s(o)}{C_s} + \frac{i_r(o)}{C_r} \right] \frac{1}{L_b} \ \ \ \ \ (C.85)
\]

Assuming a homogenous solution form of \( i_{bh} = e^{-\lambda t} \), gives the characteristic equation:

\[
\lambda^2 + \lambda \frac{R_b}{L_b} + \frac{C_r + C_s}{C_s C_r L_b} = 0
\]

Roots for a damped oscillatory response are:

\[
\lambda_1, \lambda_2 = -\beta_p \pm j\omega_d
\]
Appendix C. Analysis of Reignition Oscillations

\[
\beta_p = \frac{R_b}{2L_b}
\]

\[
\omega_p^2 = \frac{C_r + C_s}{C_s C_r L_b}
\]

\[
\omega_{d2} = \sqrt{\omega_p^2 - \beta_p^2}
\]

Damping in reactor networks is typically light and assuming damped oscillatory response is valid. With an opening resistor, damping will be much more significant but some degree of oscillation can still be expected. The homogenous solution in general form is then given by:

\[
i_{bh}(t) = K_1 e^{-\beta_p t} \cos \omega_{d2} t + K_2 e^{-\beta_p t} \sin \omega_{d2} t
\]  \hspace{1cm} (C.86)

Since the driving function is time invariant, a constant particular solution may be assumed, \(i_{bp} = K_3\) which on substitution into the differential equation gives:

\[
K_3 = \frac{C_r i_s(o) + C_s i_r(o)}{C_r + C_s}
\]

The total solutions for \(i_b\) and \(i'_b\) may then be written as:

\[
i_b(t) = e^{-\beta_p t} [K_1 \cos \omega_{d2} t + K_2 \sin \omega_{d2} t] + \frac{C_r i_s(o) + C_s i_r(o)}{C_r + C_s}
\]

(C.87)

\[
i'_b(t) = e^{-\beta_p t} [(\omega_{d2} K_2 - \beta_p K_1) \cos \omega_{d2} t - (K_1 \omega_{d2} + \beta_p K_2) \sin \omega_{d2} t]
\]

On substitution of the initial conditions given by equation C.84:

\[
i_b(0) = K_1 + \frac{C_r i_s(o) + C_s i_r(o)}{C_r + C_s}
\]

\[
= 0
\]

\[
K_1 = - \left[ \frac{C_r i_s(o) + C_s i_r(o)}{C_r + C_s} \right]
\]

\[
= -K_3
\]

\[
i'_b(0) = \omega_{d2} K_2 - \beta_p K_1
\]
Appendix C. Analysis of Reignition Oscillations

\[ K_2 = \frac{1}{L_b \omega_d^2} \left[ V_s(o) - V_r(o) - \frac{R_b}{2} \frac{(C_r i_s(o) + C_s i_r(o))}{C_s + C_r} \right] \]

The expression for \( K_2 \) may be further simplified by noting that if \( R_b \) is small, then:

- \( \omega_d \approx \omega_o \)
- The term with coefficient \( \frac{R_b}{2} \) may be neglected without much concern.

\( K_2 \) may then be written as:

\[ K_2 \approx \sqrt{\frac{C_s C_r}{L_b (C_s + C_r)}} [V_s(o) - V_r(o)] \]

A complete solution may hence be written as:

\[ i_b(t) \approx \sqrt{\frac{C_s C_r}{L_b (C_s + C_r)}} [V_s(o) - V_r(o)] e^{-\beta_p t} \sin \omega_d t \]  

\[ + \frac{[C_r i_s(o) + C_s i_r(o)]}{C_s + C_r} \left[ 1 - e^{-\beta_p t} \cos \omega_d t \right] \]  

\( C.88 \)

Since damping is not large, the damped second parallel oscillation frequency is essentially:

\[ f_{p2} \approx \frac{1}{2\pi} \sqrt{\frac{C_r + C_s}{C_r C_s L_b}} \]

Second parallel oscillation frequency is typically several hundred kHz. The current transient damps quite quickly to a quasi-steady state value in a time \( t_d \):

\[ i_b(t_d) \approx \frac{C_r i_s(o) + C_s i_r(o)}{C_s + C_r} \]  

\( C.89 \)

which forms the initial current condition for the subsequent main circuit oscillation.

The load side voltage during the second parallel oscillation can become very large and it is worth considering its general behavior. Load side voltage can be derived from the expression for current. Recalling that because the second parallel oscillation is so fast, it is valid to assume
reactor current remains constant at \( i_r(o) \) for the duration. Then noting that \( i_c(t) = i_b(t) - i_r(o) \) and \( i_c(t) = C_r \frac{dV_r}{dt} \) the following may be written:

\[
\frac{1}{C_r} \int_o^t i_c dt = \int_{V_r(o)}^{V_r(t)} dV_r = V_r(t) - V_r(o)
\]

Substituting for \( i_c(t) \), \( V_r(t) \) may be written as:

\[
V_r(t) = V_r(o) - \frac{i_r(o)t}{C_r} + \frac{1}{C_r} \int_o^t i_b(t) dt
\]  (C.90)

After evaluating the integral and performing lengthy but straightforward algebraic manipulation, the load side voltage solution reduces to:

\[
V_r(t) = \frac{V_r(o) C_r + V_a(o) C_s}{C_r + C_s} + \left[ \frac{i_s(o) - i_r(o)}{C_r + C_s} \right] t
\]  
\[
+ \frac{C_s e^{-\beta_pt}}{C_s + C_r} \left[ V_r(o) - V_s(o) + \frac{R_b}{2} \left( \frac{C_r i_s(o) + C_s i_r(o)}{C_r + C_s} \right) \right] \cos \omega_d t
\]  
\[
+ \frac{C_s e^{-\beta_pt}}{C_s + C_r} \left[ \frac{R_b}{2 \omega_p L_b} (V_r(o) - V_s(o)) - \frac{1}{\omega_p} \left( \frac{i_s(o)}{C_s} + \frac{i_r(o)}{C_r} \right) \right] \sin \omega_d t
\]  

Normally, the bus between source and load side capacitance is short and \( R_b \) is small. Further, the second parallel frequency being several hundred kHz, results in the term \( \frac{R_b}{2 \omega_p L_b} \) being small. Through application of these conditions the load side voltage may be written:

\[
V_r(t) \approx \frac{V_r(o) C_r + V_a(o) C_s}{C_r + C_s} + \left[ \frac{i_s(o) - i_r(o)}{C_r + C_s} \right] t
\]  
\[
+ \frac{C_s e^{-\beta_pt}}{C_s + C_r} [V_r(o) - V_s(o)] \cos \omega_d t
\]  
\[
- \frac{C_s e^{-\beta_pt}}{C_s + C_r} \left[ \frac{1}{\omega_p} \left( \frac{i_s(o)}{C_s} + \frac{i_r(o)}{C_r} \right) \right] \sin \omega_d t
\]  

Given that the second parallel breaker current damps quickly to a quasi-steady state as described by equation C.89 in a time \( t_d \), the load side voltage at that time is then:
Appendix C. Analysis of Reignition Oscillations

\[
V_r(t_d) \approx \frac{V_r(o)C_r + V_s(o)C_s}{C_r + C_s} + \left[ \frac{i_s(o) - i_r(o)}{C_r + C_s} \right] t_d \quad \text{(C.93)}
\]

Together with equation C.89, these form the initial conditions leading into the main circuit oscillation. Provided the second parallel oscillation damps quickly, the ramp term will not become large enough to have significant effect on the load side voltage solution.

The complexity of equations C.91 and C.92 at first glance masks the potential for large overvoltages during the second parallel oscillation. Consider the case of a reignition occurring at or near a recovery peak such that \(V_r(o)\) and \(V_s(o)\) \(\approx 1.0\) pu. At the recovery peak, both source and load side voltages will be approximately at their opposite peak values. Hence the currents \(i_r(o)\) and \(i_s(o)\) will be essentially zero. Further, it will typically be the case that \(C_s \gg C_r\). Imposing these conditions the load side voltage during second parallel oscillation becomes:

\[
V_r(t) \approx V_s(o) + (V_r(o) - V_s(o)) e^{-\beta_p t} \cos \omega_d t \quad \text{(C.94)}
\]

It is clear from this expression that beginning from \(V_r(o) = 1.0\) pu, within a half cycle of oscillation the load side voltage may rise to a value in excess of 3 pu since \(\beta_p \ll \omega_p\) and power system frequency is much less than \(\omega_p\). This simplified expression also demonstrates how the load side voltage oscillates about and finally damps to a value essentially equal to the source voltage at the start of reignition \(V_s(o)\).

C.2 The Main Circuit Oscillation

In the event that interruption is not successful at a zero of the second parallel oscillation current, the main circuit oscillation develops to involve all network elements in oscillatory energy exchange. The assumptions used to analyze the second parallel oscillation led to a quasi-steady state breaker current and associated voltage given in equations C.89 and C.93 which form the initial conditions of the main circuit oscillation.
Since main circuit oscillation is a slower phenomenon, initial source and load side inductor currents cannot be considered constant and \( i_r(o) \) and \( i_s(o) \) must be treated as initial conditions. Since voltages on the source and load side of the breaker equalize during the second parallel oscillation, the main circuit oscillation network of figure C.90 may be used for analysis. A sinusoidal power system source voltage is assumed and reactor network damping initially neglected. Using superposition, the zero input response (response to initial conditions with sources removed) will be first evaluated. The forced zero state response (initial conditions set to zero) will then be derived and added to the zero input response to give the complete main circuit oscillation solution.

Using KCL and operational notation to represent differentiation and integration with time, the zero input portion of the load side voltage \( V_r(t) \) may be expressed by the following differential equation:

\[
V_r \left[ \frac{1}{p} \left( \frac{L_r + L_s}{L_s L_r} \right) + p(C_s + C_r) \right] = 0
\]

which reduces to:

\[
V_r \left[ p^2 + \frac{L_r + L_s}{L_s L_r (C_s + C_r)} \right] = 0
\]

The roots of the characteristic equation are clearly \( \pm j \omega_m \) where \( \omega_m \) is the main circuit oscillation frequency given by:

\[
\omega_m = \frac{L_r + L_s}{\sqrt{L_r L_s (C_s + C_r)}}
\]

The free response solution then has the following form:

\[
V_{r \text{free}} = F_1 \cos \omega_m t + F_2 \sin \omega_m t
\]

with the coefficients to be determined by the network initial conditions. Then noting \( i_b(t) = C_r V_r'(t) + i_r(t) \), the coefficients may be evaluated as follows:
Appendix C. Analysis of Reignition Oscillations

\[ F_1 = V_{r \text{ free}}(o) = V_r(t_d) \]

\[ F_2 = \frac{V'_{r \text{ free}}(o)}{\omega_m} = \frac{i_b(o) - i_r(o)}{\omega_mC_r} = \frac{i_b(t_d) - i_r(o)}{\omega_mC_r} \]

Where \( i_b(t_d) \) and \( V_r(t_d) \) are the second parallel oscillation quasi-steady state breaker current and load side voltage which form the initial conditions leading into the main circuit oscillation previously given in equations C.89 and C.93. On substitution for \( i_b(t_d) \), \( F_2 \) reduces to \( \frac{i_b(o) - i_r(o)}{(C_s + C_r)\omega_m} \) and the free response load voltage may thus be written as:

\[ V_{r \text{ free}}(t) = V_r(t_d) \cos \omega_m t + \left[ \frac{i_b(o) - i_r(o)}{(C_s + C_r)\omega_m} \right] \sin \omega_m t \tag{C.96} \]

The forced load voltage response (zero state response) may be evaluated by introducing the source voltage and setting all initial conditions to zero in the network of C.90. Note that \( \psi \) represents the angle of the source voltage at the moment of reignition. Converting the source to a Norton equivalent as in figure C.90 and repeating the application of KCL, the following differential equation describing the forced response of the load side voltage results:

\[ V_{r \text{ forced}}(t) \left\{ p^2 + \frac{L_s + L_r}{(C_s + C_r)L_sL_r} \right\} = \frac{V_0 \sin(\omega_s t + \psi)}{L_s(C_s + C_r)} \tag{C.97} \]

Roots of the characteristic equation are \( \pm j\omega_m \) as for the free response and the homogenous part of the forced solution has the general form:

\[ V_{r \text{ forced h}} = E_1 \cos \omega_m t + E_2 \sin \omega_m t \]
Appendix C. Analysis of Reignition Oscillations

Assuming a particular solution of the form:

\[ V_{r,\text{forced}} = E_3 \sin(\omega_4 t + \psi) + E_4 \cos(\omega_4 t + \psi) \]

and substituting into equation C.97 yields:

\[ E_3 = \frac{\omega_m^2}{(\omega_m^2 - \omega_s^2)} \frac{V_o L_r}{L_r + L_s} \]
\[ E_4 = 0 \]

The general form of the forced response may then be written as:

\[ V_{r,\text{forced}}(t) = E_1 \cos \omega_m t + E_2 \sin \omega_m t + \frac{\omega_m^2}{(\omega_m^2 - \omega_s^2)} \frac{V_o L_r}{L_r + L_s} \sin(\omega_4 t + \psi) \]

Through application of the zero state initial conditions associated with the forced response:

\[ V_{r,\text{forced}}(0) = 0 \]
\[ V'_{r,\text{forced}}(0) = \frac{i_b(0) - i_r(0)}{C_r} \]
\[ = 0 \]

the coefficients \( E_1 \) and \( E_2 \) reduce to:

\[ E_1 = \frac{\omega_m^2}{(\omega_s^2 - \omega_m^2)} \frac{L_r}{L_r + L_s} V_o \sin \psi \]
\[ E_2 = \frac{\omega_s \omega_m}{(\omega_s^2 - \omega_m^2)} \frac{L_r}{L_r + L_s} V_o \cos \psi \]

The free and forced responses may then be added to give the total load side voltage:
Appendix C. Analysis of Reignition Oscillations

\[ V_r(t) = \left[ \frac{\omega_m^2}{L_r} - \frac{L_r}{(\omega_m^2 - \omega_s^2)(L_r + L_s)} \right] V_o \sin(\omega_s t + \psi) \]

\[ + \left[ V_r(t_d) - \frac{\omega_m^2}{(\omega_m^2 - \omega_s^2)} \frac{L_r}{L_r + L_s} V_o \sin \psi \right] \cos \omega_m t \]

\[ + \frac{1}{\omega_m} \left[ \frac{i_s(o) - i_r(o)}{C_s + C_r} - \frac{\omega_s \omega_m^2}{(\omega_m^2 - \omega_s^2)} \frac{L_r}{L_r + L_s} V_o \cos \psi \right] \sin \omega_m t \]

(C.98)

This formidable expression can be simplified by noting that:

- For shunt reactor network switching, \( L_s \ll L_r \) and \( \frac{L_r}{L_s + L_r} \approx 1 \).
- The main circuit oscillation is usually at least an order of magnitude larger than the power system frequency such that \( \frac{\omega_m^2}{\omega_m^2 - \omega_s^2} \approx 1 \).

Applying these assumptions, the load side voltage during the main circuit oscillation may be expressed as:

\[ V_r(t) \approx V_o \sin(\omega_s t + \psi) + [V_r(t_d) - V_o \sin \psi] \cos \omega_m t \]

(C.99)

\[ + \frac{1}{\omega_m} \left[ \frac{i_s(o) - i_r(o)}{C_s + C_r} - \omega_s V_o \cos \psi \right] \sin \omega_m t \]

Referring to figure C.90, and noting \( i_r(t) - i_r(o) = \frac{1}{L_r} \int_0^t V_r \, dt \), the breaker current during the main circuit oscillation may be written:

\[ i_b(t) = C_r V'_r(t) + i_r(t) \]

\[ = C_r V'_r(t) + i_r(o) + \int_0^t \frac{V_r}{L_r} \, dt \]

Evaluating the above derivative and integral in \( V_r \), the breaker current may be expressed as:
Appendix C. Analysis of Reignition Oscillations

\[ i_b(t) = \frac{L_r i_r(o) + L_s i_s(o)}{L_s + L_r} + \left[ 1 - \left( \frac{\omega_s}{\omega_m} \right)^2 \right] \frac{V_o \cos \psi}{\omega_s L_r} \]

\[ + \left[ \frac{C_r L_r \omega_s^2 - 1}{\omega_s L_r} \right] V_o \cos(\omega_s t + \psi) \]

\[ + \omega_m [V_o \sin \psi - V_r(t_d)] \frac{C_r L_r - C_s L_s}{L_r + L_s} \sin \omega_m t \]

\[ + \left[ \frac{i_s(o) - i_r(o)}{C_s + C_r} - \omega_s V_o \cos \psi \right] \frac{C_r L_r - C_s L_s}{L_r + L_s} \cos \omega_m t \]

Several simplifications can be made since for most practical reactor switching problems:

- \( L_s \ll L_r \) and \( \frac{L_r}{L_s+L_r} \ll \frac{L_r}{L_r+L_s} \)
- \( \frac{L_r}{L_r+L_s} \approx 1 \)
- \( \frac{L_s}{L_r+L_s} \approx 0 \)
- \( \left( \frac{\omega_s}{\omega_m} \right)^2 \ll 1 \)

On applying these simplifications to equation C.100, the breaker current during the main circuit oscillation reduces to:

\[ i_b(t) \approx i_r(o) + \frac{V_o \cos \psi}{\omega_s L_r} + \left( \frac{C_r L_r \omega_s^2 - 1}{\omega_s L_r} \right) V_o \cos(\omega_s t + \psi) \]

\[ + \omega_m C_r [V_o \sin \psi - V_r(t_d)] \sin \omega_m t \]

\[ + \frac{C_r}{C_s + C_r} [i_s(o) - i_r(o) - \omega_s (C_s + C_r)V_o \cos \psi] \cos \omega_m t \]

As a final point, since reactor networks are so lightly damped, there is little error introduced by initially neglecting and later reintroducing damping. If the resistor \( R_r \) shown connected with dashed lines in figure C.90 had been incorporated into the analysis, the differential equation describing zero input response would have reduced to:
On comparison to standard second order form the damping factor for the main circuit oscillation is $\beta_m = \frac{1}{2R_r(C_s + C_r)}$ and the network damping may be introduced to the main circuit oscillation breaker current and load side voltage as follows:

$$V_r \left[ p^2 + \frac{p}{R_r(C_s + C_r)} + \frac{L_r + L_s}{L_sL_r(C_s + C_r)} \right] = 0 \quad (C.102)$$
Appendix C. Analysis of Reignition Oscillations

\[ i_b(t) \approx \left[ \frac{C_r L_r \omega_s^2 - 1}{\omega_s L_r} \right] V_0 \cos(\omega_s t + \psi) + \left[ i_r(o) + \frac{V_o \cos \psi}{\omega_s L_r} \right] e^{-\beta_m t} \]  
\[ + \omega_m C_r \left[ V_o \sin \psi - V_r(t_d) \right] e^{-\beta_m t} \sin \omega_m t \]  
\[ + \frac{C_r}{C_r + C_s} \left[ i_s(o) - i_r(o) - \omega_s (C_s + C_r) V_o \cos \psi \right] e^{-\beta_m t} \cos \omega_m t \]  

\[ V_r(t) \approx V_o \sin(\omega_s t + \psi) + [V_r(t_d) - V_o \sin \psi] e^{-\beta_m t} \cos \omega_m t \]  
\[ + \frac{1}{\omega_m} \left[ \frac{i_s(o) - i_r(o)}{C_s + C_r} - \omega_s V_o \cos \psi \right] e^{-\beta_m t} \sin \omega_m t \]  

Noting that \( \left( \frac{C_r L_r \omega_s^2 - 1}{\omega_s L_r} \right) V_0 \cos(\omega_s t + \psi) = \left( \frac{C_r L_r \omega_s^2 - 1}{\omega_s L_r} \right) V_o \sin(\omega_s t + \psi) \) and \( L_s \ll L_r \), the first term in equations C.103 and C.104 are the steady state breaker current and load side voltage to which the network will tend as the main circuit oscillation damps out. Since the load and source side voltages practically equalize during the second parallel oscillation, the term \( V_o \sin \psi - V_r(t_d) \) will be small. It then appears from equation C.103, that the ratio \( \frac{C_r}{C_r + C_s} \) dictates how large the oscillatory portion of the main circuit oscillation shall be. If this ratio is small, the steady state sinusoidal current grows quickly enough that the \( \cos \omega_m t \) and \( \sin \omega_m t \) terms cannot produce current zeroes. In such cases it will be impossible for the breaker to interrupt during main circuit oscillation and a new half cycle of 60 Hz current will result. Main circuit oscillation could in fact be essentially absent for \( C_r \ll C_s \).
Appendix D

Effects of Introducing a Neutral (Grounding) Reactor

A neutral reactor is often applied where shunt compensation of transmission circuits protected by single pole tripping protection schemes is desired. This facilitates clearing single phase faults by compensating a portion of the distributed capacitive coupling from adjacent phases which continues to drive fault current and hinders extinction. Considering the four reactor scheme of figure D.91 which is grounded through a neutral reactor, a nodal formulation could be written as follows:

\[
\frac{1}{\omega} \begin{bmatrix}
\frac{1}{L_p} & 0 & 0 & -\frac{1}{L_p} \\
0 & \frac{1}{L_p} & 0 & -\frac{1}{L_p} \\
0 & 0 & \frac{1}{L_p} & -\frac{1}{L_p} \\
-\frac{1}{L_p} & -\frac{1}{L_p} & -\frac{1}{L_p} & \left(\frac{3}{L_p} + \frac{1}{L_N}\right)
\end{bmatrix}
\begin{bmatrix}
V_A \\
V_B \\
V_C \\
V_N
\end{bmatrix}
= \begin{bmatrix}
I_A \\
I_B \\
I_C \\
0
\end{bmatrix}
\]

where: \( L_p \) is the phase reactor inductance
\( L_N \) is the neutral reactor inductance

Rearranging the equation for \( V_N \) yields:

\[
V_N = \frac{(V_A + V_B + V_C)L_N}{3L_N + L_p}
\]

By back substitution, the fourth equation may be eliminated to yield:

\[
\frac{1}{\omega L_p} \begin{bmatrix}
\frac{2L_N + L_p}{3L_N + L_p} & -\frac{L_N}{3L_N + L_p} & -\frac{L_N}{3L_N + L_p} \\
-\frac{L_N}{3L_N + L_p} & \frac{2L_N + L_p}{3L_N + L_p} & -\frac{L_N}{3L_N + L_p} \\
-\frac{L_N}{3L_N + L_p} & -\frac{L_N}{3L_N + L_p} & \frac{2L_N + L_p}{3L_N + L_p}
\end{bmatrix}
\begin{bmatrix}
V_A \\
V_B \\
V_C
\end{bmatrix}
= \begin{bmatrix}
I_A \\
I_B \\
I_C
\end{bmatrix}
\]

(D.105)
The four reactor connection may be replaced with the solidly grounded equivalent network of figure D.91 whose nodal formulation is:

\[
\frac{1}{\omega} \begin{bmatrix}
\frac{1}{L_g} + \frac{2}{L_i} & -\frac{1}{L_i} & -\frac{1}{L_i} \\
-\frac{1}{L_i} & \frac{1}{L_g} + \frac{2}{L_i} & -\frac{1}{L_i} \\
-\frac{1}{L_i} & -\frac{1}{L_i} & \frac{1}{L_g} + \frac{2}{L_i}
\end{bmatrix}
\begin{bmatrix}
V_A \\
V_B \\
V_C
\end{bmatrix}
= 
\begin{bmatrix}
I_A \\
I_B \\
I_C
\end{bmatrix}
\quad (D.106)
\]

where: $L_g$ is the effective network inductance to ground

$L_i$ is the effective network phase to phase inductance due to introduction of the neutral reactor.

In order for the networks to be equivalent, diagonal and off diagonal elements of the admittance matrices of equations D.105 and D.106 must be equal. Equating the entries accordingly
Appendix D. Effects of Introducing a Neutral (Grounding) Reactor

leads to:

\[ L_I = L_p \left[ 3 + \frac{L_p}{L_N} \right] \]
\[ L_g = 3L_N + L_p \] \hspace{1cm} (D.107)

Introduction of a neutral reactor then has the following effects on the reactor network:

- Effective inductance to ground is increased over that of the phase reactor by \(3L_N\).

- An effective inductance exceeding \(3L_P\) appears between phases of the network.

As outlined in chapter 3, the first effect results in larger suppression peaks during load side oscillation following current chopping. This results in larger TRV and hence a greater likelihood of reignition than when switching the solidly grounded network. The second effect results in the phases of the reactor network being strongly inductively coupled. Although this intentionally results in more reliable single pole clearing during associated line faults, increased phase interactions will occur during current chopping, load side oscillation and reignitions when switching the reactor.
Appendix E

Effects of Introducing an Opening Resistor

In chapter 4, the use of opening resistors was briefly summarized as a means of reducing the severity of load side oscillations during reactor network switching with air blast circuit breakers. An opening resistor reduces load side oscillation severity through two mechanisms;

- As main contacts open, the opening resistor causes a lowering and phase shifting of the reactor voltage over that if the resistor were absent. Reactor voltage at the instant of current chopping, \( V_{ch} \) is thus reduced and the load side oscillation amplitude as given by equation 2.12 smaller. Reactor voltage being phase advanced with respect to the source voltage reduces the RRRV as the resistor switch interrupts.

- The opening resistor interacts with the arc to enhance stability and reduce current chopping levels over those expected without a resistor.

These are discussed in more detail in the following sections.

E.1 Reduction and Phase Shifting of Network Voltage

Interruption of the single phase reactor considered in chapter 2 with an opening resistor equipped breaker may be studied with the network of figure E.92. Since the network capacitance \( C \) is small, little error results in assuming the breaker current is just reactor current. That is \( i_b \approx i_L \), and using operational notation \( p = \frac{d}{dt} \);

\[
V_s \sin(\omega_s t - \phi) = i_b R_b + V(t)
\]

\[
V(t) = i_b \left[ R_1 + \frac{pLR_2}{R_2 + pL} \right]
\]
where: \( \omega_s \) is power system frequency

\( \phi \) represents source voltage angle at the moment of main contact separation \((t = 0)\)

After substitution algebraic manipulation, the following differential equation for breaker current results describing behavior as current commutates from the main contact to the opening resistor:

\[
\frac{di_b}{dt} + i_b \frac{(R_b + R_1)R_2}{(R_b + R_1 + R_2)} = \frac{V_s}{(R_b + R_1 + R_2)L} [R_2 \sin(\omega_s t - \phi) + \omega_s L \cos(\omega_s t - \phi)] \quad (E.108)
\]

The reactor winding resistance \( R_1 \) is in practice much smaller than \( R_2 \) representing reactor losses. Further, breaker opening resistors are usually at least several hundred ohms often exceeding 10 k\( \Omega \) and it is acceptable to state \( R_1 \ll R_2 \) and \( R_1 \ll R_b \). Imposing this condition, and manipulating the right hand of equation E.108 yields:
Appendix E. Effects of Introducing an Opening Resistor

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Figure E.93: 5000 Ω Opening Resistor Interruption with 20 A Current Chopping

\[
\frac{dib}{dt} + i_b \frac{R_b R_2}{(R_b + R_2)} = \frac{V_s \sqrt{R_2^2 + (\omega_s L)^2}}{(R_b + R_2)L} \left[ \cos(\omega_s t - \gamma) \right]
\]  \hspace{1cm} (E.109)

\[
\gamma = \phi + \alpha
\]
\[
\alpha = \arctan \frac{R_2}{\omega_s L}
\]

Assuming a homogeneous solution of the form \(i_{bh} = K_1 e^{-\lambda t}\) gives:

\[
\lambda = \frac{R_b R_2}{(R_b + R_2)L}
\]

Selecting a particular solution including both sine and cosine terms:

\[
i_{bp} = K_2 \cos(\omega_s t - \gamma) + K_3 \sin(\omega_s t - \gamma)
\]

and substituting into equation E.109 produces:

\[
K_2 = \frac{\sqrt{R_2^2 + (\omega_s L)^2} R_b R_2}{(R_b + R_2)^2(\omega_s L)^2 + (R_b R_2)^2}
\]
Appendix E. Effects of Introducing an Opening Resistor

Figure E.94: Breaker TRV for 20 Current Chopping Interruptions with Various Opening Resistors

\[ K_3 = \frac{\sqrt{R_b^2 + (\omega_s L)^2 (R_b + R_2)\omega_s L}}{(R_b + R_2)^2(\omega_s L)^2 + (R_b R_2)^2} \]

The sinusoidal terms may then be combined to simplify the particular solution to give a final solution of the form:

\[ i_b = K_1 e^{-\lambda t} + V_s \left[ \frac{R_b^2 + (\omega_s L)^2}{(R_b + R_2)^2(\omega_s L)^2 + (R_b R_2)^2} \right]^{\frac{1}{2}} \cos(\omega_s t - \gamma - \delta) \]  

(E.110)

\[ \delta = \arctan \left[ \frac{(R_b + R_2)\omega_s L}{R_b R_2} \right] \]

Initial conditions are required if \( K_1 \) is to be determined. The initial steady state breaker current prior to separation of the main contact is simply:
Appendix E. Effects of Introducing an Opening Resistor

\[ i_b \simeq \frac{V_s}{\sqrt{R_1^2 + (\omega_s L)^2}} \sin(\omega_s t - \phi - \theta) \]  \hspace{1cm} (E.111)

\[ \theta = \arctan \frac{\omega_s L}{R_1} \]

Since \( \phi \) represents the source voltage phase angle at the instant of main contact commutation \((t = 0)\), the initial breaker current is:

\[ i_b(0) = -\frac{V_s}{\sqrt{R_1^2 + (\omega_s L)^2}} \sin(\phi + \theta) \]

Imposing the initial condition on equation E.110 yields the breaker current following main contact separation as:

\[ i_b(t) = [i_b(0) - V_s K \cos(\gamma + \delta)] e^{-\lambda t} + V_s K \cos(\omega_s t - \gamma - \delta) \]  \hspace{1cm} (E.112)

\[ K = \left[ \frac{R_b^2 + (\omega_s L)^2}{(R_b + R_2)^2(\omega_s L)^2 + (R_b R_2)^2} \right]^\frac{1}{2} \]

The reactor voltage may then simply be expressed as:

\[ V(t) = V_s \sin(\omega_s t - \phi) - i_b(t) R_b \]  \hspace{1cm} (E.113)

The nature of the transient portion of these solutions is clearly dependent on the point on wave at which the main contacts are opened. In fact since \( \gamma = \phi + \alpha \), it is clear from equation E.112 that if the point on wave angle \( \phi \) were chosen correctly, the transient term could vanish. In practice, the point on wave of breaker trip command application is not controlled under normal operating conditions and various degrees of decaying offset will appear in the breaker current waveform as the main contacts open. As the opening cycle of the breaker continues, the resistor switch will open and eventually current chop. The time between opening of the main contacts and resistor switch is called insertion time, and varies with breaker designs. A 500 kV air blast, opening resistor switch equipped breaker, 5CB25 discussed in chapter 5, has an
insertion time of 20 ms. For the reactor network tested, this was not enough to allow complete decay of the transient current offset following current commutation to the resistor switch.

In chapter 2, the load side oscillation following current chopping on interruption of the network of figure E.92 was shown to be given by equation 2.12:

\[ V(t) = V_m e^{-\beta_L t} \cos(\omega_d t - \psi) \]

\[ V_m = \sqrt{V_{ch}^2 + \left(\frac{\beta_L V_{ch} - i_{ch}}{\omega_d^2}\right)^2} \]

\[ \beta_L = \frac{1}{2} \frac{R_1 R_2}{(R_1 + R_2)L} + \frac{1}{C(R_1 + R_2)} \]

\[ \omega_d = \sqrt{\frac{R_2}{(R_1 + R_2)LC}} \]

\[ \psi = \arctan\left(\frac{1}{\omega_d} \left[\beta - \frac{i_{ch}}{V_{ch}C}\right]\right) \]

\[ i_{ch} = \text{current chopped at } t = 0 \]

\[ V_{ch} = \text{load voltage at instant of chopping} \]

The transient following current chopping by the resistor switch may be predicted by the same equation but the initial voltage \( V_{ch} \) at the instant of chopping \( V_{ch} \) and \( i_{ch} \) are not the same because of the presence of the opening resistor. These initial values must instead be determined as follows:

- A chopping current level of interest \( i_{ch} \) is chosen.

- Equation E.112 is then solved for the chopping time \( t_{ch} \) corresponding to \( i_{ch} \).

- \( V_{ch} \) is then evaluated by substitution of \( i_{ch} \) and \( t_{ch} \) into equation E.113.

Equation 2.12 may then be applied directly with the simple substitution of \( t' \) for \( t \), where \( t' = t - t_{ch} \) to account for the fact that resistor switch current chopping occurs at \( t = t_{ch} \) as
opposed to $t = 0$ where main contacts separated. The presence of the opening resistor clearly reduces the initial voltage condition $V_{ch}$ at current chopping. As will be demonstrated with an example, this is due to phase shift between the system and reactor voltages caused by the opening resistor.

By application of the above solutions to a specific network, the influence of the opening resistor from main contact commutation through to load side oscillation on resistor switch current chopping, may be studied. A computer program was written to calculate the above solutions for the network and test parameters of figure E.92 to study the influence of an opening resistor on switching transients and breaker TRV.

To allow valid comparison, a fixed chopping current current of 20 amps and main contact commutation angle $\phi$ of 90° were chosen and interruption transients calculated with opening resistances of 2000, and 5000 $\Omega$ for contrast to interruption with no resistor. The results for the 5000 $\Omega$ resistor are shown in figure E.93. Current offset due to the exponential term in equation E.112 is clearly visible. Phase shifting and reduction of reactor voltage relative to source voltage, is also pronounced. The important intended effect is the successive reduction in TRV and RRRV across the breaker contacts with increasing opening resistance as demonstrated in figure E.94. It is clear from these results that in order to achieve significant TRV and RRRV reduction, opening resistance must be reasonably large.

Note from figures E.93 and E.94, that even though the suppression peak voltage is reduced, TRV at the suppression peak is actually larger with an opening resistor. This is because reactor voltage just prior to chopping is phase advanced with respect to the source voltage. The increased risk of suppression peak reignition is a small drawback when traded off against the advantages of reduced current chopping overvoltages, RRRV and maximum TRV.

### E.2 Reduced Chopping Levels Through Increased Stability

Another important effect of introducing an opening resistor may be demonstrated by considering its effect on arc stability. Load and supply inductances are so large that to a small perturbing
SOURCE AND REACTOR INDUCTANCES ARE LARGE AND APPEAR AS OPEN CIRCUITS TO THE PERTURBING CURRENT STEP.

Figure E.95: Effects of an Opening Resistor on Arc Stability

step in the arc current they will appear as open circuits. Arc stability may hence be examined using the parallel arc equivalent in the network of figure E.95 where the source \( i \) represents a small perturbing current step.

By application of KCL using operational notation:

\[
e(t) \left[ \frac{1}{R_{so}} + \frac{1}{pL + R_i} + \frac{pC}{1 + pR_b C} \right] = i
\]

Noting that \( e(t) = \left[ \frac{pR_b C + 1}{pC} \right] i_a \) and \( \frac{di}{dt} = 0 \), the above expression may be manipulated to yield:

\[
i_a \left[ p^2 + p \left( \frac{1}{C(R_b + R_{so})} + \frac{1}{L} \left( R_i + \frac{R_{so} R_b}{R_b + R_{so}} \right) \right) + \frac{R_i + R_{so}}{LC(R_b + R_{so})} \right] = 0 \quad (E.114)
\]

The threshold of stability for solutions of this differential equation occurs where the damping term becomes negative. That is:

\[
\frac{1}{C(R_b + R_{so})} + \frac{1}{L} \left( R_i + \frac{R_{so} R_b}{R_b + R_{so}} \right) \leq 0
\]
Appendix E. Effects of Introducing an Opening Resistor

By substitution of the arc equivalence parameters and recalling $R_{ao} = \frac{E_a}{I_0} = \frac{\eta}{i_0 + \tau}$, the current at which instability will begin may be solved for:

$$I_o = \left[ \frac{\alpha \eta C}{\theta + R_b C} \right]^{\frac{1}{\alpha + 1}}$$

Instability current is reduced by the presence of $R_b$. As established in chapter 2, the instability current and chopping current are almost equal since chopping occurs so soon after the onset of instability. The chopping current may then be expressed as:

$$i_{ch} \approx \left[ \frac{\alpha \eta C}{\theta + R_b C} \right]^{\frac{1}{\alpha + 1}}$$ (E.115)

This may be compared directly to equation 2.10 giving chopping current for the special case $R_b = 0$. It is clear from equation E.115 that for identical arcs in identical cooling media (accordingly identical thermal time constant $\theta$) while interrupting networks with the same capacitance, chopping currents will be smaller with an opening resistor. It may be concluded that an opening resistor will reduce current chopping by enhancing arc stability during interruption. This effect will be most pronounced where the time constant $R_b C$ is significant in comparison to the arc thermal time constant $\theta$. Hence the largest reductions in chopping current will be realized with air or gas blast breakers whose cooling intensities tend to be large and associated thermal time constants accordingly small.
Bibliography


